

Edited by
Sergio T. Amancio-Filho
Lucian-Attila Blaga

JOINING OF POLYMER-METAL HYBRID STRUCTURES

Principles and Applications



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Preface

The selection and development of lightweight hybrid structures are essential approaches for reducing weight, fuel consumption, and CO₂ emissions in modern airplanes and cars. As a result, further increase in weight-to-strength performance of engineering structures has become tangible. Recent examples of applications for lightweight hybrid structures are found in transportation (e.g., aircraft and automotive), civil engineering (e.g., modern bridge and façade construction), and medical applications (e.g., implants and prostheses). In the last decade, the main driving force for innovation in lightweight hybrid structures has come from the aircraft and automotive industries. While the development of alternative clean energy is an important way of solving emission problems, the selection and development of lightweight materials and structures provide a short-to-medium-term alternative. The development of new lightweight alloys, such as Al, Mg, and Ti, as well as of advanced polymer-based composite materials, such as carbon-fiber-reinforced thermoplastics (CFRT) and glass-fiber-reinforced thermoplastics (GFRT), has changed the current paradigm in the structural design of lightweight constructions. Concomitantly, new joining technologies are being developed and studied for the new similar and dissimilar materials combinations.

Current commercial airplanes consist of over 50% fiber-reinforced composites combined to aluminum and titanium alloys (e.g., Airbus A350 and Boeing 787); their design relies on high safety factors to ensure structural damage tolerance and to compensate for the limited intrinsic toughness of carbon-fiber-reinforced composites. This generally leads to thicker and heavier joined parts, and the desired weight reduction goal by composite incorporation (about 20% lighter than aluminum) may not be fulfilled. Future aircraft concepts (e.g., Airbus A30X, the future substitute of the A320 airplane) will increasingly select materials based on their positive properties, such as toughness and high strength-to-weight ratio (i.e., specific strength), returning to a more conservative material selection design approach based on the combination of light metals (e.g., aluminum in the fuselage panels) and fiber-reinforced polymer structures (e.g., in heavier primary structures,

such as wings). In the automotive industry, new clean energies, such as electric- and hydrogen-powered engines, usually require the use of large batteries and storage tanks, increasing the final car weight. In this way, carbon- and glass-fiber-reinforced composite parts are progressively being selected in combination with lightweight alloys – for instance, aluminum and magnesium – to reduce weight without compromising passenger safety. Therefore, high performance, cost-effective, and environmentally friendly joining technologies are necessary to ensure the sustainable production of future damage tolerant and crash-resistant metal/polymer structures.

Joining of hybrid metal–polymer and polymer composite structures has been recently identified by the International Institute of Welding (IIW) as a hot topic to be addressed in the next two decades. Joining metals to polymers is very challenging, particularly due to low mutual solubility and physical–chemical incompatibility. Thus, metal–polymer hybrid joints present a sharp interface with bonding mechanisms dictating interface strength and influencing the global mechanical performance. Furthermore, metal–polymer hybrid joints are susceptible to the highly different materials’ responses to stress concentrations and creep. Reduction in strength related to aging or weathering (e.g., due to changes in temperature, humidity absorption, and exposure to fluids) of joined parts is also an issue. In addition, surface finishing and chemistry can strongly influence interface properties, directly affecting the structural mechanical performance owing to the formation of bonding defects. State-of-the-art mechanical fastening, welding, and adhesive bonding technologies are frequently inadequate for joining metal to polymers. Therefore, latest efforts by engineers and scientists have been concentrated in developing efficient and cost-effective new joining techniques and methodologies to overcome these limitations.

This book, which is the first dealing with the joining of polymer–metal hybrid structures, is mainly based on recent research in the area of advanced joining of metals to polymers and composites by adhesion forces, mechanical interlocking, and direct-assembly methods. The selected topics were based on Dr Amancio’s teaching experience at Hamburg University of Technology (Germany) and his over 15 years R&D experience in joining technology at the Solid-State Joining Processes Department (Helmholtz-Zentrum Geesthacht, Germany), as well as Dr Blaga’s experience with mechanical fastening processes. Valuable contribution has been provided by other distinguished research groups around the world, which makes this book unique and a reference for future developments in this area.

The book intends to introduce the concepts of joining technology for polymeric materials and polymer–metal hybrid structures by addressing current and new joining methods, focusing on joint engineering (performance, design, and modeling), and scientific (structural, physical, chemical, and microstructural) properties. This volume was conceived to be an introductory

text for engineers and engineering students, willing to update or extend their knowledge in joining technology.

The book was divided into four parts. In Part 1, the joining processes relying on adhesive forces are addressed. Chapter 1 briefly introduces the fundamental concepts and theories of adhesive bonding. The chapter focuses on the common surface preparation techniques, types of adhesives, joint manufacture (joint design fabrication), and analysis of bonded joints (experimental and modeling). Chapter 2 discusses the recent developments in adhesive bonding of polymer composites to lightweight alloys. An extensive literature review is presented on joint manufacturing and bonded joint properties. Special attention is also given to the manufacturing of bonded hybrid joints, the preparation of adherends, proper application of adhesives, as well as adequate mechanical tests to evaluate and analytically/numerically predict joint strength. In Chapter 3, the IIW's Henry Granjon prize-winning-technology Friction Spot Joining for metal–composite overlap joints is introduced. Process principles, microstructure, and mechanical performance are addressed for this alternative technology to adhesive bonding. Emphasis is given focusing on the fundamental understanding of the correlations between process microstructure and properties for future structural applications. Chapter 4 introduces a new welding-bonding technology – the Induction Welding – a new application of induction heating for joining lap joints between thermoplastic composites and metal alloys. Focus is set on the description of joining equipment and principles for two different process variants, by detailing how inductive heating influences bonding mechanisms and changes joint mechanical performance. Chapter 5 briefly introduces the direct bonding of metal and thermoplastics by laser heating. The basic principles of this new process, as well as its joint properties, are presented along with the description of differences in joint mechanical properties and microstructure triggered when heating the metal or the polymer partner.

Part 2 presents the category of joining technologies relying on mechanical interlocking. This part begins with Chapter 6 providing a comprehensive description of the main theories and process features in mechanical fastening. The authors of the chapter provide a broad discussion on the main fasteners and techniques used in similar and hybrid joints in aircraft structures, one of the major players applying mechanical fasteners. The main fastener types and base materials, joint design, and mechanical behavior are addressed. Chapter 7 deals with the challenges of mechanically fastened polymer composite and metal–polymer composite structures. New analysis models for bolted joints are emphasized in this chapter. A semianalytical method for joint design (Finite Fracture Mechanics method) and new developments in numerical methods are discussed. Chapter 8 introduces an innovative friction-based riveting technology. The prize-winning Friction Riveting technology – a potential substitute to bolts and rivets – is addressed for polymer and polymer composite–metal joints. A detailed description of the process parameters, heat

generation, microstructural development, and joint mechanical performance (quasistatic and cyclic) is presented. In Chapter 9, the staking techniques for polymer metal hybrid parts are thoroughly discussed for the first time in the literature. Special attention is given to the manufacturing procedures and types of staking process, joint design, and mechanical performance. New advanced staking techniques, such as the friction-based ICJ and Thermoclinching, are discussed to illustrate the potential of staking in future structural parts.

In Part 3, two new technologies are introduced for direct assembly of high-performance polymer and composite–metal hybrid parts. Originated in the automotive industry, the Injection over Molding of polymer–metal structures is presented in Chapter 10. The chapter addresses the essential steps for designing, simulating, and fabricating the hybrid structures, whose bonding mechanisms depend on combined mechanical interlocking and adhesion forces. Examples are provided for injection molding of neat or short-fiber-reinforced polymers over perforated and surface-treated metallic substrates. Chapter 11 brings in the new Ultrasonic Joining (U-Joining) technique. This new joining technology makes use of ultrasonic energy to join injection-molded metallic parts with 3D-surface reinforcements (e.g., pins or columns) with fiber-reinforced composites. Pins or columns act as through-the-thickness reinforcements in the composite part, increasing the hybrid joint's out-of-the-plane strength.

Finally in Part 4, a preview is provided on the available design of experiments (DoE) and statistical tools for joining process optimization and evaluation of the process–microstructure–mechanical performance correlations in polymer–metal structures. Chapter 12 presents the basic theory of factorial design, while Chapter 13 addresses the fundamentals of Taguchi design and response surface methodology. Case studies are discussed for friction-based joining processes' optimization and evaluation.

The editors are indebted to all who directly or indirectly contributed to this book (especially several colleagues and alumni from the Polymer–Metal Joining Group at HZG) and particularly to the following authors, who contributed the chapters composing the critical mass of this book:

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After some 5 years of hard work, we are delighted to announce the completion of this endeavor!

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Part I

Joining Processes Based on Adhesion Forces

1

Principles of Adhesive Bonding

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1.1 Introduction

Nowadays, new products consist of a combination of special new materials, which need to be joined according to their specific characteristics. Adhesives can be used to join metals, polymers, ceramics, cork, rubber, and combinations of any of these materials. Adhesive bonding has found applications in various areas, from high technology industries such as aeronautics, aerospace, electronics, and automotive to traditional industries such as construction, sports, and packaging.

Adhesively bonded joints are an increasing alternative to mechanical joints in engineering applications and provide many advantages over conventional mechanical fasteners. Among these advantages are lower structural weight, lower fabrication cost, and improved damage tolerance. However, there are still important issues that need to be solved before this technique can be totally trusted and employed at large scale by the industry. The most important are the joint strength in severe environments and the durability. On the other hand, a lack of suitable material models and failure criteria has resulted in a tendency to “overdesign” structures. Safety considerations often require that adhesively bonded structures, particularly those employed in primary load-bearing applications, include mechanical fasteners (e.g., bolts) as an additional safety precaution. These practices result in heavier and more costly components. The development of reliable design and predictive methodologies can be expected to result in more efficient use of materials and adhesives. The fundamentals and practices for adhesive bonding are described in a large number of handbooks or textbooks, such as those of Adams *et al.* [1] and Petrie [2] and more recently that of da Silva *et al.* [3].

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In order to design structural joints in engineering structures, it is necessary to be able to analyze them. This means to determine stresses and strains under a given loading and to predict the probable points of failure. There are two basic mathematical approaches for the analyses of adhesively bonded joints: closed-form analysis (analytical methods) and numerical methods (i.e., finite element analyses) [4].

Before an adhesive can be specified for an application, screening tests should be conducted in order to compare and evaluate the various adhesion parameters. Properties of adhesives can vary greatly, and an appropriate selection is essential for a proper joint design [5]. There are a wide range of test methods and associated test specimens which are used to evaluate the performance of adhesives and adhesive joints [6, 7]. The approaches used for determining the properties of adhesives are the measure of the properties of bulk adhesive specimens and the use of specially designed joint geometries with a thin bondline (often referred to as “*in situ*” testing). The measured parameters are the load and strain needed to induce failure. The test geometry should provide a pure state of stress, uniformly distributed across the contact surface and through the bondline, free of stress concentrations, and the surface treatment should be sufficient to ensure cohesive failure in the adhesive layer. Currently, there are many ASTM and ISO standards, which have been written to analyze and experimentally verify adhesive properties. These standards provide a basis for testing.

This chapter provides an understanding of adhesive bonding principles. The advantages and disadvantages of using the adhesive bonding compared to other methods of joining are briefly explained. The effects of surface preparation and the environmental factors on the joint behavior are briefly described. Adhesive properties along with the main test methods and associated test specimens that are used to evaluate the performance of adhesives and adhesive joints are presented. Some basic principles for adhesively bonded joint manufacture are also addressed. Failure modes and the main analytical and numerical methods of stress analysis required before failure prediction are discussed. Finally, it ends with recent developments and conclusions.

1.2 General Basics

Adhesive bonding is a material joining process in which an adhesive, placed between the adherend surfaces, solidifies to produce an adhesive bond. An adhesively bonded joint is generally formed by adherends, adhesive, primers (if necessary), and the interphase regions (see Figure 1.1). The substrate is the material to be bonded, which after bonding is often referred to as an adherend. The area between the adhesive and the adherend is referred to as the interphase region, which is a thin region near the point of adhesive–adherend contact.

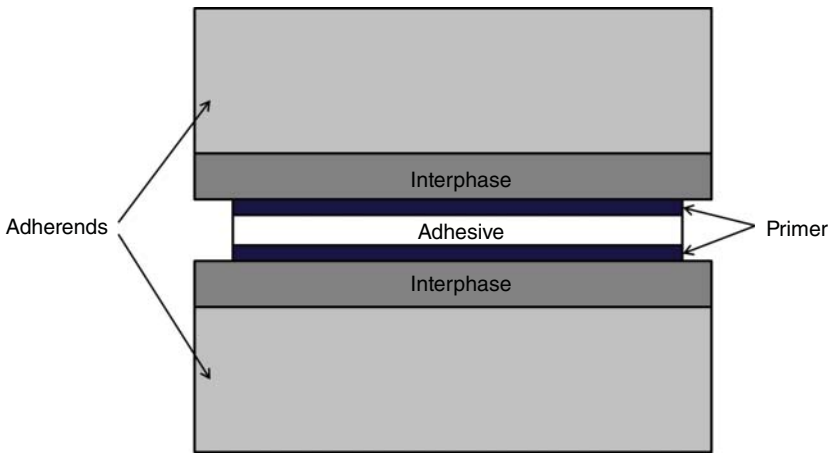


Figure 1.1 Components of an adhesively bonded joint.

The interphase region has different chemical and physical characteristics than either the bulk adhesive or the adherend. The properties and the quality of an adhesively bonded joint depend on the nature of the interphase region.

The interface is contained within the interphase and is the plane of contact between the surfaces of one material to another. Sometimes, a primer is applied to a surface prior to the application of an adhesive in order to improve the performance of the adhesive bond or to protect the surface until the adhesive can be applied.

A structural adhesive is an adhesive with high shear strength (usually superior to 5 MPa) and good environmental resistance. Examples of structural adhesives are epoxy, acrylic, and urethane systems. Nonstructural adhesives are adhesives with much lower strength and durability. They are generally used for temporary fastening or to bond weak substrates. Examples of nonstructural adhesives are pressure-sensitive films, elastomers, and sealants. Different specifications and test methods apply to structural and nonstructural adhesives, and most often, they are designed to perform different functions. In this chapter, only structural adhesives are considered.

1.3 Advantages and Disadvantages of Adhesive Bonding

Adhesive bonding offers many advantages compared to other joining process. However, in designing and producing modern structures, the decisions whether to use adhesives, mechanical fasteners, some type of welding, or combination of these methods depends on various factors. Nevertheless, some processes

will have distinct advantages and disadvantages in specific applications. For example, brittle or damage-prone materials are difficult to drill in order to use mechanical fasteners. In addition, in case of fiber-reinforced composites, the traditional fasteners usually result in the cutting of fibers and hence the introduction of stress concentrations, both of which reduce structural integrity. By contrast, bonded joints are more continuous and have potential advantages of strength-to-weight ratio, design flexibility, and ease of fabrication.

Adhesive bonding is particularly well suited for joining of large surfaces of different materials, such as in the construction of metal–composite sandwich structures.

Another important advantage of adhesive bonding is the fact that, along with mechanical joining (i.e., fastening), it is the only joining technique that does not change the microstructure of the materials being joined. Additionally, it usually causes little or no chemical alteration of the materials it joins. This makes possible that dissimilar (as well as similar) materials in virtually any combination can be joined. Combination of materials such as metals to polymers, metals to ceramics, ceramics to polymers, and ceramics or polymers to reinforced metals, and so on can be joined. However, there are distinct differences between joining similar and dissimilar materials using adhesives that one must be aware of in order to maximize the chances of success. For example, differences in flexibility or thermal expansion between adherends can introduce internal stresses into the bondline. Somewhat, stresses can be minimized through joint design, but the performance of the bond is still affected by them.

There are often significant advantages in using fusion techniques (e.g., welding), in load-bearing structure where the adherends are similar or compatible, but the use of adhesive bonding provides other benefits. Besides avoiding overheating of the material and associated distortion effects (especially with thin-walled parts), it offers the possibility of integration of novel or complex joint designs.

As the adhesive bonding isolates one adherend from another through an intermediate adhesive, it usually prevents galvanic corrosion between dissimilar adherends better than mechanical joining processes. However, the adhesive selected must be compatible with each adherend. The viscoelastic polymeric adhesives can also offer damping capabilities.

On the other hand, one important disadvantage of adhesive bonding is the relatively poor temperature resistance as compared to inorganic materials such as metal or glass. In addition, most probably all adhesives are adversely affected by moisture, especially in a stressed condition. As the long-term behavior of adhesives is not yet completely known, it has not been possible to develop reliable mathematical models for durability of adhesive joints. However, the empirical values obtained with adhesive joints have meanwhile made it possible to conceive safe and sufficiently reliable bonded structures. The main advantages and disadvantages of adhesive bonding process are summarized in Table 1.1.

Table 1.1 Advantages and disadvantages of adhesive bonding.

Advantages	Disadvantages
A more regular stress distribution and reduced stress concentration	Sensitivity to peel or cleavage pure tension or shear
Suitability to very thin as well as thick adherends	Requires specific processing (surface pretreatment, curing, process control)
High load-carrying capacity possible due to large (surface) area bonding	Sometimes very limited working times
Properties of the adherends are preserved	Curing times can be long
Suitability for joining similar or dissimilar materials (i.e., metals, ceramics, glass, plastics, wood, etc.)	Nondestructive quality tests only partially possible
Seals against many environments	Thermal load capacity of the adhesives is limited compared with other materials (e.g., metals)
Insulates against electricity or heat	Sensitivity to the environment
Minimizes or prevents galvanic corrosion between dissimilar materials	Sensitivity to attack by some solvents
Damps vibrations and shock loads	Distinct adhesive choice in relation to application (various range of adhesives, various application possibilities)
Better fatigue resistance and damage tolerance	Requires specific clamping devices to fix the joint during cure
Attractive strength-to-weight ratio	Difficult to dismount the joint in a nondestructive way
Joints with smooth, large surfaces can be produced	
Can be faster and less expensive than mechanical fastening or welding	

1.4 Effect of Surface Preparation and the Environmental Factors

The surfaces play an important role in the bonding process, and surface preparation is, perhaps, the most important process step governing the quality of an adhesive bond joint [8]. Correct surface preparation is essential for good joint strength and maintaining long-term structural integrity of bonded joints. It is widely recognized that the ideal surface to bond to should be: clean, dry, dust free, smooth, wettable (high surface energy), and polar.

Most structural adhesives work as a result of the formation of chemical bonds (mainly covalent, but some ionic and static attractive bonds may also be

present) between the adherend surface atoms and the compounds constituting the adhesive [9]. These chemical links are the load transfer mechanism between the adherends. Most adhesive bond failures can be attributed to poor processes during fabrication, with lack of quality surface preparation being the most significant deficiency [10].

Surface preparation must be tailored to the adherend and possibly will differ for various types of materials. For example, the surface preparation of metals prior to bonding is important due to the oxidization buildup that occurs with metals. This is especially important with metals, such as aluminum and titanium. On the other hand, as plastic surfaces are very smooth, show bad wetting, and have low surface energy, the surface treatment is also very important.

For metals, traditional methods of surface treatment – such as grit blasting, mechanical abrasion, and acid etching – have been used with good success. In contrast, for plastic materials, sometimes achieving adhesion is quite challenging because of the low surface energy. Nevertheless, there are several methods of increasing the surface energy and polarity of plastics, including: wet chemical treatments (which are often harsh and environmentally damaging), high-temperature flame torch treatments, corona treatment, and finally, plasma surface activation.

In summary, surface treatments prior to the application of adhesives are recommended in order to achieve maximum mechanical strength. By increasing surface tension, increasing surface roughness, and changing surface chemistry, a more intimate bond can be formed, which allows for increase in strength and durability.

Adhesively bonded joints may be exposed to various environmental conditions during their service life. As it has been shown, the performance of adhesive systems can be considerably deteriorated when exposed to harsh environments. The environmental factors must be considered a critical factor in determining the long-term durability of adhesively bonded joints and need to be carefully identified and related to the type of service the material will undergo.

The main environmental factors in climatic exposure are temperature and humidity. The prolonged exposure or even short-term exposure to elevated temperatures will often produce irreversible chemical and physical changes within adhesives. As the temperature increases, the bond strength decreases [11, 12]. One example can be seen in Figure 1.2, where average lap shear strength of an epoxy adhesive as a function of temperature is shown. In addition, the moisture absorbed in a polymeric material can lead to a wide range of effects, both reversible and irreversible, including plasticization, swelling, and degradation. At temperatures below the glass transition temperature T_g , polymer property reduction is reversible upon dehydration, whereas above T_g , the properties are permanently altered.

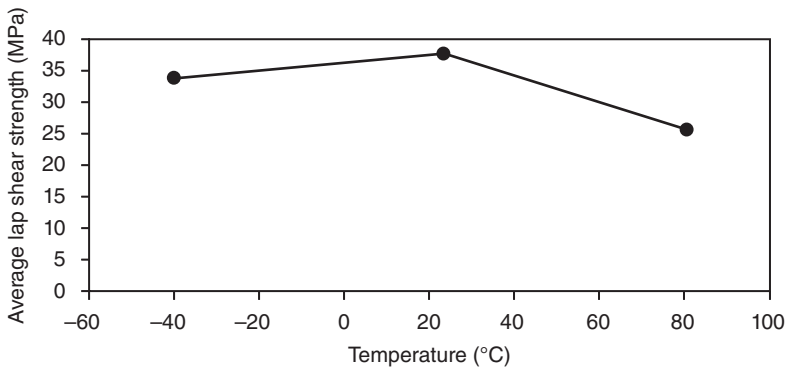


Figure 1.2 Average lap shear strength of AV118 epoxy adhesive as a function of temperature.

The presence of moisture in adhesive joints may weaken not only the physical and chemical properties of the adhesive itself but also the interface between the adhesive and the substrate. For example, in the case of adhesively bonded joints involving metals, it is now well established that loss of joint strength can be minimized by selection of a suitable pretreatment. However, in the case of composite joints exposed to humid environments, the mechanisms of degradation are quite different compared to adhesively bonded metal joints. Unlike metals, the work of adhesion for composite to epoxy joints remains positive in the presence of water [13] and thus decreases the likelihood of interfacial failure on ageing. In addition, the composite adherend will absorb water, which can affect the kinetics of water absorption into the adhesive. Temperature and moisture can also influence the mechanical properties of the composite matrix material and the interface between fibers and matrix may be weakened in the presence of moisture [14].

The degradation of adhesives due to environmental factors can be assessed by constitutive and fracture tests. Constitutive tests on adhesives show a significant lowering of adhesive strength and stiffness, often accompanied by an increase in ductility with increasing moisture content [15].

Various studies were conducted on the effects of various environments on some adhesive properties, but it is still necessary to address the performance of specific adherend–adhesive combinations and to combine environmental, fatigue, and fracture studies of bonded systems. For example, it is known that moisture absorption results in varying degrees of plasticization, strength loss, and increased ductility of some epoxy adhesives. However, the effect of moisture on the fatigue and fracture properties of bonded joints employing these adhesives is still not fully understood. In addition, since adhesive joints are systems comprised of adherends, adhesives, and interphase regions, the

performance of each of these components may strongly affect the performance of the joint. Thus, general knowledge of the behavior of adhesives exposed to various environments must be supplemented by knowledge of the behavior of specific bonded systems. In this way, it is necessary to conduct experiments on joints that are subjected to different mechanical loadings and humid environments to investigate the failure mechanisms and further to develop numerical models to accurately predict the experimentally observed failure behavior. One approach, which has been extensively used to predict the durability of adhesively bonded joints exposed to humid environments, is the cohesive zone model (CZM) modeling [16–18]. In addition, the influence of environmental aspects has specific relevance for multimaterial (hybrid) structures, where components with different reactions to the same environmental conditions can significantly alter the behavior of the structure as a whole.

1.5 Adhesive Properties

Adhesives used in structural applications include: epoxies (having high strength and temperature resistance), cyanoacrylates (fast bonding capability to plastic and rubber but poor resistance to moisture and temperature), anaerobics (suitable for bonding cylindrical shapes), acrylics (versatile adhesives with capabilities of fast curing and tolerance to dirtier and less prepared surfaces), polyurethanes (good flexibility at low temperatures and resistance to fatigue), and high-temperature adhesives (phenolics, polyimides, and bismaleimides). Table 1.2 presents several typical properties for different types of adhesives.

As it is well known, to achieve a good bond, first it is necessary to start with a good adhesive. The adhesive selection process is difficult as there is no universal adhesive that will fulfill every application and the selection of the proper adhesive is often complicated by the wide variety of available options. However, adhesive selection includes many factors such as: type and nature of substrates to be bonded, cure and adhesive application method, and the expected environments and stresses that the joint will face in service. In addition, the cost of the adhesive may sometimes be an important factor of adhesive selection in a particular production situation.

Before an adhesive can be specified for an application, screening tests should be conducted in order to compare and evaluate the various adhesion parameters. Properties of adhesives can vary greatly, and an appropriate selection is essential for a proper joint design. Some typical mechanical properties' values for different types of adhesives are presented in Table 1.3.

There are a wide range of test methods and associated test specimens that are used to evaluate the performance of adhesives and adhesive joints. The approaches used for determining the properties of adhesives are the measure of the properties of bulk adhesive specimens and the use of specially designed

Table 1.2 Typical properties of adhesives.

	Comments	Service temperature (°C)	Cure
Epoxy	High strength and temperature resistance, relatively low cure temperatures, easy to use, low cost	−40 to 100 (180 ^a)	One-part epoxies cure with temperature Two-part epoxies cure at room temperature (cure can be accelerated with temperature)
Cyanoacrylates	Fast bonding capability to plastic and rubber but poor resistance to moisture and temperature	−30–80	Fast cure (second or minutes) upon exposure to moisture at room temperature
Anaerobics	Designed for fastening and sealing applications in which a tight seal must be formed without light, heat, or oxygen, suitable for bonding cylindrical shapes	−55 to 150	Cure in the absence of air or oxygen at room temperature.
Acrylics	Versatile adhesives with capabilities of fast curing and tolerate dirtier and less prepared surfaces	−40 to 120	Cure through a free radical mechanism
Polyurethanes	Good flexibility at low temperatures and resistant to fatigue, impact resistance, and durability	−200 to 80	Room temperature
Phenolics	Good strength retention for short periods of time, limited resistance to thermal shocks	−40 to 175 (260 ^b)	Cure with temperature and high pressure
Polyimides	Thermal stability, dependent on a number of factors, difficult processability	−40 to 250 (300 ^b)	Cure with temperature and high pressure
Bismaleimide	Very rigid, low peel properties	−50 to 200 (230 ^b)	Cure with temperature and high pressure

a) With different filler materials.

b) Intermittent.

joint geometries with a thin bondline (often referred to as “*in situ*” testing). The measured parameters are the load and strain needed to create failure. The test geometry should provide a pure state of stress, uniformly distributed across the contact surface and through the bondline, free of stress concentrations, and the surface treatment should be sufficient to ensure cohesive failure in the

Table 1.3 Typical adhesives’ mechanical properties’ values.

Adhesive	Type	Shear modulus (MPa)	Shear strength (MPa)	Shear strain (%)
XN1244 [19]	Epoxy	2150	32	8.05
Redux 326 [20]	Bismaleimide	1180	36.5	3.63
DP805 [21]	Acrylic	159	8.4	180
Sikaflex552 [11]	Polyurethane	1.30	2.39	330
RTV106 [12]	Silicone	0.55	1.97	408

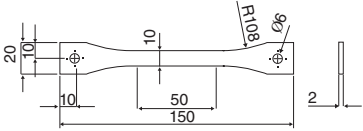
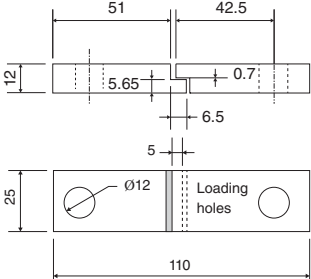
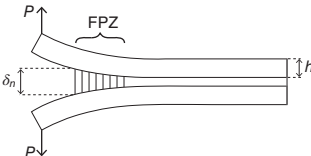
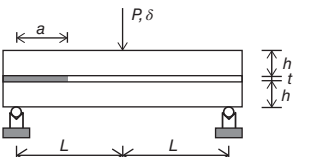
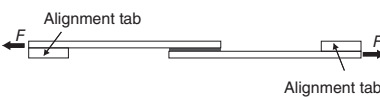
adhesive layer. Currently, there are many ASTM and ISO standards, which have been written to analyze and experimentally verify adhesive properties. These standards provide a basis for testing. Commonly used test methods that have been developed and used to obtain properties of the adhesives include: tensile tests, shear tests, compression, peel, durability tests, and dynamic tests.

For example, if a continuum mechanics approach is used for the adhesive joint design, the availability of the stress–strain curve of the adhesive is sufficient (the bulk tensile test or the thick-adherend shear test TAST test is used), while for fracture-mechanics-based design, mode I and mode II toughness is needed (double cantilever beam DCB and end-notched flexure ENF tests are used). However, for the more realistic and sophisticated methods such as progressive damage methods, damage laws of adhesives are necessary. The parameters that define the damage law are the fracture toughness and the maximum stress for each fracture mode. Nevertheless, the most widely used adhesive-bond test specimen is the single-lap tension test. The failure mode of the single-lap joint (SLJ) is rarely controlled by the shear strength of the adhesive but is largely the result of joint deflections and rotations and induced peel stresses. Because of the rotation at the overlap, data from single-overlap tension test specimen cannot be used to obtain adhesive shear design data but are often used for screening tests to compare several adhesive systems and the effects of the environment on the adhesive properties in the selection process of the adhesive. Table 1.4 summarizes some typical test methods used to evaluate the performance of adhesives and adhesive joints.

1.6 Joint Manufacture

Besides the selection of an appropriate adhesive, the performance of an adhesively bonded joint depends on the preparation of the adherends, mixing and application of the adhesive, joint assembly, and the curing process. A high percentage of failures can be attributed to poor joint manufacture or a lack of

Table 1.4 Summary of some typical tests methods used to obtain the adhesive properties.

Property measured	Test method	Specimen geometry	Standard
Tensile properties	Tensile bulk tests		ASTM D 638 ISO 2818 ISO 572-2
Shear properties	Thick-adherend shear test (TAST)		ISO 11003-2
Mode I fracture toughness	Double cantilever beam (DCB)		ASTM D3433 ISO 2009
Mode II fracture toughness	End-notched flexure (ENF)		—
Lap shear strength	Single-lap joint (SLJ)		ASTMD1002 ISO 4587

understanding of the factors that influence the joint performance. This section examines the key issues relating to the manufacturing of adhesively bonded joints.

1.6.1 Preparation of the Adherends

The adherends should be manufactured and machined accurately in order to assure that the specimen dimensions meet the design specifications. They should be free of any surface damage. The surfaces must ensure uniform

contact through the entire bond area when the two surfaces are clamped or pressed together. The specimens are always prepared to remove dust, dirt, oil, oxides, or release agents in order to improve the interfacial bonding. The effect of surface preparation on adhesive joint performance was discussed in more detail in Section 1.4.

1.6.2 Adhesive Application

Correct mixing and application of the adhesive are important in producing reliable adhesive joints. Application of the adhesive depends on the adhesive form. The adhesives may be supplied as low-viscosity liquids or highly viscous pastes. For liquid adhesives, thin bondlines should be used to avoid spreading out of the adhesive. In the case of film adhesives, the adhesive application is also straightforward even though gaps between the film and the substrates can lead to voids in the adhesive.

Depending on how the adhesives are mixed and stored, they may contain air and other gases. One-part adhesives may be stirred in vacuum, and this can remove most of the entrapped air. However, the process is not easy nor cheap. Two-part adhesives also contain trapped air, and the separate components can also be stirred in vacuum to release all or most of the air. But two-part adhesives need to be mixed just before use, and special care should be taken to avoid introducing air, and hence voids, in the cured adhesive. Recent sophisticated machines, where the mixing is made at high speed under vacuum, can ensure that the adhesive is relatively void free.

Most adhesives are supplied in a tin, a cartridge, or a tube and are frequently stored in freezers, so that it is important to ensure that the adhesive has reached room temperature before application. Additionally, some paste adhesives with high viscosity may need to be heated above ambient temperature before application. Furthermore, film adhesives are usually supplied in designated thicknesses and partially cured, and it is essential that it is maintained in a sealed bag until it has reached ambient temperature.

1.6.3 Joint Assembly

During bonding process, a good alignment of the specimens should be assured. Small misalignment can affect the strength and fatigue performance of the joint. A bonding fixture (jig) is recommended to ensure correct bond length, accurate alignment, and uniform bondline thickness. There are various devices to fix the specimens in place, such as springs, clamps, weights, presses, vacuum bags, autoclaves, molds, among others. Before the adhesive application, release agent must be used to guarantee easy release of bonded components from the clamping fixture. The release agent should be well cured before the adhesive is applied to avoid interaction of the release agent with the adhesive. Teflon molds do not require a release agent, but cannot be used at high temperatures.

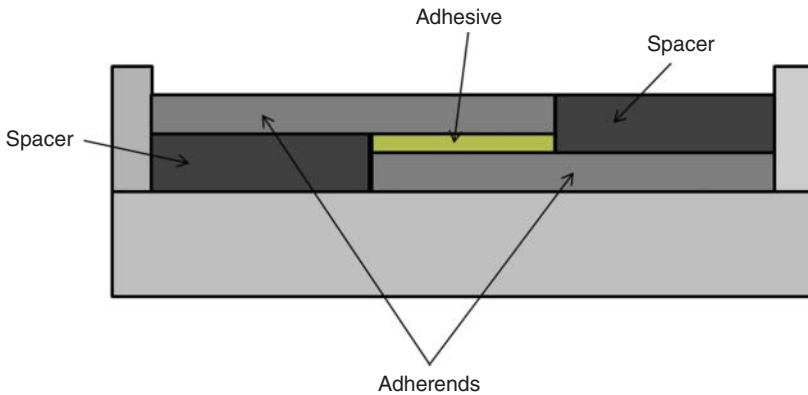


Figure 1.3 Bondline thickness control with spacers.

However, for high-temperature cures, molds made of the same material as the substrates to reduce the residual thermal stresses are recommended.

The bondline thickness needs to be accurately controlled (i.e., uniform adhesive layer thickness across the entire bonded area) in order to obtain consistent and reliable joint strength. There are various methods used to control the bondline thickness. One example can be seen in Figure 1.3 where spacers are placed at each end of the SLJ.

Another way to control bondline thickness is through the use of thin wire spacers (e.g., stainless steel) inserted between the adherends. The wire spacers should be located well within the bonded area away from the specimen edges and regions of high-stress concentrations (i.e., ends of joints). The bondline thickness can also be controlled by adding glass spheres of known sizes to the adhesive. Used in very small quantities, and dispersed uniformly throughout the adhesive, they will not alter the adhesive properties. Nevertheless, the method used to control bondline thickness must not introduce voids or promote void formation in the adhesive; otherwise, the joint performance will be compromised.

An optimum bond thickness will depend on the type of adhesive used. A too thin bondline may result in adhesive starvation and debonding, while a thick adhesive bondline may change the cure properties producing internal stresses, thereby reducing short and long-term performance.

Besides bondline thickness control, the adhesive fillet (called adhesive spew) is an important issue in joint assembly. The spew is the result of the adhesive squeezed out of the lap region at the moment of the joint manufacture. It was shown that significant increases in shear strength of lap joints, compared with square-ended bondlines, can be achieved through the formation of an adhesive spew at the overlap ends. The peak stress is dependent on the size and shape of the adhesive spew. Furthermore, shaping the spew can provide smoother

transition in joint geometry, significantly reducing the stress concentration [22]. However, these additional features may increase the costs of specimen manufacture.

1.6.4 Curing

It is well known that adhesives are supplied in many forms. They are one-part, which will be cured by heating to some temperature, or they may be in two parts, which must be mixed in agreed proportions and applied within a specified time, with or without the application of heat. One-part adhesives can also be supplied as a thin film, often containing some supporting medium such as a woven web. If there is no supporting web, these films can be melted at an elevated temperature and poured.

In the case that curing takes place at temperatures higher than the ambient room temperature, this should be monitored with a thermocouple placed as close as possible to the adhesive layer, in order to ensure that the temperature within the adhesive actually reaches the specified cure temperature. Temperatures in the adhesive should be monitored throughout the cure cycle. In case of adhesives that cure by a rapid exothermic reaction, care should be taken to prevent the temperature of the material from rising above the maximum value specified by the adhesive supplier. The curing time should be determined from the moment that the adhesive reaches the curing temperature.

Residual thermal stresses may be generated as a result of nonuniform (rapid) cooling, shrinkage, and thermal expansion coefficient mismatch between the adhesive and adherend. Additionally, in case of hybrid adhesive joints, curing at elevated temperatures leads to thermal residual stresses due to the thermal expansion mismatch between the adherends (i.e., polymer composite materials and metals). As the joint is cooled down from the cure temperature, residual stresses are frozen in the material. Consequently, the cooling rate should be slow to minimize residual stresses. In addition, the specimens should be allowed to reach ambient conditions before being removed from the bonding fixture. Finally, similar cure conditions of the adhesive layer in the adhesive joint to that of bulk adhesive specimens should be assured as different cure conditions can result in significant differences in material properties.

1.7 Joint Design

In order to design structural adhesive joints in engineering structures, it is necessary to be able to analyze them. This means to determine stresses and strains under a given loading and to predict the probable points of failure. This section describes the failure modes and the main approaches used for the analyses of bonded joints.

1.7.1 Failure Mode

One essential element of adhesive bonding technology is the adhesive joint failure mode. Failure modes are determined by the quality of the bond at each interface, specimen geometry, and loading. There are different modes of failure: adhesive failure, cohesive failure, or mixed failure. These modes are illustrated in Figure 1.4.

The cohesive failure is a failure of the adhesive itself. The adhesive failure is a failure of the joint at the adhesive–adherend interface. This is typically caused by inadequate surface preparation. The mixed mode failure occurs if the crack propagates at some spots in a cohesive and in others in an adhesive (interfacial) mode. Mixed fracture surfaces can be characterized by a certain percentage of adhesive and cohesive areas.

In addition, there is substrate failure, which is a failure that occurs when the adherend fails instead of the adhesive. In metals, this occurs when the adherend yields. In composites, the laminate typically fails by way of interlaminar failure, that is, the matrix fails in between plies. A substrate failure indicates that the adhesive is stronger than the adherend in the joint tested. This is a desirable situation in practical design, but not when determination of adhesive behavior is being studied.

The modes of failure must be characterized in order to gain a full understanding of the properties of the adhesive and the joint being investigated.

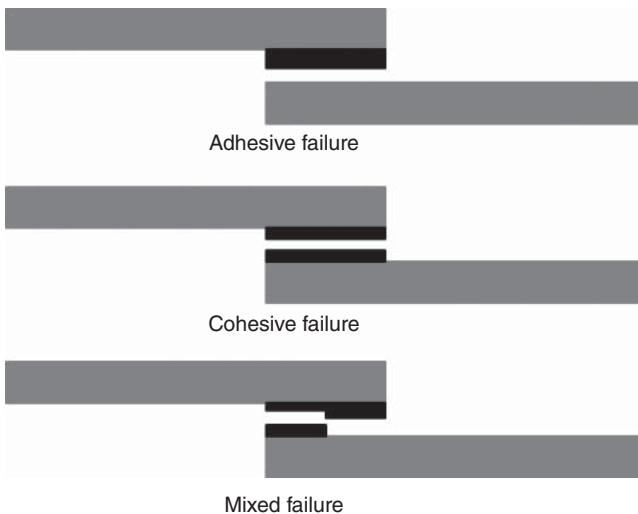


Figure 1.4 Failure modes.

1.7.2 Analysis of Adhesively Bonded Joints

There are two basic mathematical approaches for the analyses of adhesively bonded joints: closed-form analysis (analytical methods) and numerical methods (i.e., finite element analyses).

1.7.2.1 Analytical Methods

Adhesively bonded joints have been intensively investigated in the past years, and numerous analytical models have been proposed. A simple shear lag model for adhesively bonded lap joints with the assumption that the adherends are in tension and the adhesive is in shear only and both stresses are constant across the thickness was presented for the first time, in 1938, by Volkersen [23]. However, the Volkersen's solution does not reflect the effects of the adherend bending and shear deformations, which are potentially significant for composite adherends with a low shear and transverse modulus and strength. Goland and Reissner [24] extended this study by taking into consideration the effects of the adherend bending leading to peel stress in the adhesive layer, in addition to the shear stress. Hart-Smith [25] proposed a simple analytical model by considering that the adhesive layer has perfect elastoplastic behavior. He could show that the maximum load that an adhesively bonded joint can transfer depends on the shear deformation energy of the adhesive layer, regardless of the stress–strain curve. This approach allows a better prediction of the mechanical behavior of ductile adhesive layers. Hart-Smith analyzed in detail single-lap, double-lap, scarf, and stepped-lap joints in which the adherends were isotropic or anisotropic elastic, and the adhesive was modeled as elastic, elastic–plastic, or bielastic. Thermal effects were also included in the analysis, and it was shown that reduction in joint strength, due to thermal mismatch, increases as adherends thickness and/or stiffness increases. A review of most of his work can be found in Ref. [26], and the main results are summarized there.

After the early classical analytical models, some authors tried to obtain more general closed-form solutions such as by including nonidentical adherends (thickness and material properties) or composite adherends [27, 28]. However, as the model gets more general, the governing equations become increasingly complicated and require the use of a computer for solution.

Recently, da Silva *et al.* [29, 30] reviewed the analytical models available in the literature and discussed the conditions of applicability for each of them in order to facilitate their application. Most of the analytical models for adhesively bonded joints considered the adherends to have linear elastic behavior, some considered plastic behavior only for the adhesive layer, and some included variations in the distribution of adhesive stresses through the thickness of the adhesive. However, material nonlinearity due to plastic behavior is difficult to incorporate because the analysis becomes very complex in the mathematical formulation, the nonlinear material behavior being implemented only in

finite element models (FEM). Thus, for complex geometries and elaborate material models, a FEM is preferable. Nevertheless, for a fast and easy answer, a closed-form analysis is more appropriate.

1.7.2.2 Finite Element Method

The finite element method has the great advantage that the stresses in a body of almost any geometrical shape under load can be determined. Linear and nonlinear finite element analyses have been carried out on different types of adhesive joints, and the adhesive effective stresses and strains have been evaluated. As the adhesive layer is thin compared with the thickness of the adherends, a fine mesh in these areas is required so the number of degrees of freedom in a joint is rather high. A full finite element analysis should include the effects of bending, adherend shear, end effects, and nonlinear behavior of the adhesive and adherends.

Adhesively bonded joints contain inherent defects from their manufacture. From these defects, the crack initiation starts and leads later to failure of the assembly. The fracture strength of adhesive joints depends on a number of factors and their combinations, for example, adhesive type, cure cycle, adherend type, bondline thickness. However, a lack of reliable failure criteria still exists, limiting in this way a more widespread application of adhesively bonded joints in structural applications. An accurate strength prediction of adhesively bonded joints is essential to decrease the amount of expensive testing at the design stage.

Currently used approaches for predicting the strength of adhesively bonded joints are: the continuum mechanics approach (stress-based), fracture mechanics approach, and damage mechanics approach.

Continuum Mechanics Approach The continuum mechanics approach has been used by many researchers to predict the joint strength. The adhesive and adherends are modeled using continuum elements, assuming that the adhesive is perfectly bonded to the adherends. The assumption of a perfect bond means that the finite element analysis takes no account of the adhesion properties of the interface.

The criterion that is used varies between maximum values of stress [25], strain [31], or plastic energy density [32]. However, it is difficult to use maximum stress or strain criteria due to the bimaterial singularities inherent in a bonded joint (see Figure 1.5). As was shown by Bogey [33], a bimaterial wedge gives rise to a singular strain distribution. As there will always be a singularity at the ends of idealized bonded joints, the maximum strain for such a model will coincide with the value at the singularity and thus will vary greatly with mesh refinement.

Adams and Harris [32] and Zhao *et al.* [34, 35] showed that by introducing rounding, the singularity was removed although the level of peak stress (or plastic strain energy density for the nonlinear analyses) became dependent

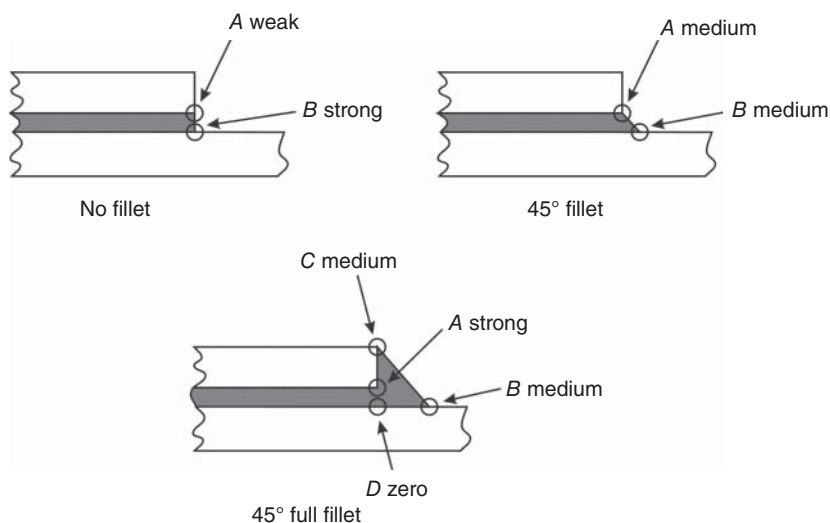


Figure 1.5 Strength of singularities in lap joints.

upon the degree of rounding. The problem now is shifted toward deciding how much rounding to use in order not to affect the joint strength. Knowledge of the exact end shape becomes then essential for accurate strength predictions. Consequently, the continuum mechanics approach often uses the same operators as mentioned earlier, but this time applied at a certain distance from the singularity. This method is known as stress or strain at a distance [36].

Fracture Mechanics Approach In the fracture mechanics approach, an energy parameter (toughness) is used as the failure criterion. In principle, it is possible to determine the values of toughness as a function of the relative amounts of normal and shear deformation acting at the crack tip (in the adhesive, along the interface, or through the adherend) and to use the concepts of mixed-mode fracture mechanics (failure occurs if local mixed-mode energy release rate exceeds a critical value) to predict the appropriate crack path to calculate the strength of the joint under different loading conditions [37, 38].

This is the case of linear elastic fracture mechanics, which relies on the existence of a crack and linear elasticity. However, well-fabricated joints may not have macroscopic defects large enough to be considered cracks. In addition, with laminated structures, it is easy to induce large-scale plasticity in the adherends. These factors fundamentally limit the use of linear elastic fracture mechanics approach in practical applications, and alternative approaches need to be sought.

Another method is the stress singularity approach (the fracture mechanics approach with no initial crack). The use of a generalized stress–intensity

factor, analogous to the stress–intensity factor in classical fracture mechanics, to predict fracture initiation for bonded joints was investigated by some researchers [39, 40]. Groth [39] suggested a fracture initiation criterion at the interface corners for bonded structure. It was assumed that initiation of fracture occurs when the generalized stress–intensity factor reaches its critical value. Gleich *et al.* [40] carried out a similar study and calculated the singularity strength and intensity for a range of adhesive thicknesses. They found that the intensity increased with increasing adhesive thickness and observed that this would account for decreasing joint strength with increasing adhesive thickness, which is verified experimentally.

Damage Mechanics The progressive damage modeling approach enables the complete response of structures up to the final point of failure to be modeled in a single analysis without the need for additional postprocessing of finite element analysis results. This is an emerging field, and the techniques for modeling damage can be divided into either local or continuum approaches. In the continuum approach, the damage is modeled over a finite region. The local approach, where the damage is confined to zero volume lines and surfaces in two and three dimensions, respectively, is often referred to as cohesive zone approach.

In a (CZM) approach, used to model the progressive damage and failure, a predefined crack path has to be defined. A CZM simulates the macroscopic damage along this path by specification of a traction–separation response between initially coincident nodes on either side of the predefined crack path. In most of the CZMs, the traction–separation relations for the interfaces are such that with increasing interfacial separation, the traction across the interface reaches a maximum (crack initiation), then decreases (softening), and finally, crack propagates, permitting a totally debond of the interfaces. The whole failure response and crack propagation can thus be simulated.

A CZM models the fracture process extending the concept of continuum mechanics by including a zone of discontinuity modeled by cohesive zones, thus using both strength and energy parameters to characterize the debonding process.

The main difference in CZMs is in the shape of the traction–separation curves and the parameters that describe this shape. In general, the parameters that describe the cohesive law are: the area under the traction–separation curve (the toughness) and a characteristic strength (typically, the cohesive strength) or a characteristic displacement that represents the failure strain of the cohesive zone [4].

An important concept of the CZM approach is that both strength and energy parameters are used to characterize the debonding process along the crack path, allowing the approach to be of much more general utility compared to conventional fracture mechanics. However, the cohesive models present a

limitation, as it is necessary to know a priori the critical zones where damage is prone to occur and place the cohesive elements in accordance. Continuum damage models constitute a valuable alternative when crack path is not known a priori. In addition, continuum damage models acquire special relevancy when adhesive thickness has to be considered [41].

In summary, there are a large number of approaches and studies in the literature for predicting the strength of adhesively bonded joints, and the number of possibilities in choosing a failure criterion is overwhelming. It is difficult to assess which one is the best, and there is no general agreement about the method that should be used to predict failure as the failure strength and mode are different according to various bonding methods and parameters. However, progressive damage models are quite promising since important aspects of the joint behavior can be modeled using this approach.

1.8 Recent Developments

Nowadays, one aspect that becomes more and more important is the fact that the materials can be recycled and repaired. In both cases, it is necessary to separate the joint between the bonded components. When it comes to recycling, this separation is necessary so that the different materials can be reused on a qualitatively high level. Recently, specific debonding techniques have been developed. For example, adhesives modified with thermally expandable particles (TEPs) can be dismantled in a few seconds, as Nishiyama and Sato [42, 43] have shown.

On the other hand, repair is costly and, in many cases, impossible due to limited access or risk of further degradation of the structure. Thus, a polymer with a built-in mechanism to repair cracks by restoring broken bonds is of great interest for numerous applications to extend the service life and improve the safety of structural materials. Currently, self-healing materials, inspired by biological systems in which damage triggers an autonomic healing response, are under intense research [44–49]. The first work that demonstrated the self-healing in an engineered material system was the work of White *et al.* [44] with self-healing epoxy resins and composites. The approach was based on the introduction of microencapsulated healing agent and a catalyst in a polymer matrix. When a crack propagates through the capsules, the monomer within stored in them is released along the fissure where it comes into contact with a dispersed catalyst, initiating polymerization and thus repair. Nevertheless, since then, rapid advancement has been made in the field following this conceptual approach. Besides microencapsulated healing agent, there are currently several techniques used for self-healing such as hollow glass fibers, microvascular networks, supramolecular networks, and so on [46]. All these techniques that were applied with promising results to composite [45] and

polymeric materials [49] might be applied to adhesive joints. Therefore, adhesives with self-healing capability to suppress service-induced damage and cracking in particular provide a promising potential in extending the service life of bonded joints. Self-healing adhesives would alleviate longstanding problems in structures associated with multiple types of damage mechanisms such as mechanical/thermal fatigue, microcracking, and debonding.

1.9 Conclusions

Adhesive bonding is a viable technique for joining a wide range of materials. Suitable surface treatments and adhesives for a given application have to be chosen. The choice of which adhesive is best is usually dictated by the type of material to be bonded, the application, the service environment, and costs. In addition, the behavior of specific bonded systems exposed to various environments should be taken into account for durability design. Furthermore, the influence of environmental aspects has specific relevance for multimaterial (hybrid) structures, where components with different reactions to the same environmental conditions – for example, metals and composites with different coefficient of thermal expansion – can significantly alter the behavior of the structure as a whole. In addition, they must be designed to be resistant to fatigue caused by alternating mechanical stresses.

A high percentage of failures can be attributed to poor joint manufacture or a lack of understanding of the factors that influence the joint performance. Thus, besides selection of the right adhesive, correct mixing, and application of the adhesive, joint assembly and the curing process are also important issues in producing reliably adhesive joints.

There are a wide range of test methods and associated test specimens that are used to evaluate the performance of adhesives and adhesive joints, but not all of these are able to generate data for design.

The strength of a given type of joint depends, for a given type of load, on the stress distribution within the joint. This stress distribution depends on the joint geometry and the mechanical properties of adhesives and adherends. There are a large number of approaches and studies in the literature for predicting the strength of adhesively bonded joints, and the number of possibilities in choosing a failure criterion is overwhelming. It is difficult to assess which one is the best, and there is no general agreement about the method that should be used to predict failure as the failure strength and mode are different according to various bonding methods and parameters, but progressive damage models are quite promising since important aspects of the joint behavior can be modeled using this approach. However, a lack of reliable failure criteria still exists, limiting in this way a more widespread application of adhesively bonded joints in principal load-bearing structural applications. An accurate strength

prediction of the adhesively bonded joints is essential to decrease the amount of expensive testing at the design stage.

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2

Adhesive Bonding of Polymer Composites to Lightweight Metals

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2.1 Introduction

Adhesive bonding is a permanent joining process between components of a structure that employs an adhesive to bond the components after its solidification/curing. It is used to allow the fabrication of complex-shaped structures that could not be manufactured in a single piece, for reinforcement/repairing purposes or generic bonding, aiming to provide a structural union that theoretically should be at least as resistant as the parent materials. Adhesive joints can replace welded or fastened/riveted joints in engineering applications, on account of large investigation and optimization efforts over the past decades that, together with the continuous developments in the adhesives characteristics, have made this process highly efficient [1, 2]. Currently, they provide many advantages over mechanical methods, such as lower structural weight, lower fabrication cost, ease of fabrication, improved damage tolerance, and design flexibility. Compared to mechanical fastening, which is only expected to achieve a tensile strength of nearly half that of the weakest adherend in the joint, due to the stress concentrations near the fastener holes, bonded joints can attain the strength of the weakest adherend even with a single-lap configuration. As a result, this technology is presently chosen in areas from high-technology industries such as aeronautics, aerospace, electronics, and automotive to traditional industries such as construction, sports, and packaging [3]. Despite these facts, bonded joints cannot be disassembled without damage, they are very sensitive to environmental factors, such as humidity and temperature, and they tend to collapse abruptly, rendering damage monitoring not feasible. Though, the most serious handicap of adhesive bonding is the designers' uncertainty regarding long-term structural integrity.

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Concurrently to the developments in the bonding processes, fiber-reinforced composites are becoming increasingly popular in many sectors of industry as a result of some attractive characteristics [4] over typical construction materials. Composite materials are typically used in structures that require high specific strength and stiffness (i.e., weight-normalized values), allowing reduction of the weight of components, while maintaining the necessary strength and stiffness to withstand the imposed loads. This feature is at the origin of the increasing use of high-performance carbon-fiber composite materials in aerospace applications. For glass–fiber composites, it is possible to match the specific strength of structural alloy steels at a much smaller cost of raw material and fabrication, and, in some scenarios, the resistance to extreme environments (contact with chemicals and moisture) also plays a role in their benefit. These characteristics can be easily provided by a consistent choice of the polymeric resin acting as matrix or even by the use of additives or coatings. Overall, there has been a progressive increase in the number of metal parts replaced by composites in military and civil aircrafts. As an example, composite materials make up 22% of the total weight of the Airbus A380, including the center wing box, the tail cone, the pressure bulkheads, and the vertical and horizontal tails [5], and this tendency is likely to increase in the near future (for the Boeing 787, it is around 50%). Regarding fabrication of composite structures, even though current fabrication methods offer the possibility to reduce structural coupling to a minimum, by means of integral design and special manufacturing techniques, postfabrication interconnections are still required due to some issues such as the typical size of the components and design, technological, and logistic limitations. Other problems related to repair, maintenance, inspection, and handling requirements may also bring the need to connect different parts. Independently of the method, coupling increases structural complexity and local stress concentrations, but can also reduce the advantage of lightweight of composite design. As a result, advanced joining technologies must be developed to bring the corresponding weight penalties to a minimum. Because of the aforementioned characteristics and advantages of adhesive bonding, it is thus easy to accept that the application of bonding in structures involving fiber-reinforced composites has significantly increased in recent years. One of the main reasons is the preservation of structural integrity that bolted or riveted joints cannot accomplish, because of cutting of fibers and hence the introduction of stress concentrations.

This chapter addresses the available adhesive bonding techniques between composite materials and lightweight metals. Initial emphasis is given to the description of the different kinds of bonds between these materials, main advantages and applications, focusing on bonding of hybrid bonding between these materials as different components, and fiber metal laminates (FML). Some considerations are then provided concerning the experimental fabrication of these structures and examples of application available in the

literature. The final section deals with predictive techniques for hybrid bonded structures, ranging from analytical to finite element (FE)-based methods, giving a basic description on the principles of each technique and relevant works, with focus on the modeling techniques and obtained results.

2.2 Characteristics and Applications of Hybrid Bonding

On several occasions, polymer composites require joining with lightweight metals such as aluminum or titanium to produce multimaterial structures [4]. Actually, as previously discussed, fiber-reinforced composites are superior to metals, in relation to specific strength and stiffness; however, metals are required in some structures or specific parts of an assembled structure due to their high reliability [6]. Hence, in many advanced engineering structures, hybrid joints between composites and metals can be expected (Figure 2.1; L_T – total joint length, L_O – overlap length, t_{pi} – adherend i thickness, t_A – adhesive thickness).

The fuselage of supersonic aircrafts, where aluminum- or titanium-composite joints are common [7], usually experience surface temperatures ranging from -55 to over 200°C , because of the kinetic heating at high speeds due to air friction. Bonded composite aircraft repairs are another possible application of hybrid bonding, extensively studied in the literature [8]. These consist of the application of a composite patch to an underlying aluminum plate, either by adhesively bonding a precured patch or stacking prepreg plies to be subsequently cured. The benefits of using composite patches involve easiness

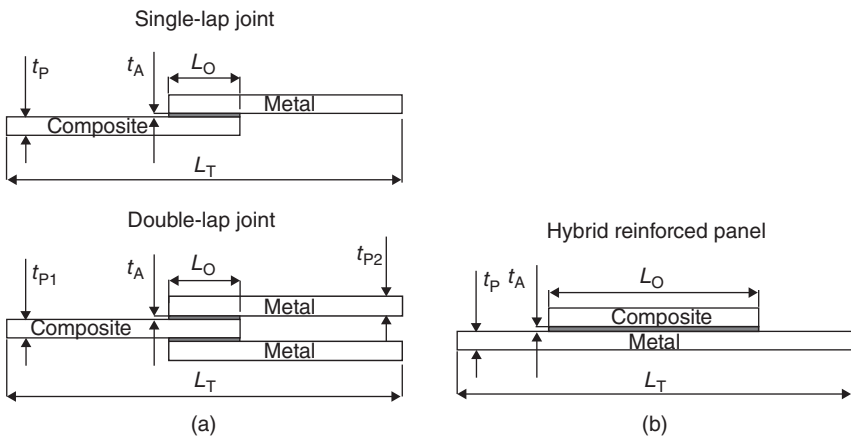


Figure 2.1 Single- and double-lap hybrid joints (a) and hybrid reinforced panel (b).

to build small-thickness repairs and easy assembly and conformance to curved geometries. Nonetheless, the resulting hybrid structure has highly mismatched adherends that will exhibit significant thermal strains after curing.

The reinforcement of composite propellant tanks in space-launch vehicles with bonded aluminum liners is yet another innovating application of hybrid structures [9]. Actually, studies have been conducted on the weight minimization of these spacecraft components, which invariably leads to the use of carbon–epoxy composites for the tanks. However, these materials tend to develop micro cracks in the matrix due to the difference in coefficients of thermal expansion (CTE) to the reinforcement fibers, which can lead to fuel leakage and put the tank safety at risk. This opens an opportunity for aluminum reinforcement as bonded liners placed circumferentially around the tank. Nonetheless, when the reinforced tanks are full of pressurized cryogenic propellant, the adhesive layer bonding the liners to the tank undergoes extreme thermal stresses, which may cause failure of the bond and emphasizes the need to select and design the bonded system with the utmost attention.

The need to reinforce ship hulls of large dimensions originally made of glass–fiber composites, because of the larger vertical bending moments also led to the study of this technique [10], that is, with bonded aluminum reinforcements. The work of Caccese *et al.* [11] addressed a specific scenario in which bonding of composites to metallic structures can bring significant operational benefits: a hybrid strut/ship bonded connection for underwater operation. The strut was made of a stack of pultruded and filament-wound composite plies, and it was connected to the ship by an adhesively bonded aluminum boot acting as the ship interface.

Owing to the potential weight and stiffness improvements in hybrid bonded structures between composites and lightweight metals, applications were also tested for automotive drive shafts, headstocks of grinding machines, and robotic structures [12]. The improvement of impact energy absorption is also possible to accomplish by hybrid structures. The axial crush and bending collapse of hybrid square tubes made of aluminum and glass composites was studied by Shin *et al.* [13] with the objective of improving energy absorption in automotive side rails of the front structure. Results showed the effectiveness of the hybrid tubes, allowing reduction of the vehicles' weight for similar absorption characteristics.

On the other hand, bonding between composites and metals in stratified panels is also highly advantageous, giving rise to FML (Figure 2.2). The concept was initially developed at Delft University of Technology in the 1980s [14], and the most common combinations were aluminum with aramid (Arall) and glass composites (Glare). One of the first motives for developing these structures was related to the increase of fatigue strength in aluminum panels used in aeronautical structures. Actually, this is a highly relevant issue since aluminum structures are prone to suffer fatigue failures, but also because in

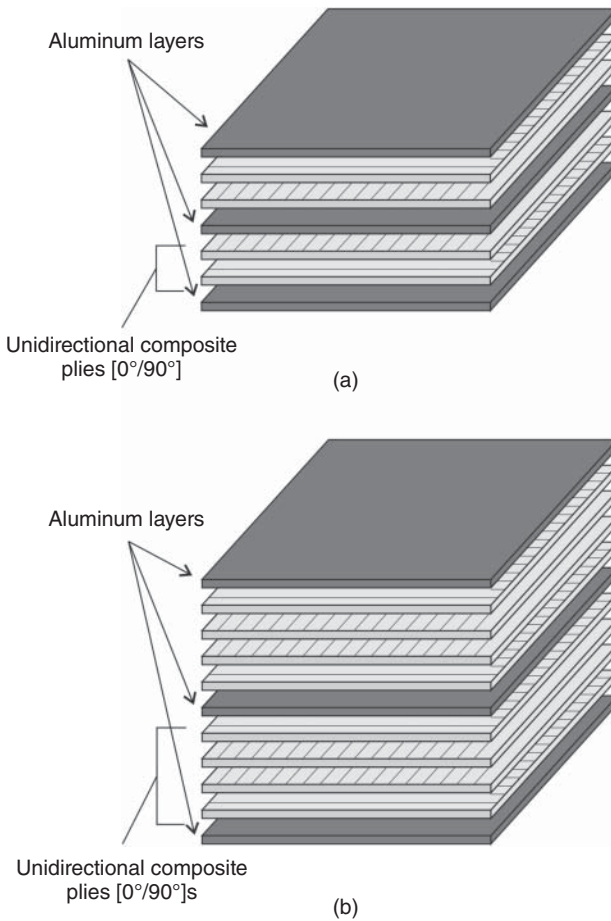


Figure 2.2 Examples of FML between unidirectional composite plies and aluminum layers.

many situations, both civil and military aircraft are forced to operate at loads higher than the design values and for periods of time well beyond their original design life. Fatigue cracking often initiates from impact damage occurring during flight (e.g., bird strike), preflight, or taxiing operations, tool or object impacts during maintenance, runaway debris, or hail.

While one of the initial solutions was to use bonded laminated layers of aluminum to bridge the cracks and slow down their growth, composite plies were then introduced in these structures for additional weight reduction. Weight savings of about 30% were reported by this technique using aramid plies [15]. Glare was initially employed in the Airbus A320 fuselage, leading to weight savings of 25%, with proven effectiveness in retarding or arresting

the crack, because of bridging to the crack opening by the intact fibers in the wake of the advancing crack tip. Another study [16] reported weight savings of 6% with Glare construction over the aluminum alloy, to match the design bending stiffness, and of 17% based on the yield strength. In these materials, cracks grow in the aluminum foils with progressive delamination between the aluminum and composite plies. The cracked metal layers are bridged with the intact fibers, and crack propagation is slowed down (the fatigue crack propagation in the aluminum layers of Glare can be 10–100 times slower than in equivalent monolithic aluminum [16]).

Further benefits in terms of the impact strength, flame and corrosion resistance, damage tolerance, and simplification of the fabrication process were found [17]. In FML, the outer layers are the metal sheets, to protect the core plies of polymeric composites. As a consequence, the resistance to environmental attack and foreign object impact is also enhanced. More recently, combinations of aluminum with carbon composites (Carall) were introduced [18] and, for high-temperature applications, between titanium and carbon composites (TiGr) [19]. In the quest for structural weight reduction, some researchers have also explored the use of magnesium [17]. The generalized success of FML and especially Glare is that these hybrid materials perfectly fit in the damage tolerance principles required in current aircraft design. Actually, these laminated materials provide a second load path, making it a damage-tolerant material [20]. FML, which were initially developed for primary aircraft structures, are currently being applied in fuselage skin structures, more specifically the upper section prone to high fatigue loads, and lower wing skins. Moreover, Glare is also employed for the D-noses of the horizontal and vertical tail planes, because of the excellent impact resistance of this hybrid material. Because of this, studies were also conducted on impact-prone areas of aircraft, for example, the cockpit, attempting to optimize a Glare configuration specifically for impact performance. These works showed that using unidirectional S2-glass fiber plies provided maximum impact resistance when compared to laminates made with S2-glass or E-glass weaves [21]. Carbon–epoxy composites are best combined with titanium alloys, on account of the compatibility between CTE and yield and ultimate elongations [5]. These features make this FML suited to structural skin materials, local reinforcements, repair patches, or high-temperature applications (e.g., components of high-speed aircrafts).

The local hybridization with metal foils of composite laminates near bolted joints is another application of FML, in this situation, to increase the bolted joint strength between composite laminates. This reduces the weight and increases the cost efficiency of spacecraft structures that face increasing requirements in these directions. Actually, the result is a hybrid material with high bearing, shear, tension, and compression strengths at the coupling region, which are necessary to high bolted joint efficiencies. Moreover, the higher

strength of this FML avoids the need for local thickening and eccentricities and permits a reduction of the number of bolts, resulting in a mechanically and cost-efficient design. This application was addressed by Fink *et al.* [22] on the bolted joints of a carbon–epoxy spacecraft payload adaptor, using ply substitution techniques without any additional adhesive film to provide a hybrid composite–titanium structure near the bolt holes. Application of this reinforcement granted a large weight reduction of the structure.

For both of these solutions, that is, bonding composites with lightweight metals, and fabrication of FML panels, paste or film adhesives can be selected. However, giving the particular characteristics of the materials to be joined, co-curing is also available to reduce fabrication time and cost of hybrid structures [6]. Co-curing uses the excess resin of the composite matrix as an adhesive, which bonds the composite and metal parts. Composite curing and joining with the metal part are thus achieved simultaneously. Although this simplification is a clear advantage, a significant increase of the bond strength, when compared with conventional bonded joints, cannot be expected. Notwithstanding the bonding method, the static and dynamic joint strengths of the bonded structures highly depend on design parameters such as the lay-up of the composite adherend, geometric parameters, and surface roughness of metallic adherend (co-cured joints) or both adherends (adhesive bonding) [23]. The composite lay-up, for instance, highly influences the structural stiffness and CTE. The thickness ratio between the composite and metallic adherends, on the other hand, also affects the stiffness and differential deformation between both adherends. All of these parameters affect the stress distributions and, as a natural consequence, the bonding strengths as well.

On account of this and since the bond is usually the weakest part among the components of assembled structures, a large number of works were made available in recent years for a large range of applications and using the two bonding methods. An overall perspective of the available design information is provided in the next sections, divided into experimental and predictive works.

2.3 Experimental Evaluation of Hybrid Structures

This section describes the general principles for a correct bonding between composites and metals, with emphasis on the adherends preparation, bonding procedure (adhesive bonding and co-curing), and destructive testing techniques. A few experimental works on hybrid bonds are described together with relevant results, showing the potential of hybrid bonds.

2.3.1 Preparation of the Adherends

The adherends preparation method largely depends on the material. For metallic adherends, machining is often used after cutting oversized specimens from

laminated sheets or bulk slabs, using milling tools such as high-speed steel, carbide, or eventually diamond-coated mills for stronger metals. Alternatively, water jet cutting also provides good finishing. Roughening by grit blasting (using SiO_2 sand or similar with grain size between 0.2 and 0.6 mm) is recommended to provide a strong bond or, alternatively, manual abrasion with rough sandpaper. This allows removing the surface oxide layer and contaminations. Cleaning of the bonding surfaces can be carried out with acetone. Etching or anodization can be used for durability.

For composites, the structures/adherends are usually composed of stacked plies and set to their final dimensions by machining. Grinding stones at a high rotational speed (>2000 rpm) and small linear feed (<50 mm/min) are suggested. Milling tools are not recommended based on their fast wear rate (carbide or carbide-coated mills) or high cost (diamond-coated mills). Rudawska [4] used a solvent cleaner by Loctite[®] (reference 7063) for cleaning and degreasing composite (aramid/epoxy), titanium, and aluminum parts before hybrid bonding with an epoxy adhesive after manual roughening. This technique proved to be quite effective in providing a strong bond. Lawcock *et al.* [24] investigated the adhesion between aluminum sheets and carbon fiber prepreg for Carall hybrid plates and showed a marked advantage in the interfacial fracture energy in tension by performing a modified FPL-Etch procedure followed by application of a silane coupling agent, compared to standard etching.

2.3.2 Application of the Adhesive

A strong bond highly relies on a correct fabrication procedure. A mold should be considered for a correct alignment and uniform application of pressure. When considering paste adhesives, pouring of the adhesive in the bonding surfaces is carried out with a low-moisture atmosphere to avoid moisture absorption and preferably on both adherends. This is essential for fast-curing adhesives to prevent weakening at the upper adhesive/component interface due to partial adhesive curing before assembly of the upper component. For precision of the design value of adhesive thickness, the placement of calibrated steel spacers at the outer periphery of the bonding length is recommended [25]. Alternatively, calibrated glass beads can be mixed with the adhesive in amount up to 0.5% of the total weight [26]. Film adhesives can also be used to bond the layers of different materials. By the co-curing technique, the hybrid joint can be manufactured by alternatively piling prepreg and metallic plies or by hand lay-up, that is, manually placing reinforcing mat, woven fabric, or roving over the metallic piece, while resin is poured, brushed, or sprayed over the composite plies. Utmost attention must be given to the curing temperature, curing time, and required pressure on the bonded set. Curing/co-curing can occur at room or high temperature and with or without pressure, depending

on the adhesive characteristics. A hot plates press (for flat components) [26] and an autoclave [16, 27] are the most straightforward approaches to induce the specified thermal/pressure cycle. For pressure application, vacuum bagging is another alternative, although the final quality is poor compared to autoclave curing. In the work of Sugiman *et al.* [25], the static response of aluminum–glass FML was evaluated. The test coupons were fabricated by manual piling of alternating layers of these materials with 0.22 mm thickness FM[®] 73 M film adhesive supplied by Cytec in-between, after preparing the bonding surfaces with BR 127 primer by Cytec. The whole set was cured in an autoclave at the manufacturer-specified conditions for optimal bonding.

Bagnoli *et al.* [20] studied the fatigue crack retardant capabilities of Glare laminates adhesively bonded to an aluminum alloy. The fabrication of the Glare structure and bonding to the aluminum plate was accomplished with the FM[®] 94 modified epoxy adhesive film by Cytec. For surface preparation, the bonding surfaces were etched with a sulfuric acid/sodium dichromate bath at 60 °C. The structure was cured in an oven at 125 °C for 1 h. Ultrasonic phased array C-scanning confirmed the bond quality before testing. Park *et al.* [23] studied co-cured double-lap joints between carbon–epoxy and aluminum adherends. An average surface roughness (R_a) of 2.0 μm was produced by mechanical abrasion to achieve the maximum fatigue strength. For bonding, the prepreg plies were stacked on the aluminum adherend and vacuum-bagged. Co-curing was accomplished in an autoclave with a pressure of 0.6 MPa.

2.3.3 Testing of the Specimens

Hybrid structures can be tested in any conventional electromechanical or hydraulic machine that records the load–displacement P – δ curve. However, for high-precision measurement of structural displacements, linear variable differential transformers (LVDT) can be used to remove the grips deformation [26], preferably mounted on the machine structure. Noncontacting video extensometry is another possibility for the displacements measurement [16], with the advantage of independency to the loading system. The testing velocity, chosen depending on the strain rate desired for the tests, should be sufficiently small to allow for quasistatic conditions. In the work of Sugiman *et al.* [25], the crosshead rate was kept at 0.1 mm/min for an accurate characterization of the damage process. Damage uptake in the structures can be recorded with a high-resolution camera to take photographs (preferably automatically) within specified time intervals. The correlation of the damage events with the P – δ data is possible by the elapsed time from the beginning of the test, which is easily and accurately related to the P – δ data by the testing velocity and shooting time interval. This procedure was followed by Kweon *et al.* [28], who used a digital microscope camcorder to record damage propagation in hybrid composite–aluminum joints under tension loads.

Matsuzaki *et al.* [6] experimentally studied hybrid aluminum/glass composite co-cured single-lap joints and used acoustic emission measurements to investigate the fracture mechanisms of the joints during testing. An acoustic emission sensor and tester were used for the measurements, and the damage events were recorded onto a computer. Depending on the application, hybrid structures may be required to work at extreme temperatures. Kang *et al.* [9] tested composite–aluminum joints at a cryogenic temperature of -150°C using a sealed environmental chamber. Preloading the specimens with a tensile load of 4 kN and gluing of emery cloth tabs were necessary to prevent sliding when tensioning at -150°C .

2.3.4 Experimental Works

Recently, a considerable number of experimental works on hybrid joints were published. In the work of Kang *et al.* [9], the cryogenic performance of adhesives in hybrid composite/aluminum double-lap joints was evaluated, considering an inner adherend of aluminum (6061-T6) and outer adherends of graphite–epoxy stacked composite. Three film adhesives (Bondex606 by Hankuk, EA9696 by Loctite, and FM73[®] by Cytec) were tested at room temperature and -150°C to compare the bond strength of each adhesive and fracture characteristics. Experimental testing revealed that at -150°C , the bond strengths of the double-lap joints increased and that the EA9696 has shown the highest improvement over room temperature testing (89.8%). The FM73[®] showed an increase of 44.3%, and the Bondex606 of 53.0%. On account of extensive testing of the bulk adhesives, the reported improvement was mainly related to the increase of tensile strength of the adhesives.

da Silva and Adams [27] studied bonding of conventional and hybrid double-lap joints, attempting to improve the strength of these joints by the dual adhesives technique over a wide temperature range (-55 to 200°C ; i.e., range of temperatures in the fuselage of a supersonic airplane during flight). The hybrid joints were composed of composite inner adherend and outer adherends of titanium and vice versa. The Advanced Composites Group HTM552 was used for the composite adherends ($0^{\circ}/90^{\circ}$ carbon fabric laminate of high-strength carbon fibers in a 2×2 twill weave, impregnated with a bismaleimide resin). The dual adhesive joints were initially proposed to increase the performance of joints bonded with brittle adhesives, by using a ductile adhesive at the ends of the overlap. The authors used a joint with a high-temperature adhesive (HTA) in the middle and a low-temperature adhesive (LTA) at the ends. Experimental testing revealed that for identical adherends (titanium/titanium), the mixed technique was of small benefit. In fact, at high temperatures, the joint strength was lower than for a joint with a HTA alone at low temperatures. Conversely, at low temperatures, the mixed technique gave better results compared to a conventional joint with a HTA and

could effectively increase the strength of brittle adhesives at low temperatures. The titanium/composite joints showed a real improvement over a conventional joint by using the mixed modulus concept, due to the large difference in the CTE. For a composite adherend stiffness smaller than the metal adherend stiffness ($E_{lc}t_c < E_m t_m$), the thermal stresses severely reduced the strength of conventional joints with a HTA at low temperatures, and a mixed adhesive joint significantly improved the strength, because of the high stress-free temperature of the HTA in the middle of the overlap. At intermediate and high temperatures, the mixed joints also revealed better results compared to the joint with a HTA alone. To further testify the feasibility of dual adhesive systems at either low or high temperatures, the mixed joints were thermally cycled. Results showed no signs of degradation of the LTA after a stage at high temperature, nor of the HTA after a stage at low temperature.

Kweon *et al.* [28] tested hybrid double-lap joints between carbon–epoxy and aluminum (7075-T62) adherends, by three joining methods: adhesive bonding, bolting, and adhesive–bolt joining. For the bonding and bond/fastening solutions, the film adhesive FM73[®] from Cytec and the paste adhesive EA9394S from Henkel were evaluated. The average adhesive shear strength for the FM73[®] was 31.9 MPa and for the EA9394S was 4.74 MPa. Combination of bonding with bolting revealed a strength improvement when mechanical fastening is stronger than the bonding layer, that is, for the paste adhesive. When the bolted joint strength was smaller than that of the bonded joint, as it occurred for the film adhesive, no benefits were found in improving the hybrid joint strength.

A reinforcement technique was proposed by Matsuzaki *et al.* [6], for hybrid aluminum/glass composite co-cured single-lap joints. In this method, two small holes were drilled in the metallic adherend, and a high-strength inter-adherend (IA) fiber that pierced into the uncured prepreg weave and holes in the metal adherend interlocked both adherends together and enhanced the crack propagation resistance. Experiments clearly demonstrated that the IA fiber created a bridge between the aluminum and glass adherends and efficiently arrested the propagation. Because of this, the failure displacement and static strength significantly increased using the IA fiber (the average strength improvement was approximately 15%), without compromising the fatigue strength. The noneffectiveness of this modification for fatigue loadings was accredited to fatigue damage uptake of the IA fibers.

In the work of Lee *et al.* [29], a new manufacturing method was developed for hybrid aluminum/carbon–epoxy drive shafts for automotive applications. Motivation for the proposed study was based on weight reduction and elimination of whirling vibration by changing the bending natural frequency, on account of the higher specific stiffness of composites over aluminum or steel. For cost reduction, the composite is combined with aluminum, which carries most of the transmitted torque. The drive shafts were designed such

that the composite layer was co-cured on the inner surface of the aluminum tube, instead of the more conventional method of wrapping it on the outer surface, thus preventing damage by external impact and absorption of moisture. The proposed drive shaft was able to transmit the design loads with 75% mass reduction and 160% increase in torque capability compared with the steel counterpart. An increase in the natural frequencies over the design specification of 9200 rpm was also accomplished.

Abdullah and Cantwell [30] experimentally examined the impact resistance of composite–aluminum FML. For an optimal adhesion between the aluminum and composite sheets, a 60 μm thick adhesive layer was used. The initial tests were based on sandwich coupons with 2024-O or 2024-T3 aluminum skins and a polypropylene fiber–polypropylene matrix core (PP-PP composites). The FML fabricated with the stronger 2024-T3 alloy gave higher perforation strength compared to those based on the 2024-O alloy. Multilayered plates with alternating composite/aluminum sheets were also tested. Between the two FML solutions, for a given laminate thickness, the multilayered laminates showed a higher perforation strength compared to the sandwich laminates. The specific perforation energy was also used to compare the different laminated systems, showing that the PP/PP-composite-based FML clearly outperformed the multilayered FML since it showed specific energies roughly two times higher. Different failure mechanisms were found: ductile tearing, delamination, and fiber failure in the composite plies, plastic deformation or thinning, and shear fracture in the metal layers. In a first stage, the front and rear surfaces of the impacted coupons were examined. For smaller impact energies, damage was found as localized dent and cracking of the rearmost layer of aluminum. By increasing the energy, the extension of these damage mechanisms increased as well around the hit location.

At a bigger and specific value of impact energy, the panel was perforated, with a relatively clean hole similar in dimensions to the projectile. Damage consisted of local ductility of the aluminum and composite layers and fracture of the constituent materials. Figure 2.3 represents optical micrographs with low magnification of 4/3 FML (4 layers of aluminum with 3 layers of PP/PP composite in-between) impacted with an energy of 178.2 J at the impacted surface (a) and at the rear surface (b). Bagnoli *et al.* [20] presented an experimental evaluation of the low velocity impact and postimpact fatigue behavior of high-strength aluminum plates, reinforced by adhesively bonded Glare laminates, attempting to retard fatigue crack growth. Impact and fatigue testing involved monitoring by an ultrasonic phased array C-scan, together with metallography and scanning electron microscopy (SEM). For impact characterization, energies ranging from 10 to 50 J were considered. The maximum load was measured, giving a linear correspondence to the input impact energy. The Glare/aluminum bond showed no evidence of damage, while damage to the Glare, bonded on the opposite side of the impacted substrate, occurred by fiber and matrix cracking.

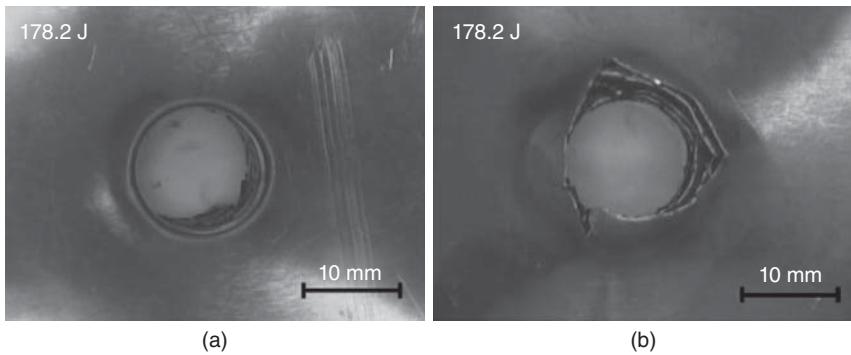


Figure 2.3 Optical micrographs of impacted 4/3 FML at the impacted surface (a) and at the rear surface (b). (Source: Abdullah and Cantwell 2006 [30]. Reproduced with permission of Elsevier.)

The fatigue crack growth tests showed the crack retardation effect by bonding the Glare panel to the aluminum substrate (Figure 2.4(a); diamond dots), with reductions in aluminum fatigue crack growth rate of upto five times, when the crack attains the strap edge. Exiting from the reinforced region, the growth rates were about a factor of 2 less than those found in the aluminum. Figure 2.4(b) plots the crack length versus cycles for nonimpacted and impacted specimens (10 and 30 J). Before the first strap edge is reached, the behavior is similar. Once it is attained, the nonimpacted specimen gives a longer fatigue life compared to the damaged ones. Globally, reinforcing after impact gave a crack retardation factor of approximately 2.

2.4 Predictive Techniques for Hybrid Structures

In the past, the nonexistence of accurate material models and suitable failure criteria led to “overdesign” of adhesive joints, which invariably resulted in heavier and more expensive structures. With the availability of reliable design and predictive methodologies, new possibilities emerged for the efficient use of bonded joints. Design is often associated to stresses and strains determination for the component in-service loading and prediction of the structure strength and probable points of failure. Two alternatives can be followed: closed-form analyses (analytical methods) and numerical methods (i.e., FE analyses). The study of adhesive joints dates back to the 1930s, with the closed-form model of Volkersen [31] that considered fully elastic materials and adhesive deformation only in shear. However, the analytical formulation of adhesive joints becomes more complex if the adhesive deforms plastically, if composite adherends are used, or if different adherend materials (e.g., composites with metals) are employed. The FE is unquestionably the most popular technique for adhesive

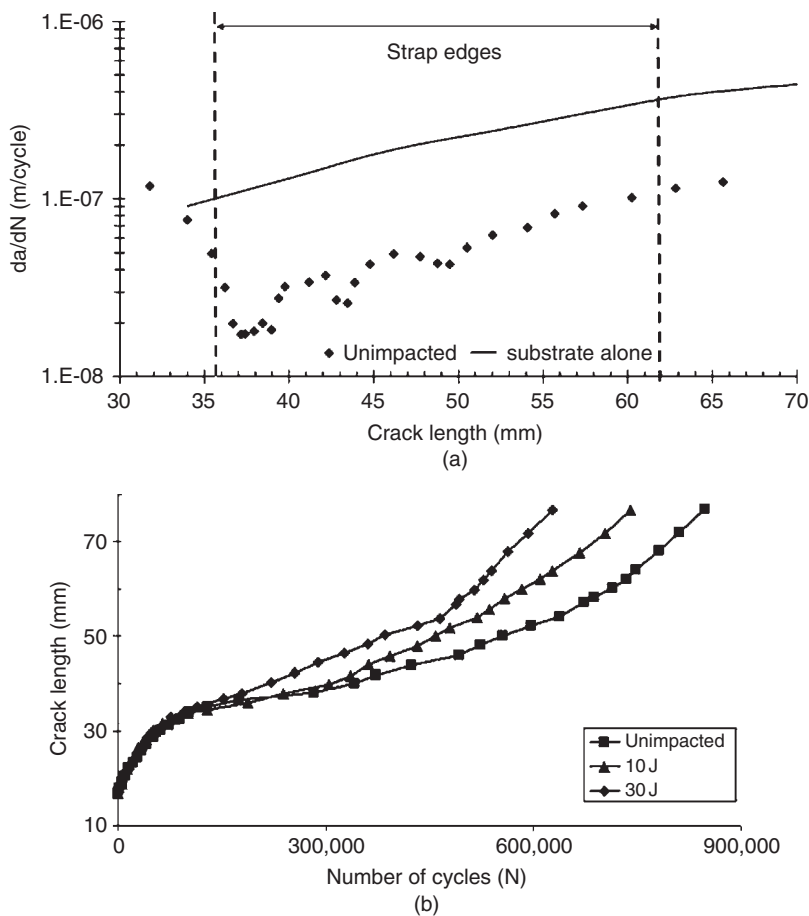


Figure 2.4 Fatigue crack growth rate versus crack length for nonreinforced and reinforced panels without impact damage; (a) and fatigue crack length versus number of cycles for reinforced, nonimpacted, and impacted panels (b). (Source: Abdullah and Cantwell 2006 [30]. Reproduced with permission of Elsevier.)

joints, and Adams and coworkers were pioneers in this technique [32]. The joint rotation, the adherends and adhesive plasticity, and the influence of the spew fillet were studied first by Harris and Adams [33]. For strength prediction, the continuum mechanics approach was initially used, requiring the stress distribution (obtained by a FE analysis or a closed-form model) and a suitable failure criterion. For the obvious reasons, in the presence of complex geometries and elaborate material models, the FE method is recommended.

One of the simplest failure models is based on a stress or strain limit state that, depending on the characteristics of the bonded system, can rely on the

allowable equivalent stress or strain by specific criteria, or a stress/strain component such as shear or peel. An FE analysis can also be used together with Fracture Mechanics criteria for strength prediction, either by the stress intensity factor or by energy approaches such as the virtual crack closure technique (VCCT). However, with these techniques, modeling of crack growth becomes cumbersome because of remeshing taking place during the analysis to update the crack evolution during propagation and respective high computational cost required (referred to as FE with adaptive remeshing) [34]. Because of this issue, applications of these techniques to hybrid structures are not available.

On account of these limitations, major developments were made on the interaction of crack propagation techniques with numerical methods such as the FE, and one of these combines FE with cohesive zone modeling (CZM). This technique joins together conventional FE modeling for the structure regions that are not expected to undergo damage and a fracture mechanics approach via the CZM elements to simulate crack initiation and growth. Meshless methods such as the Element-Free Galerkin Method or Mesh-Free Penalty Approach are another possibility, although not yet applied to hybrid joint modeling. A very recent alternative to model crack propagation within materials is the *extended* finite element method (XFEM), which uses enriched shape functions to represent a discontinuous displacement field. The main advantage of XFEM is that the crack may initiate at any point in the material and propagate based on loading conditions. No remeshing is required as the crack can grow within an element, and it does not need to follow element boundaries. Application of XFEM to hybrid structures is very scarce and limitative at the moment [34].

In general, the analysis of hybrid structures, to bond either different materials or FML laminates, by means of computer-aided software, provides a major cost reduction to extensive experimentation, which makes it highly attractive for designers. This is particularly true because of the complex damage process, possibly including discontinuities, singular regions, nonlinear interfaces, mixed damage modes, and initiation and propagation of cracks. The most relevant analysis approaches for hybrid bonding between composites and lightweight metals (analytical, continuum mechanics, CZM, and alternative damage mechanics techniques) are described in this section with practical examples of application to illustrate the most recent advances in the particular topic addressed in this chapter, together with modeling tips for a correct analysis.

2.4.1 Analytical

The works of Volkersen [31] and Goland and Reissner [35] were precursors for a number of theoretical studies that had the advantage of simplicity to reach the stress fields in the bonded structures because of the employed simplifying

assumptions in terms of the structures geometry, loading, and boundary conditions, giving efficient closed-form elasticity solutions for the local fields in the adhesive region [36]. The simplifications consisted of assuming linear elastic adhesives and/or adherends. With these models, strains and stresses could be easily calculated, along with the distribution of the bending moment in the structure. The strength was typically predicted by stress- or strain-based criteria (to be described further in this work). These studies were progressively replaced by numerical analyses, although theoretical studies were also refined over the years in terms of modeling capabilities, such as material nonlinearity of the adhesive layer [37] or nonlinear geometric analyses [38]. Analytical models of bonded hybrid structures were also developed more recently, with the following description of some of the most relevant studies.

Owens and Sullivan [39] presented a simple analytical model to predict the stiffness of composite–aluminum single-lap joints, considering each component of the joint as a separate spring element, each one sustaining different amounts of deflection. The individual deflections were derived by basic mechanics and by applying the Adams–Peppiatt stress equation. The model was also able to predict the reduction in stiffness due to crack propagation in the adhesive, by considering the reduction of the overlap length.

Romilly and Clark [8] proposed an analytical technique based on material elasticity to analyze stresses and compliance of hybrid repairs, that is, aluminum aircraft structures with bonded boron–epoxy composite patches. The presented formulation was an enhancement of previous work [40], by inclusion of effects such as orthotropic adherends, adherend lateral shear deflection, through-thickness adherend shear deformation caused by adhesive shear stresses, transverse deformation of the adherends caused by adhesive peel stresses, adhesive longitudinal stresses, and thermal stresses resulting from CTE mismatches. The strength and compliance of the repair were calculated. The results were compared to FE results, showing the efficiency of the proposed set of design equations.

Hoo Fatt *et al.* [41] proposed an analytical solution to predict the ballistic strength and energy absorption of clamped Glare laminates when subjected to ballistic impacts, validated with test data. The methodology to obtain the ballistic limit followed an iterative process in which the initial kinetic energy of the projectile had to equal the sum of the deformation energy dissipation, delamination/debonding, and fracture. The transient deformation of the panel was obtained from a mass–spring system. The analytical predictions for the ballistic strength, derived from the resulting nonlinear differential equation, presented a deviation up to 13% of the experimental results. Moreover, it was possible to account for the participation factors of the different mechanisms on the absorption of deformation energy, with 84–92% related to bending and membrane effects, 2–9% of energy absorption by delaminations, and about 7% on account of tensile fracture of the glass/epoxy and aluminum.

2.4.2 Numerical

At the beginning of the age of computers, FE codes were quickly developed and applied to simulate bonded structures [32]. In the conventional FE, each part of the adhesive structure is a continuum and the geometry representation can be either three-dimensional (3D) or modeled as a simplified two-dimensional (2D) model. Large displacements often encountered are easily implemented, as well as thermal–mechanical interaction and materials plasticity. For strength prediction, different approaches were developed over the years for general-purpose bonded structures. The continuum approach provides estimates in a similar manner to analytical methods, that is, comparing stresses/strains with the material allowables. Fracture methods suppose initial cracking and use energy or stress intensity factor methods to predict crack growth. Damage mechanics is based on the damage uptake in the FE elements according to different laws (e.g., CZM or alternative approaches). The XFEM allows cracking to progress within continuum FE elements by increasing the degrees of freedom.

When hybrid structures began to receive attention due to the aforementioned advantages, some decades later, compared to conventional bonded structures, these predictive techniques were then applied to their analysis, although due to the increased complexity, only very recently complete analyses were proposed. In the following sections, some examples of the most relevant techniques are described. Fracture Mechanics and XFEM are not addressed on account of their restricted relevance to hybrid joining.

2.4.2.1 Continuum Modeling

In the continuum mechanics approach, the maximum values of stress, strain, or strain energy, predicted by the FE analyses, are usually used in a suitable failure criterion to estimate the assemblies' strength. This is accomplished when the predicted values, obtained by stress- or strain-based criteria, attain the strength of the structure constituents [1]. Advantages of this technique include the analysis of bonded structures with or without initial damage and under an arbitrary loading. Nonetheless, these criteria hold an intrinsic limitation, since bonded structures, disregarding the geometry, are prone to have stress singularities at the end of the overlapping regions due to sharp corners. As a result, in an FE analysis to a bonded structure, stresses at a singular region increase with the mesh refinement and convergence is not attained, giving mesh-dependent predictions [36]. Over the years, improvements were introduced to this modeling technique, such as the definition of more complex stress- or strain-based failure criteria for different materials and fracture mechanisms, or combination with more realistic continuum material models, but still the mesh dependency could not be solved.

Jumbo *et al.* [42] presented an FE study of thermal stress prediction in double-lap joints with outer adherends of 7075-T6 aluminum alloy and inner

adherend of IM7/8552 unidirectional CFRP composite, bonded with the FM73[®] by Cytec adhesive and comprising adhesive fillets at the bond edges. A 100 °C decrease of temperature was applied to represent cooling from the adhesive curing temperature to the operating temperature. The FE analyses comprised 2D simulations, considering plane strain (PE), plane stress (PS), generalized plane strain (GPE) and also full 3D analyses that accounted for out-of-plane bending deformations induced by CTE mismatch. The evaluation of the stress distributions showed that the GPE model gave the best approximation to the 3D results at the joints mid-width. The analyses also revealed the usual areas of high stress concentrations near the adhesive free edges, and the PE models overpredicted stresses in the transverse axial direction owing to the constraint imposed by the model. The PS model, on the other hand, is inherently unable to predict stresses in that direction. Thermal strain plots from the FE analysis were validated using neutron diffraction experiments, giving results within the limits of accepted experimental errors. Despite the available data from the continuum FE analysis, no strength prediction was performed.

da Silva and Adams [7] investigated the FE titanium/titanium and titanium/composite bonded double-lap joints, for use from low to high temperatures (e.g., the fuselage of aircrafts), by using the dual adhesive technique. A ductile adhesive suited for high temperatures was placed at the overlap edges, and a brittle one for low temperatures was used at the middle of the overlap. With this configuration, at high temperatures, the brittle adhesive transfers the entire load, while at low temperatures, the ductile adhesive becomes the load-bearing adhesive. An FE analysis was presented regarding the stress distributions and the initial yielding load to optimize the joint design. The yielding load was found as the smallest of the following: (1) shear yielding of the adhesive, (2) tensile yielding of the adherends, and (3) transverse tensile failure of the composite. Focusing on the tested hybrid joints (Figure 2.5(a)), it was shown that the dual adhesive technique gives a better performance over the considered temperature range compared to a HTA alone.

Figure 2.5(b) shows the yielding load versus testing temperature for the stiff and brittle adhesive Redux 326 from Hexcel Composites, which is a modified bismaleimide suited for high temperatures, for the low temperature, ductile adhesive Supreme 10HT from Master Bond, which is a modified epoxy, and for dual adhesive joint, designs MAJ1-1, MAJ1-2, and MAJ3 (Figure 2.5(a)). The best design was achieved with design MAJ3, showing similar strengths to the Supreme 10HT at low temperatures and to the Redux 326 at high temperatures.

2.4.2.2 Damage Mechanics

Advanced modeling techniques that assure accurate failure predictions, surpassing the limitations of the previous approaches, are currently available. In general, structural damage can occur by micro cracks over a finite volume or interfacial region, reducing load transfer. An FE simulation based on

solid continuum modeling wrongly outputs generalized plasticization in the elements without damage evolution, while a damage mechanics model can actually induce damage in the elements by reduction of the transferred loads. As a result, the simulation of step-by-step damage and fracture at a predefined crack path or arbitrarily within a finite region are allowed [1].

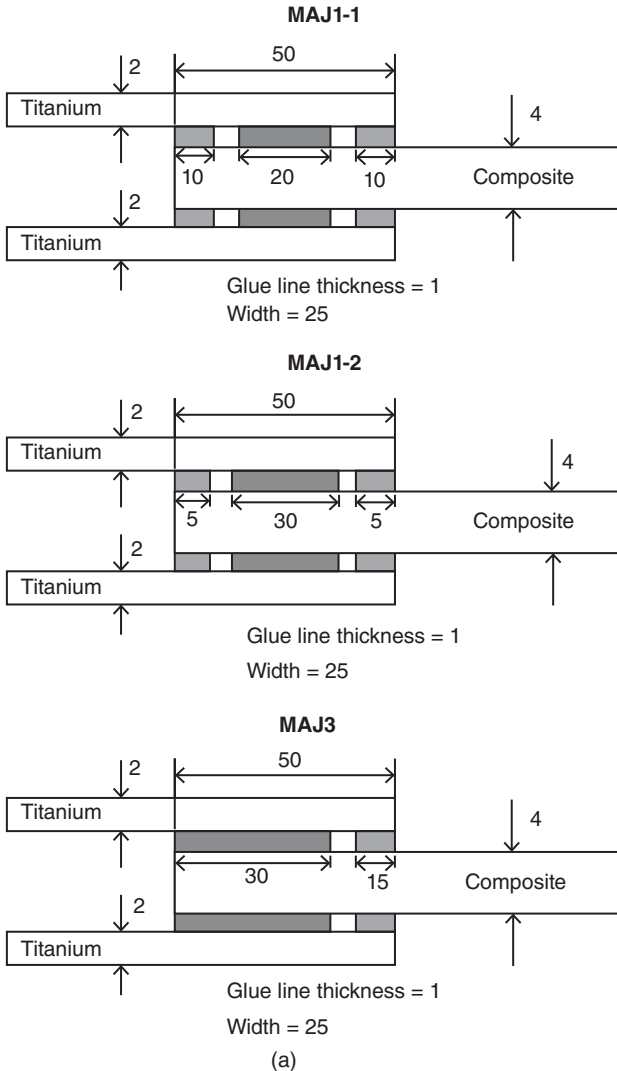


Figure 2.5 Hybrid joint configurations tested (a) and yielding load FE predictions for the titanium–composite joints (b). (Source: da Silva and Adams 2007 [7]. Reproduced with permission of Elsevier.)

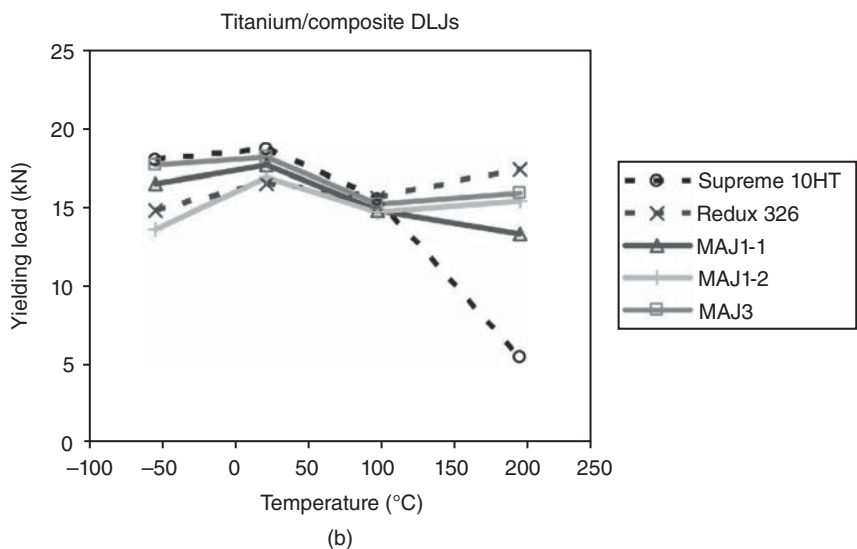


Figure 2.5 (Continued)

Although these methods are available for quite a long time, only more recently these were applied to hybrid structures. Despite this fact, this is still an innovative field under intense development, regarding more accurate modeling techniques, reliable and simple material parameter estimation methods, increase of robustness and elimination of convergence issues [43]. The damage modeling techniques can be either local or continuum-based. Within local damage modeling, damage occurs in a zero-volume line or surface (2D or 3D analysis, respectively), simulating an interfacial failure between materials, for example, between the adhesive bond and the adherend, the interlaminar fracture of laminated composites, or the interface between solid phases of materials. By continuum modeling, the damage extends over an area or volume (2D or 3D analysis, respectively), to simulate a bulk failure or to model a cohesive fracture of the adhesive [1].

Cohesive Zone Modeling The computer implementation of linear elastic fracture mechanics (LEFM) methods had great success some decades ago, but these were limited to small-scale yielding beyond the crack tip. In addition, modern toughened adhesives develop plastic zones under load that can be larger than the adherends' thickness. CZM were developed in the late 1950s/early 1960s [44] to describe damage under static loads at the cohesive process zone ahead of the apparent crack tip. CZM were then improved to simulate crack initiation and propagation in cohesive and interfacial failure problems or composite delaminations. CZM can rely on spring or more conventionally

cohesive elements, and they can be easily incorporated in conventional FE analyses to model the fracture behavior in various materials, including adhesively bonded joints [1]. The main concept of CZM is that one or multiple fracture interfaces/regions can be artificially introduced in structures, in which damage growth is allowed by the introduction of a possible discontinuity in the displacement field. This is made possible by the definition of traction–separation laws to model interfaces or finite regions. The CZM laws are enforced between paired nodes of cohesive elements, and they can connect superimposed nodes of elements representing different materials or different plies in composites, to simulate a zero-thickness interface (local approach), or they can be applied directly between two noncontacting materials to simulate a thin strip of finite thickness between them, for example, to simulate an adhesive bond (continuum approach). CZM thus provide a macroscopic reproduction of damage along a preestablished path by the traction–relative displacement response between paired nodes along the crack path, which relies on large-scale parameters that rule the crack growth process such as the fracture energies. The strength evolution and softening up to failure are modeled, to account for the gradual degradation of material properties.

More details on the 3D CZM formulation implemented in Abaqus®, representative of typical laws used within the scope of bonded structures, can be found in the work of Campilho *et al.* [45]. This methodology is being used for some time for bonded structures, including topics of general strength prediction, geometry optimization, delaminations in composite structures, impact events, aging of materials, and was more recently applied to hybrid structures, in an attempt to simulate with detail the different damage events possible to occur in these structures, especially for FML structures.

Rudawska [4] evaluated the tensile strength of bonded structures between similar and dissimilar adherends, considering both experimentation and CZM for fracture prediction. The testing program regarding the hybrid structures involved aramid/epoxy composite adherends bonded with titanium or aluminum sheets. The simulations were run based on geometric nonlinearities to account for the unbalance and corresponding joint deflections with an incremental-iterative Newton–Raphson solver, while the adhesive layer was modeled by cohesive elements for fracture prediction. The meshes were built with element size grading toward the ends of the overlapping regions, to account for the marked stress gradients. Furthermore, to allow reduction of the number of elements, the adhesive layer and adherend meshes were not matched, which made necessary to apply surface-base tie constraints between the neighboring parts. Comparing the aramid/epoxy–titanium joints with nonhybrid joints made of aramid/epoxy and titanium, the hybrid solution improved the joint strength over the joint for which the lower value of strength was obtained. Conversely, the aramid/epoxy–aluminum joints showed opposite results, as its strength was similar to the smaller strength

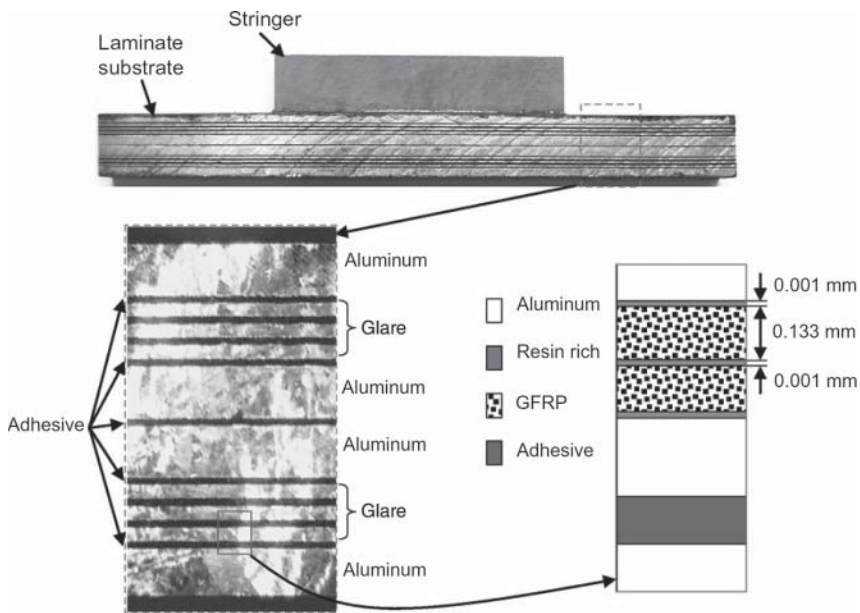


Figure 2.6 A hybrid reinforced specimen of aluminum–Glare. (Source: Sugiman *et al.* 2011 [25]. Reproduced with permission of Elsevier.)

between two corresponding single-material joints (i.e., the aluminum). The FE analyses allowed plotting the stress distributions within the adhesive layer, to provide an insight of the differences between joint configurations, and showed the accuracy of the CZM technique in predicting the joint strengths under various material conditions.

The experimental and numerical study of Sugiman *et al.* [25] reported the static mechanical response of bonded aluminum–Glare structures reinforced by stringers on one of the panel faces (Figure 2.6), as a preliminary testing program for their certification as aeronautical panels by Airbus. The adhesive FM 73 M by Cytec was used. The tested coupons had the Glare fibers either parallel to the load (spanwise configuration) or orthogonal (chordwise configuration). Because of fabrication limitations regarding the maximum sheet dimensions, inevitably Glare sheets had to butt together somewhere along the laminate length, which clearly represent a weak spot and potential locus of crack initiation.

As a result, their positioning was evaluated along the panel. The experimental results showed a higher strength for the spanwise specimens with no butt, followed by the chordwise specimens with no butt, with butt at nearly the mid span of the stringer, and finally, the worst results were found for the specimens with butt adjacent to the stringer ends, where stress concentrations

occur. FE models were built to predict the test data, considering damage modeling for fracture in the composite layer and composite/aluminum and aluminum/aluminum bondlines. Final failure of the aluminum was not equated, although the FE models accounted for plastic dissipations. The Hashin criterion was used for initiation of damage in the composite layers, while damage evolution was predicted by the fracture energy. To model damage growth in the bondlines, both longitudinal and butt, a triangular CZM was employed, whose property characterization was performed by fracture tests. Mixed-mode damage conditions were ascertained by a quadratic stress criterion for damage initiation and the Benzeggagh and Kenane criterion [46] for final failure. The 2D FE models used 4-node plane strain elements for the aluminum layers and the stringers, and 4-node cohesive elements for the adhesive layers and resin-rich regions (Figure 2.6). The FE results showed a generally good correspondence to the test data on the failure load and damage events.

Figure 2.7 shows two examples of FE-test data comparison: chordwise specimens without butt joints (a) and chordwise with butt near the mid span of the stringer (b). The correlation was quite accurate on all of the tested configurations. The strength of the spanwise and chordwise specimens without butts was ruled by the Glare layer failure. For specimens with butts, the butts controlled the ultimate strength. In the end, the tested CZM technique was successfully applied to the hybrid structures, showing accurate failure load and damage event predictions. Sugiman and Crocombe [47] extended the previously mentioned work by considering the fatigue response of the reinforced FML laminates of Figure 2.6 and also metal-laminate doublers bonded to T-panels. CZM techniques were undertaken, including damage in the adhesive layers, the butt joints between FML plies, and the metal and fiber plies. Static modeling was performed in a similar manner to [25], while fatigue modeling was accomplished by applying the maximum fatigue load statically in consecutive increments, each one of these increments representing a block of cycles among the total number of cycles to be simulated. Incrementally, the properties of the adhesive CZM elements were degraded by application of a fatigue damage law with increasing sustained cycles until the materials could not sustain the applied load any more, giving final failure. To model fatigue damage in the continuum elements of the aluminum plies, the reported approach was applied to their elastic properties. Predicted data included the load-life curves and failure mechanisms, which were consistent with the test data. In the bonded doublers, the butt has shown to influence the static and fatigue responses of the structure. In the hybrid FML, considering both spanwise and chordwise fiber orientations, the most critical butt position was near the doubler ends. For the test coupons without butts, the static failure was ruled by Glare plies failure, while the fatigue strength was precipitated by failure in the aluminum sheet.

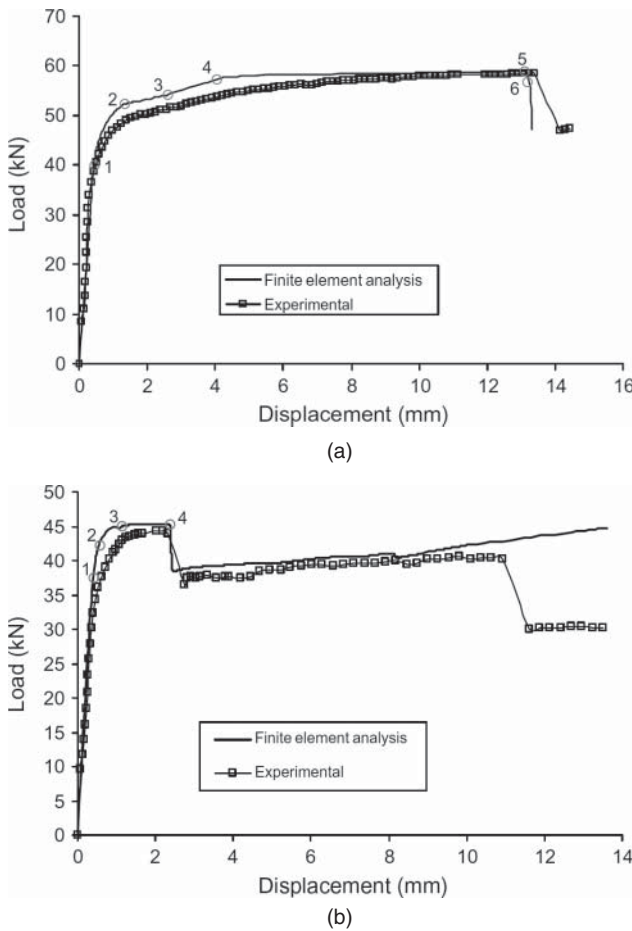


Figure 2.7 Experimental and FE P – δ curve chordwise specimens without butt joints (continuous fiber layers) (a) and chordwise with butt near the mid span of the stringer (b). (Source: Sugiman *et al.* 2011 [25]. Reproduced with permission of Elsevier.)

Alternative Approaches to Cohesive Zone Modeling This section describes alternative approaches to CZM techniques (ACZM), but that can be categorized under the damage mechanics framework by depreciation of the elastic properties of continuum-based elements. These techniques are based on damage parameters to modify the constitutive response of materials by reduction of stiffness or strength, to represent the severity of material damage uptake at it is being loaded (either precracking damage or crack growth) [48]. The damage variables can be categorized under two main groups: (1) variables that predict the amount of damage by the redefinition of the

material constitutive properties but that do not directly relate to the damage mechanism, and (2) variables linked to the physical definition of a specific kind of damage, such as porosities or relative area of microcavities. These techniques also permit different damage mechanisms, each of them by an independent damage variable. The damage growth is based on the load for static modeling or the cyclic count for fatigue simulations.

ACZM were also applied to model constant- and variable-amplitude fatigue [49]. For bonded joints, either hybrid or not, little work is published in this field. Compared to fatigue CZM, ACZM techniques suffer from not providing a clear distinction between fatigue initiation and propagation phases, although they can give a basis for the predictive analysis. On the other hand, damage modeling with fatigue CZM requires predefined crack paths before the analysis initiation. Thus, if the damage is widespread in the materials or the failure paths are not known, ACZM is recommended.

Yaghoubi and Liaw [21] published an experimental and damage-based numerical work on the ballistic impact strength of Glare FML beams of different thicknesses. Experimental testing initially allowed obtaining accurate footage from a high-speed camera to measure the impact and residual/rebound velocity, but also gave detailed data on the damage progression in the FML panels. The ballistic limit velocity, defined as the velocity required for a projectile to perforate the target, was calculated from the impact and residual velocities using an empirical law for further processing and validation with the FE results. It was found that the interfacial debonding and bending and stretching in the aluminum layers played significant roles in dissipating the impact energy in the FML beams.

A 3D FE analysis was carried out in LS-DYNA, used to model and validate the experimentally obtained results on the impact and residual projectile velocities, damage mechanisms, and bullet residual lengths. An initial analysis allowed the selection of the mesh parameters because of the stress dependency on the element size. Thus, the FE model was optimized with the available computer power to provide relatively insensitive results, since the proposed damage model used strain failure criteria. In addition, only a quarter-symmetric model was considered, with a finer mesh at the impact region. The plastic behavior of the aluminum layers and the copper bullet were approximated by the simplified Johnson–Cook material model, which ignored thermal effects and damage. Damage in the glass layers was modeled with the Chang–Chang criteria (linear-orthotropic material model with brittle failure), accounting for three failure possibilities: matrix cracking, compressive, and fiber breakage failures. A strain-based criterion was then used to detect failure and element erosion, that is, when any of the criteria was met at a given element, it was eroded and removed from the calculations. Comparisons of the ballistic limit velocity were quite close. Figure 2.8 reports the FE-test data postimpact damage comparison for a 2/1 (2 layers of aluminum/1 layer of glass composite) hybrid plate and

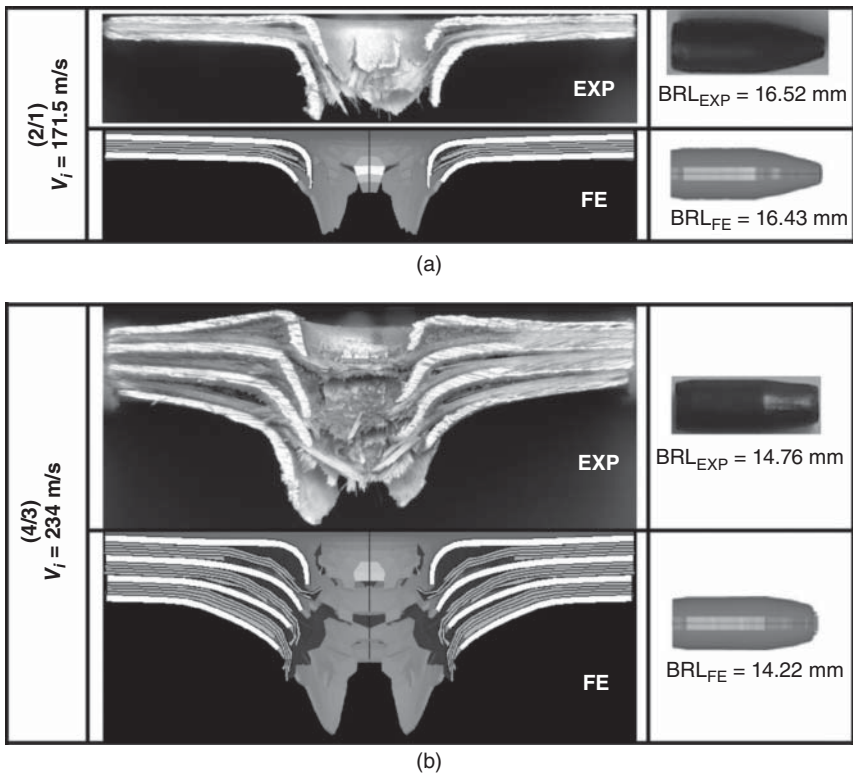


Figure 2.8 FE-test data postimpact damage comparison for 2/1 (a) and 4/3 hybrid plates (b). (Source: Yaghoubi and Liaw 2012 [21]. Reproduced with permission of Elsevier.)

initial projectile velocity of 171.5 m/s (a) and a 4/3 (4 layers of aluminum/3 layer of glass composite) hybrid plate and initial projectile velocity of 234 m/s (b).

A good correspondence was obtained in all tested scenarios, although the broken fibers that appear in the experiments around the impact area are not visible in the FE predictions because of the element deletion-based damage criteria. The bullet residual lengths, that is, after perforation, were also compared (Figure 2.8) with a good match between both analyses. After validation of the FE data, the penetration resistance force versus time plots were analyzed and correlated to the damage events, allowing full characterization of the process.

2.5 Conclusions

The available adhesive bonding techniques between composite materials and lightweight metals were described in detail, giving a perspective of the state

of the art on hybrid bonds and FML. Potential advantages of using hybrid joints over conventional bonds (between identical materials) are the weight reduction, increase of structural efficiency, improved behavior over a wide range of temperatures, and increase of the damage tolerance (static and fatigue) and of the impact resistance. Discussion of fabrication and testing issues for these structures showed important principles for correct procedures (some of these common to conventional adhesive bonds, while others were specific to hybrid bonds), all of these necessary to produce a high-quality bond. Strength prediction techniques are available for these structures, although modeling is more difficult than conventional joints, because of the increase in number and complexity of the failure mechanisms. Between the addressed predictive techniques, CZM is probably the most suited to hybrid joint analysis, provided that the failure mechanisms prone to occur can be defined for a specific geometry. Actually, CZM can provide complete and accurate damage characterization, but it requires the knowledge of the damage sites, to properly define the numerical models. Alternative damage mechanics models can be recommended over CZM if damage is more widespread or if the damage sites are not known beforehand, bearing in mind that this technique is not as accurate as CZM. Analytical and continuum FE modeling are another possible solution, coupled to stress/strain criteria for strength predictions, although these suffer from mesh dependency. Fracture-based methods and XFEM are not commonly used for these structures: in the former case, due to modeling complexities and limitations of the method itself; for the XFEM, this is a very recent technique that is yet to be explored for this particular scenario.

List of Abbreviations

2D	two-dimensional
3D	three-dimensional
CTE	coefficient of thermal expansion
FE	finite elements
FML	fiber metal laminate
GPE	generalized plane strain
HTA	high temperature adhesive
IA	interadherend
LEFM	linear elastic fracture mechanics
LTA	low-temperature adhesive
LVDT	linear variable differential transformer
PE	plane strain
PS	plane stress
VCCT	virtual crack closure technique
XFEM	eXtended finite element method

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3

Friction Spot Joining (FSpJ)

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3.1 Introduction

The requirements of modern society have changed over the centuries and particularly in the last few decades. Reliable and safe structures with high performance, new energy sources, and environmentally friendly, sustainable products and processes are some of the demands in this new era. In addition, as a result of globalization, rapid transportation has become a major requirement with the need for travel and the export of goods and services around the world. Revolutionary transportation concepts such as frictionless vehicles, flying cars, superfast trains, and supersonic aircraft have been designed or their development proposed for future generations. Furthermore, environmental protection is now a major concern. Reduction in emissions of greenhouse gases (such as carbon dioxide), particularly from vehicles by weight saving and leaner fuel consumption, helps circumvent negative environmental impacts. Such requirements and concerns have motivated scientists and engineers in scientific communities and industrial sectors to design lightweight yet mechanically strong and reliable structures. Lightweight structures are nowadays increasingly in demand for a wide range of engineering applications, such as the transport industry [1–3], wind power [4], and bridge construction [5]. Especially in the transport industry, where energy efficiency is required, the use of high-performance polymers and their respective composites, such as carbon-fiber-reinforced polymers (CFRPs) and glass-fiber-reinforced polymers (GFRPs), has been emerging in the design of hybrid, multimaterial structures. This is primarily due to their inherent capacity to reduce the weight of an engineering structure, such as an aircraft or car. In addition to the lightweight character of composites, they possess

outstanding corrosion resistance, environmental stability, high strength and high fatigue performance, making them attractive for a variety of industries [6, 7]. Furthermore, advanced lightweight metals such as aluminum, titanium, and magnesium alloys are being further developed to reduce the weight of a structure while maintaining high mechanical performance. The development and employment of different materials with a diverse range of properties helps designers to select the right combination of materials to fulfill the required properties of the desired structure [6]. New-generation aircraft, for example, Boeing 787 Dreamliner and Airbus A350 XWB, are examples of large multimaterial structures. In both aircraft, approximately 50 wt% composites are mixed with 50 wt% lightweight metal alloys such as aluminum [8, 9].

Moreover, automotive manufacturers intend to use more polymers and composites in the bodies of their cars [10–14]. Recently, it was stated that by 2030 in the United States, polymers and composites will be the primary materials used by the automotive industry to fulfill its demands [12]. Thus, more material mixing is expected in automobiles in the near future. The Mercedes-Benz F125 research vehicle [15] is an example of a new concept under development for the year 2025 and beyond. The car will consist of metal–polymer hybrid structures to reach its goal of emission-free mobility.

Despite the benefits of using different lightweight materials in a structure, the joining of dissimilar materials such as metal alloys and composites presents a great challenge due to their distinct physicochemical properties [2]. Amancio-Filho and dos Santos [16] have categorized various joining technologies for metal–polymer hybrid structures; these range from more conventional adhesive bonding and mechanical fastening to new welding-based technologies. Nevertheless, traditional bonding and mechanical fastening exhibit technological and environmental limitations. For instance, the curing time of an adhesive for structural adhesive bonding is a major shortcoming. In addition, bonding of thermoplastic composites requires special pretreatments to increase the intrinsic adhesion between the composite and the adhesive to improve the wettability and surface tension of the thermoplastics [17]. Problems in the mechanical fastening of metal–composite joints have also been reported for the Airbus A380 [6]. During the development phase of the A380 wing, composite ribs were joined to the metal skin by bolting on aluminum brackets. During the operation, some cracks appeared in the brackets, which could have led to failure of the entire component. Such technical limitations have motivated recent investigations into alternative and advanced joining technologies suitable for hybrid structures, to overcome or reduce the drawbacks of traditional techniques.

Friction spot joining (FSpJ) is an alternative joining technology for producing metal–composite joints, patented by and developed at Helmholtz-Zentrum Geesthacht, Germany [18]. As the name implies, FSpJ belongs to friction-based joining and welding technologies. The new technology was developed as a

variant of the friction spot welding (FSpW) of metals [19], polymers [20], fiber-reinforced polymer composites [21], and nanocomposites [22]. This chapter aims to address the fundamental scientific and technological aspects of the FSpJ process. Examples of different combination of materials are given to address the various features of the joints. Emphasize is given to aluminum alloy AA2024 and carbon-fiber-reinforced poly(phenylene sulfide) (CF-PPS) laminated composites due to their applications in transport industries.

3.2 Principles of the FSpJ

3.2.1 FSpJ Tool

In the FSpJ process, a nonconsumable tool consisting of three parts is used to generate frictional heat. The tool includes a clamping ring, sleeve, and pin, which are mounted coaxially and can be moved independently of each other (Figure 3.1). The clamping ring is the external component of the tool that is used to hold the parts to be joined together during the process against a backing bar. The pin and sleeve can move vertically and rotate independently. They produce the required heat as a result of the friction between these parts and the metal [24].

3.2.2 FSpJ Equipment

There are at present different companies producing joining equipment for FSpJ [25–27]. Currently available equipment to produce FSp joints are displacement-controlled machines. These machines were originally manufactured for the FSpW of metals such as aluminum and magnesium. One of the advantages of the FSpJ process is that the same FSpW equipment

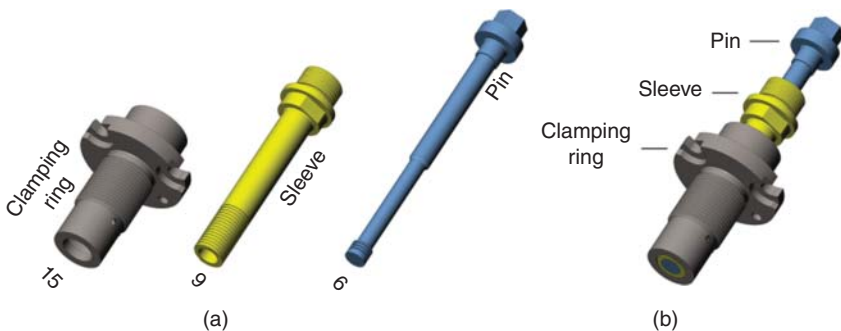


Figure 3.1 Illustration of the FSpJ tool; (a) consisting of three parts (dimensions in millimeters) and (b) tool parts coaxially mounted together. (Source: Goushegir *et al.* 2015 [23]. Reproduced with permission of John Wiley & Sons.)

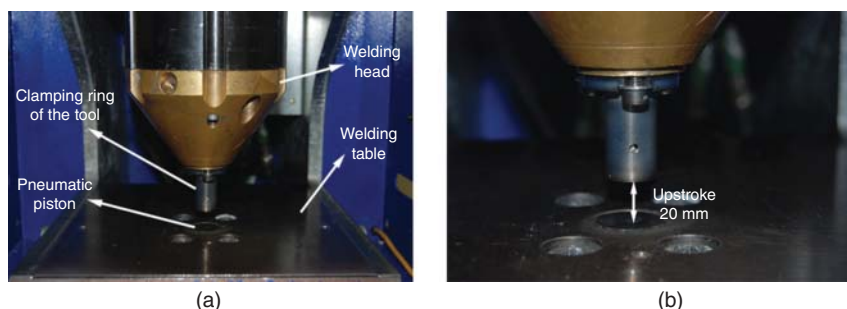


Figure 3.2 (a) A photo of the RPS100 welding head, the assembled tool, welding table, the pneumatic piston and (b) a magnified image of the welding head and table showing the upstroke in the equipment.

already available in the market can be readily used without the need for any modifications. The equipment consists of a welding head, in which the FSpJ tool is assembled, and a pneumatic piston that applies the joining pressure (JP) against the FSpJ tool. In this work, the RPS100 equipment (Harms & Wende GmbH, Germany) was used. The design of the equipment is such that the welding head is fixed and the pneumatic piston applies the JP from below. The piston moves up and down vertically to apply and release the JP. A welding table is used to place the joining parts or a sample holder on. A switch cabinet is attached to the equipment as the main electrical unit to control the rotation and movement of the FSpJ tool parts. Figure 3.2 depicts various features of the RPS100 FSpW/J equipment.

The machine is capable of applying the tool's rotational speed (RS) in the range 500–3300 rpm. The specimen can be cooled down after the joining process by blowing a pressurized air stream, and the airflow can be controlled.

3.2.3 FSpJ Process

Before the process starts, the sheets to be joined are held together in an overlapping configuration on a sample holder, using a clamping device. The parts are kept clamped during the whole process to avoid any separation of the sheets during the cooling phase. This is due to the large differences in coefficients of thermal expansion and shrinkage behavior of metal and polymer. Next, the tool approaches the top sheet (in this case, a metal alloy), and the joining parts are fixed by the clamping ring against a backing bar, through the application of the JP. Figure 3.3 illustrates the configuration of the joining parts before the process starts.

The FSpJ process can be divided into three main steps [24]. There are two possible variants for the FSpJ technique: “Pin Plunge” and “Sleeve Plunge” [24]. In the sleeve plunge variant, the first step of the process starts when the rotating sleeve plunges into the metallic sheet at a predefined position and the

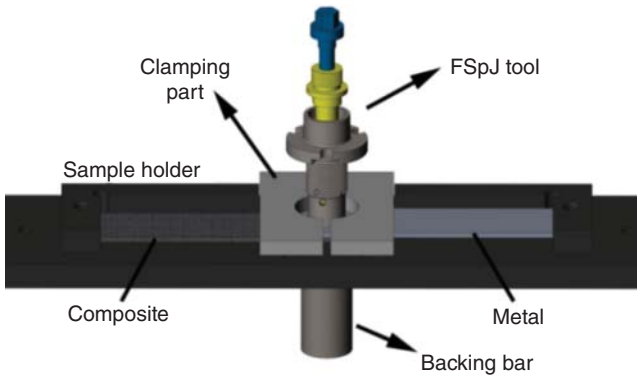


Figure 3.3 Configuration of the joining parts and the FSpJ tool before the process starts. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

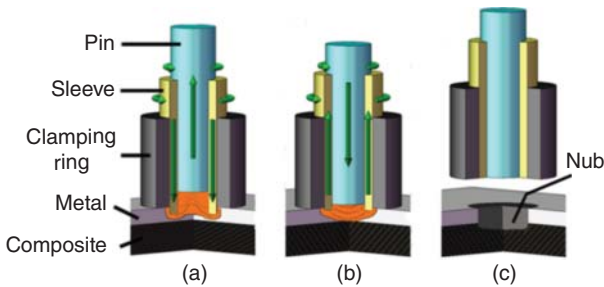


Figure 3.4 Schematic representation of the FSpJ process steps; (a) the sleeve plunging plasticizes the metal, (b) spot refilling, and (c) joint consolidation. (Source: Amancio-Filho *et al.* 2011 [24]. Reproduced with permission of Elsevier.)

pin retracts upward. Due to the friction between the rotating sleeve and the metal, the temperature rises locally in a volume around the tool, but below the melting point of the metal, this causes local softening and plasticizing of the metal alloy. The plasticized metal alloy flows into the reservoir left behind by retraction of the pin (Figure 3.4(a)). As a second step, the pin is pushed against the softened metal to refill the keyhole left in the metallic sheet that was created by the plunging sleeve (Figure 3.4(b)). Finally, the tool is retracted, and the joint consolidates under cooling and pressure (Figure 3.4(c)). Note that the tool only plunges into the metal part to a shallow depth that does not reach the composite interface, to avoid any damage to the load-bearing network of fibers. Figure 3.5 depicts the top view of a sound metal–composite FSp joint.

In the pin plunge variant, the pin penetrates into the metal piece while the sleeve is retracted [24]. The other process steps are equivalent to those for the sleeve plunge variant. As the area of the sleeve is larger than the area of the pin,

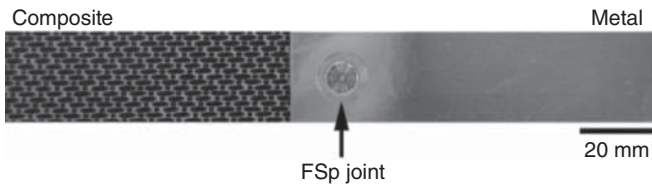


Figure 3.5 Top view of a sound metal–composite FSp joint; (Ex.: AA2024/CF-PPS). (Source: Goushegir *et al.* 2015 [29]. Reproduced with permission of Elsevier.)

it can be assumed that in the sleeve plunge variant, the bonding area is larger, resulting from a higher heat input, and hence, better mechanical performance may be achieved. The sleeve plunge variant is used throughout this chapter to explain the process.

It is worth mentioning here that there are three main differences between FSpJ and FSpW [30]. Firstly, unlike the FSpW of metals and thermoplastics, with FSpJ, the tool only plunges to a shallow depth in the metallic part and does not reach the composite. The reason for this is to avoid excessive degradation of the matrix of composite and damage to its network of load-bearing fibers. Secondly, with FSpJ, the adhesion forces from creation of a molten layer act as one of the main bonding mechanisms. However, atomic diffusion (in metal spot welds) and molecular interdiffusion (in thermoplastic spot welds) are the main mechanisms leading to weld formation. Furthermore, in FSpJ, material mixing does not occur due to the huge physicochemical differences in metal and polymer parts, which usually lead to sharp interfaces.

The spot formed in an FSp joint consists of three concentric rings. Figure 3.6 illustrates a detailed view of the rings that are related to impressions of the FSpJ tool's parts that are left behind on the metal in the spot area. Figure 3.6(a) illustrates the top view of the spot area, whereas Figure 3.6(b) shows its 3D

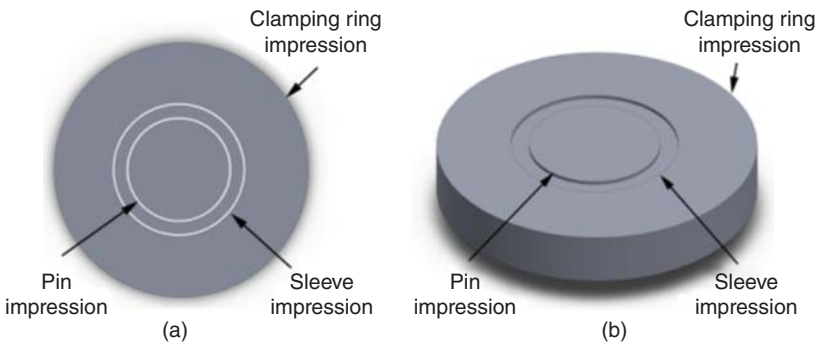


Figure 3.6 Schematic illustration of the spot area showing the impression of the tool parts on the top surface of the metal; (a) top view and (b) 3D view.

view. The outer ring corresponds to the impression left by the clamping ring, the middle ring is associated with the sleeve, and the inner ring appears because of the pin's impression. However, it is not always possible to distinguish the pin and sleeve impressions, and one ring is likely observed as a result of materials mixing below the sleeve and the pin as shown in [24].

During the joining process, high temperatures around the tool increase the local formability of the metal. For this reason, the plasticized metal is deformed by the plunging motion of the sleeve and the pin, and it creates a geometrical undercut in the form of a “nub” [24]. As a result of the axial force exerted by the tool, the nub is slightly inserted into the composite, and this increases mechanical interlocking between the joining parts. Figure 3.7 shows the cross section of an FSp joint in the middle of the spot, where the nub is indicated by a black rectangle. A reservoir is created in the center of the nub where the composite is entrapped and accommodated inside. The nub reservoir is also indicated in a 3D graphical view of the spot area in Figure 3.8 (without the composite partner to aid visualization).

The shape of the nub depends not only on local formability of the metal at high temperature, but also on the properties of the composite beneath the metal. Generally, the nub is not so pronounced in the case of a continuous fiber-reinforced composite due to high stiffness of the network of fibers. In

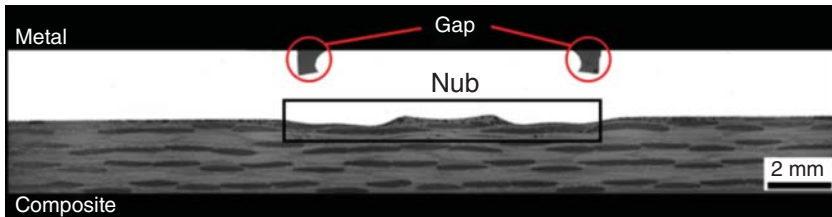
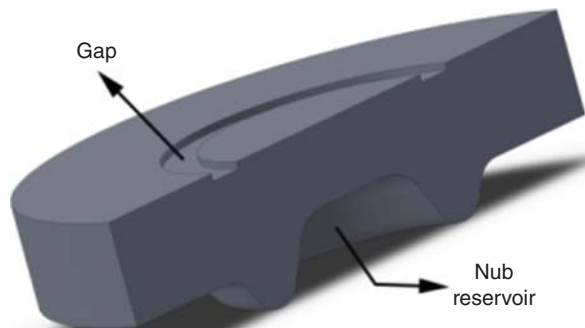


Figure 3.7 Example of a cross section of an FSp joint in the middle of the spot. The rectangle indicates the nub, and the red circles show the gap formation below the plunging sleeve; (Ex.: AA2024/CF-PPS). (Source: Goushegir *et al.* 2016 [31]. Reproduced with permission of Elsevier.)

Figure 3.8 Schematic illustration of the cross section of the middle of the spot area. A nub reservoir is formed that accommodates the molten polymer. The composite partner is omitted for better visualization. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)



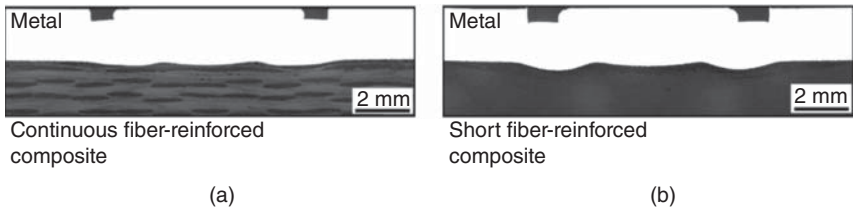


Figure 3.9 Comparison of the nub shape in case of (a) a continuous fiber-reinforced composite and (b) a short fiber-reinforced composite; (Ex.: AA2024/CF-PPS).

addition to stiffness of the fiber network, a large volume fraction of continuous fibers in the composite makes it difficult for the metal to displace the fiber bundles and penetrate the composite. In contrast, using an unreinforced polymer or a polymer reinforced with short fibers and lower local stiffness leads to a more noticeable nub. Figure 3.9 shows a cross section of two FSp joints, comparing the shape of the nub when using (a) continuous-fiber-reinforced composite and (b) short-fiber-reinforced polymer. In both cases, the joining parameters, the metal alloy, and the matrix of the composites are the same; the only difference is the use of continuous and short fibers. It is clear from these cross sections that using a less stiff short-fiber-reinforced composite leads to a larger nub. This observation was also reported by Amancio-Filho *et al.* [24] for FSpJ of magnesium alloy with continuous- and short-carbon-fiber reinforced composite.

Another feature that can be observed in an FSp joint is the formation of a gap below the sleeve (between the pin and the sleeve impressions), as can be seen in Figures 3.7 and 3.8. The gap is formed on the surface of the metal below the plunging sleeve, because a portion of the metal mass is displaced into the composite at the interface in the nub region. The mass displacement occurs because of the increased local formability of the metal, induced by the high temperature and axial force applied by the tool. This means there is insufficient material to refill the gap completely that is created below the plunging sleeve. It should be noted, however, that the extent of the gap depends very much on the shape of the nub. If the nub is not inserted deeply into the composite, more material is available to refill the gap. Figure 3.10 shows the cross section

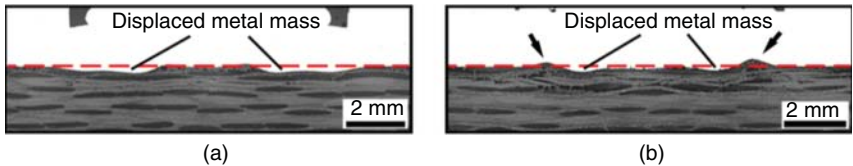


Figure 3.10 Comparison of the gap formed on the surface of the metal with a PD of (a) 0.8 mm and (b) 0.5 mm. The dashed lines display the midplane between metal and composite before the joining process. The arrows in (b) indicate the two bumps helping the keyhole filling; (Ex.: AA2024/CF-PPS).

of two joints, (a) where the gap is pronounced as a result of large metal mass displacement into the composite and (b) where there is almost no gap present on the surface of the metal, because less metal is displaced into the composite and the keyhole is completely filled. Horizontal dashed lines in the figure illustrate the midplane before joining, that is, the original level of the metal at the interface with the composite (and outside the nub region). All the white parts under the lines show displaced metal into the composite. Further, the two small bumps (indicated by arrows) in Figure 3.10(b) that formed at the sides of the nub help the keyhole filling. Usually, large tool plunge depth (PD) leads to the displacement of more metal mass and hence formation of the gap on the top surface of the metal. In Figure 3.10(a), a PD of 0.8 mm was used in comparison with Figure 3.10(b), where the PD was 0.5 mm.

3.2.4 Bonding Mechanisms

In the course of the joining process, the frictional heat generated is transferred from the metal alloy to the composite interface via conduction. Resulting from the very low thermal conductivity of the composite, this heat is accumulated at the interface and leads to a local increase of the temperature. Since the temperature exceeds the melting or glass transition temperature of the composite's matrix, a thin layer of the molten or softened polymer forms in the spot area. A part of the molten layer is squeezed out and flows laterally throughout the overlap region as a result of the axial pressure exerted by the tool. The molten layer is then consolidated under pressure, whereby it induces adhesion forces between the metal and the composite. The total bonding area in the FSpJ corresponds to the outer periphery of the consolidated molten layer, as shown in Figure 3.11. Figure 3.11(a) depicts the presence of adhesion forces between metal and composite inside the bonding area. A fracture surface of

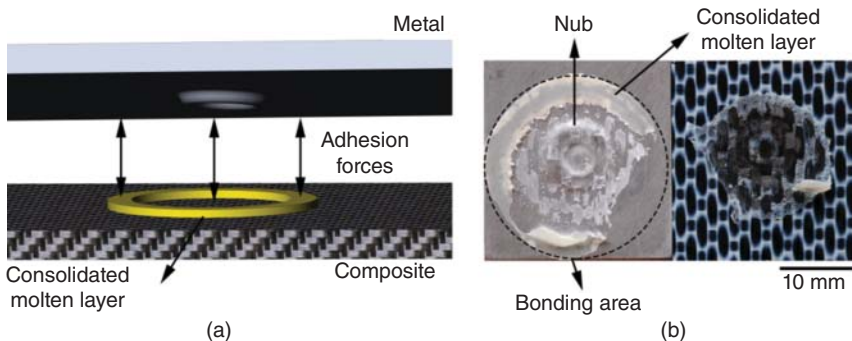


Figure 3.11 (a) Illustration of the adhesion forces as one of the primary bonding mechanisms in FSpJ and (b) an example of the fracture surface of an FSp joint tested by lap shear; (Ex.: AA2024/CF-PPS). (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

the joint after lap shear testing is also illustrated in Figure 3.11(b) where the total bonding area is indicated by the dashed circle on the metal part.

“Mechanical interlocking” and “adhesion forces” are the primary bonding mechanisms in the friction spot joints. Both these bonding mechanisms can be further subdivided. Mechanical interlocking occurs on both the macro and micro levels. Macromechanical interlocking is a result of polymer entrapment in the formed nub. Micromechanical interlocking may also occur. Surface pretreatments have been used frequently to increase the surface roughness and to generate pores, crevices, and asperities. Penetration of the molten polymer into such micro-scale metal irregularities leads to the micromechanical interlocking phenomenon. Furthermore, with FSpJ, some of the fibers in the composite are entrapped by the metal, particularly in and around the nub region where the metal is plasticized and slightly deformed. Fiber entrapment is also classified under the micromechanical interlocking category. Adhesion forces can also be divided into physical and chemical bonding. Physical adsorption and bonding (as a result of weak van der Waals forces) are believed to be present in FSpJ where the molten polymer is in intimate contact with the metal. In addition, strong chemical adsorption and bonding may occur between the molten polymer and the joining parts if the surface of the metal and/or composite is activated by suitable surface pretreatments [23].

A simple model was proposed to discuss the bonding zones and failure mechanisms in FSpJ [30]. As illustrated in Figure 3.12(a), three zones can be identified in the fracture surface of an FSp joint. The first zone corresponds to the area in which the mechanically deformed nub is present and the process temperature was high enough in the region to produce a thin molten polymer layer. This zone is called plastically deformed zone (PDZ). The PDZ is believed to be the strongest part of the joint, as there is intimate contact between the composite’s matrix and the carbon fibers with the plasticized metal volume.

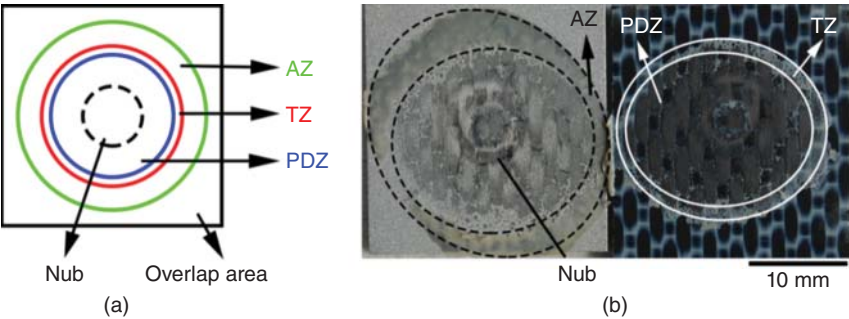


Figure 3.12 (a) Schematic fracture surface of an FSp joint and (b) real fracture surface of an AA2024/CF-PPS FSp joint, indicating three bonding zones; plastically deformed zone (PDZ), transition zone (TZ), and adhesion zone (AZ). (Source: Goushegir *et al.* 2015 [29]. Reproduced with permission of Elsevier.)

Most of the failure mechanisms, such as cohesive failure inside the composite and partial fiber breakage, occur in this region. The outer region is the zone in which the flowing molten polymer is consolidated in a few seconds as a result of the very fast cooling rates (CRs). This zone is called adhesion zone (AZ). There is also a transition zone (TZ) identified next to the internal border of the consolidated layer. Due to the low viscosity of the flowing molten polymer during the joining process, air bubbles remain entrapped in this layer. Usually, these three zones are not homogeneously distributed around the nub as shown in Figure 3.12(b), which may be an indication of inhomogeneous heat distribution in the metal alloy.

As explained, mechanical interlocking in FSpJ is divided into “macromechanical interlocking” and “micromechanical interlocking.” Macromechanical interlocking is only the result of nub insertion into the composite inside the PDZ. By contrast, micromechanical interlocking occurs throughout the bonding area in all three zones. The polymer molten layer flows into porosities and asperities on the surface of the metal alloy, creating micromechanical interlocking. Another subcategory of mechanical interlocking is fiber entrapment by the metal. Some of the carbon fibers become embedded in the metal inside the PDZ, by which further micromechanical interlocking is achieved. Figure 3.13(a) and (b) illustrate the cross section of a joint examined by laser scanning confocal microscopy (LSCM) showing the micromechanical interlocking phenomenon by metal pore and crevice filling by the molten polymer and fiber entrapment in the PDZ. High magnification scanning electron microscopy (SEM) imaging (Figure 3.13(c)) shows that in some cases, a layer of molten polymer flows between the carbon fibers and the metal. Although direct contact between the metal and carbon fibers may improve mechanical performance of the joints by improved micromechanical interlocking, it could nonetheless prove detrimental to its corrosion behavior when exposed to a corrosive environment. Therefore, the presence of such a layer may in fact reduce the rate of corrosion.

3.2.5 Process Parameters

Similar to the FSpW of metals [32, 33], there are four primary process parameters in FSpJ [24, 29, 34]: RS of the plunging sleeve, sleeve PD into the metal alloy, joining time (JT), and JP. Further important process parameters are: clamping pressure (CP), when the joining parts are clamped to avoid separation in the cooling phase, and CR at the end of the joining process. The process parameters affect the bonding mechanisms, microstructure, and therefore the mechanical performance of the joints. RS controls the heat input and temperature evolution during the joining cycle. Due to the temperature changes, the local microstructure of the metal part is influenced by the process. Furthermore, the amount of molten polymer generated is directly related to the heat input and

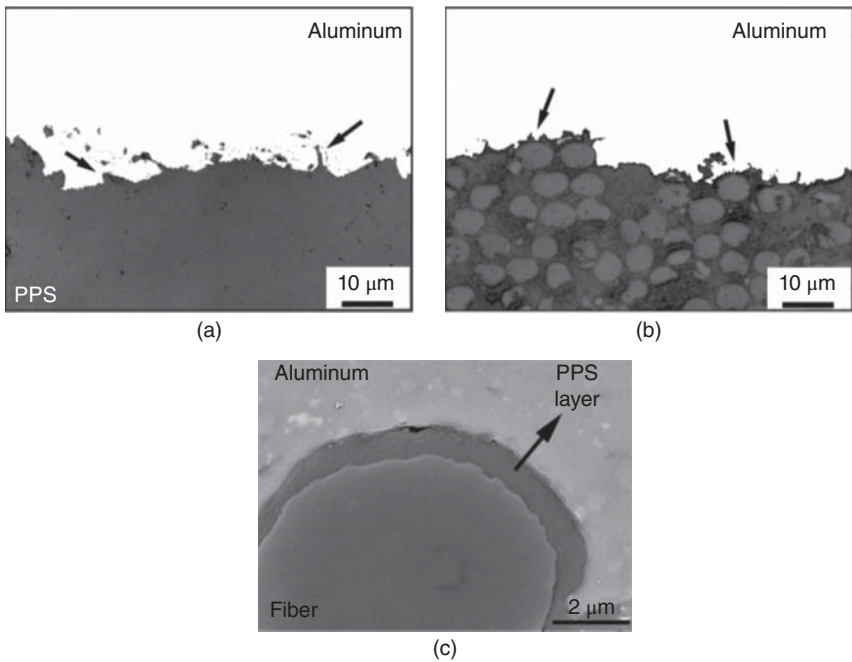


Figure 3.13 Cross section of an AA2024/CF-PPS FSp joint in the PDZ examined by LSCM showing micromechanical interlocking through (a) aluminum pore/crevice filling by the molten PPS in a resin-rich area and (b) fiber entrapment. (Adapted from Ref. [30].) (c) High-magnification SEM image showing a thin layer of the PPS between aluminum and carbon fibers. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

hence the RS of the sleeve. In addition, the viscosity of the molten polymer, its rheological behavior as well as the formation and extent of defects in the composite are all temperature-dependent phenomena that are influenced indirectly by RS. JT, which includes sleeve plunging time and retraction time, governs the joining speed. It also contributes to the amount of the heat input into the process and the extent of temperature development around the tool. Similar to RS, the aforementioned temperature-dependent phenomena are also affected by JT. Another important function of RS and JT is to indirectly control the shape of the metallic nub, since the generation of more frictional heat increases the local formability of the metal. PD of the sleeve has a forging function and is mainly responsible for the shape and inserted depth of the nub into the composite. Higher PD usually leads to a deeper indentation of the deformed metal into the composite, thus better macromechanical interlocking between the parts, as we reported recently in our manuscripts [29, 34]. JP is a parameter with the main function of controlling the flow of the molten polymer. An exerted axial force on the spot area as a result of the applied JP influences the lateral flow of molten polymer. The combination of heat input and JP influences

the flow and the bonding area as explained in [29]. In addition, an intimate contact between the molten polymer and the metal at the interface depends on the applied JP. This is especially important when the surface of the metal is rough, for instance, resulting from surface pretreatment. Complete filling of asperities in the metal surface by the molten polymer by applied JP leads to an increase in micromechanical interlocking and hence mechanical performance of the joints.

CP is required during and after the joining cycle to avoid separation of the joining parts. However, a very high CP may hinder lateral flow of the molten polymer and thus decrease the total bonding area. CR is another parameter that influences the joint's characteristics. The cooling phase starts after retraction of the tool by blowing pressurized air onto the joining area. CR can influence the crystalline state of the molten polymer in cases of semicrystalline thermoplastics such as poly(phenylene sulfide) (PPS). A very high CR may reduce the crystallinity of the molten polymer or even turn it into an amorphous state, so affecting the local mechanical properties of this layer. It has also been discussed that generation of large residual thermal stresses at the metal–composite interface may be another effect of fast CRs [35]. The CR may also influence generated volumetric defects, since a lower CR gives more time for the entrapped air to escape the molten layer, leading to a reduction of defects. Table 3.1 summarizes FSpJ joining parameters and their respective functions.

Table 3.1 Controllable FSpJ process parameters and their respective functions.

Joining parameter	Symbol	Function
Rotational speed	RS	Influences heat input and temperature evolution, shape of the nub, microstructural changes in metal and composite, formation of defects
Plunge depth	PD	Controls shape of the nub and its inserted depth into the composite
Joining time	JT	Controls the joining speed, extent of temperature development around the tool
Joining pressure	JP	Governs the type of molten polymer flow
Clamping pressure	CP	Controls a firm contact between the joining parts and hinders their separation during the cooling phase
Cooling rate	CR	Influences crystallinity of semicrystalline composites as well as the formation of volumetric defects

The influence of process parameters on the microstructure and mechanical behavior of FSp joints using different combinations of joining parts such as magnesium Mg AZ31/CF-PPS [36], AA5754/CF-PPS [37], AA6181/CF-PPS [34, 35], and AA2024/CF-PPS [29] was thoroughly investigated. Since the effects of process parameters vary depending on the selected base materials (BMs), refer to each individual publication for more information.

3.3 Heat Generation During FSpJ Process

During FSpJ process, the friction between the plunging tool and metal sheet generates frictional heat. Heat input is an important parameter in all solid-state joining processes because it governs the temperature in the joining area and therefore the microstructure, local and global properties of the joints. Equation 3.1 shows a model used for the friction stir spot welding (FSSW) process to estimate the energy input [38]:

$$Q_{\text{applied}} = \sum_{n=1}^N \text{Force}(n)(X_n - X_{n-1}) + \sum_{n=1}^N M(n)\omega(n)\Delta t \quad (3.1)$$

where X is the PD, M is the torque (N m), ω is the rotational speed (rad/s) of the tool, Δt is the JT, and n refers to the experiment number. Assuming that all the mechanical energy is converted into heat input during the process, – a convention generally assumed in friction-based joining processes [39] – Equation (3.1) may be used to better understand the influence of process parameters on the generated frictional heat. Because of the similarities in heat generation between FSpJ and FSSW, this equation can be used to explain the effect of the FSpJ process parameters on the heat generation.

It was explained [29] that according to Equation (3.1), RS and JT directly control the heat input: increases in both of them increases the heat input in the process. It has also been discussed that the increase in temperature as a result of heat generation by solid friction is proportional to the reverse of the thermal conductivity of the material [40, 41]. Due to the low thermal conductivity of the polymers, the conducted heat from the metal to the interface is accumulated in the joint area, leading to a higher temperature in a volume of the composite at the center of the joint. Therefore, a reduction in the viscosity of the generated polymer molten layer is expected. The low viscosity polymer molten layer flows easier and spreads out laterally outward in a larger area under the applied JP. However, after a short time, the viscosity of the flowing molten layer starts to increase (because of the very fast CR after the joining process), which leads to the formation of the TZ before the final consolidation of the layer in the AZ region. In a general trend, the bonding area increases with increasing the process temperature, leading to a higher joint strength [29, 34].

3.4 Microstructural Zones in FSpJ

Microstructural zones form in an FSp joint as a result of the interaction of the tool with the metal, generating frictional heat and a local rise of temperature, and the amount of the heat conducted from the metal to the composite. Generally, the microstructural zones can be divided into four areas: metal stir zone (MSZ), metal thermomechanically affected zone (MTMAZ), metal heat-affected zone (MHAZ), and polymer heat-affected zone (PHAZ).

The microstructural zones depend very much on the type of the selected metal alloy. The generated microstructure may vary between heat-treatable and non-heat-treatable alloys as well as cast metals. To describe the microstructural zones, an example of an AA2024/CF-PPS joint is selected here.

The microstructural zones on the aluminum are very similar to those reported for FSpW of AA2024 [32] and AA6181 [42]. The microstructural zones in FSpJ are shown in Figure 3.14, where Figure 3.14(a) shows an overview of the microstructure, and Figure 3.14(b)–(f) illustrate the detailed microstructure of each zone. Figure 3.14(b) and (c) compare the microstructure of the BM and MTMAZ, respectively. The microstructure of the AA2024 in the MTMAZ is influenced by the relatively high temperature attained during the process as well as severe deformation [32, 42]. This zone forms in a volume close to the plunging sleeve where the aluminum grains are affected by the high strain rate as a result of the tool's rotational speed as well as axial movement [32, 42, 43]. The microstructure in this zone is thus characterized by elongated grains that are rotated approximately 45° compared with unaffected grains. One can see that the grains in MTMAZ are elongated and rotated from top-left to bottom-right (Figure 3.14(c)) compared to the AA2024 BM (Figure 3.14(b)). The induced grain deformation in this zone may contribute to the strain hardening effect [42]. However, since the temperature and strain rate are relatively high in MTMAZ, some dynamic recovery may also occur, which reduces the strain hardening effect [44, 45]. In addition, due to the relatively high temperature achieved in this area, coarsening of the strengthening particles may also take place [46]. Therefore, the local mechanical strength of the aluminum alloy 2024 in the MTMAZ is affected by these competing phenomena (strain hardening, dynamic recovery, and precipitation coarsening).

MSZ lies in the center of the spot that undergoes the highest strain rate as a result of the aluminum being stirred by the tool [42, 47]. The highest frictional heat generation and attained temperature are also expected in this zone [42, 47]. Dynamic recrystallization (DRX) is one the main mechanisms taking place in the MSZ in FSpW of aluminum, which leads to an equiaxed, refined grain structure [32, 42]. DRX occurs in many metallic materials above 0.6 Tm under high stress or high deformation rates [44, 45]. High temperatures obtained during FSpJ of AA2024/CF-PPS [29] in combination with a high strain rate due to the tool's rotational speed result in a very fine grain structure

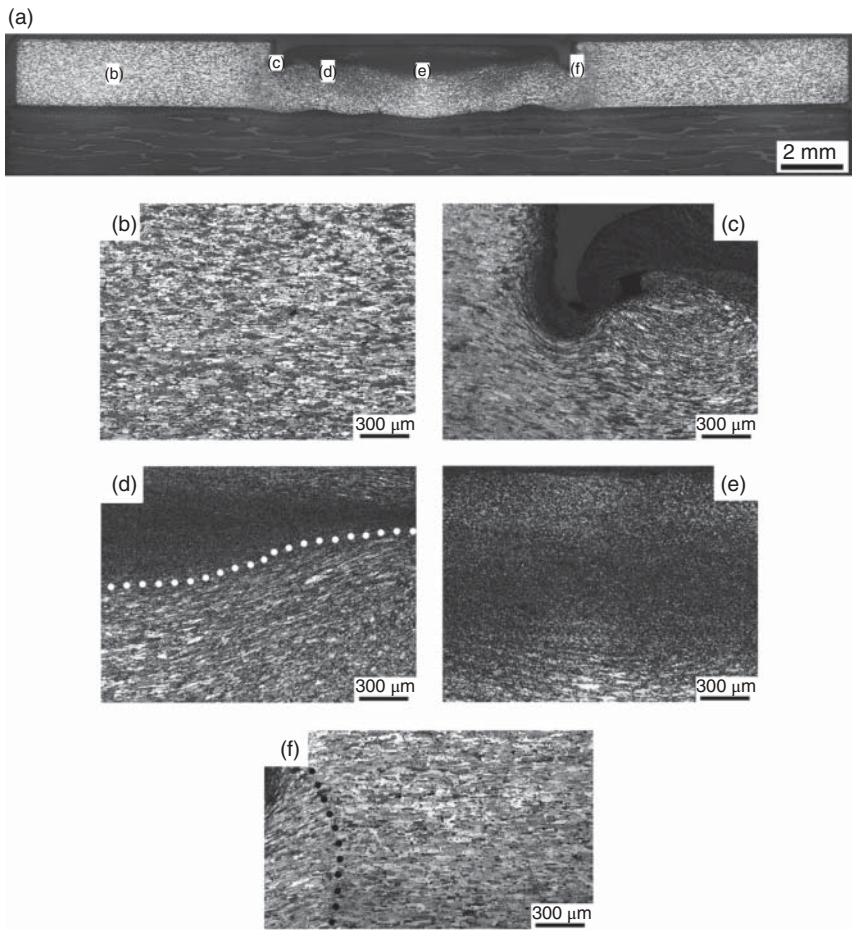


Figure 3.14 Microstructural zones that form in the aluminum. (a) General overview of the joints microstructure, (b) microstructure in the BM, (c) MTMAZ showing elongated grains rotated compared to the BM grains, (d) boundary between MTMAZ and MSZ, (e) MSZ showing very fine DRX grains, and (f) boundary between MTMAZ and MHAZ. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

in the MSZ. Figure 3.14(d) shows the boundary between MSZ and MTMAZ, and Figure 3.14(e) illustrates the MSZ with very fine grain structure. In addition to grain refinement, solubilization of the strengthening particles in the course of the joining cycle is expected in the MSZ as a result of the high temperature. Reprecipitation of these particles during the cooling phase is believed to increase the local strength of the MSZ [42].

MHAZ is the third zone formed during the FSpJ process between the BM and MTMAZ. In this region, the aluminum is not affected mechanically and

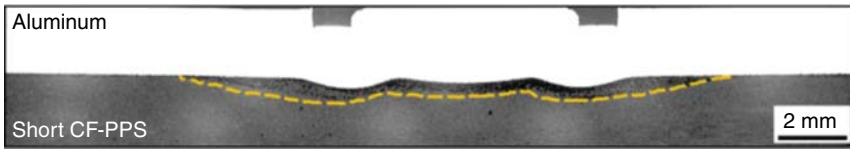


Figure 3.15 Microstructure of an AA2024/CF-PPS joint. Dashed line indicates the boundary between PHAZ and BM.

the temperature is not as high as in the MSZ and MTMAZ. The moderate temperatures achieved in MHAZ may lead to the recovery phenomenon and coarsening of the strengthening particles [42]. Although such metallurgical phenomena may have occurred, no differences in grain structure could be observed between the MHAZ and BM by optical microscopy. Figure 3.14(f) illustrates the boundary between MTMAZ (left part of the figure) and MHAZ (right part of the figure).

In addition to the microstructural zones in the aluminum, a PHAZ was detected in the CF-PPS, as illustrated in Figure 3.15. The dashed line in the figure shows the boundary between the PHAZ and the CF-PPS BM.

The temperature in the PHAZ was reported to be higher than the T_g of CF-PPS as well as being above T_m in the vicinity of the interface [48]. Some of the molten PPS matrix is squeezed out of the nub. In addition, the viscosity of the molten polymer is reduced, which leads to the entrapment of air pockets in the slowly flowing PPS. As the CR is very fast, these entrapped air pockets cannot escape from the PPS and remain as volumetric flaws in the joint. The main type of flaw identified in the PHAZ close to the interface with the aluminum was reported as pores and voids [48]. The formation of voids giving rise to defects as a result of air entrapment is also detected in resistance welding [49] and laser welding [50] of thermoplastics.

3.5 Mechanical Properties of FSp Joints

3.5.1 Local Mechanical Properties

Local mechanical properties in FSpJ are divided into metal properties and composite properties. Microhardness was used to evaluate the local mechanical properties of the joining parts. Similar to the microstructural analysis, an AA2024/CF-PPS joint is selected here to describe the local mechanical properties of the metal alloy. In case of the local mechanical properties of the composite, a joint between Mg AZ31 and short glass-fiber-reinforced PPS is chosen.

3.5.1.1 Metal (AA2024)

Figure 3.16 shows microhardness map of the aluminum part. Due to microstructural symmetry around the center of the spot in FSpJ, only half of

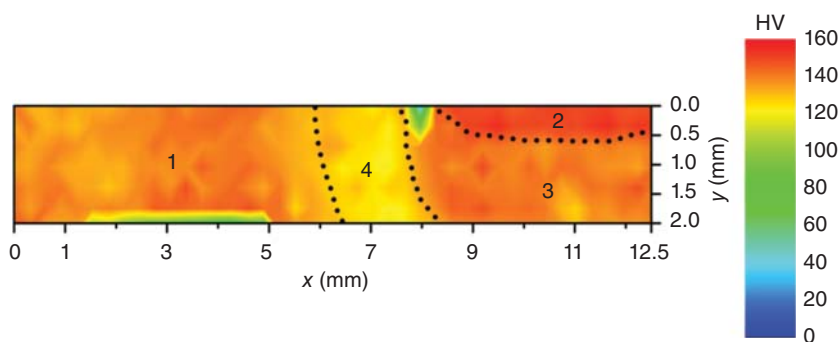


Figure 3.16 Microhardness map of the AA2024 from the edge of the specimen ($x = 0$ mm) to the middle of the spot ($x = 12.5$ mm) in an AA2024/CF-PPS joint. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

the sample was needed to generate the microhardness map. On the x -axis, 0 mm corresponds to the edge of the sample, whereas 12.5 mm is the exact center of the spot. On the y -axis, 0 mm shows the top surface of the aluminum, approaching the interface with the CF-PPS at 2 mm.

From the microhardness map presented in Figure 3.16, one observes four distinct zones, numbered in the figure. The BM (Zone 1) with an average hardness of 135 HV starts from the edge of the sample. The BM ends at a distance of approximately 6 mm from the edge of the joint. Inhomogeneous distribution of the microhardness map in this zone may be attributed to the distribution of the strengthening particles. In positions where the indenter touches or is very close to the particles, a higher value of hardness was obtained.

The MSZ (Zone 2) with higher hardness compared to the BM is obvious as the upper portion of the aluminum part inside the spot area, with an average hardness of 151 HV. The high hardness in the MSZ is related to the very fine DRX grains as well as reprecipitation of strengthening particles as explained in the previous section.

The area below the MSZ approaching the interface is a mixture of MTMAZ (Zone 3) and MHAZ with an average value of 139 HV. As explained earlier, there are three competing mechanisms taking place in the MTMAZ. From the microhardness results, it seems that strain hardening has a larger influence than dynamic recovery or precipitation coarsening, since the average hardness is higher than that of the BM.

Finally, the MHAZ (Zone 4) with a much lower hardness of 123 HV is visible between the BM and the beginning of the spot area. Recovery and coarsening of the strengthening particles are the primary mechanisms, leading to a reduction of hardness in the MHAZ.

It is worth mentioning that the areas in the figure with very low hardness on top of the aluminum surface and close to the interface result from indents very close to the embedding resin and PPS polymer, respectively.

3.5.1.2 Composite (Short Glass-Fiber-Reinforced PPS)

In order to explain the changes in the local mechanical properties of the polymer or composite part during FSpJ process, three lines were evenly spaced throughout the thickness of the composite (short glass-fiber-reinforced PPS) [36]. As can be seen in Figure 3.17(a), the upper horizontal line close to the interface with metal interferes with the PHAZ at the center of the spot. This is the reason for the abrupt reduction of the composite's hardness (5–10 HV) compared to the areas further from the spot center toward the edge of the joint (~ 20 HV), as illustrated in Figure 3.17(b). As explained in the previous section, PHAZ may contain defects such as pores and voids. Although not reported so far, thermal degradation, decrease in molecular weight, and reorientation of chains may also occur in the PHAZ depending on the type of the polymer or composite being used [36]. Since PPS is a high-performance, engineering thermoplastic with a good thermal stability, extensive thermal degradation is not expected when the FSpJ process is optimized [48]. The second and third lines placed further from the PHAZ (Figure 3.17(a)) show a more constant and stable hardness profile with an average value of the PPS BM (~ 20 HV). Slight fluctuation in the hardness profile of all lines was detected (Figure 3.17(b)), which is believed to be the result of the presence of short fibers. In some areas, indenting

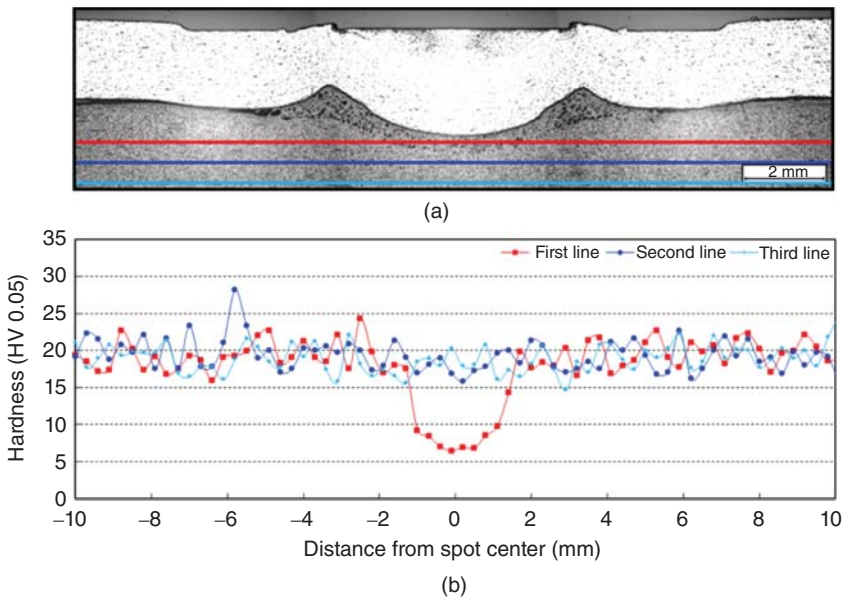


Figure 3.17 (a) Microstructure of an Mg AZ31/GF-PPS joint showing three microhardness lines on the composite and (b) hardness variation of the short glass-fiber-reinforced PPS according to the selected lines in (a).

very close to the glass fibers was unavoidable, leading to higher hardness values.

The combination of microstructural analysis and local mechanical properties suggests that the polymer or composite part in an FSp joint contains one affected zone, PHAZ, in an area around the spot joint and close to the interface with the metal.

3.5.2 Quasistatic Global Mechanical Properties

3.5.2.1 Influence of Surface Pretreatment

As explained before, mechanical interlocking and adhesion forces are the primary bonding mechanisms found in FSpJ. So, any alteration on the surface of the metal part resulted in changes in the surface morphology and chemistry will influence the bonding mechanisms and hence the strength of the FSp joints.

Various metal surface pretreatments were investigated to understand their influence on the strength of the FSp joints. In an early work [51], it has been shown that simple manual mechanical grinding (MG) on the Mg AZ31 specimens increased the lap shear strength of an AZ31/CF-PPS joint compared to the as-received (AR) condition by approximately 50% from 1500 to 2200 N. Moreover, acid pickling (AP) led to an increase of about 20% from 1500 to 1800 N. In another work, Esteves *et al.* [35, 52] demonstrated that the strength of AA6181-T4/CF-PPS single-lap shear (SLS) joints was increased when AP, MG, and a combination of both pretreatments were applied on the aluminum specimen. An increase of up to 160% compared to the AR condition (from 708 ± 55 N to 1861 ± 203 N) was achieved when a combination of AP and MG was selected.

It was described in these works that mechanical pretreatments such as MG increase the surface roughness of the metal part by generating well-distributed surface crevices and pores. Such crevices increase the micromechanical interlocking between the metal part and the polymer molten layer at the interface increasing the shear strength of the joints. Since micromechanical interlocking is one of the main bonding mechanisms in FSpJ, any type of surface pretreatment increasing the metal surface roughness will lead to an increase in the lap shear strength of the joints. Moreover, AP may also generate very fine porous structure on the surface of the metal; however, the pores are not as pronounced as those generated by MG. In addition, AP increases the surface tension of the metal part by removing surface contamination, leading to larger adhesion forces between the faying surfaces at the interface.

The influence of various pretreatments on the mechanical performance of AA2024-T3/CF-PPS FSp joints were recently investigated [30, 48]. The surface pretreatments varied from mechanical to chemical and electrochemical pretreatments as well as a combination of mechanical and chemical pretreatments.

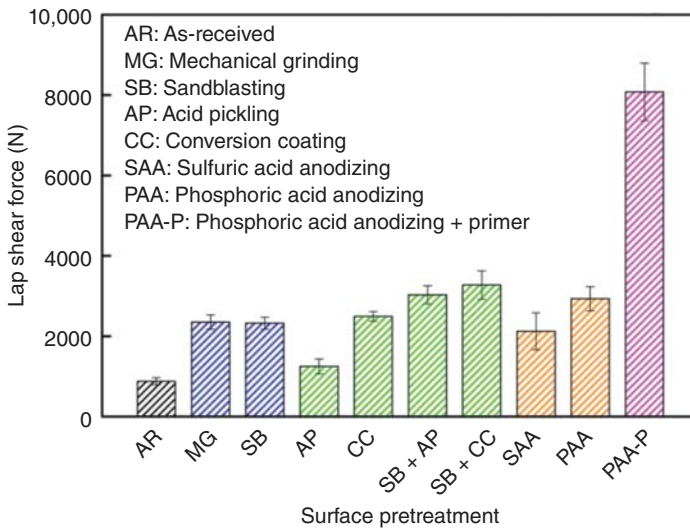


Figure 3.18 Mechanical strength of single-lap shear AA2024/CF-PPS FSp joints after aluminum surface pretreatments. (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

The entire aluminum surface pretreatments increased the lap shear strength of the joints to some extent compared to the AR condition as shown in Figure 3.18.

Mechanical surface pretreatments as well as some electrochemical pretreatments such as phosphoric acid anodizing (PAA) increase the surface roughness of the aluminum and therefore shear strength of the FSp joints as shortly explained before. We recently showed [23] that specific surface pretreatments such as conversion coating (CC) led to the formation of strong chemical bonds between pretreated aluminum and the polymer molten layer at the interface. Deposition of new elements on the surface of AA2024 after CC was identified by X-ray photoelectron spectroscopy (XPS). The new elements such as zirconium were able to strongly bond with the carbon from the polymer molten layer, increasing the strength of the joints. The highest lap shear strength of approximately 8 ± 0.7 kN was achieved when PAA pretreatment was followed by the application of a primer layer (PAA-P) [48]. This was explained by the fact that the primer was a carbon-based epoxy layer leading to very strong carbon–carbon primary bonds with the polymer molten layer during the joining cycle [48].

3.5.2.2 Influence of Joint Geometry

Single-lap joint geometry is the most used configuration because of its simplicity and efficiency [53]. However, it is well known [54] that single-lap joint geometry is the worst design scenario in terms of load transfer and

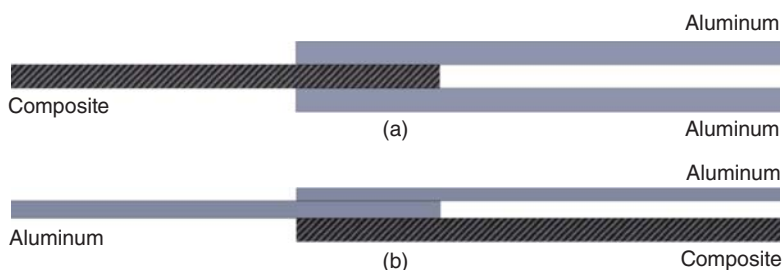


Figure 3.19 Double-lap joint configuration, (a) aluminum–composite–aluminum (ACA) sequence and (b) aluminum–aluminum–composite (AAC) sequence.

mechanical performance when the joint was subjected to shear loading. This is due to the presence of load eccentricity causing out-of-plane, peel stresses at the edges of the overlap joint. Such peel stresses are usually larger than in-plane shear stresses, leading to premature failure and lower mechanical performance. Another joint geometry with a better mechanical performance is the double-lap joint configuration, since the load eccentricity and peel stresses are largely reduced [53, 54].

In a similar manner to other joining techniques, joint geometry influences the mechanical performance of FSp joints. Two double-lap joint configurations were investigated. In the first configuration, a composite specimen was placed in between two aluminum parts, that is, aluminum–composite–aluminum (ACA) sequence as illustrated in Figure 3.19(a), whereas the second configuration was in the sequence of aluminum–aluminum–composite (AAC) as can be seen in Figure 3.19(b).

The mechanical behavior of the double-lap joint specimens with ACA configuration was very similar to the behavior of single-lap joint specimens as shown in Figure 3.20(a). Both specimens showed a quasilinear elastic region prior to the final catastrophic failure. This is because the behavior of the joint is governed by each individual aluminum/composite joint. At the final stage, both aluminum parts were detached from the composite at the same time. The ultimate lap shear force (ULSF) of double-lap joint specimens was 50% to more than twice that of the single-lap joint specimens, depending on the aluminum surface pretreatment.

In contrast to ACA configuration, when the parts joined according to the AAC sequence, the behavior of the double-lap joint specimen was very different compared to the single-lap joint. As apparent from the force–displacement curve shown in Figure 3.21(a), AAC double-lap joint specimen first demonstrated a quasilinear elastic behavior similar to single-lap joint geometry until the failure of the aluminum/composite joint (Point 2). This failure corresponds to a brittle, catastrophic type of fracture. In this stage, the force suddenly decreases because of the loss of the aluminum/composite

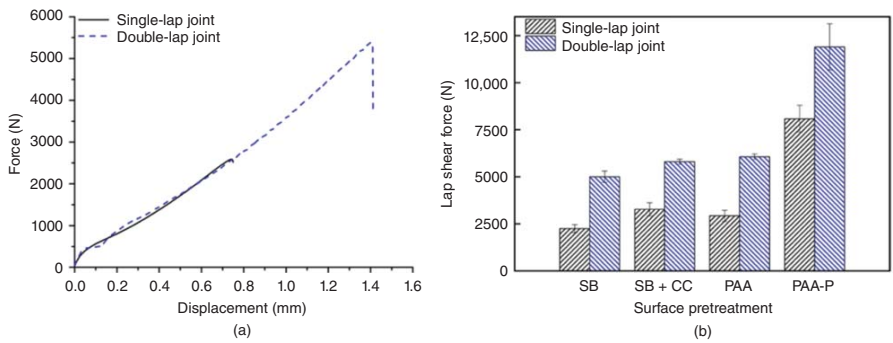


Figure 3.20 (a) Comparison of force–displacement curves of single-lap shear and double-lap shear AA2024/CF-PPS joints and (b) mechanical strength of double-lap shear joints after aluminum surface pretreatments.

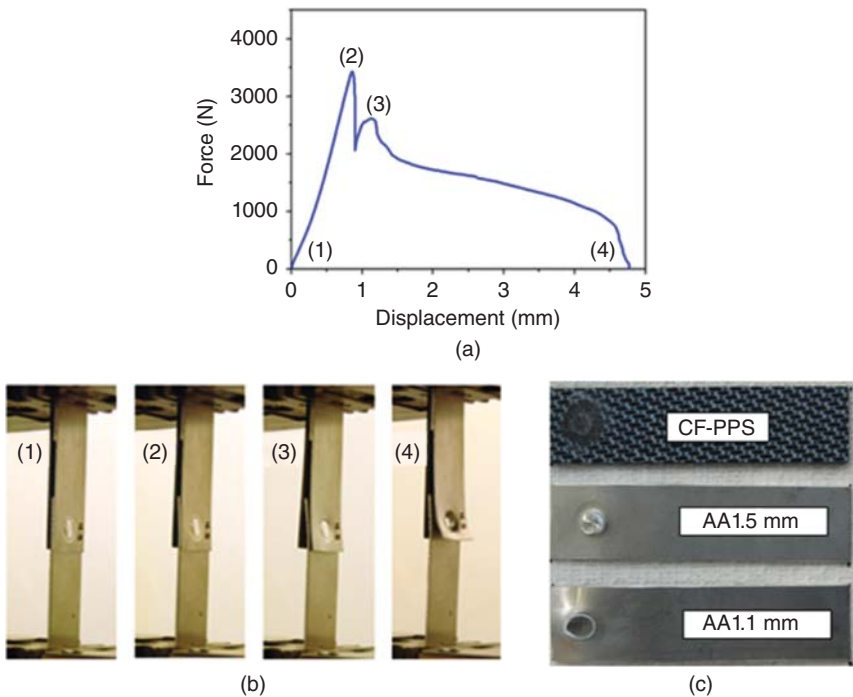


Figure 3.21 (a) Force–displacement curve of an AA6181/CF-PPS double-lap joint according to the AAC configuration, (b) sequence of the joint failure under quasistatic lap shear testing, and (c) fracture surface of the aluminum sheets and composite of a failed specimen.

joint. Upon further loading, the joint showed a nonlinear ductile behavior; the aluminum/aluminum spot weld governs the behavior of the joint. The force slightly increased (to Point 3) until annular and circumferential cracks started to propagate into the spot weld. Finally, the aluminum/aluminum spot weld was failed by a plug pull-out fracture type (Point 4) where the plug remained attached to the middle aluminum plate. Plug pull-out type of fracture was reported to lead to a ductile fracture in aluminum friction spot welds [32] as could also be observed here. Figure 3.21(b) shows the sequence of the joint failure under shear loading while Figure 3.21(c) demonstrates the final fractured sheets. Plug pull-out fracture can be observed between two aluminum parts where the MSZ is removed from the top aluminum and remained attached to the middle aluminum sheet.

Moreover, to understand the behavior of the joints under mode I tensile loading (which might be important for certain engineering applications), cross-tensile (CT) geometry was selected as depicted in Figure 3.22.

Figure 3.23 shows the CT strength (achieved ultimate CT force) of AA2024/CF-PPS FSp joint for selected pretreatments. One observes that the

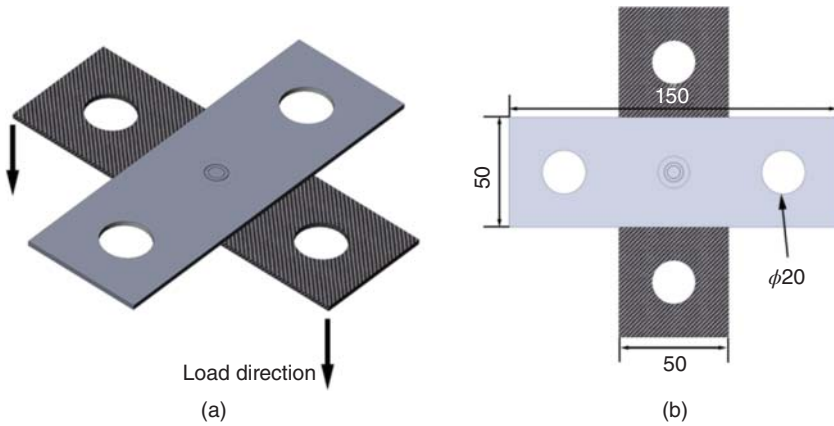


Figure 3.22 (a) Schematic illustration of the CT joint geometry and (b) top view of the joint design with the respective dimensions used. All dimensions are in millimeter.

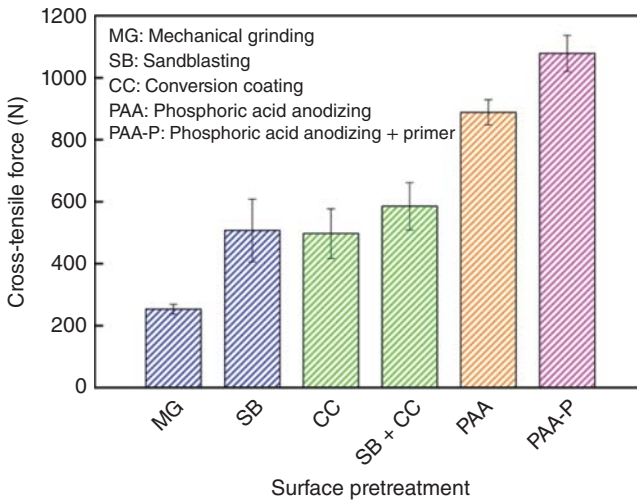


Figure 3.23 Cross-tensile strength of an AA2024/CF-PPS FSp joint after selected aluminum surface pretreatments.

strength of the joint under tensile loading is much lower than that under shear loading (see Figure 3.18). Similar to the SLS results discussed earlier, PAA-P achieved the highest CT strength (1078 ± 58 N) among the surface pretreatments tested, due to the strong primary bonds between the primer layer and polymer molten layer.

In summary: CT strength of the FSp joint was approximately 10–30% of the respective SLS strength for the various surface pretreatments [48]. CT testing

has not been used frequently in the literature to evaluate the mechanical performance of metal–polymer joints. From the scarce results in the literature, for example, Seidlitz *et al.* [55] recently reported on the CT strength of steel/GFRP riveted joints, and CT forces between approximately 500 and 1200 N were reported in their work. Therefore, the reported CT strength of the FSp joints gives comparable results with those reported in [55].

3.5.3 Cyclic Global Mechanical Properties

This section briefly describes the fatigue performance of the SLS AA2024-T3/CF-PPS FSp joints based on the S – N curve life analysis, under constant amplitude loading. A load ratio of $R=0.1$ (usual in aircraft industry) and a frequency of 5 Hz were used. Four aluminum surface pretreatments were selected to carry out the fatigue experiments; these were sandblasting (SB), sandblasting with subsequent conversion coating (SB + CC), PAA, and PAA with subsequent application of the primer (PAA-P). The exponential model (also known as *Lin-Log*) was selected to compare the fatigue behavior of the pretreated specimens.

This comparison is shown in Figure 3.24. It is possible to observe that PAA-P specimens exhibited a much better fatigue performance compared with the rest of the pretreatments. This was expected, because the quasistatic strength of the PAA-P joints was higher, as shown in Figure 3.18. The other three surface pretreatments showed a similar fatigue performance, but the SB + CC specimens performed slightly better than PAA and SB specimens did. This

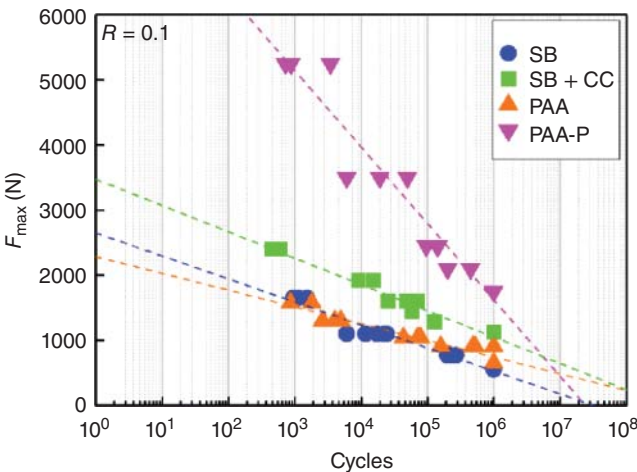


Figure 3.24 Comparison of the S – N curves of the various surface pretreatment specimens based on the exponential model (AA2024/CF-PPS). (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

also accords with their quasistatic strength, where SB + CC showed a higher strength than PAA and SB specimens. One can also observe that the fatigue performance of the pretreated specimens in the low cycle fatigue (LCF) regime (103 cycles) was very similar to their quasistatic strength, being much higher for the PAA-P. By contrast, in the high cycle fatigue (HCF) range (106 cycles), the fatigue performance of the PAA-P specimens tended to approach the other surface pretreatments, although it remains better than the other specimens, as shown in Figure 3.24.

It is known that SLS geometry has very poor resistance to shear loading due to the secondary bending effect [56, 57]. It is also reported that during fatigue testing, peel stresses are generated at the edges of the joints during each cycle [56, 57]. As the applied fatigue loads were higher for the PAA-P specimens, it is believed that higher peel stresses were generated in these joints, particularly in the HCF regime. Such peel stresses reduce the fatigue performance of SLS joints. Moreover, it is well known from the principles of adhesive bonding that shear stresses are maximized (peak shear stresses) at the edges of the joint. Generally, higher applied external loads and shear modulus of the adhesive increase the peak shear stresses at the edges [58–60]. Due to the similarities between FSpJ and adhesive bonding, analogous conclusions may be drawn. Since the PAA-P specimens led to much stronger joints, compared with other surface pretreatments, and the applied fatigue loads were higher as well, it would be expected that the generated shear stresses at the edge of the joint are also higher. Increased peel stresses and peak shear stresses in the PAA-P specimens resulted in a steeper slope of the $S-N$ curve.

The fatigue loads under which the samples survived 1 million cycles correspond to 25% of the initial lap shear strength for SB, PAA, and PAA-P specimens and 35% for the SB + CC samples.

3.6 Comparison Between the Quasistatic Mechanical Performance of FSp and State-of-the-Art Adhesively Bonded Joints

To evaluate the mechanical performance of FSpJ, a comparison was made with the state-of-the-art adhesive bonding. Adhesive bonding was selected because it is one of the currently available joining techniques in industry for hybrid structures. Moreover, FSpJ has similarities in bonding and fracture mechanisms with adhesive bonding, making such comparison relevant.

For a direct comparison, AA2024 and CF-PPS were used. Further, PAA-P was selected as the aluminum surface pretreatment because of its relevance in aerospace industry. The specimens were adhesively bonded according to the standards of the aircraft industry using a structural adhesive. For more information on the chosen parameters and sample preparation, refer to [28, 48].

Table 3.2 Comparison of the mechanical performance of the FSp and bonded joints.

Joining process	ULSF (N)	Displacement (mm)
FSpJ	8264 ± 645	1.7 ± 0.1
Adhesive bonding	5459 ± 2036	0.9 ± 0.3

Both FSp and adhesively bonded joints were tested under lap shear loading to evaluate their quasistatic mechanical performance. Table 3.2 lists the average ULSF and average displacement at the peak load obtained.

One observes that FSp joints showed approximately 50% higher ULSF and an increased 88% in displacement compared to the state-of-the-art bonded joints.

In addition to better mechanical performance, there are two advantages associated with FSpJ over adhesive bonding. Firstly, the JT is much faster for FSpJ (4 s in the mentioned work) compared to several minutes and hours for adhesive bonding. Furthermore, FSp joints are lighter than bonded specimens (in this case, ~3.5%) because no additional material such as adhesive is required to join the parts with FSpJ [28, 48].

The mentioned advantages show the potential of FSpJ process for further investigations and future applications in the industry to join metal–composite hybrid structures.

3.7 Defects in FSpJ

In welding-based joining techniques, where high temperatures and melting of the thermoplastic matrix are involved [16], defects may be introduced into the joining area. In FSpJ, two main types of defect could be detected [24, 29, 34, 52, 61].

The first type of defect is related to a deconsolidation phenomenon, which is also reported in the literature as a major defect occurring in the welding of thermoplastics. In FSpJ, two types of deconsolidation may occur: firstly, generation of voids and porosity in the consolidated molten layer, especially adjacent to the interface with the metal (Figure 3.25(a)); and secondly, fiber–matrix debonding (Figure 3.25(b)). Voids are thought to be formed as a result of air entrapment in the molten polymer during consolidation. Since the temperature during the joining cycle is relatively high, the viscosity of the molten polymer reduces and air can become entrapped in the molten layer. During the consolidation phase, when the CR is extremely fast, this entrapped air cannot escape from the molten layer, and this leaves voids of air pockets after consolidation. To reduce the amount of deconsolidation voids, sufficient pressure should be applied during

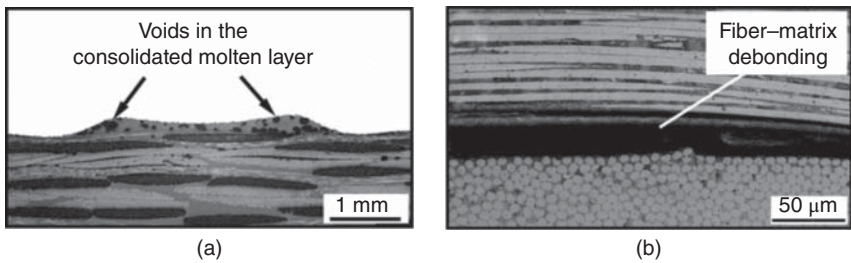


Figure 3.25 Deconsolidation defects in the composite with FSpJ, showing (a) voids in the consolidated molten layer and (b) fiber–matrix debonding; (Ex.: AA2024/CF-PPS). (Source: Goushegir 2016 [28]. Reproduced with permission of Springer.)

the joining process [62]; otherwise, coalescence and growth of volumetric voids can occur. In addition to air entrapment, examples of other phenomena that may cause volumetric flaws in a joint are polymer thermal degradation and the evolution of structural or absorbed water [63]. The formation of voids as a result of thermal degradation and decomposition is especially reported in the laser-based joining of thermoplastics and metal–polymer systems [64–68] as a result of the very high concentration of temperature. The second type of deconsolidation defect, fiber–matrix debonding, occurs because the coefficient of thermal expansion and the shrinkage behavior of the fibers and thermoplastic matrix are immense. In addition, the squeeze flow of the low-viscosity molten polymer leads to a mass displacement from the central region of the spot joint that may further the generation of voids as well as the debonding phenomenon. Another reason for deconsolidation defects may be thermoplastic degradation. As the process temperature is high, a part of the thermoplastic matrix can undergo thermal degradation. However, thermal degradation is unlikely to occur due to the very short joining cycle.

In addition to the aforementioned causes that lead to the generation of deconsolidation defects, there are other phenomena that should be considered as well. In the production of fiber-reinforced composite laminates, a relatively high consolidation pressure is used to process the laminate and pack the fiber bundles. Such pressure generates elastic energy in the fiber bundles [69]. During the joining process, as the viscosity of the polymer matrix decreases, the stored elastic energy is released, thereby potentially inducing deconsolidation and void growth. Furthermore, generation of inhomogeneous thermal stresses is an important phenomenon that leads to defect formation, especially in semicrystalline thermoplastic composites [49]. The CR has an opposite effect on the generation of residual thermal stresses in crystalline and amorphous phases [70]. This leads to different shrinkage behaviors of the two phases, which in turn may facilitate defect generation.

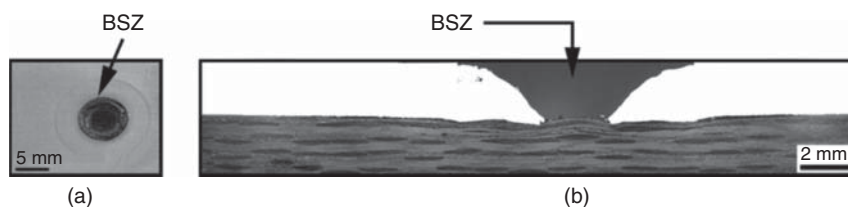


Figure 3.26 Illustration of a broken stir zone (BSZ) that occurs in the metal part; (a) top view of the metal part and (b) cross section of the joint in (a) at the center of the spot; (Ex.: AA2024/CF-PPS).

The second type of defect we reported for FSpJ [29, 34, 61] is known as a broken stir zone (BSZ). A part of the metal below the tool in the center of the spot joint breaks off and becomes detached. The broken metal remains attached to the retracted pin and is removed from the joining area as can be seen in Figure 3.26. As the broken part is removed from the stir zone in the metal, this defect is termed a BSZ.

The causes of a BSZ are explained [34, 61] as due to excessive formability of the metal and tool PD, as can be seen illustratively in Figure 3.27. Excessive heat input increases the local formability of the metal under the sleeve and the pin. In addition, the local strength of the metal is reduced, due to the high temperature around the tool [29, 34]. It is known that the strength and toughness of metals (such as aluminum) are reduced by increasing the temperature [71, 72]. When the sleeve plunges excessively into the metal piece, the thickness of the metal below the sleeve is reduced enormously, as indicated in Figure 3.27(a). This combination of high temperature and low thickness reduces the local strength of the softened metal. As well the local stiffness of the composite below the tool is reduced as a result of the formation of molten polymer, and so it cannot support the plasticized metal to bear the applied pressure of the plunging sleeve. Therefore, the load imposed by the plunging sleeve exceeds the local strength of the metal, leading to an initiation of cracks, as indicated in Figure 3.27(b). Crack initiation is considered to be related to: (1) the reduction of the metal's local strength, (2) reduction of the metal's thickness under the sleeve, (3) reduced local stiffness of the composite, and (4) stress concentration at the interface of metal and molten polymer below the sleeve. The cracks that are initiated start to propagate in the direction of the tool at the interface between the plasticized and unplasticized metal as the pin pushes the displaced softened metal downward to the original metal surface as shown in Figure 3.27(c). Eventually, a piece of metal is removed and remains attached to the tool after retraction (Figure 3.27(d)). The composite under the BSZ is thereby exposed to environmental conditions, which may further deteriorate the mechanical properties of the joint.

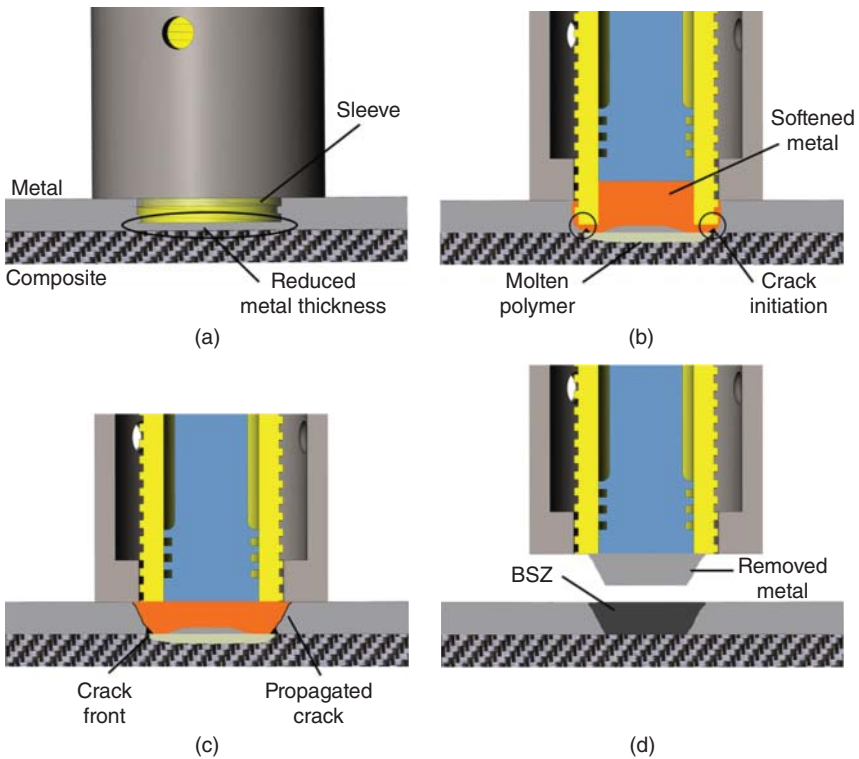


Figure 3.27 Mechanism of the BSZ formation; (a) excessive sleeve plunge, (b) initiation of micro cracks in the metal, (c) propagation of the crack, and (d) formation of the BSZ.

3.8 Advantages, Limitations, and Potential Applications

The main advantages of FSpJ are listed as follows:

- Short joining cycles:** the JT in FSpJ varies between 2 and 8 s depending on the metal alloy and the polymer or composite in use [24], which is much faster than state-of-the-art adhesive bonding.
- Weight saving:** because in FSpJ, no obligatory use of additional material (such as fillers or rivets) is necessary for the joining parts, no additional weight is added to the structure.
- No drilling:** unlike traditional mechanical fastening techniques, there is no need to predrill a hole with FSpJ. This is especially important in cases of continuous fiber-reinforced composites, because these materials are highly susceptible to cracking as a result of drilling, which deteriorates their local mechanical strength, leading to premature failure of the joint.

- d) *Environmentally sound*: the process is green, environmentally friendly, because there are no emissions generated during or after the joining cycle [24].
- e) *Repairability*: since thermoplastics (as one of the joining parts) can withstand being remelted and solidified several times, the joints may be repaired on the same spot (a preliminary result is given in [48]).
- f) *Recyclability*: the joints can be recycled by separating the joining parts. The joint can be heated above the melting temperature of the thermoplastic, to separate the metal from the composite.
- g) *Joining over sealant*: in real-life applications, joints are normally sealed against corrosion and environmental effects. If joining can be performed over a preapplied sealant (e.g., by the addition of a film interlayer as we have shown in [61]), the time and required effort for postapplication of sealant is reduced.
- h) *Low-cost machinery*: the equipment required for production use of FSpJ is the same as the available FSpW machine, and no further alteration or adaptation is required [24].

Besides the main advantages that FSpJ offers, there are several limitations of the process as follows:

- a) *Joint geometry*: only overlap configuration joints can be produced with FSpJ [24].
- b) *Disassembling*: since the joining parts are physically and chemically bonded, there is no possibility of disassembling and reassembling the joints.
- c) *Low torsion and peeling strength*: similar to adhesive bonding, FSp joints are mainly capable of bearing a shear loading, but the torsion and peeling strengths of the joints are relatively low.
- d) *Thickness of metal sheet*: due to local mechanical deformation of the metal (nub area) and the possibility of defective joint formation (BSZ as explained previously), thin metallic sheets (<1 mm for engineering aluminum alloys) cannot be FSp joined with present knowledge.

Weight saving is a major issue in transportation vehicles, so there is an increasing tendency to use the benefits of thermoplastic composites and lightweight metal alloys in engineering structures. For this reason, FSpJ may be seen as a potential technology for transportation, particularly in the aerospace and automotive industries, for joining metal–composite hybrid structures.

Figure 3.28 illustrates an example of a potential metal–composite hybrid fuselage in an aircraft where the parts could be joined by advanced friction-based joining technologies. Figure 3.28(a) shows the inside overview of a presumptive overlapped metal–composite skin structure stiffened by both metallic and composite stringers. The outside view of the hybrid skin can be

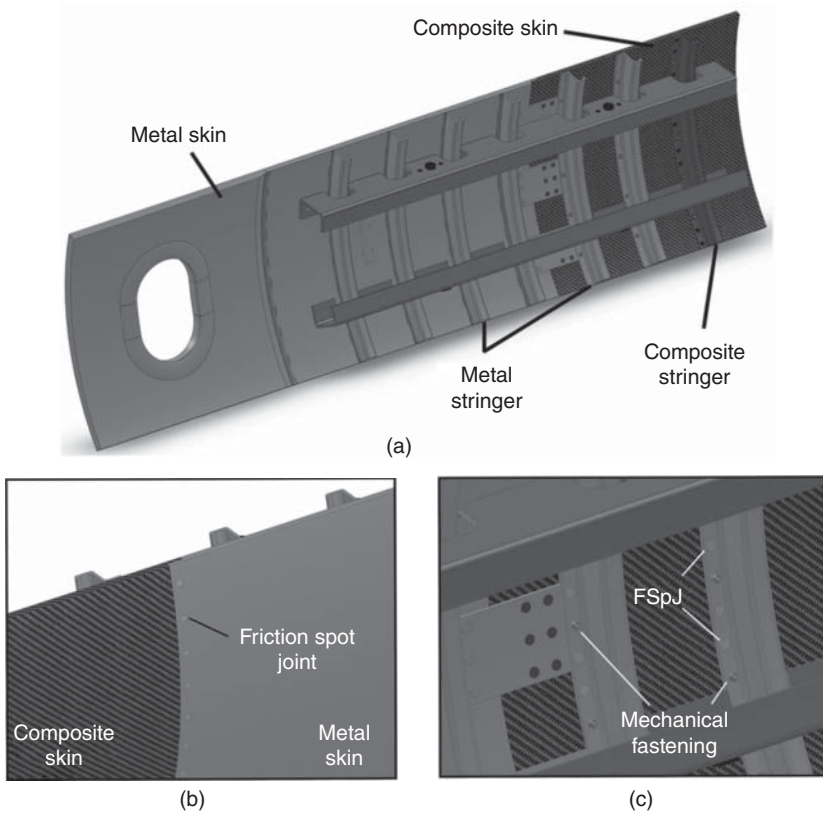


Figure 3.28 Schematic illustration of potential FSpJ applications; (a) in the aircraft structure, to join (b) a metal skin to a composite skin and (c) a metal stringer to the composite skin.

seen in Figure 3.28(b) where FSpJ could be used to join the two overlapped parts of the skin. Furthermore, the metallic stringers, which are used as stiffeners for the skin, are the other potential areas for FSpJ use. Figure 3.29 shows an example of skin-stringer aluminum-composite subcomponent produced with FSpJ. Mechanical fastening is the dominant method in joining skin-stringer parts [73, 74], which indeed increases the weight of the structure by the use of rivets. As can be seen from Figure 3.28(c), some of these fasteners may be replaced by FSpJ. A combination of FSpJ and mechanical fastening can give the required mechanical strength, leading to a reliable structure and at the same time saving weight in the whole structure tremendously. The risk of the crack initiation may also be reduced as a result of less drilling required on the composite, further leading to a safer structure and increasing the flexibility of designing new-generation aircrafts.

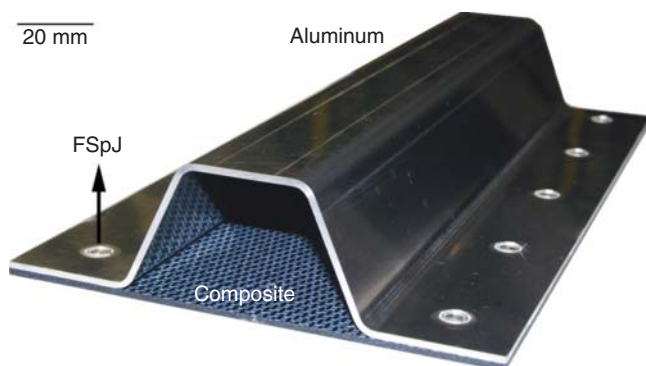


Figure 3.29 Skin–stringer aluminum–composite subcomponent produced with FSpJ.

3.9 Final Remarks

The main objective of this chapter was to give a general overview on different aspects of FSpJ process. Besides the raised topics in this chapter, other subjects such as the influence of metal surface pretreatments on the behavior of the FSp joints, the durability of the joints, fatigue performance as well as fracture micromechanisms were investigated in detail. Therefore, we would like to encourage those interested in FSpJ process to follow and refer to our recently published manuscripts.

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4

Induction Welding of Metal/Composite Hybrid Structures

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4.1 Introduction

Currently, joining methods of metal/composite hybrid structures can be divided into adhesive bonding, thermal, and mechanical methods, based on their joining principles. While thermosets can be joined only by mechanical fastening methods and adhesive bonding, thermoplastics can be thermally joined, for example, by welding-based technologies. Throughout this chapter, metal/composite hybrid structures are defined as a combination of metal and fiber-reinforced polymer composites (FRPCs) with a thermoplastic matrix. Thermoplastic materials can benefit from their softening and melting behavior being thermally joinable or weldable materials, while thermoset do not melt due to their densely cross-linked macromolecular structure.

Induction welding of metal/composite hybrid structures is a new welding-based technology. Thermal joining is accomplished through heating of the metallic component by an alternating electromagnetic field. Melting (for semicrystalline polymers) or softening (in case of amorphous polymers) of the thermoplastic matrix of the FRPC component is achieved mostly from the heat conducted from the metal component (in case of carbon-fiber-reinforced polymers (CFRPs), conductive carbon fibers will also be heated) establishing an intimate contact between the components after consolidation. Generally, the strength of the induction-welded joint is mainly dependent on the component surface pretreatment, the temperature, the applied pressure, and the dwell time [1].

The principles of the process, a general description of the properties, advantages and limitations, applications, and available equipment will be presented in the following sections.

4.2 Description of the Principles of the Joining Technique

4.2.1 Process Overview

As explained earlier, for induction welding of metal/composite hybrid structures, the thermoplastic composite has to be softened or molten, and an intimate contact between the components is required for a proper bonding.

Figure 4.1 shows an example of a discontinuously working induction welding device. The specimens are fixed on a sample holder (the metal onto the FRPC). To generate the heat, a magnetic field alternating at a frequency between 20 kHz and 10 MHz is provided by the induction welding equipment. Firstly, the metallic component is heated by the inductor's alternating magnetic field. Following that, the sample holder moves to the consolidation unit, where both parts are pressed together to enable the softened or molten matrix material to wet the metallic surface and to fill the natural or artificial surface asperities of the – usually surface pretreated, for instance, by sand blasting – metal component. Finally, the formed joint is cooled below the glass transition temperature of the FRPC component. The consolidation unit shown in Figure 4.1 consists of a pneumatic cylinder that applies the pressure and a temperature-controlled stamp.

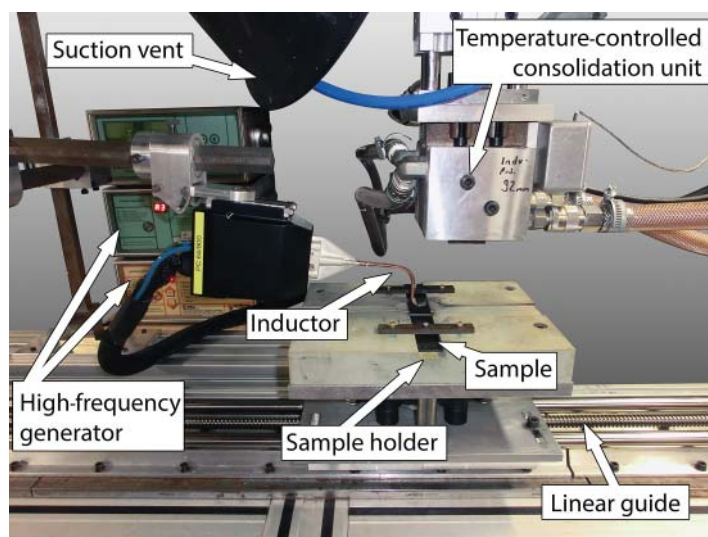


Figure 4.1 Experimental setup of a discontinuous induction welding apparatus.

4.2.2 Heating Process

One basic physical phenomenon in induction welding is that every electric current is surrounded by an electric field. If the electric field changes by time or place, a magnetic field is generated. An alternating magnetic field induces eddy currents into electrically conductive material [2]. This effect is used for the inductive heating. An alternating current passes through the inductor, an alternating electromagnetic field is generated, and the alternating magnetic field induces eddy currents in the metal component. Depending on the used metal (or carbon-fiber-reinforced composites), joule heating and – if the metal is magnetic – hysteresis losses are responsible for the heat generation. Figure 4.2 presents the alternating magnetic field and the induced eddy currents during induction welding.

The energy of the eddy currents is transformed into heat due to the electrical resistivity of the material. These losses are called joule losses. In a conductor loop, the induced real power is a function of the induced voltage and the resistance. The following equation describes the induced power dissipation (the power completely converted into heat) in an ideal resistor,

$$P_{\text{ind}} = \frac{u_{\text{ind}}^2}{R_f} = \frac{4\pi^2 \cdot f^2 \cdot \mu^2 \cdot H^2 \cdot A^2}{R_f} \quad (4.1)$$

where u_{ind} is the induced voltage and R_f is the resistance of the material where the voltage is induced. In this equation, there are two kinds of contributions: the material properties of the metal component, which are electrical resistance

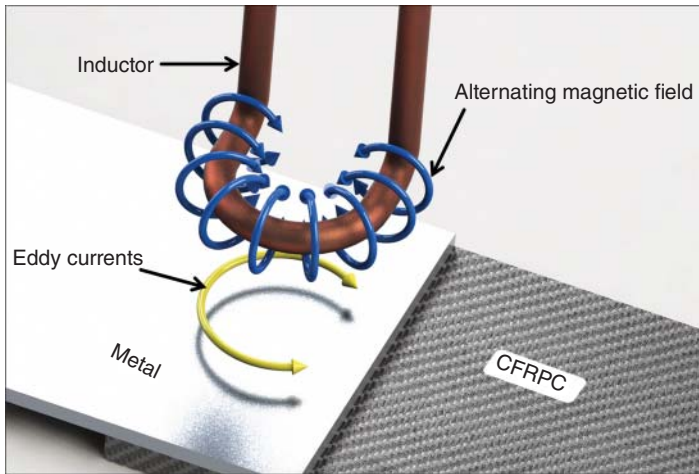


Figure 4.2 Principle of induction heating in induction-welded hybrid joints.

(R_f) and magnetic permeability (μ), and the process parameters, which are the magnetic field intensity (H), enclosed area (A), and frequency (f). It can be seen from Equation 4.1, that the frequency – a process-controllable joining parameter – has a significant effect on the induced power. A high frequency leads to a fast heating and therefore decreases the joining process time. Another important joining parameter is the magnetic field intensity H , which will be discussed in Section 4.2.2.1.

A magnetic field, characterized by the magnetic field intensity, aligns the polarization of the magnetic domains of the metal. An alternating magnetic field switches the alignment to fit the polarization of the outer field. Therefore, a magnetic flux will be created. The magnetic flux density (B) in the metal is as a function of the magnetic field intensity (H). The magnetic flux density always lags behind the applied magnetic field, therefore inducing hysteresis. When the polarization reaches its maximum, a saturation of the flux density is achieved. Hysteresis losses are caused by the energy needed to switch the magnetic polarization of the material with every cycle of the outer magnetic field, and thereby heat is generated by internal friction [2].

Figure 4.3 shows an example of two hysteresis curves for a soft and a hard magnetic material. The hysteresis losses are proportional to the area inside the loops. The property that combines the magnetic field intensity (H) and the magnetic flux density (B) is the magnetic permeability (μ).

$$B = \mu \cdot H \quad (4.2)$$

The hysteresis losses increase linearly with the frequency, while Joule losses show a quadratic increase. Thus, joule losses become the dominating part of the heating mechanism in induction welding of metal/composite structures [2].

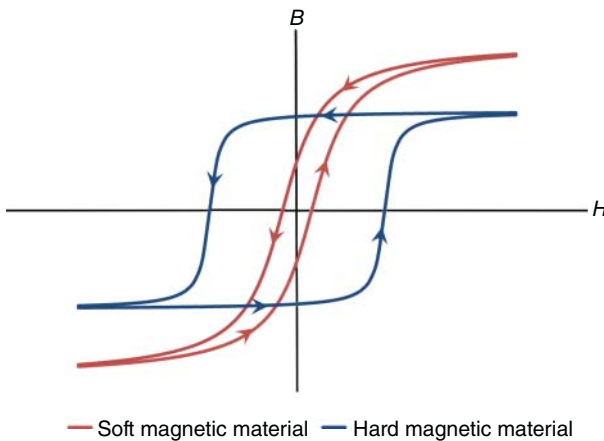


Figure 4.3 Hysteresis curves of soft and hard magnetic materials.

The influence of the frequency on the heating depth is described in Section 4.2.2.2 into more details, since it is also highly dependent on the material properties. For metals, a high frequency leads to low heating depths, and the eddy currents occur mainly at the surface facing the inductor and just beneath it.

4.2.2.1 Geometry of the Inductor and the Magnetic Field

For a steady alternating current, the magnetic field intensity (H) of a thin conductor with any geometry can be punctually described as the superposition of the electromagnetic fields created by all infinitesimally small elements of the conductor. This can be derived from the law of Biot–Savart and is described in Equation 4.3:

$$\vec{H} = \frac{I}{4\pi} \oint \frac{d\vec{s} \times \vec{r}}{r^3} \quad (4.3)$$

where I is the current of the conductor, $d\vec{s}$ is a curve element of the conductor in direction of the current, $\vec{r}$ is the vector from the curve element to the point, and r is the distance between the conductor and the measuring point. It becomes clear that the calculation of the magnetic field intensity inducing currents in a work piece is complex and can only be calculated by a numerical modeling. Hence, the magnetic field intensity depends on the distance to the inductor as well as on the changing current generated by the alternating current.

To convert most of the magnetic energy into heat, it is necessary to position the joining partners very close to the inductor, where the density of the magnetic field is high. When increasing the distance to the inductor a homogeneous heating area is generated but energy efficiency of the induction welding technology decreases and a longer heating period is required until the desired temperature is reached.

Besides the induced current, the induced current distribution is another significant parameter in induction heating. This parameter is influenced by the inductor geometry. The geometry of an inductor cannot be understood as a single conductor; it is more complex and therefore, it can be interpreted as several current-carrying conductors in close proximity. Every conductor has its own magnetic field which is influencing the others nearby. This effect is called proximity effect. This phenomenon distorts the magnetic field of the single conductors indirectly influencing the current and power density distribution. If the currents of two conductors are flowing in different directions, the magnetic field is concentrated between the two conductors since both fields are added. In this case, the external field is weak. If the current flows in the same direction it is concentrated in the opposite way. In this case the fields between the conductors are cancelling each other and the external field is strong because the fields are added to each other. Therefore, the description of the magnetic field of an inductor with complex geometry is challenging. Furthermore, the inductor

and the work piece – where a current is also induced – could be understood as two conductors as well. In other words, the induced magnetic field of the work piece also influences the current distribution in the inductor [3].

Figure 4.4 shows the induced temperature distribution for different inductor geometries. In this example, a carbon fiber reinforced laminate is heated and recorded by a thermo-camera. By using a laminate the effect of the magnetic field could be demonstrated better than using metal. This can be explained because of the poorer thermal conductivity that decelerates the heat output, concentrating the induced heat around the carbon fibers (the conductor being induction heated). From Figure 4.4 it can be seen that, for the pancake and pancake-with-flux concentrator inductors, the maximum of the magnetic field is concentrated approximately in a ring-shaped area, with cold area present in the middle. Conversely, the butterfly inductor concentrates the magnetic field more at one point (in Figure 4.4b), but is less efficient. The consequence is that using a butterfly inductor with the same parameters for frequency and inductor current, the heating process is slower and the maximum temperature is lower than using a pancake-shaped inductor.

The pancake inductor with a flux concentrator (Figure 4.4c) induces magnetic fields oriented in a preferential direction. This allows a strong magnetic field with more uniform shape to be placed where it is desired (the heating positions in the welding parts). Therefore, the selection of these inductors with flux concentrators usually reduces the coupling distance and the stray losses. Where the flux concentrator covers the inductor, the magnetic field is squeezed sidewise to the uncovered volumes. Usually a C-form is used around the inductor, so that the open side faces the work piece. This effect is called “slot effect,” which is correlated with the proximity effect. The most important properties of an ideal flux concentrator material are a high saturation flux density and a high magnetic permeability, but the hysteresis losses have to be low. Therefore, a perfect flux concentrator material would have a higher hysteresis loop, tending to zero. In reality, the used flux concentrator materials are so-called soft magnetic materials and in comparison to hard magnetic material they have a high and narrow hysteresis loop curve. Moreover, the material should have high electrical resistance and thermal conductivity to keep the joule losses low [4].

4.2.2.2 Skin Effect

The skin effect occurs when the penetration depth of the induced current is small, meaning that the currents will be induced on the surface of the heated material. The electromagnetic properties of the materials have a significant influence on the induction welding process. The induced current I_0 inside a work piece decreases with the distance from the surface (Equation 4.4, [3]).

The induced current I_x at certain location x can be calculated as

$$I_x = I_0 \cdot e^{-2\pi x \sqrt{\frac{f \cdot \mu}{\rho_{el} \cdot 10^7}}} = I_0 \cdot e^{-\frac{x}{\delta}} \quad (4.4)$$

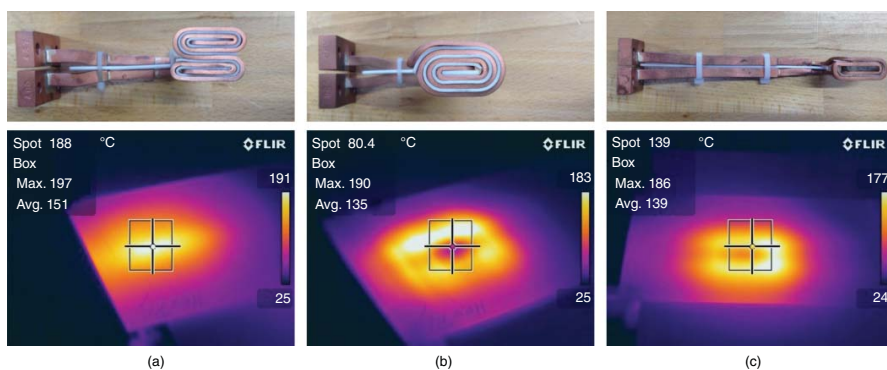


Figure 4.4 Different inductors and their heating patterns ((a) Butterfly; (b) Pancake; and (c) Pancake with flux concentrator) in carbon-fiber-reinforced composite laminates.

where f is the frequency of the alternating magnetic field, μ is the magnetic permeability, and ρ_{el} is the electric resistance, which are the affecting parameters. The magnetic permeability determines the permeability of matter for magnetic fields. This parameter is a material property and for ferromagnetic materials it depends on the magnetic field intensity and temperature. The electrical resistance is also a function of the temperature and may increase during heating.

The penetration depth (skin depth) is defined as the distance from the work piece surface, where the current I_0 decreases by a factor of $e^{-1} = 0.368$ [3].

$$\delta = \frac{1}{2\pi} \sqrt{\frac{\rho_{el} \cdot 10^7}{f \cdot \mu}} \quad (4.5)$$

$$I(\delta) = 0.368 \cdot I_0 \quad (4.6)$$

Figure 4.5 shows the penetration depth for different materials. The electrical resistance of CF/PPS plain weave laminate is $2.1 \times 10^{-4} \Omega \text{ m}$ measured in [5] and its magnetic permeability can be approximated to $1.256 \times 10^{-6} \text{ H/m}$, the air magnetic permeability.

The influence of the frequency on the heating depth for different materials is shown in Figure 4.6. As it can be seen, the skin effect increases, as the penetration depth is lower when the frequency is higher.

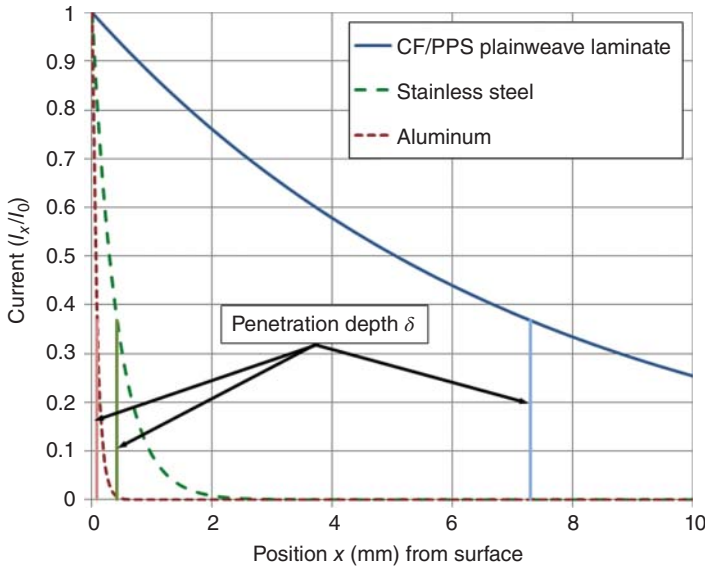


Figure 4.5 Current density inside different materials ($f = 1 \text{ MHz}$).

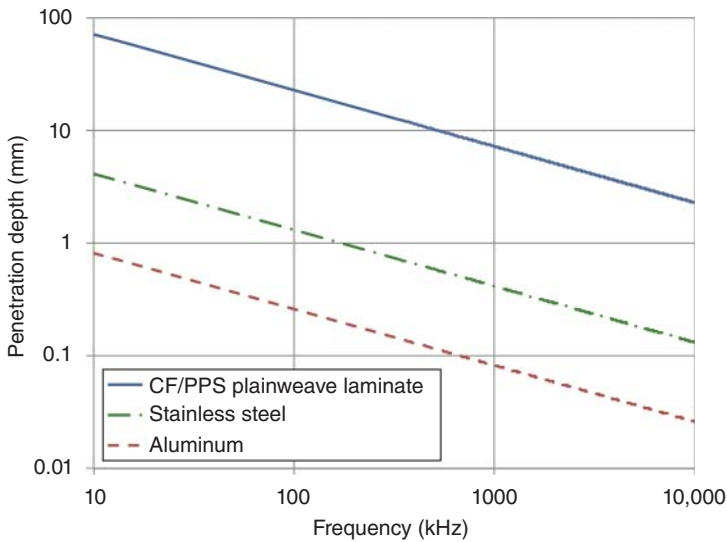


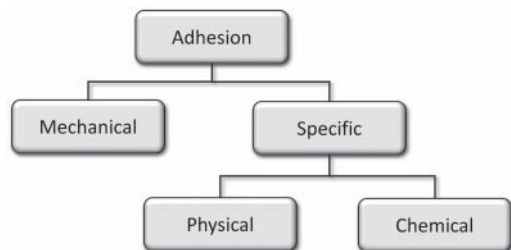
Figure 4.6 Dependence of the penetration depth to the frequency.

4.2.3 Theory of Adhesion and Influence of the Surface

The adhesion between hybrid materials involves a rather complex phenomenology, and different theories are proposed to explain their mechanisms. Usually, they are grouped into the following categories: (1) mechanical adhesion mechanisms and (2) specific adhesion (Figure 4.7). The first describes the mechanical bonding mechanisms, such as penetration of the adhesive into the pores, crevices, and other asperities of the metallic joining part's surface. The second mechanism takes into consideration the complex physical and chemical bonding formation, which takes place on the interfacing surfaces of the joining partners.

The physical adhesion consists of dipole forces, dispersion forces, and hydrogen bridges. Chemical bonding is formed by functional polymer groups, which are able to build strong bonds between the joining parts. Thermodynamic

Figure 4.7 Different adhesion models. (Source: Mitschang *et al.* 2009 [6]. Reproduced with permission of SAGE Publications.)



bonding – the Thermodynamic Adsorption Model or Wetting Model proposed by Schonhorn [7] – occurs due to specific interface and surface energies, which play an important role in the compatibility of the joints [6]. It may happen when the intermolecular bonding forces have similar magnitudes and molecular distance. As explained by Schonhorn, an ideal prerequisite for a good adhesion is to establish intimate contact between the joining parts. Moreover in the case of the induction welding, where melting of the thermoplastic matrix will occur, bonding can only take place if the molten polymeric material wets the surface of the metal.

As described in several sources (e.g., [8]) the importance of surface pretreatments on adhesively bonded joints is a key factor for strong joints. Due to the similarities of induction welding of metal/composite structures, surface pretreatments must be addressed. For the reason that during the welding process, the surface of the composite is melted, the pretreatment of the metal part should be understood. As shown in Table 4.1, there are different surface pretreatment methods with completely different effects available for induction welding. The engineering selection of the most adequate method should be carried out on the basis of their costs and the required quality of the bonding. Different methods for the surface treatment have a completely different influence on the bonding strength and may also weaken the joining partners if not applied properly.

To compare the different treatment methods, it is necessary to characterize the joining part’s surface. One possibility is the measurement of the surface roughness in the case of the metallic part, before and after the surface treatment.

Table 4.1 Typical surface treatment methods used in induction welding of metal/composite hybrid joints.

Method	Treatment	Effect
Cleaning, decreasing	Cleaning with a solvent (i.e., acetone)	Cleaning the surface of the joining partner
Mechanical treatment	- Polishing - Grinding - Sandblasting	- Increasing the roughness of the surface - Removal of contamination layers and oxidized material
Chemical treatment	- Pickling in acid - Etching in NaOH - Anodization	- Formation of a coupling layer - Changes in chemical structure of the surface
Physical treatment	Plasma cleaning	- Removal of organic structure - Surface activation

Source: Mitschang *et al.* 2009 [6]. Reproduced with permission of SAGE Publications.

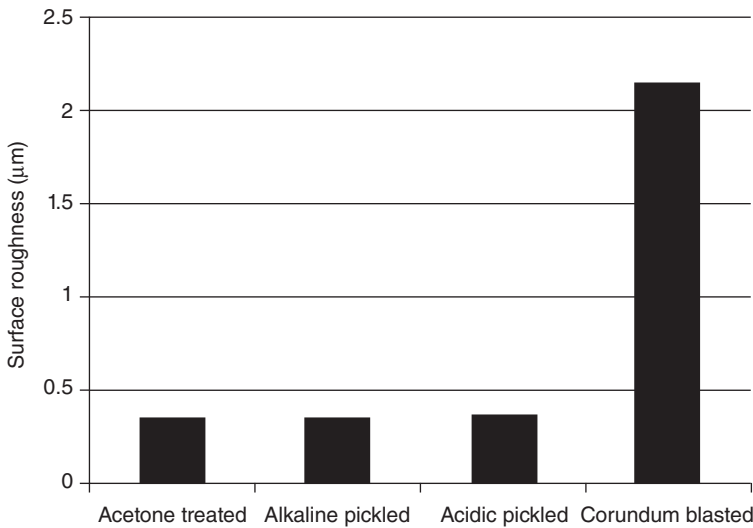


Figure 4.8 Surface roughness (R_a) of AlMg₃ alloy with different surface treatment methods.

Figure 4.8 indicates that the mechanical treatment is most appropriate to increase the surface roughness (within the selected surface treatment methods in this example), while acidic or alkaline pickling just slightly increases the roughness. Wiping with acetone does not influence the surface roughness, so this value can be seen as the reference value of the surface roughness without treatment. Increasing the surface roughness causes a surface area enlargement. In this context (mechanical bonding mechanisms), the outcome of a surface area enlargement is a larger bonded area, which will result in stronger induction-welded joints.

Another way to characterize the surface quality is to calculate the surface free energy after different treatment methods. The surface wettability is an important issue to ensure an intimate contact between the materials to be joined. If the molten thermoplastic matrix does not flow easily over the metallic surface, it is difficult to obtain an intimate contact between the materials to be joined. Therefore, voids may remain entrapped in the joining area. This wetting efficiency can be estimated by the determination of the surface free energy of both joining parts, for example, through contact angle measurements.

As Figure 4.9 shows, the highest surface free energy for aluminum AlMg₃ sheets is achieved after alkaline pickling. However, acidic pickling and corundum blasting also result in increase of the surface free energy. The best surface treatment for CF/PA66 (Figure 4.10) experimentally determined in our laboratory is plasma pretreatment, while wiping with acetone has no effect or even reduces the surface free energy.

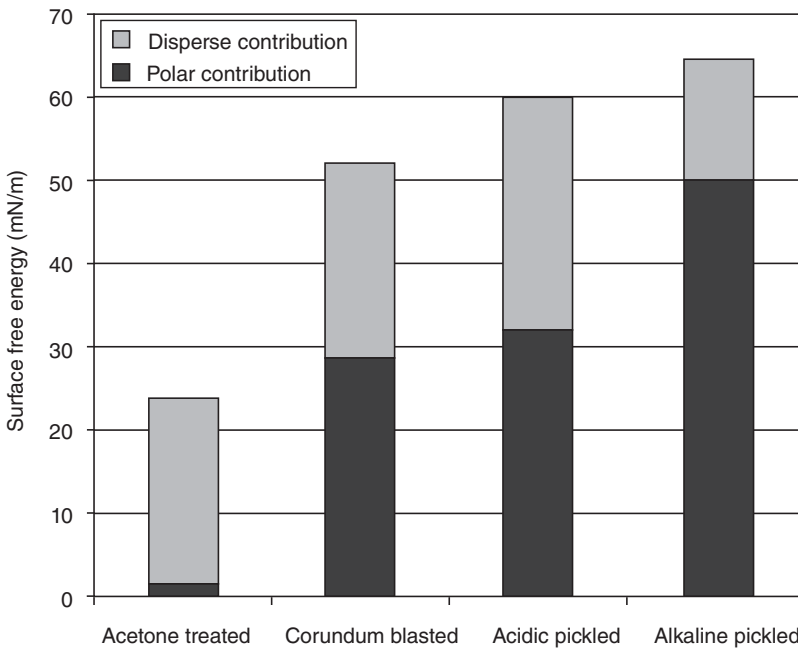


Figure 4.9 Comparison of the surface free energies of AlMg₃ alloy in dependence of the surface pretreatment (calculated according to the Owens–Wendt–Rabel–Kaelble method). (Adapted from Velthuis, R. [1] and Hellerich *et al.* 2001 [9].)

The surface chemistry can be evaluated by X-ray photoelectron spectroscopy (XPS). The level of carbon on the part’s surface can be used as the indicator of organic contamination. Low levels of carbon means that the surface is nearly clean, which could be achieved by corundum blasting or alkaline pickling as shown in Table 4.2. A high concentration of aluminum at the surface and therefore an active surface can be generated by corundum blasting as well as alkaline and acidic pickling [6].

Figure 4.11 shows the influence of the surface pretreatment on the shear tensile strength for induction-welded single-lap specimens. The joint geometry was 100 mm length and 25 mm width with an overlap of 12.5 mm. The testing parameters were carried out according to DIN 1465. The welded components consisted of the AlMg₃ aluminum alloy sheet with 1 mm thickness, pretreated by different methods. The composite part was a CF-PA66 sheet with 2 mm thickness degreased with acetone. Carbon fiber reinforcement was a standard woven fabric (Satin 5H weave) based on 3k-carbon fibers. A three-phase discontinuous induction welding process was used to produce the specimens.

While alkaline pickling results in the highest surface free energy (Figure 4.9), the joint lap shear strength (Figure 4.11) was virtually unchanged in comparison

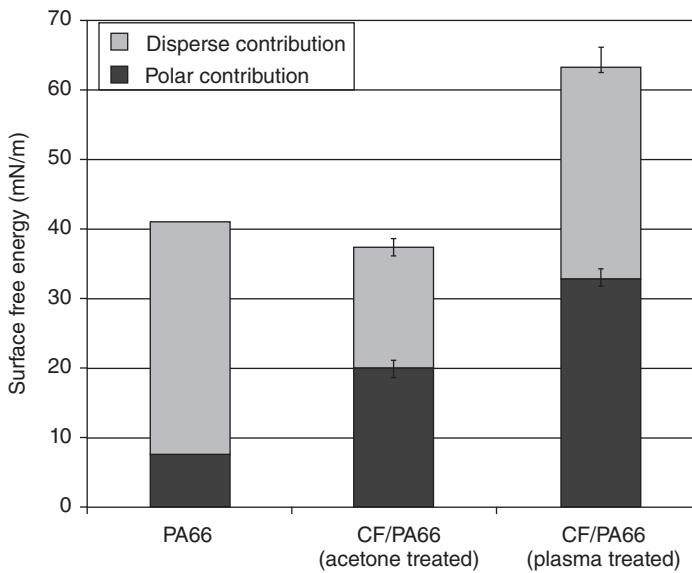


Figure 4.10 Comparison of the surface free energies of CF/PA66 in dependence of the surface pretreatment (calculated according to the Owens–Wendt–Rabel–Kaelble method). (Adapted from Velthuis, R. [1].)

Table 4.2 Surface concentrations on the treated AlMg₃ surfaces obtained by XPS analysis.

Treatment	Element concentration (at.%)				
	C (1s)	O (1s)	Al (2p)	Mg (1s)	Others
Acetone	33	47	8	5	Rest
Corundum blasting	15	52	26	1	Rest
Alkaline pickling	11	59	22	1	Rest
Acidic pickling	32	44	22	1	Rest

Source: Adapted from Velthuis 2007 [1].

to acetone wiping. It seems that for achieving high bonding strengths, the increase of the surface area by Corundum blasting plays a more important role than the increase of the surface free energy. Details on the conducted experiments are to be found in [6].

4.2.4 Thermal Degradation

The thermal degradation refers to changes in the physical properties of polymers caused by excessive temperatures. The level of thermal degradation

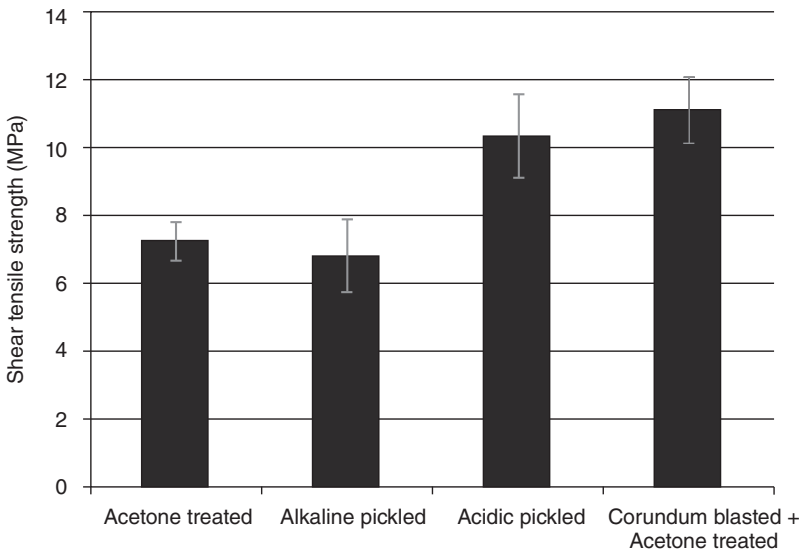


Figure 4.11 Influence of the surface pretreatment on the shear tensile strength of AlMg₃/CF-PA66 induction welds. (Source: Velthuis 2007 [1]. Reproduced with permission of Institute of Composite Materials.)

is dependent on the temperature and the dwell time the material is held at this temperature. Thermal degradation is a continuous process in which the molecules of the polymer are decomposed into small fragments such as free radicals, free ions, H₂, CO, and so on, that is, its constituent parts. These physical–chemical changes cause a significant decrease in mass and mechanical properties and must be avoided or reduced during induction welding.

There is no fixed rule of how to measure the level of thermal degradation. However, one possibility is to measure the weight changes by thermogravimetric analysis (TGA) and compare to a reference mass or the decrease of special mechanical properties, such as the shear strength. There are other available techniques to study joining-process-related polymer degradation. Differential scanning calorimetry (DSC) and dynamic-thermal analysis (DTA) are more effective to detect the physical changes in polymers, such as changes in glass transition, degree of crystallinity, and so on, which are affected by thermal degradation. Moreover, techniques based on thermal volatilization analysis (TVA) combined with ultraviolet (UV), infrared (IR), and mass spectroscopy are powerful to identify the degradation products and to understand the degradation mechanisms during the induction welding. Changes of the molecular weight induced by thermal degradation can also be evaluated by chromatography analysis.

When heating polymers above their service temperatures, which is – in the case of induction welding of semicrystalline composites – the melting temperature, there is no possibility to avoid thermal degradation. The only countermeasure is to expose the polymers at these temperatures as short as possible but long enough to ensure proper bonding. For example, for PA 66 matrix, a maximum of 6 min at 280 °C (536 °F) has been suggested [9]. During induction welding, the matrix material is heated to the process temperature for not more than 1 min, meaning that no extensive significant degradation should take place.

4.2.5 Deconsolidation and Consolidation

4.2.5.1 Deconsolidation

Deconsolidation is the negative effect of the FRPC to expand (formation of internal voids and separation of the different plies if the laminate temperature exceeds the melting temperature in the thermoplastic polymer matrix. For simplification purposes (for detailed discussion, please refer to [10]), deconsolidation takes place in induction welding when the applied pressure is too low. Resulting gaps and voids in the laminate may lead to poor bonding. Along with the negative effect on the bonding strength, the surface quality may also be affected, leading to reduction in finishing quality. Surface distortion may also occur when the molten surface polymer is solidified by different cooling rates, which will generally induce in different degrees of crystallization (i.e., differential contraction leading to distortion).

An example of deconsolidation (in this case leading to ply delaminations) is shown in Figure 4.12. The composite consists of woven glass-fiber-reinforced polypropylene (PP) laminate. The specimen was heated at a rate of 10 °C/s to 140 °C and then held for 5 min to allow a homogeneous temperature

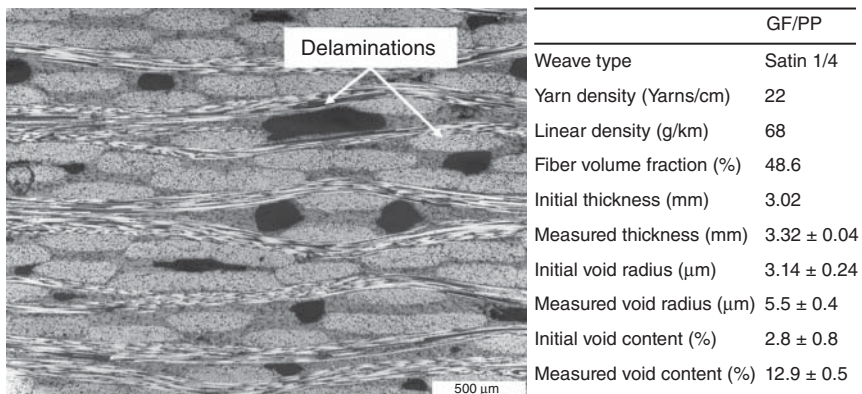


Figure 4.12 Deconsolidation of GF-PP composites.

distribution through the thickness. In a second step, the specimen was heated to 195 °C at 10 °C/s and kept at this temperature for 10 min. Finally, the specimen was cooled down to room temperature with 1 °C/s. Analyzing the deconsolidation of the material shows that the thickness increased by 0.3 mm and the void content increased from 2.8% to 12.9%.

4.2.5.2 Consolidation

In order to create a proper bonding, it is necessary to hold both joining partners under pressure during the polymer matrix melting and subsequent consolidation. This pressure guarantees the required intimate contact, increases the contact area, and forces the molten polymer to flow into the microvoids and asperities of the metallic part [11]. If the applied pressure is too high, the molten polymer will be squeezed out of the weld zone, which will decrease the weld strength.

4.2.6 Cooling

In order to develop a reliable induction welding process, a controlled cooling of the components below the solidification temperature of the thermoplastic polymer is necessary. For semicrystalline polymers, the cooling rate affects the rate of crystallization and formation of spherulites in the joining zone, which may affect the joints' mechanical performance. Moreover, distortion and residual stresses can be formed during the cooling stage, which may be related to frozen molecular orientation parallel to the applied pressure.

4.2.7 Internal Stresses in the Weld Zone

Joining materials with different coefficients of thermal expansion lead to residual stresses in the weld zone. The coefficients of thermal expansion of metals (e.g., for aluminum $\alpha = 2.35 \times 10^{-5} \text{ K}^{-1}$ between 23 and 80 °C [9]) are very high in comparison to composite laminates. A composite is a combination of fibers and polymer matrix, and hence, the different thermal expansion coefficients of the individual components, as well as the amount and orientation, influence each other. While matrix materials and glass fibers extend in all directions with increasing temperature, aramid fibers and carbon expand perpendicular to the fibers' axis and shorten in the fibers' direction. For CFRP composites with a fiber volume content between 40% and 60%, the thermal expansion coefficient in the longitudinal direction can be assumed to a good approximation as 0 K⁻¹. Perpendicular to the fiber direction, carbon fibers also cause a decrease in the mixed coefficient of thermal expansion compared to the unreinforced matrix material. However, the influence is significantly low [12].

During induction welding, both metal and composite are simultaneously heated. In this modified state, they are clamped together. When the material is cooled down below the glass transition temperature, tensions begin to

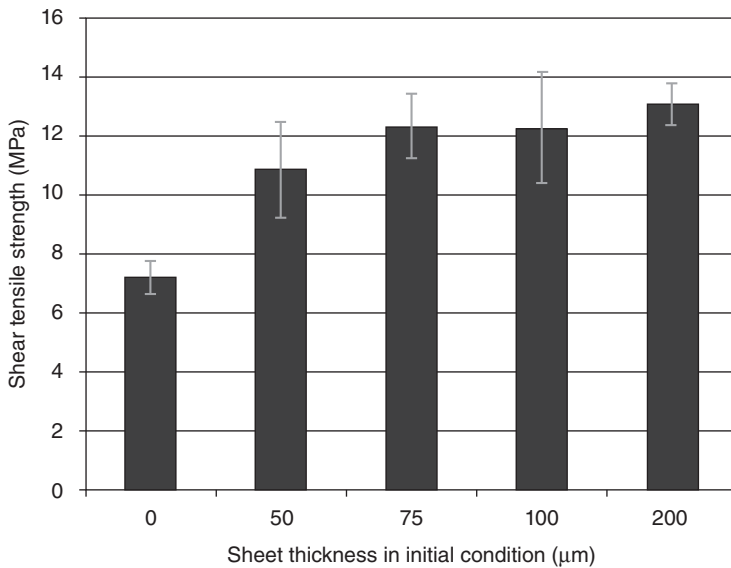


Figure 4.13 Influence of the sheet's thickness in the welding zone on the lap shear strength of (AlMg₃/CF/PA66) induction welds [1].

build up. The ongoing decrease of the temperature will cause the metal component to shrink much more to its original geometry than the CFRP. This results in internal stresses at the welding zone. These tensions may be above the resistance of the polymer and damage the joining zone decreasing the bonding strength. In case of adhesive bonding, thick adhesive layers or films are used when materials with different thermal expansion coefficients are joined. These adhesive layers or films compensate internal stresses by bearing part of the imposed plastic deformation [13]. A comparable effect is observed when a neat polymer layer is added to the joining zone of metal–CFRP induction-welded lap joints. An improvement in lap shear strength can be found in single-lap shear tests as demonstrated in Figure 4.13. The samples were prepared according to DIN 1465.

4.2.8 Process Variants

There are different process variants in induction welding. The discontinuous process and the spot-welding process are described in this section.

4.2.8.1 Three-Phase Discontinuous Welding Process

As shown in Figure 4.14, the three-phase discontinuous welding process is characterized by point-shaped heating of the metal (some induction heating may be created when working with CFRP as well), melting the polymer matrix at the

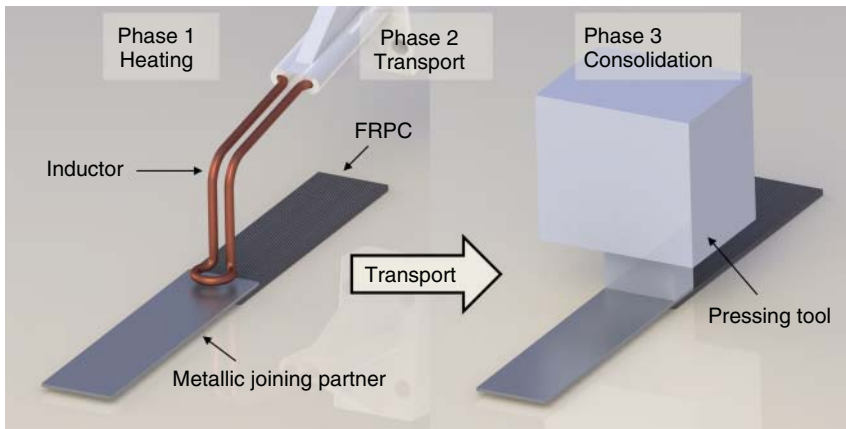


Figure 4.14 Three-phase discontinuous induction welding process setup.

interface, followed by transport to the consolidation unit, and consolidation of the joint. The consolidation stamp may cause the loss of the specimen's heat. This can be mitigated by using a cooled stamp. Moreover, the temperature of the stamp can be controlled, whereby the crystallinity of semicrystalline polymers may be affected, which in turn changes the joint strength.

The temperature profile in the welding zone of the three-phase discontinuous process is shown in Figure 4.15. During the first phase, the temperature in the welding zone rises above the melting temperature of the polymeric matrix.

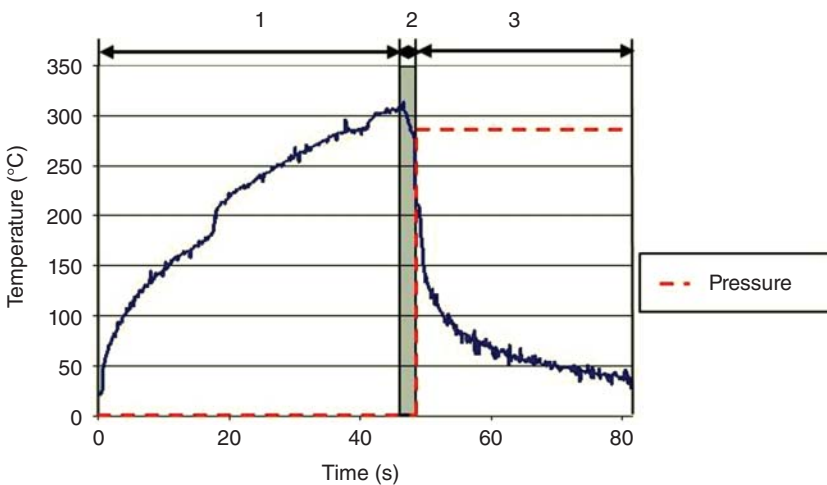


Figure 4.15 Three-phase discontinuous induction welding process temperature profile and pressure (the pressure is presented qualitative without Y-axis).

The welded material is AlMg_3 alloy with CF/PA66. The temperature curve in Figure 4.15 demonstrates that in phase 1, the temperature in the welding zone is over the melting point of PA66 (264°C). When the matrix is molten, the whole sample is moved to the consolidation unit and thus pressed together and cooled down. During this transport phase (phase 2), there are temperature losses; therefore, it is necessary to heat up the welded parts to higher than the melting temperature to reduce heat losses. Phase 3 shows the weld consolidation where the components are pressed together and cooled down.

The main advantage of this process is the easy implementation of various inductor geometries and consolidation units. This offers many design opportunities. Another aspect is the process control. The sample is not covered by the stamp during the heating phase, thus allowing a better heating control on the joint surface.

4.2.8.2 Spot Welding

In contrast to the three-phase discontinuous process, the components are heated under pressure in the spot-welding process. As Figure 4.16 shows, the inductor is placed in the middle of the consolidation unit. The consolidation unit consists of a ring stamp with a cooling chamber that is covered by a cover plate. Holes are integrated into the cover plate for the inlet and the outlet for the water cooling. This cooling concentrates the temperature at one point and prevents heating of the material outside the welding zone. It also leads to a constant temperature level of the spot-welding head. Therefore, high process-parameter reproducibility can be achieved. The ring stamp is closed by an inlay. It is possible to use different materials for the inlay, depending on the materials of the components being joined. A ceramic inlay, for example, is invisible to the magnetic field but causes an increased coupling distance and less energy efficiency. A ferromagnetic inlay would be heated itself by induction very efficiently, but also leads to an additional heat transfer between

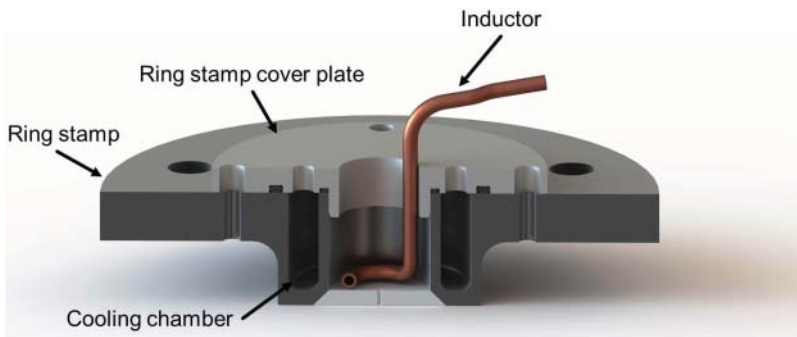


Figure 4.16 Induction spot-welding setup.

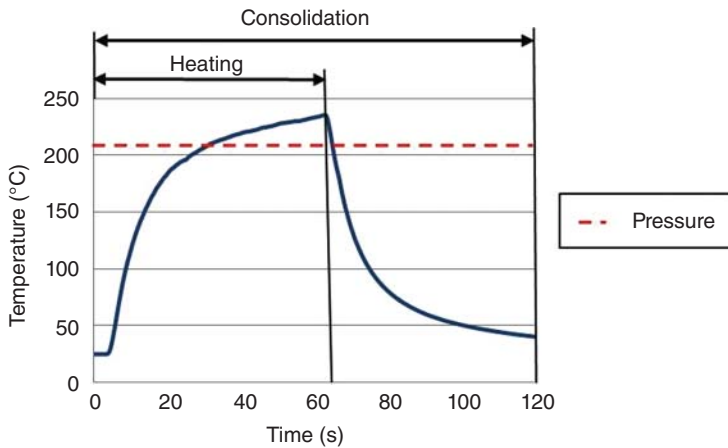


Figure 4.17 Induction spot-welding process temperature profiles and pressure (the pressure is presented qualitative without Y-axis).

the stamp and the component. Regardless of the material used, all types of inlays can ensure heating under pressure.

Prior to the process start, the induction spot-welding device is positioned pressing the to-be-joined parts against a backing plate. Subsequently, the inductor starts to heat up the parts while the axial pressure kept constant. After reaching the melting point of the polymer in a defined area (the spot-weld size), the inductor is switched off and heating stops. The device maintains pressure on the components in order to consolidate the joint while cooling down. Figure 4.17 shows an example of the temperature profile in the welding zone of an induction-spot metal/composite joint.

Cooling is very important to reduce internal stresses. When the whole component is heated during the welding process, the metal expands more than the composite. After welding, the metal shrinks again, and this leads to residual stresses, sometimes parts distortion, and even full separation or partial damaging of the welded parts. Usually, a cooled pressing tool is used to prevent thermally induced changes of the surrounding material's mechanical properties and reduce the thermal expansion of the metal part due to restriction of the heating volume.

There are three important process temperatures influencing the weld quality: (1) the maximum process temperature that could be used without causing excessive thermal degradation of the matrix material; (2) the melting temperature of the polymer, to be reached at the rim of the spot weld; and (3) the maximum temperature that is acceptable at the ring stamp edge to prevent damages due to thermal expansion. Figure 4.18 shows an example of a real temperature distribution inside the welding zone. The schematic

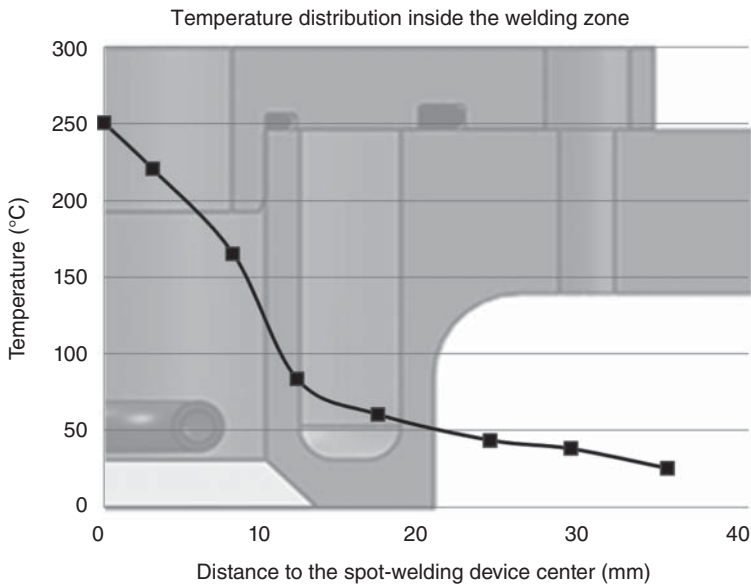


Figure 4.18 Spot-welding temperature distribution within the welding zone.

drawing in the background is intended to illustrate at which positions under the spot-welding device the measurements were carried out in the joined parts. The cooling component of the spot-welding tool is located in a ring with radius of 10 and 20 mm; therefore, underneath this location, the temperature in the welding parts decreases significantly.

A limitation of this process is the inductor size. The inductor must be water cooled, and the flow rate must be high enough to avoid overheating by Joule losses occurring as a result of the alternating current on its surface. Another fact is that the application of energy depends on the inductor size and could also be a limitation. Here a flux concentrator could partly compensate this deficit.

4.3 Mechanical Performance of Induction Welds in Comparison to Adhesive Bonding

The aim of this section is to compare induction-welded metal/composite hybrid structures in comparison to adhesive bonding. Adhesive bonding is a versatile joining process with a wide range of applications. One common feature of adhesive bonding, also shared with induction welding technology, is the bonding mechanism by adhesion forces. Hence, the strength of the joint produced by these two joining techniques will depend on the pretreatment method of the joining partners (as previously discussed in Section 4.2.3).

In the respective chosen case study, the to-be-joined components were an aluminum alloy (AlMg₃) with a thickness of 1 mm and the CFRP (CF/PA66) with a thickness of 2 mm. The reinforcement consisted of a woven fabric (Satin 5H weave) based on 3k-carbon fibers. The fiber volume content is set to 50%. The geometry of the single-lap joints was 100 mm in length and 25 mm in width with an overlap of 12.5 mm. This geometry and the testing parameters were chosen according to DIN 1465. To manufacture the specimens, the three-phase discontinuous process was used.

The adhesively bonded metal/composite hybrid joints were produced with standard 1 K epoxy resin adhesive, which is typically used for joining similar CFRP configurations in the automotive industry. Alkaline pickling was chosen to pretreat the aluminum part, while the CFRP parts were degreased with acetone prior to adhesive bonding. The metallic partner of the induction-welded specimens was corundum blasted, and an additional polymer film (an inter-layer) was placed at the overlap zone. CFRP parts were degreased with acetone. Subsequently, the bonded samples were mechanically tested to determine the lap shear strength and compared to the lap shear strength of induction-welded specimens. The results are shown in Figure 4.19.

The shear tensile strength (i.e., the ultimate lap shear strength) of adhesively bonded joints was about 15% higher than the lap shear strength of optimized induction welds. Considering the fact that the induction-welded samples are able to be loaded directly after preparation and no curing or conditioning is

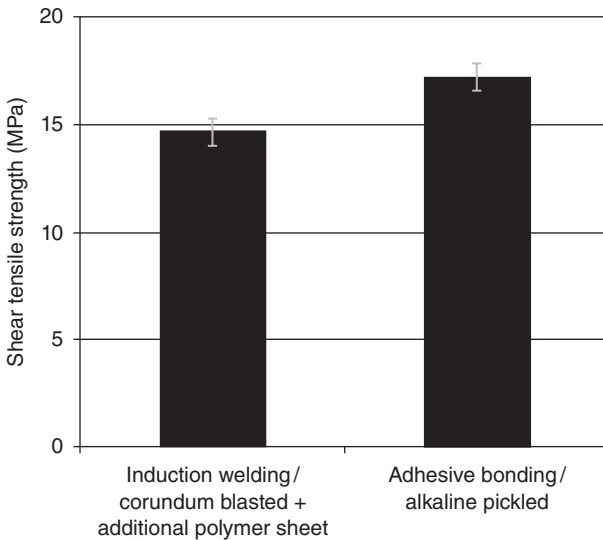


Figure 4.19 Comparison of the mechanical performance of inductive welded and adhesively bonded samples. (Source: Velthuis 2007 [1]. Reproduced with permission of Institute of Composite Materials.)

necessary, the induction welding technology can be regarded as a competitive technology compared to adhesive bonding, which requires several minutes to hours to cure.

4.4 Advantages and Limitations

The main advantage of induction welding is the high flexibility of the process. The equipment can be used universally and can be mounted on an industrial robot. Therefore, it is possible to weld/join complex parts and the process is nearly independent of the component size. Furthermore, heating occurs from one side only, which reduces the problem of accessibility. In comparison to the use of mechanical fasteners, induction welding reduces stress concentrations (absence of through-holes) and additional joining elements are not necessary. In comparison to other welding technologies, such as vibration welding, the investment costs are low. Another advantage is the relatively short processing time, since no curing time is required compared to adhesive bonding. Nevertheless, it has to be mentioned that the process time is a limitation (from a few seconds up to a few minutes). There are welding processes such as ultrasonic welding, which are much faster (usually less than a second).

4.5 Applications

The technique of induction welding has already found its way into the automotive and aeronautical industry, even though not with a hybrid joint (only similar welds): an elevator and a rudder of the Gulfstream G650 consisting of CF/PPS are welded through inductive heating [14]. As for the automotive industry, a lightweight rear seat back support has been developed for the BMW M3 CSL, which is welded by inductive heating [15]. A weight reduction of 50% without losses of stability was achieved [16].

Currently, induction welding of metals to FRP is being extensively studied. The difficulties mentioned earlier, such as different thermal expansion coefficients of the joining partners, require a profound understanding of the correlations between welding/joining parameters and joint properties. Potential applications in the automotive or aeronautical industry are conceivable, as the selection and application of metal/FRP are increasing in both industries. For instance, car interior linings or dash panels, which are mounted to the car body, could be attached by induction welding, as well as other covers. In order to reduce weight as well as an optical feature, professional racing cars, engine hoods, roofs, and trunk lids are made of CFRP. These parts are currently being adhesively bonded to the car body, but might as well be welded. The same assumption is valid for airplanes, where the inside panels of the passenger cabin could be attached to the body without additional fittings by induction welding.

4.6 Available Equipment and Tools

The alternating electric current is provided by high-frequency generators. These generators may vary in their construction. Due to the attachment of the inductor, some of them are more suitable to be fixed to a robot, for example. Depending on the materials to be welded, customers can choose between different power levels and frequency ranges. These can vary from 3.2 to 500 kW and from 50 kHz to 3 MHz, respectively [17, 18]. To apply the consolidation force, different pressing tools are necessary depending on the process, materials, and joining geometry. These pressing tools should be individually designed for each application and are not commercially available.

4.7 Further Reading and Additional Literature

A very comprehensive book about induction welding technology is the “Handbook of Induction Heating” [3]. This does not refer to composite materials, but to metals, and describes very extensively the theoretical background and the various electromagnetic phenomena. Between 2006 and 2012, the DFG-Research Unit 524 has worked on the topic “Manufacturing, properties, and simulation of welded lightweight structures of metal/FRPCs.” Three different welding processes, ultrasonic welding, induction welding, and impulse welding, were investigated and compared. Many of the results were published in a special edition of the journal *Advanced Engineering Materials* in September 2013 [19]. Finally, the materials’ physical mechanisms involved in the fusion bonding process of joining composite and dissimilar are well described in [20, 21].

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5

Direct Joining of Metal and Plastic with Laser

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5.1 Introduction

Metals and thermoplastics have been widely used in many industrial applications such as aircrafts, automotive and transportation, measuring apparatuses, and electronic devices [1]. On one hand, metals possess the main properties of high strength, high toughness, high heat conductivity, and good stability at high temperatures. On the other hand, plastics are characterized by lightweight, high corrosion resistance, and excellent formability. Joining of a metal and a thermoplastic is an outstandingly important and inevitable technology from the manufacturing point of view. The joining has conventionally been performed by using adhesive bonding (glues) or mechanical fasteners, such as bolts, screws, and rivets [2]. However, such joining methods for metal–thermoplastic joints have several limitations, and the respective advantages of metal and plastic properties are not fully utilized. Adhesives or glues have environmental burdens and need severe quality control in the production process. In particular, restriction of volatile organic compound (VOC) emission has been identified as an important problem to be solved. Conventional mechanical joining of metal–plastic structures presents some limitations such as the nonflexible design or low productivity due to the use of fasteners, screws, or rivets. Therefore, the authors of this chapter have newly developed a laser direct joining process for metal–polymer joint named “laser-assisted metal and plastic (LAMP)” joining technology, which is accomplished without adhesive bonds or glues, fasteners, screws, or rivets [3–10]. The LAMP joining is a simple procedure, in which a laser beam is directly irradiated on the lapped sheets of a metal and/or a thermoplastic. The process can be automatized. It shows the potential of overcoming the problems of the conventional bonding methods using adhesives or mechanical fasteners.

In this chapter, some important points are described concerning the procedures of LAMP joining: (1) the characteristics of LAMP joining methods and joints, (2) the formation of strong joints, (3) the evaluation of the mechanical properties, (4) the LAMP joining mechanisms, (5) joint reliability evaluation tests by the leakage and heat-shock testing, and (6) the evolution of LAMP joining process to the application for dissimilar metals.

5.2 Direct Joining Procedures of Metal and Plastic with Laser (LAMP Joining Procedure)

Laser sources, such as yttrium aluminum garnet (YAG), fiber, and disk (disc) laser of 1.03–1.09 μm wavelength and diode laser of 0.8–1 μm wavelength can be used in continuous- or pulsed-wave mode. CO_2 laser of 10.6 μm wavelength can also be used similarly in the case of laser direct irradiation on the metal surface and some transparent resin. An example of a direct diode laser apparatus with 3 kW maximum power and experimental arrangement for LAMP joining is shown in Figure 5.1.

The laser beam is focused by a focusing optic. The focused beams and power densities of a YAG laser and a diode laser are demonstrated in Figure 5.2. Such a focused beam of YAG, fiber, or disk laser is used near the focal point to melt a

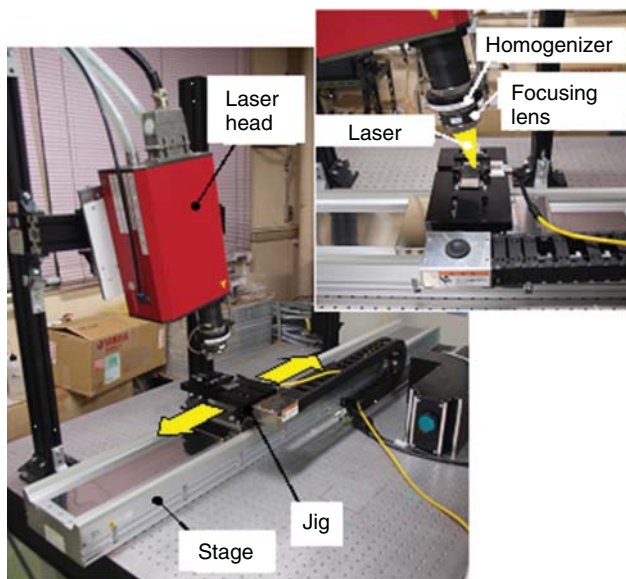


Figure 5.1 3 kW diode laser apparatus, jig, and stage and their arrangement for LAMP joining.

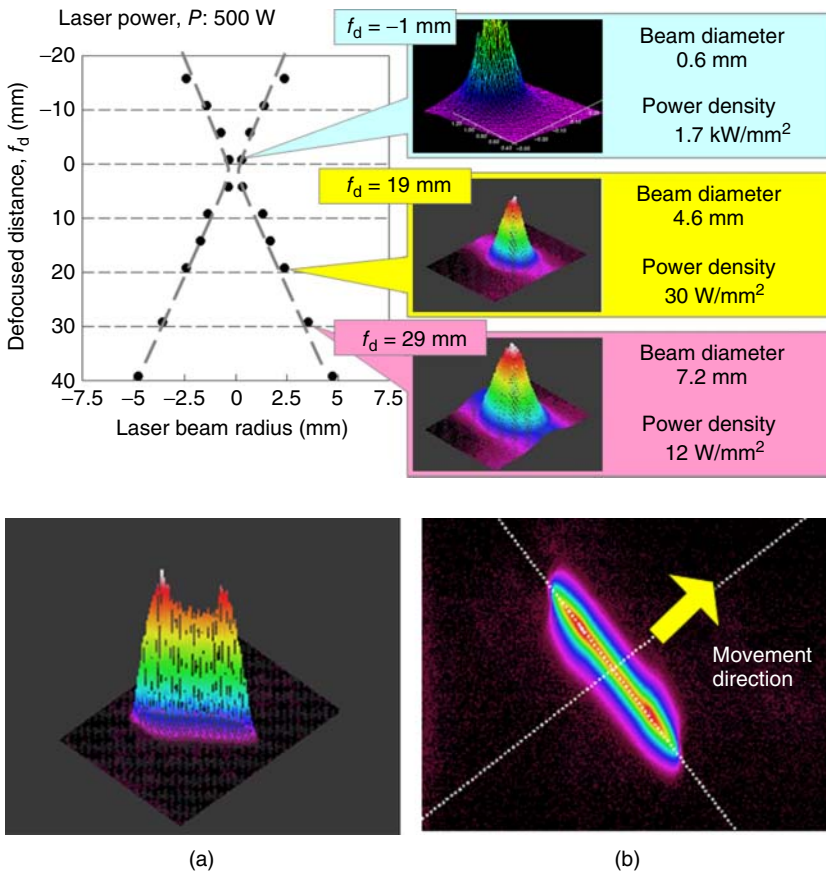


Figure 5.2 Features of power density distribution and mode of YAG laser beam (a) and diode laser beam (b). In (a), the focusing of the YAG laser beam is shown as a function of defocused distance, mode, power density, and beam diameter. In (b), the diode laser beam power density distribution, mode, and traveling direction are detailed.

metal or, under the defocused conditions, to heat a wider area. The line-shaped beam mode can be used for LAMP joining. The laser for LAMP joining is generally utilized under the defocused conditions of a YAG laser (Figure 5.2(a)), but at the focal position for a diode laser of a line-shaped beam (Figure 5.2(b)).

The LAMP joining methods are schematically illustrated in Figure 5.3. The laser beam is shot from the metal or plastic side in continuous- or pulsed-wave modes on the lapped sheets of metal and plastic in order to melt the polymer near the lap joint interface and simultaneously to generate small bubbles in the molten plastic material. A polymer with high laser transparency of more than 70% can be directly subjected to the laser irradiation from the plastic

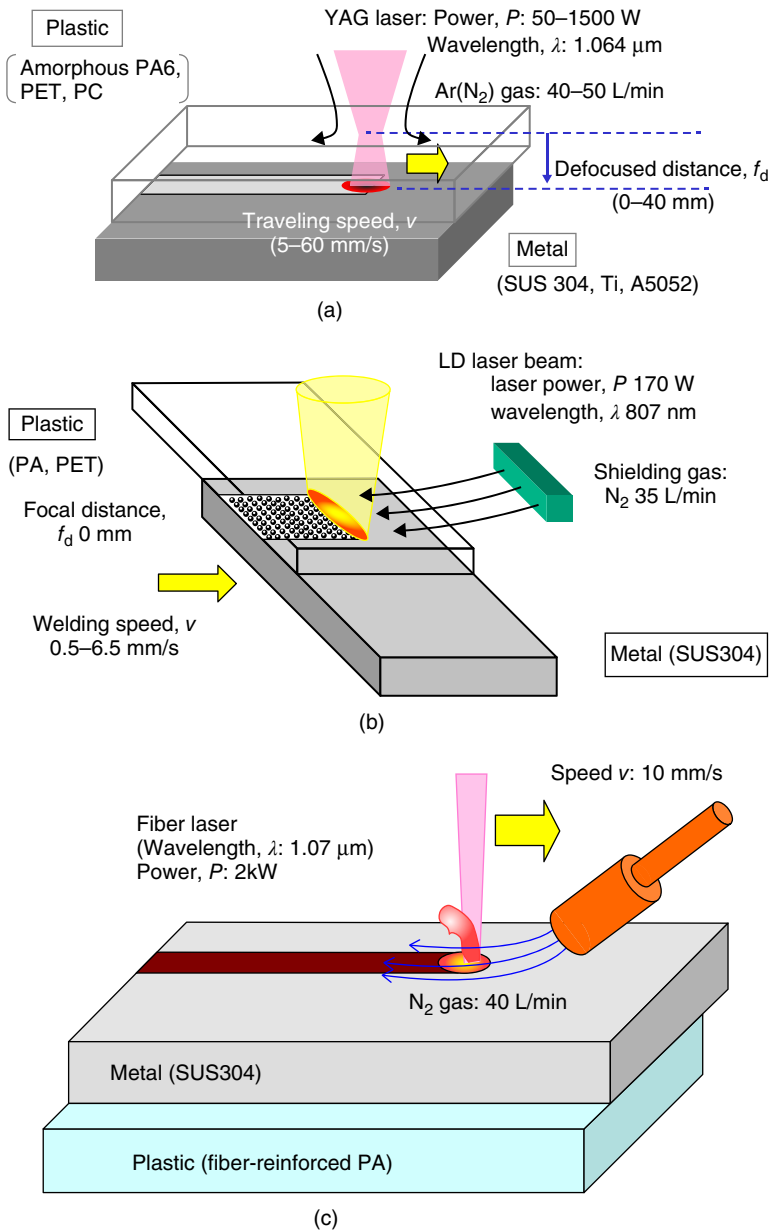


Figure 5.3 Schematic experimental setup of laser-assisted metal and plastic (LAMP) direct joining methods using various lasers: (a) YAG laser irradiation for transparent plastic, (b) diode laser irradiation for transparent plastic, and (c) fiber laser irradiation for nontransparent plastic. Feasible materials combinations and their joining parameters are shown in the schematics.

side (Figure 5.3(a) and (b)). On the other hand, the laser shooting onto the metal surface can be applied to any thermoplastic (also the nontransparent polymers), especially for CFRP or GFRP composites. The laser joining conditions are chosen depending on the metal plate properties and thickness. Laser joining is performed under the shielding gas flow, similarly to the usual metal laser welding. In addition, in the case of heating from the metal side (Figure 5.3(c)), other heat sources, such as electron beam, plasma, arc, friction (e.g., the Friction Spot Joining process, see Chapter 3), can be utilized.

The advantages or merits of the LAMP joining process are as follows: (1) short joining times, leading to high productivity; (2) easy automation; (3) long-term stability because of bonding due to the melting of plastic matrix without adhesive bonds or glues; (4) absence or reduced surface treatments required for metal plate; (5) small scattering of joint mechanical properties; (6) high joint tensile shear or peeling strength; (7) highly significant reduction in VOC emission; (8) no use, preparation, and storage of adhesives and/or mechanical fasteners; (9) no limit of part size and related costs; (10) feasible for any metal; and (11) no increase in weight, since no fasteners are being used. The drawbacks or limitations of LAMP joining are listed as follows: (1) newly developed processes and limited investigation data for combinations of metals and plastics; (2) not adequate for all polymers (thermoset are not adequate for LAMP); (3) tight contact interface between metal and plastic (without gap) is required; (4) large variety of part geometries feasible; (5) limited experimental data available on mechanical properties, fatigue properties, heat resistance among other joint properties essential for designing hybrid parts.

5.3 Features and Mechanical Properties of Metal–Plastic Laser Joints (LAMP Joints)

As mentioned before, LAMP joining is feasible between any metal and thermoplastic materials, but not for thermosetting resins, which do not melt and reconsolidate. Figure 5.4 shows an example of a LAMP lap joint between a Type 304 stainless steel plate of 3 mm thickness and an amorphous PA (polyamide; PA6) sheet of 2.3 mm thickness. The PA sheet side was subjected to the irradiation of a CW Nd:YAG laser beam. The top surface of PA sheet was not damaged, showing the as-received smooth surface finishing. This LAMP joint was produced by melting the thermoplastic matrix adjacent to the lap interface of the metal, although the metal surface was not melted. Some small bubbles are also present in the plastic fusion zone as a result of thermal degradation.

Figure 5.5 shows the joints produced at laser powers (P_0) from 110 to 850 W at the defocused distance of 20 mm and the welding speed (v) of 10 mm/s. The bead (welding seam) width became wider with the laser power. Many bubbles are formed inside the polymeric part of the LAMP joint at the laser power

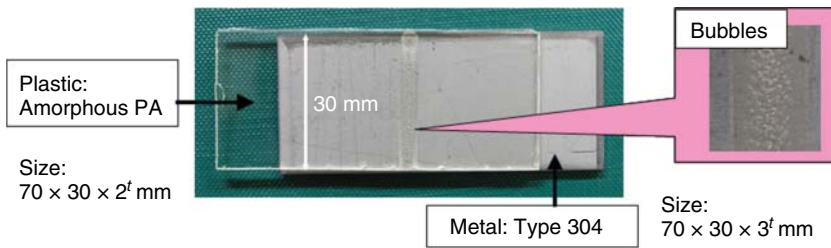


Figure 5.4 Surface appearances of LAMP joint of amorphous PA6 and Type 304 Type 304 sheets made with YAG laser at $f_d = 20$ mm and welding speed $v = 10$ mm/s. Detailed picture shows small bubbles in the weld seam.

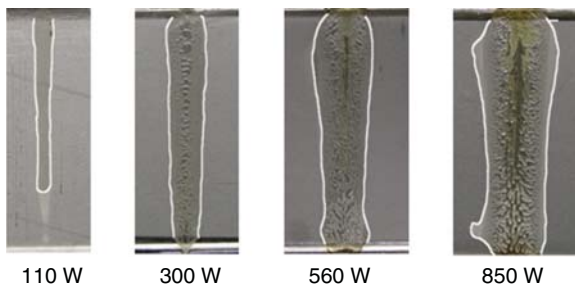
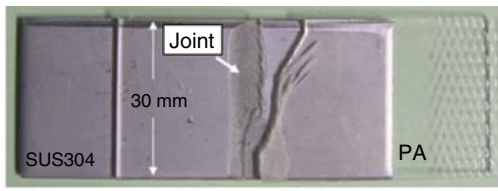


Figure 5.5 Effect of YAG laser power on weld seam of LAMP joints of amorphous PA6 and stainless steel Type 304 sheets. (Joining parameters: defocused distance, f_d : 20 mm; traveling speed, v : 10 mm/s; Ar gas flow rate: 50 L/min).

of 300 W, as a result of thermal degradation. The size and number of bubbles visually increase with the laser power. If the laser power is set too high, large bubbles or holes are formed in the molten plastic volume, decreasing the mechanical properties of the joint.

The quasistatic strengths of LAMP lap joints were evaluated by tensile shear testing. Figure 5.6(a) and (b) exhibit examples of LAMP joints between Type 304 stainless steel and PA6 or PET after the test. The fracture occurred outside the joined area in the polymer base plate (Figure 5.6(a)); in some cases, the final crack does not occur concomitantly with the plastic deformation in the ductile polymeric base plate (Figure 5.6(b)).

Figure 5.7 shows the tensile shear loads of the LAMP joints between Type 304 stainless steel and amorphous PA as a function of YAG laser power at three defocused distances. The highest loads are obtained by the fracture of the plastic base plate at the proper laser powers for the respective defocused distances. An ultimate lap shear force of 2000 N was achieved at the laser power of 750 W, with the defocused distance of 30 mm and the speed of 10 mm/s. The lower forces or the reductions in strength of the joints are attributed to the narrower joining areas at lower powers and the formation of large bubbles (thermal



(a)



(b)

Figure 5.6 LAMP joints after tensile shear test, showing fracture in base PA (a) and elongation of base PET (b).

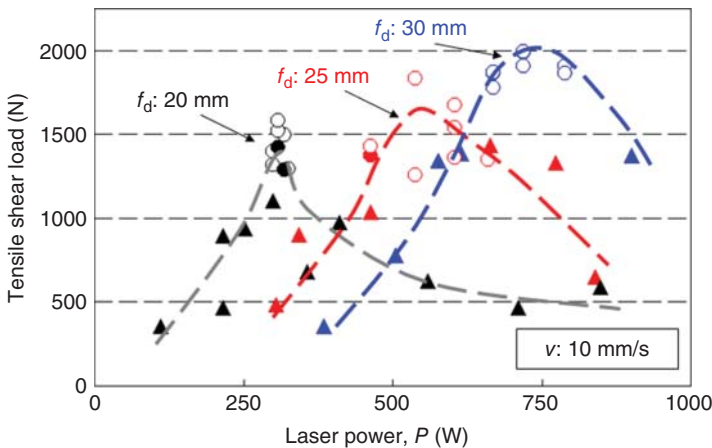


Figure 5.7 Effects of the laser power and defocused distance on the tensile shear loads of LAMP joints between Type 304 stainless steel and amorphous PA6 produced with YAG laser.

degradation) at higher powers. Figure 5.8 shows the tensile shear loads of the LAMP joints between Type 304 stainless steel and PET made with diode laser with a line-shaped beam. Ultimate loads of about 3000 N can be obtained, with the final failure taking place by the elongation of the PET base plate.

In consideration of less transparent polymers, a laser beam should be irradiated on the metal side. Figure 5.9 shows the LAMP joint between 3 mm thick stainless steel Type 304 plate and 2.3 mm thick amorphous PA6 sheet

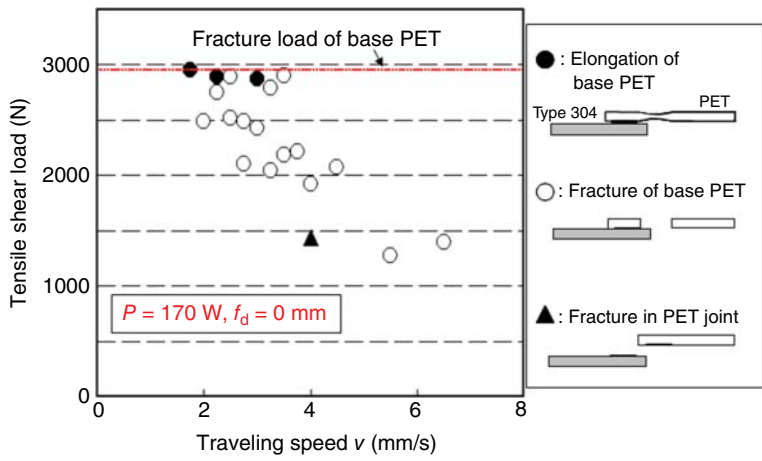


Figure 5.8 Effect of traveling speed on tensile shear loads of LAMP joints of Type 304 stainless steel and amorphous PET made with diode laser of line-shaped beam.

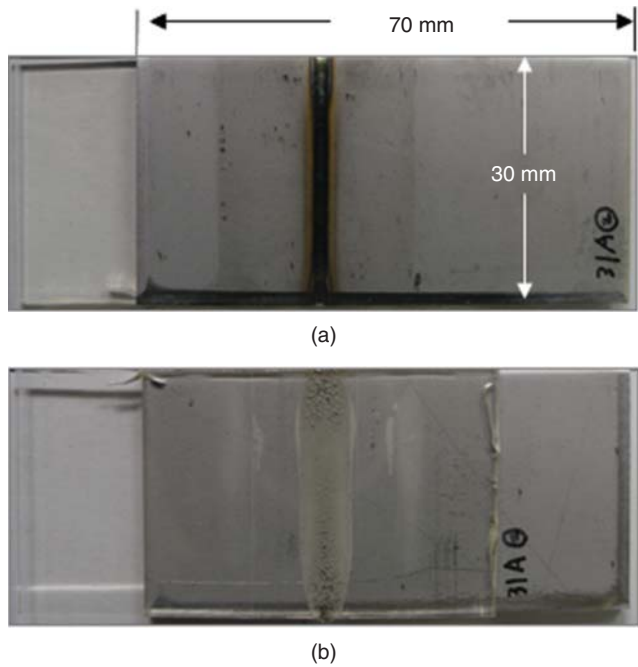


Figure 5.9 Surface appearances of LAMP joint of Type 304 stainless steel and amorphous PA sheets produce by laser irradiation on metal surface, showing partial melting of Type 304, partial melting of PA near interface (a), and formation of bubbles in melted PA (b).

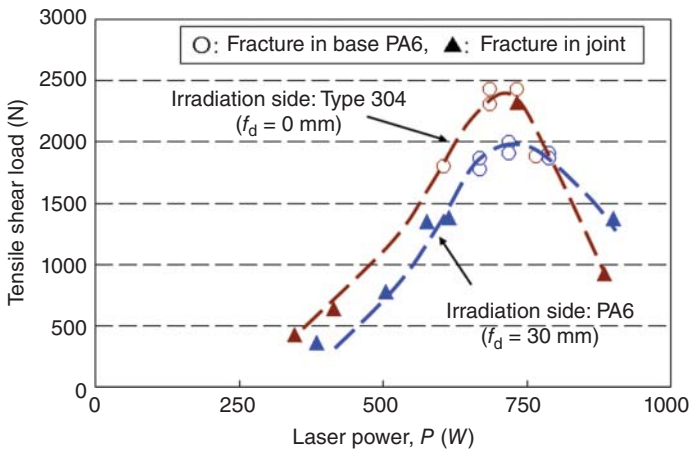


Figure 5.10 Effects of YAG laser irradiation direction and laser power on tensile shear load of LAMP joint of Type 304 stainless steel and amorphous PA6.

produced by the fiber laser irradiation onto the metal surface at the power of 2 kW, with the defocused distance of 20 mm and the speed of 10 mm/s under 40 L/min nitrogen gas flow (nozzle of 8 mm diameter). A partially penetrated laser fusion zone is formed in the Type 304 stainless steel, and the molten zone including many small bubbles is present in the PA plastic, which are intrinsic to the LAMP joining process. Figure 5.10 compares the tensile shear loads of the LAMP joints made by laser irradiation on the metal side and the plastic side. High loads of the joints can be obtained by the fracture of the polymer base plate under the proper conditions in both processes. In the case of PA6 including 30% glass fibers (GFRP), the joint with the maximum load of about 4000 N could be produced (results not shown in this chapter).

Recent studies at the Osaka University [3–10] have shown that laser-assisted metal and plastic (LAMP) direct joining was feasible between some engineering plastics such as amorphous, crystalline, and amorphous–crystalline mixed PA (polyamide), amorphous PET, PC, and PBT, PA reinforced with glass fibers, and PA and PC reinforced with carbon fibers, and different metals, such as mild steel, Zn-coated steel, Type 304 stainless steel, AA 5052 aluminum alloy, titanium, and magnesium alloy.

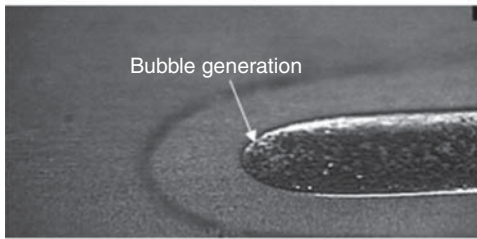
5.4 Mechanisms of LAMP (Laser-Assisted Metal and Plastic) Direct Joining

In order to understand the LAMP joining mechanisms, high-speed video observation and temperature measurement using thermocouples and radiation

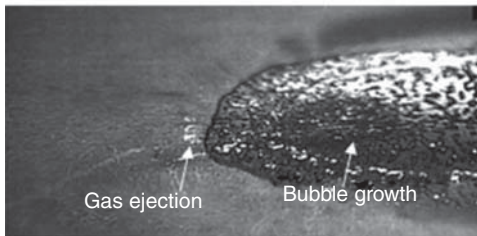
thermometry or pyrometry were performed during joining. These results are exhibited in Figure 5.11. At the laser power of about 215 W (Figure 5.11(a)), heat discolorations were observed around the laser beam spot, but large bubbles were not generated. At about 310 W (Figure 5.11(b)), small bubbles were formed inside and in the vicinity of the molten area of the polymer. At 560 W (Figure 5.11(c)), the size and the number of the bubbles visually increased, while large bubbles, resulting in low joint strength, were retained. Furthermore, abrupt gas eruptions were observed as narrow lines out of the boundary of the molten area (Figure 5.11(c)). This suggests that high gas pressures were induced due to the evolution of bubbles during joining. Figure 5.12(a) and (b) show examples of high-speed video recording and thermometry observation results of the polymer and metal bottom side during laser joining (laser irradiation on the metal side). During joining, the thermoplastic is softened and molten, and then bubbles are generated. Large bubbles in the centerline are easily formed due to higher process temperatures. A line-shaped laser



(a)



(b)



(c)

Figure 5.11 High-speed video observation results near laser irradiation part during LAMP joining of PA sheet lapped on Type 304 stainless steel plate: (a) laser power of 215 W, (b) laser power of 310 W, and (c) laser power of 560 W.

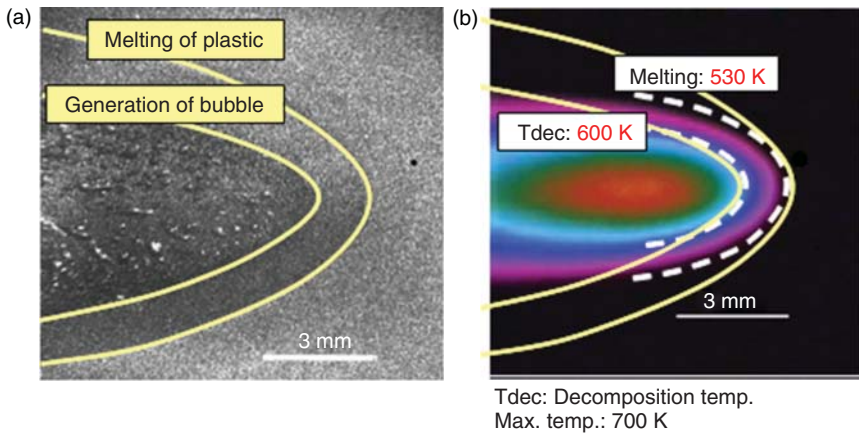


Figure 5.12 High-speed video and thermometer observation results of bottom side of Type 304 plate during LAMP joining: (a) high-speed image and (b) thermometer observation.

beam should be preferable to prevent the formation of large bubbles, since it would allow rapid heating and cooling rates.

The gases' compositions inside the bubbles were investigated by mass spectroscopy, by drilling the plastic sheet with the drill of 2 mm diameter toward the steel plate under the vacuum of 7×10^{-6} Pa. The mass spectra of the released gases were measured with a Q-mass spectrometer. The obtained results from the ejected gases and their kinds are shown in Figure 5.13. When the tip of the drill reached the degraded polymer region, the gases are released, contaminating the vacuum environment. It was confirmed that the bubble gases consisted

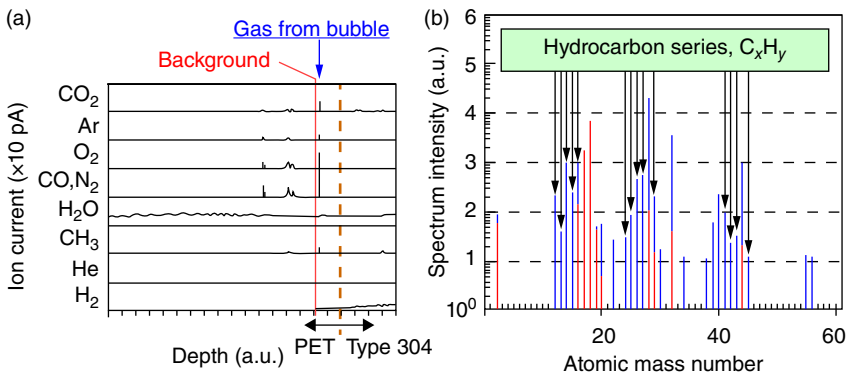


Figure 5.13 Q-mass spectrometer measurement results showing ejection of various gases from entrapped bubbles (a) and compositions and amount of gases released from the bubbles and background (b).

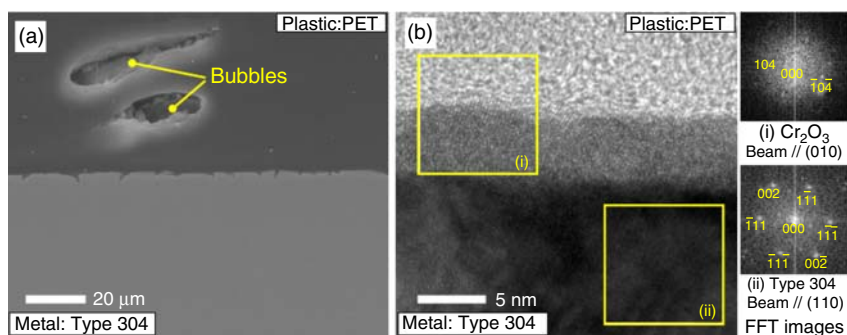


Figure 5.14 SEM and TEM observations and analysis results of LAMP joint of Type 304 stainless steel and amorphous PET produced with diode laser. a) Metal-polymer interface with small bubbles; b) Detailed view of the metal-polymer interface showing polymer infiltrated in the natural oxide film covering the metal.

of air (N_2 and O_2) present in the gap between the sheets before joining and several kinds of hydrocarbons due to the extensive decomposition of the plastic.

The LAMP joints were observed at higher magnifications with scanning electron microscopy (SEM) and transmission electron microscopy (TEM). The examples of the joint between Type 304 stainless steel and PET are shown in Figure 5.14. The SEM photo shows that the polymer is bonded all over the interface on the metal, and small bubbles are present within the thermoplastic near the joint interface (Figure 5.14(a)). The base metal and intermediate layer in the TEM image in Figure 5.14(b) were identified to be fcc austenite phase and 5 nm thick Cr-oxide film from the diffraction patterns, respectively. The TEM image demonstrates that the metal and the polymer are tightly bonded on the atomic or molecular levels through the natural oxide film always covering the metal. Moreover, the LAMP joints were analyzed via X-ray photoelectron spectroscopy (XPS) and time-of-flight secondary ion mass spectrometry (TOF-SIMS) to confirm the possibility of chemical bonding. The results in [8] indicate that there is a possibility of chemical bonding due to the formation of organic–metallic bonds, such as C_6H_7OCr , metal–Cr–O, or Cr–O in the LAMP joint.

Figure 5.14 SEM and TEM observations and analysis results of LAMP joint of Type 304 stainless steel and amorphous PET produced with diode laser.

Figure 5.15 shows a SEM photo near to the joint interface of PET and Mg alloy AZ91D (with pits or porosities produced artificially by surface treatment). EDX spot analysis results of the area (1) and (2) in the healed pit are also present in the figure. Molten PET flow can be observed, filling a pit or a pore in the Mg alloy. As polymer is detected by EDX in the pit/pore, it indicates that high pressures were induced to force the melting of the polymer on the metal surface, probably by the bubbles formation in the plastic during LAMP joining.

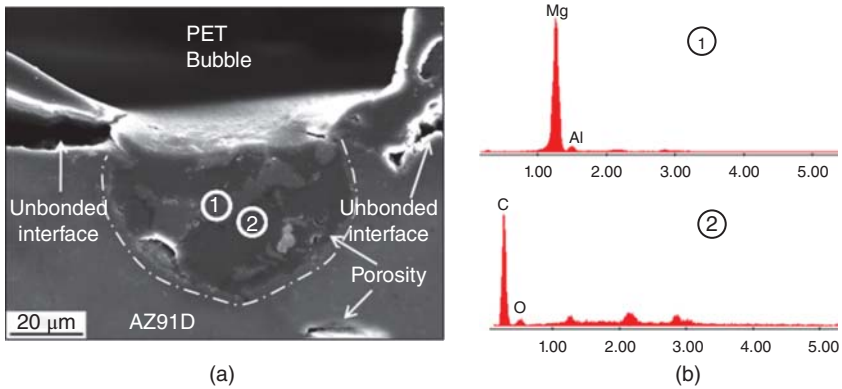


Figure 5.15 (a) SEM photo of LAMP joint between surface mechanically treated AZ91D and amorphous PET produced with diode laser and (b) EDX analytical results of areas inside (a) Mg pore, showing plastic-melt flow into pit.

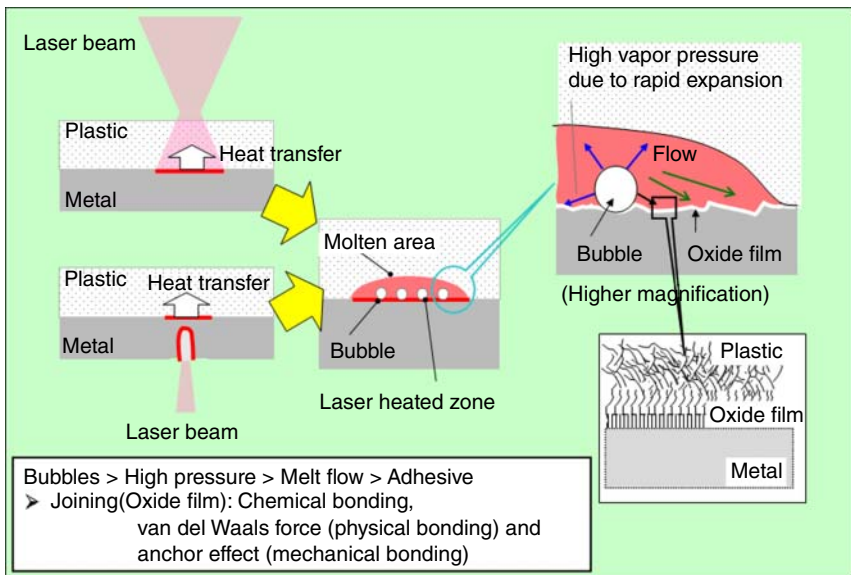


Figure 5.16 Schematic representation of mechanisms of laser direct joining process of metal and plastic (LAMP joining process).

From the previous observation and the investigation of results, the mechanisms of LAMP joining are schematically represented in Figure 5.16. A laser beam is irradiated through the transparent plastic sheet or directly on the metal plate to melt the plastic. The thermoplastic is melted, and some small bubbles are formed due to the expansion of air and/or the partial

decomposition of the melted polymer. The melt becomes active and is forced to flow on the metal surface. The molten polymer can flow into the pits or concave parts and is bonded onto the oxide film. Consequently, strong LAMP joints can be produced by the anchor (mechanical bonding) effect as well as van der Waals interaction force and chemical bonding.

5.5 Reliability Evaluation Tests

The leakage test was performed for Type 304 stainless steel and PET LAMP joints produced with a line-shaped diode laser. The schematic and actual procedures and the measurement results are shown in Figure 5.17. It was consequently confirmed that LAMP joints were resistant to He leakage, although the bubbles were present in the joints. Therefore, a tight bonded area must be present.

The heat-cycle resistant test of LAMP joints was also performed. Maximum tensile shear strength samples of LAMP joints between PET and Type 304 stainless steel were produced with a diode laser of 170 W laser power, at 3 mm/s welding speed. The test conditions and the appearances of the samples after the heat-cycle test and the results of the tensile shear test are shown in Figure 5.18. The temperature was changed in the range between -40°C (233 K) and 40°C (313 K) at intervals of 1 h. This cycle was repeated 25 and 50 times. Three and thirteen samples were used in the 25- and 50-cycle test, respectively. After the heat-cycle test, the samples were tested under tensile

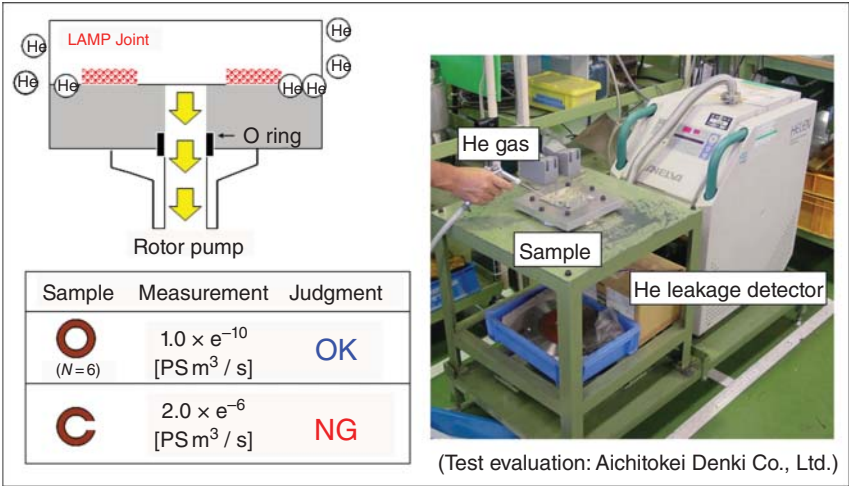


Figure 5.17 Schematic and actual leakage test procedure and leakage measurement results of LAMP joints. (Notation: Judgment “OK”: joint passed test; Judgment NG (not good): joint did not pass test). (Courtesy of Aichitokei Denki Co., Ltd.).

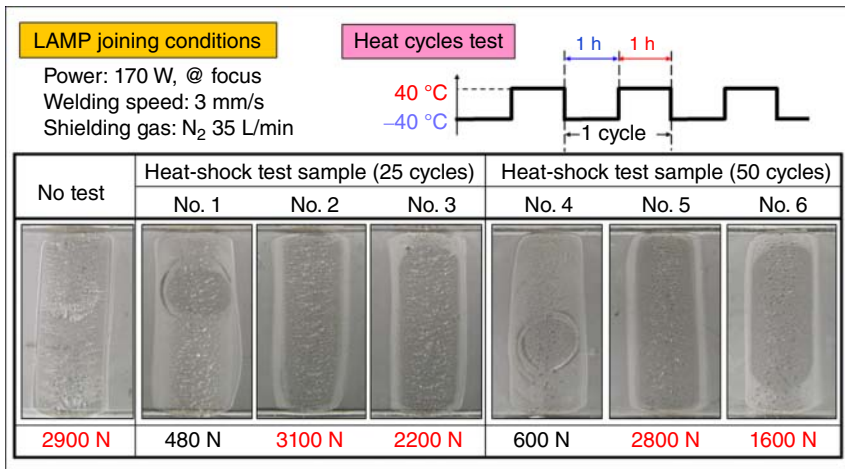


Figure 5.18 Heat-shock test results of LAMP joints of Type 304 and PET. (Fracture load of specimen before shock test: 2900 N).

shear loading and compared with the ones before the heat-cycle test. The obtained samples are divided into three groups, according to the joining conditions. Consequently, some LAMP joints could resist heat cycles, where the joining areas were still present and the tensile shear loads reached over 2700 N. On the other hand, the tensile shear loads of the other sample groups decreased depending on the joining areas left after the heat-cycle test.

5.6 Evolution of LAMP Joining

Laser welding/joining of dissimilar metals joined with an intermediate polymeric layer was carried out by utilizing the LAMP joining technology. The process to produce a mild-steel-aluminum alloy joint with a PET intermediate layer is illustrated in Figure 5.19. The first step of dissimilar metals welding was performed by the LAMP joining between PET and mild steel by plastic side irradiation, and the second step was done between AA 5052 and PET by metal side irradiation. A high ultimate load of over 5000 N was achieved, as shown in Figure 5.20. One advantage of this LAMP joint geometry in comparison to the respective dissimilar welds is the possible improvement of the corrosion resistance between different metals, due to the used PET interlayer.

Dissimilar lap joints between Type 304 stainless steel plate, Zn-coated steel sheet or AA 5052 aluminum alloy sheet, and CFRP sheet could be produced by irradiating a disk laser on the Type 304 stainless steel plate (results not shown in this chapter). The tensile shear test results, specimen, and cross sections of the joint are shown in Figure 5.21 for Type 304 stainless steel–CFRP joints of 3 mm thickness and 20 mm width. Generally, high mechanical strengths were

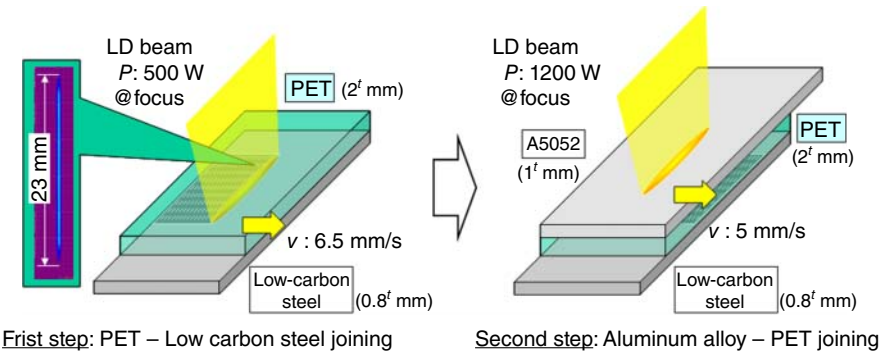


Figure 5.19 Schematic joining process of dissimilar Type 304 stainless steel and AA 5052 sheets with PET interlayer plate.

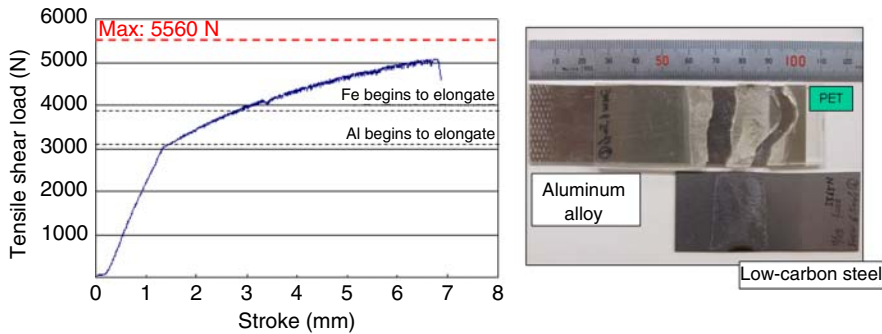


Figure 5.20 Mechanical test results (stress–strain curve) and fractured sample of dissimilar Type 304 stainless steel and AA5052 sheets with PET interlayer plate.

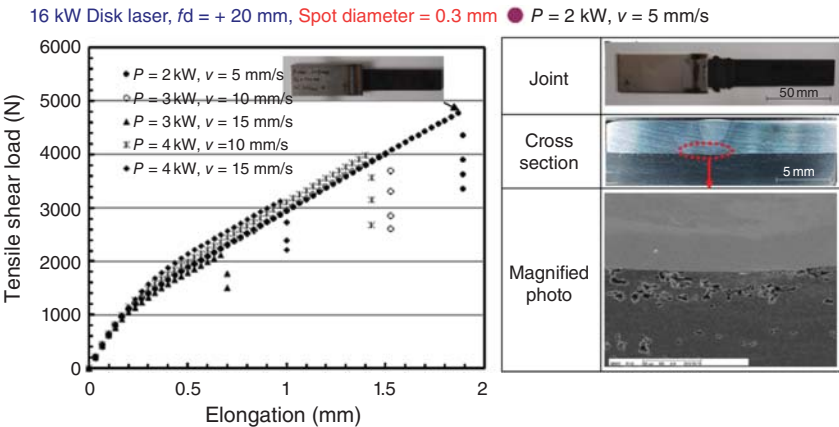


Figure 5.21 Tensile shear test results of Type 304 stainless steel and CFRP sheets LAMP joints (a), and specimen example, joint cross section, and magnified photo near joint interface showing bubbles in CFRP (b).

achieved. Final fracture occurred in the CFRP base plate, where entrapped gas bubbles were also present. Some joints were very strong, depending upon the joining areas according to the setup joining conditions. The ultimate tensile shear load of the joint reached 4770 N in the sample of 20 mm width. 3000 N loads could be achieved for the joint of 3 mm thick CFRP plate and 0.7 mm thick Zn-coated steel sheet or 1 mm thick aluminum alloy sheet. Therefore, it was confirmed that high-strength joints of CFRP and metal could be produced.

5.7 Conclusions

In this chapter, we demonstrated and described the new laser direct joining (LAMP) method for metal–polymer hybrid structures. Virtually any metal alloy can be directly joined with engineering thermoplastics, including GFRP and CFRP composite sheets. LAMP joints with high quasistatic strength can be produced when working with the proper process parameters/joining conditions. Thermal degradation and air-expanded bubbles are present in the molten plastic seam. However, the joints with small bubbles are strong and durable under quasistatic loading. In the near future, industrial applications of laser direct joining of metal and plastic (LAMP joining) without glues or adhesives can be expected for lightweight structures.

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Part II

Joining Processes Based on Mechanical Interlocking

6

Principles of Mechanical Fastening in Structural Applications

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6.1 Introduction

This chapter starts with one of the first questions that arise regarding mechanically fastening: “why parts should be mechanically fastened?” The answer to this question, in a simple and obvious manner, is that some parts can only reach a determined functionality when attached to each other. Nevertheless, there are several reasons by which parts must be manufactured separately and later joined to each other, and we can exemplify some of them:

Technical feasibility: some inherent restrictions in the manufacturing process, such as equipment and raw material sizes, do not allow parts to be manufactured into their initially desired dimension, so it drives this whole part to be manufactured into subcomponents of smaller dimensions before they compose a final assembly (e.g., rolled sheets to manufacture the segments of a whole aircraft fuselage);

Relative motion: to allow the parts undergo relative displacement and motion (e.g., aircraft actuators with rods and bearings);

Enclosing parts: to allow parts to be assembled in compartments (e.g., engines with internal components, and also with relative motion);

Assembly composition: to allow parts of different kinds of materials and technologies to be composed in one final product (e.g., electronic devices, with metals, ceramics, plastics, etc.);

Structural requirements: to meet determined requirements, as imposing restraints to crack growing, for instance (e.g., wing skin panels with divisions to avoid crack propagation);

Accessibility: to allow temporary access to a specific enclosed compartment (e.g., inspection doors in an aircraft structure).

The need of joining parts to create a new part with a specific functionality dates from the ancient times of humanity, when humans started to create some rough tools from rock and wood chips.

The first metallic fasteners date from the bronze era, with the manufacturing of rough nails for wood attachment.

Since then, mechanical fastening concepts and devices have been developed in far-gone levels, so in the modern industrial environments, joining by fasteners is totally widespread.

In an assembled car, there are hundreds of screws and bolts to mechanically fasten parts from engine, suspension, steering, and all the car systems. A typical car may have on average 1500 bolts and screws. In modern aircraft structures, the heat-treatable aluminum alloys employed do not show good fusion weldability – one more reason for the vast usage of fasteners. In a jet aircraft, for instance, there must be more than hundreds of thousands of rivets, screws, bolts, and other hardware involved in mechanical fastening of wing structures, empennages, fuselage, landing gears, hydraulic system, fuel system, air system, and so on. A typical regional jet may have more than 400,000 fasteners. For larger aircraft, that number can reach some millions. Another emblematic example is the Eiffel Tower, with more than two and a half million rivets in its iron structure.

There are many kinds of joining processes in the industry. For instance, the fusion welding is a kind of process recommended for permanent similar (metal-to-metal) joining, showing good fusion weldability. Relatively newer processes, such as friction welding and derivations, do not depend on fusion weldability and can meet the demand of joining dissimilar materials, virtually unweldable by conventional fusion welding techniques.

In this chapter, the main focus of our revision is the mechanical fastening in the aircraft industry, which means joining parts by using fasteners for the aircraft assembly and their related concepts. This chapter mainly focuses on the mechanical behavior of fastened structures. It also addresses the joint structural design concepts and revises the corrosion behavior and the main fastener material properties for mechanically fastened structures.

6.2 General Joint Structural Design

Joints are usually interpreted as the weakest part of the structure when compared to the components or parts that are being joined, and for this reason, they usually deserve special attention from the points of view of static strength as well as fatigue, crack growth resistance, and occasionally, resistance to dynamic loads. In order to assure safe and optimum design, joints are frequently subjected to more detailed analyses, when compared to other components, and to more tests for verification of their structural integrity and durability, as well

as for correlation with analysis data. Further, when experimental data is not available, specific design factors are frequently applied to joining components.

The selection of a joint depends on the structure application, geometry, and loading conditions. Manufacturing processes and maintenance requirements must also be taken into account. Regarding maintenance, some joints are designed to be assembled and disassembled, while for others, it must be assured that once they are assembled during fabrication, their properties (such as clamping, fastener tightening, interference, etc.) will remain the same for long periods without maintenance.

While civil and naval applications have extensive use of welding, most of the aerospace applications still rely on mechanical fastening. Some structural parts are joined through adhesive bonding (e.g., when one or both members of the joint are made of nonmetallic materials), and some combinations of adhesive bonding and fastening have been found, aiming to assure a certain level of redundancy.

For all types of joint configurations, the joined materials may be similar or dissimilar, depending on the purpose of the structure. Examples of dissimilar materials include joints of different metallic alloys and hybrid – metal-to-composite – joints that are riveted, welded, or bonded. Usually, when different materials are joined, it is necessary to observe all design aspects related to similar materials and other issues that may arise due to dissimilarity. Some examples of design considerations for dissimilar material joints are thermal loads (when both materials have different expansion coefficients) and corrosion in the material interface (when the materials have different galvanic properties).

Joints may work subjected to tension, bending, shear, or combinations of these loads. Even when the remote load is purely tension, if there is some degree of eccentricity between the joined parts, locally there will be the so-called secondary bending, whose influence particularly on the joint fatigue behavior will be addressed later in this chapter.

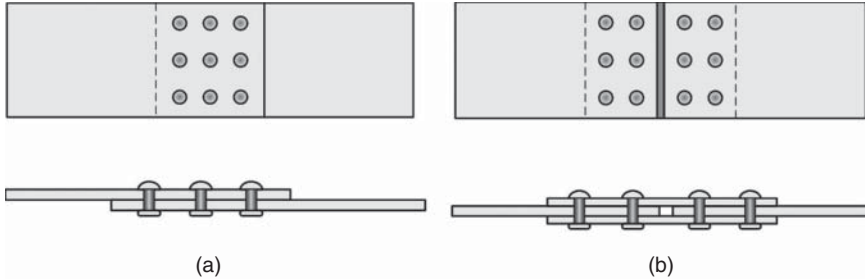
Figure 6.1 shows some examples of shear joints and tension joints. Shear joints are divided into lap joints and butt joints and may also be divided into single- or double-shear configurations (Figure 6.1(a) and (b)). Tension joints may be subjected to tension loads only (symmetric) and to tension and bending loads (nonsymmetric) (Figure 6.1(c) and (d)).

6.3 Shear Joints

6.3.1 Failure Modes

As most of the structural components, joints are dimensioned for static and dynamic loading conditions. Ideally, for an appropriate sizing, the loading and environmental conditions should be well understood.

Shear joints



Tension joints

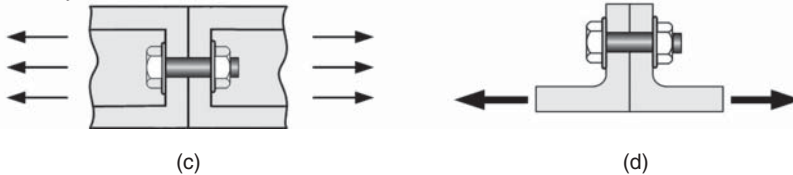


Figure 6.1 Types of joints (general). (a) Lab joint, single shear; (b) butt joint, double shear; (c) axial (no bending); (d) tension and bending.

For static loading conditions, fastened joints can fail by fastener shear-out and hole bearing, plate shear, and plate tension. These four failure modes are shown schematically in Figure 6.2. While fastener shear-out and the plate failure modes presented may be predicted by easy calculations based on the material strength allowables (i.e., yielding and rupture), hole bearing is a somehow more complex failure mode, and allowables for hole bearing are usually obtained by means of experiments. Further, as depicted in the figure, hole bearing may be mixed with some level of plate bending and consequently fastener pull-through failure. The allowable bearing stresses for various materials are supplied by [2] and [3, 4] in supply tables with allowables for combinations of typical fasteners and plate materials used for aerospace applications.

Even joints with the same type and size of fastener may be subjected to different failure modes, depending on the plate thickness, fastener pitch, and edge distance. The increase in plate thickness, for example, is responsible for transitions from fastener bearing to shear-off and from plate shear to plate tension failures. The distance from the fastener hole to the edge of the plate (in any direction) is also an important design parameter, and edge distances larger than two times the hole diameter are usually recommended in aircraft design.

Besides static failure due to limit loads, the most critical problem observed in mechanically fastened joints is failure due to fatigue. When the joint is

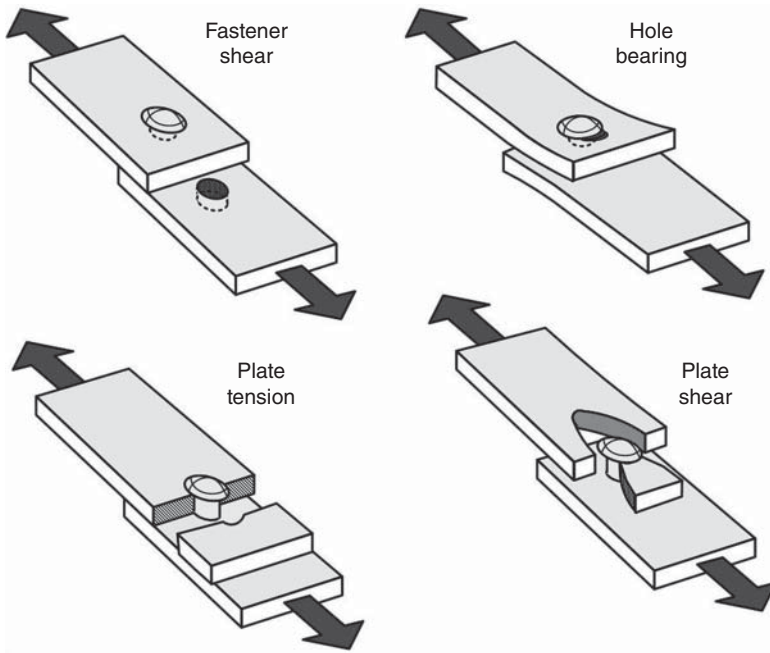


Figure 6.2 Static failure modes in joints (Source: Adapted from Rijck 2005 [1]).

subjected to cyclic loading during long periods, small cracks may nucleate from hole edges and propagate toward the adjacent holes (e.g., in metallic structures). When compared to static failure, damage due to fatigue presents a larger variability and is influenced by other aspects such as hole finishing, interference, and plate surface treatments. Fortunately, there are presently various engineering methods to predict crack nucleation and to address crack propagation in mechanically fastened joints, such as analytical and numerical approaches, to be further discussed in this text.

All failure modes for a fastened joint are primarily related to load transfer. The load that is transferred from one member to the other may be divided in two parts: (1) the load that passes across the members, usually named the bypass load; and (2) the load that is effectively transferred from one member to the other through the fasteners, which known as the bearing load. While the first two failure modes shown in Figure 6.2 (fastener shear and hole bearing) are related to a high bearing load, the other two failure modes (plate shear and tension) are attributed to high bypass loads.

For a fastened lap joint or butt joint, usually the outer row is the most loaded and therefore the first candidate to failure. Figure 6.3 shows – as a hypothetical example – a lap joint with three rows of fasteners. In the upper left row, the bearing load is 35% of the total and the bypass load is 100%. Just after, in

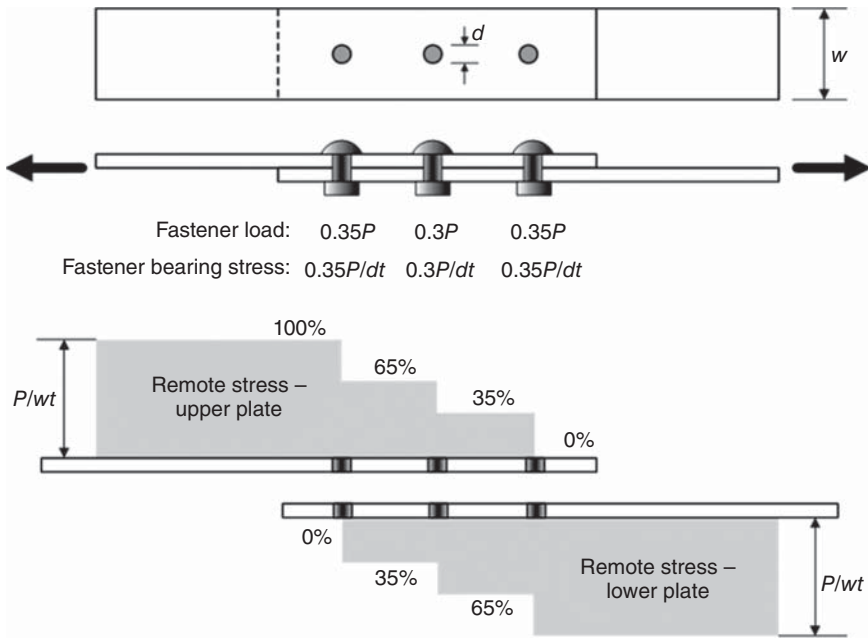


Figure 6.3 Representation of load transfer through a fastened lap joint with three rows of rivets.

the second row, the bearing load is 30% of the total, and the bypass load then becomes 65% of the remote load. Therefore, the upper-left and the lower-right holes have the maximum bearing and the maximum bypass loads, and these locations will be the first candidates to fail.

When the plate thickness is constant and the fastener type and dimensions are the same for all fasteners, the joint usually becomes unbalanced; or in other words, the amount of bearing and bypass load that passes through each row of fasteners will be different. The degree of unbalancing becomes higher as long as the number of rows increases. Many design parameters may be modified in order to obtain a balanced joint. Increasing the fastener diameter will decrease the effect of bearing, because there will be a better stress distribution around the fastener hole, once the bearing stress S_b is given by $S_b = P_b/dt$ (where P_b is the bearing load, d is the diameter, and t is the plate thickness). If the fasteners are too rigid when compared to the members, they will not allow an even load distribution, and whenever it is possible, the use of more flexible fasteners is desirable.

In addition, to allow a better distribution of the bypass stress, the most usual design practice is to increase or to decrease the net area of the members. Increasing the net area increases the stiffness of the cross section, and

assuming that each section works as an elastic spring, whose stiffness is given by $k = AE/L$ (where A is the cross-sectional area, E is the elasticity modulus, and L is the section length), it becomes clear that if A increases, the stiffness increases, and as a consequence, the amount of load transferred through this section will be higher. Hence, tapered joints or stepped joints are a common design practice in order to obtain a better load distribution.

Looking at the fastener neighborhood, the influence of bypass loads on the stress concentration around the fastener holes can be better understood when one looks at Figures 6.4 and 6.5. As shown in Figure 6.4, the plate members impose tension, bending, and concentrated bearing loads in the fasteners. While the influence of bearing results in stress concentrations in locations 1–5, the bypass load more specifically affects location 5, which is usually the region where fatigue cracks will nucleate. Figure 6.5 shows schematically the stress profiles due to remote tensile loading, remote bending, and local pin loading.

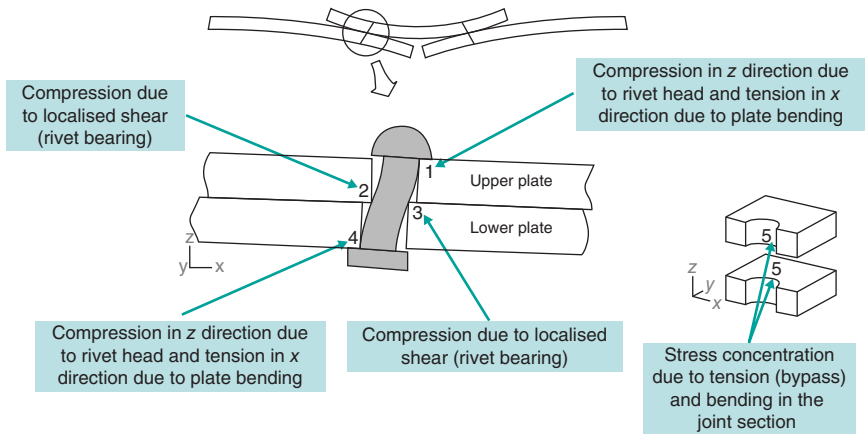


Figure 6.4 Stress concentration around fastener holes – influence of bearing and bypass loads.

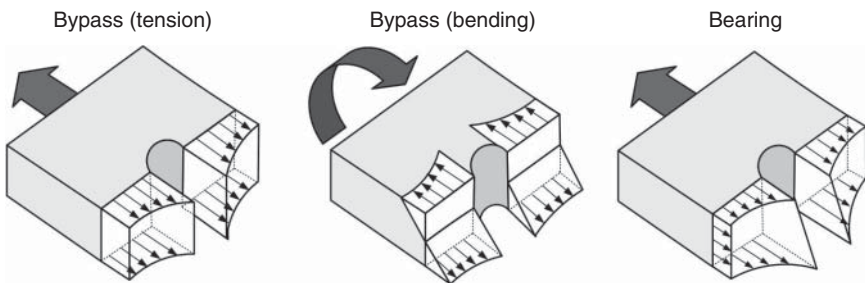


Figure 6.5 Stress profiles (schematic) for different remote loading conditions.

For a real joint, where usually these three efforts exist, superposition of stresses leads to the actual stress profile as a result. Further, the stress concentration due to each condition may be obtained separately, and the resulting stress concentration factor K_t around the hole is obtained.

There are various ways to calculate the amount of load transfer, the bearing load at each fastener, the stress at the members, stress concentration factors, and fracture mechanics parameters. These subjects will be discussed in the following section.

6.3.2 Models for Joint Analysis and Dimensioning

Joints may be modeled through various methods and levels of detail, and the concept of hierarchical modeling [5] may be helpful for appropriate simulation and understanding of the joint behavior. Table 6.1 shows means for modeling an aircraft fuselage metallic lap joint according to the results expected from the analysis. A first analytical approach [6], where only the rivet and plate stiffnesses are taken into account, allows estimating the amount of load transfer at each fastener row. This information is important mainly for static analysis. Besides a direct comparison with the fastener allowable, it permits rough fatigue life estimation [4]. One step ahead is the use of the elastic line theory in order to estimate the stress levels of the joint components [7], where the application of this theory for the calculation of bending stress in beams has shown remarkably accurate results when compared to experimental measurements.

Table 6.1 Hierarchical modeling for lap joint analysis.

Model type		Purpose	Comments
Analytical models	Spring model	Load transfer	Easy and fast
	Neutral line model	Load transfer + stress distribution	
Numerical (FEM) models	Bar elements (plates) and spring elements (fasteners)	Load transfer	Easy and fast
	Plate elements (plates) and spring elements (fasteners)	Load transfer + stress distribution	Includes bending
	Solid elements (plates) and spring and bar elements (fasteners)	Load transfer, stress distribution, stress concentrations, stress intensity factor	Includes bending
	Solid elements (plates) and solid elements (fasteners), including contact	More accurate results for stress concentration and stress intensity factors	Time-consuming

Table 6.1 also presents various ways to evaluate the joint structural behavior using the finite element method (FEM). While models with plate elements for the joined parts and spring elements for the fasteners will allow the understanding of the stress fields and the amount of load transferred, when the estimation of stress concentration around fastener holes for further detailed fatigue and fracture mechanics analyses is desired, a numerical model with solid elements, friction, and contact is preferred. Figure 6.6 shows an example of detailed FEM model used to estimate stress concentration around loaded holes in a fuselage shear joint. The contact between the hole and fastener surfaces due to bearing results in higher stress concentration locations, which are observed in the model.

Models for fracture mechanics analysis require the computation of stress, strain, and displacement fields in order to obtain the crack driving force parameters, such as the stress intensity factor K . Models usually correspond to through-cracks or corner cracks that originate from locations where peak stress values are observed in static FEM analysis or tests. The simulation of crack growth, although it follows the basic laws of fracture mechanics, is not an obvious task, and the complexity of the analysis may be attributed to the following reasons: (1) there are many possible crack nucleation and propagation scenarios, including the geometry transition from corner to through-crack, the influence of adjacent cracks, holes and boundaries, and the so-called continuing damage, when the growing crack reaches the adjacent hole and a new crack nucleates in the hole on the opposite side; (2) there may

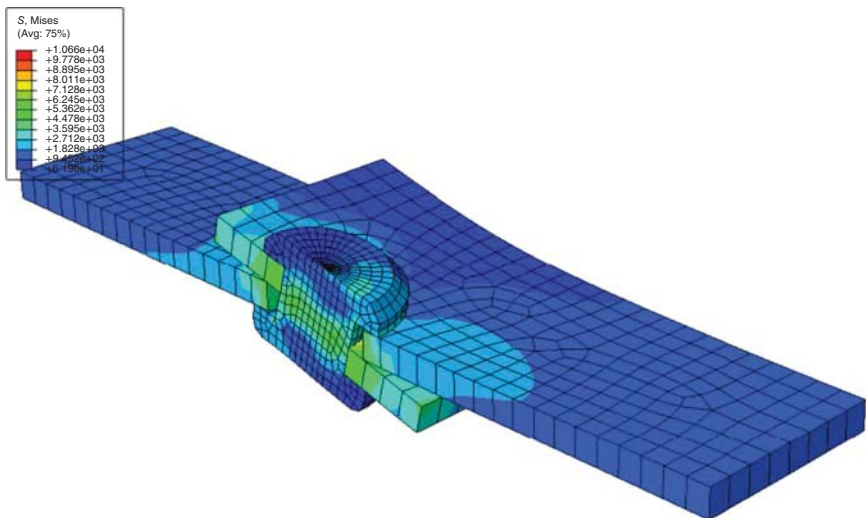


Figure 6.6 Detailed FEM model analysis results – model with solid elements, contact between upper and lower plate, and contact between fastener and holes.

be more than one crack growing simultaneously, characterizing a multisite damage (MSD) problem; (3) as long as the crack grows, there is a decrease of the net section, consequently increasing the net stress values along the remaining section.

Nevertheless, fracture mechanics analysis for joints can be carried out by means of analytical methods with compounding of basic solutions available in handbooks [8] and also by means of numerical approaches, where various commercial software can be used, some of them based on built-in analytical solutions [9] and others based on FEM and other numerical methods.

Hence, when joints are analyzed, there will be a trade-off between the information that is desired and the level of complexity of the model, where the resources available (not only in terms of computing resources, what is becoming no longer a problem, but mainly the availability of skilled people to develop and evaluate wide complex numerical models) should also be taken into account. In other words, if the only desired information is the fastener load or the amount of load transfer at each row, analytical approaches are faster and sufficient for this purpose, and complex numerical models are unnecessary.

The joint flexibility is very important information for design and analysis. For fastened joints, in order to model the joint appropriately, it becomes necessary to know the fastener flexibility. This information may be obtained from the literature [10] or by performing dedicated tests. The main parameters affecting the fastener flexibility are the material type (e.g., aluminum, titanium, steel, or Inconel alloys), the fastener diameter, and the thickness of the plates.

Friction is another property that influences the overall joint behavior. The friction between plates is mainly attributed to the clamping force. However, it is quite difficult to define a friction parameter for two clamped surfaces, because the clamping may vary with time while the joint works. Hence, shear joints are usually evaluated conservatively, with an approach where all loads are assumed to be transferred by bearing [4].

6.3.3 Secondary Bending

Two joined elements subjected to tension, such as a longitudinal lap joint in an aircraft fuselage with hoop loads, present a local effect called secondary bending. This effect is shown schematically in Figure 6.7. Each plate has a thickness t , such that there will be an eccentricity between both plates equal to t . As long as the load increases, both neutral lines tend to self-align, and mainly the outer rows are subjected to a higher stress levels due to bending as a consequence. While the tension stress varies linearly with the load applied (i.e., $\sigma_t = P/wt$, where w is the plate width and t is the thickness), the stress due to secondary bending initially increases (or decreases) following a nonlinear trend until a geometrical condition is reached such that both curves (corresponding to minimum and maximum stress) tend to increase proportionally to the axial stress.

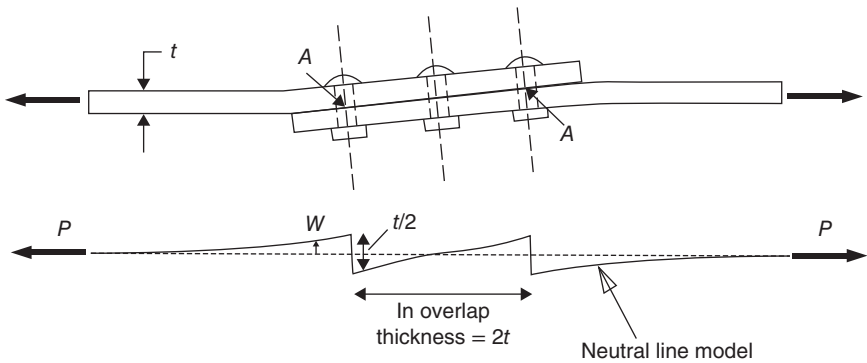


Figure 6.7 Plate and neutral line deflection for a three-rivet lap joint (Adapted from Schijve 2004 [11]).

The result of this effect in terms of stress values can be quantified by a bending factor k (see [11, 12], which is expressed by the relation between the bending stress and the tensile stress. For a joint with only one row of fasteners, $k = 3$, and this turns out to be the most critical configuration. For an increased number of rows and for a larger distance between the rows, k decreases to values between 1.0 and 1.5 [13].

The stress due to secondary bending can be calculated analytically, using the neutral line model. When a joint is analyzed with FEM, care should be taken such that the nonlinearities due to this effect are appropriately considered, because a local linear elastic analysis could lead to very conservative stress results as long as the load level increases.

From the point of view of design, obviously the best practice to avoid secondary bending effects is to use double-lap joints, for which $k = 0$. However, there are many circumstances when this is not possible, and in these cases, what is recommended as good practice to avoid too large eccentricities is (a) to enlarge the joint overlap area, by increasing the number of rows and the distance between the rows, and (b) to use staggered joints, which result in slightly smaller k values (see [13]).

6.3.4 Multiple-Site Damage in Riveted Joints

Since the Aloha accident in 1988 [14], the problem called multiple-site damage (MSD) has gained increasing importance in the aerospace industry. In the Aloha accident, a Boeing 737 aircraft lost a full section of the fuselage due to MSD. This took place in flight due to multiple fatigue cracks that grew from fastener holes without being previously detected. MSD is the damage that results when a fastened joint works subjected to a very large number of fatigue cycles, often related to aging of the structure to which the joint belongs.

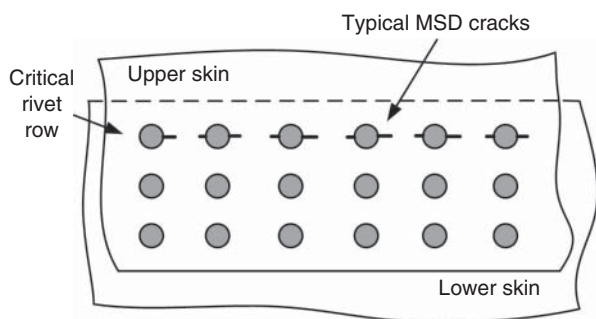


Figure 6.8 Schematic representation of MSD (multisite damage) in a fuselage lap joint.

Figure 6.8 shows a lap joint where a certain number of cracks have nucleated and reached a certain size. It becomes primarily important to know the period of time for which the structure withstands the working load without losing its integrity. Some important aspects must be considered for this evaluation, such as: (1) the time for nucleation of fatigue cracks and the distribution of these cracks are random and must be addressed by means of statistical analysis; (2) for the crack growth analysis, the interaction between cracks must be taken into account, for example, by means of compounding or appropriate numerical methods; (3) when the critical crack size is reached, two events are possible – firstly, the cracks link up, and secondly, the final failure takes place. For each of these events, there are models developed based on the fracture mechanics theory, such as the plastic zone touch [15] and the crack tip opening displacement (CTOD).

The MSD phenomenon has been studied extensively during the past decades since the Aloha accident, not only through analytical approaches but also by means of various experiments, with small coupons, large panels, substructures, and so on. This extensive work has been performed by the academics, research centers, and the aerospace industry, with the aircraft certification authorities pushing the development by means of new and updated certification requirements requiring avoidance of MSD. This has led to a deeper understanding of fatigue in joints and to increasing efforts in order to avoid this problem.

6.3.5 Influence of the Squeezing Force in Riveted Joints

In aeronautical structures, when the sheet thickness is relatively small or when the amount of load transferred is not excessive, expanded fasteners are widely used due to their low cost and easy installation. These fasteners are mechanically inserted into the holes in such a way to create a radial expansion, which is the function of the squeezing force, that is, of the amount of force applied in the axial direction.

Many studies [16] have shown that, particularly for countersunk holes, the increase of squeezing forces leads to compressive stress fields around the fastener holes and significantly increases the joint fatigue life. Similar trends are observed for joints with Hi-lite or Hi-lok fasteners applied with higher levels of interference. There are limits of expansion, translated to industrial standards or practices, such that the desired mechanical properties of the fastener are assured after installation, and the hole surfaces are not damaged due to excessive local deformation.

6.3.6 Welded and Bonded Shear Joints

Welding and bonding processes have been developed as alternatives for fastened joints in an increasing number of applications. For aerospace, while bonded joining solutions have been used for decades, only more recently the application of laser beam welding and friction stir welding (FSW) has been observed in commercial aircraft fuselage structures.

Bonded joints have the possibility to transfer loads more continuously from one member to the other as main advantage. Unlike fastened joints, where there are high stress concentrations around the fastener holes, bonding allows an even stress distribution. As a result, the fatigue life of these joints is improved. Usually, failure modes are associated to stress increases at the edges of the overlap between both plates.

The most important issues to be observed for bonded joints are: (1) appropriate adhesive curing, in order to reach the specified cohesion forces during the joint assembly process; and (2) verification, through extensive experimental campaigns, of the joint durability when subjected to long periods under actual environmental exposure. Some aircraft manufacturers apply bonding to fail-safe components [3] or develop hybrid (bonding and fastened) joining solutions, still relying partly on mechanical fastening properties, but taking advantage of bonding as means to optimize the joint characteristics.

Regarding welded joints, similar conclusions may be drawn when one compares this technology with fastening. If the welding process is well controlled, a relatively uniform distribution of stresses is obtained along the joint line, and as expected, a direct comparison between a fastened joint and its innumerable crack nucleation sites results in some advantage for the welded joint.

However, unlike bonding, all welding processes comprise thermally affected zones along the joint transversal section. These thermal effects will result in residual stresses, which consequently affect the joint mechanical behavior. Figure 6.9 shows, for a welded plate of length H , typical residual stress profiles after welding. For FSW joints, the maximum residual stresses are usually not so severe when compared to the working stresses, but as long as these values are added, it is very important to assure that they are appropriately addressed during the joint design.

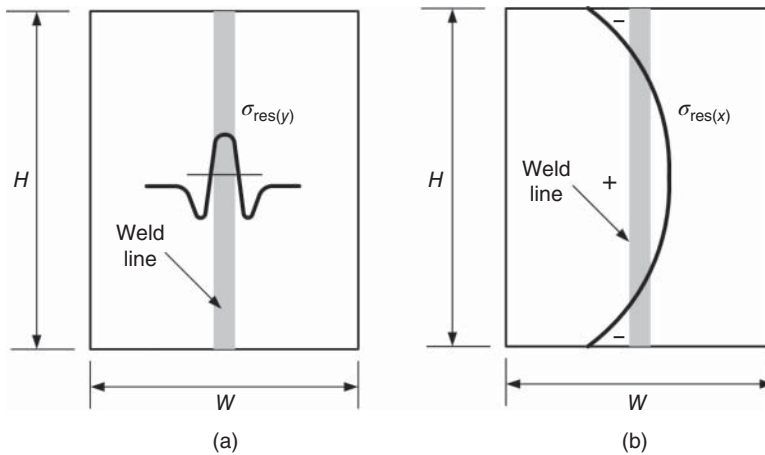


Figure 6.9 Typical residual stresses after welding (a) in the transversal direction (b) in the longitudinal direction.

Further, from the point of view of fracture mechanics, it should be noted that, unlike bonded joints, welded joints result in monolithic structures. Further, when comparing a welded joint and a fastened joint, one observes that, due to the absence of stress concentration at holes, a fatigue crack will take more time to nucleate in the welded joint. However, after the crack nucleates, there will be less elements in front of it to promote crack arrest, because fastener holes work not only as crack nucleation sites but also as crack arresters. In other words, welded joints may be less efficient from the point of view of fracture mechanics, and as a consequence, welded joints require different philosophies in order to assure damage tolerance requirements.

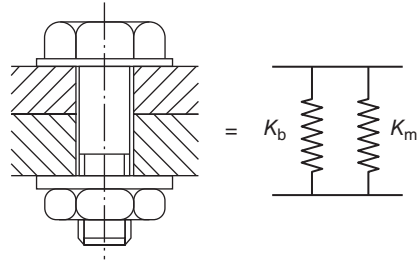
6.4 Tension Joints

Joints are many times designed to work under tensile loading. This is because tension joints are usually assembled by means of bolts, what allows for assembly and disassembly. However, for tension joint configurations, there are design considerations other than the previously presented for shear joints.

Tension joints can also be evaluated by experimental, analytical, and numerical approaches. As long as the bolts or fasteners will be working mainly under axial loading conditions, for these joints, the pretorque becomes a very important variable in the problem.

The tension joint may be modeled as an elastic spring system where the axial stiffness is a key information. Such a system is shown schematically in Figure 6.10, where the bolt has a spring constant K_b and the members to be

Figure 6.10 Spring system as an idealization of a bolted tension joint (Reproduced with permission of ESDU 85021 [17]).



joined have a spring constant K_m . The relation between applied loads and displacements can be written in terms of the stiffness of the bolt and of the members, as follows through Equations 6.1 and 6.2:

$$K_b = P_b / \delta_b \quad (6.1)$$

$$K_m = P_m / \delta_m \quad (6.2)$$

where P_b and P_m are the loads at the bolt and members and δ_b and δ_m are the corresponding displacements. If there is no torque or working loads, the system is resting and $P_b = P_m = 0$. If there is a pretorque with a corresponding load P_t and no working loads, then $P_b = P_m = P_t$. When an external tensile load is applied to the system trying to “stretch” the bolt and consequently alleviating the members, the bolt load increases and the load at the members decreases accordingly. Finally, there could be a condition where the applied load is larger than the torque load, such that the members would separate. This condition is not acceptable in a tension joint design, and the torque to be applied must assure that separation will never occur under any working condition.

Figure 6.11 shows the events described earlier, where P_a introduced in this figure corresponds to the applied (working) load.

Figure 6.12 shows how the component loads vary as functions of (a) the joint displacement and (b) the external (working) load. For these plots, it is observed that, when the applied load is zero, there is a pretorque load equal to P_t . The plot presented in (b) results from a transformation of the plot in (a), where γ_b and γ_m are functions of K_b and K_m only and are given by Equations 6.3 and 6.4.

$$\gamma_b = \frac{K_b}{(K_m + K_b)} \quad (6.3)$$

$$\gamma_m = \frac{K_m}{(K_m + K_b)} \quad (6.4)$$

Some interesting aspects observed from these diagrams are:

- If the bolt material is steel and the members are made of aluminum, despite the fact that the elastic modulus of steel is about three times the one of aluminum, the joint stiffness takes into account not only the material stiffness but also the area of contact surfaces, such that K_b is usually lower than K_m .

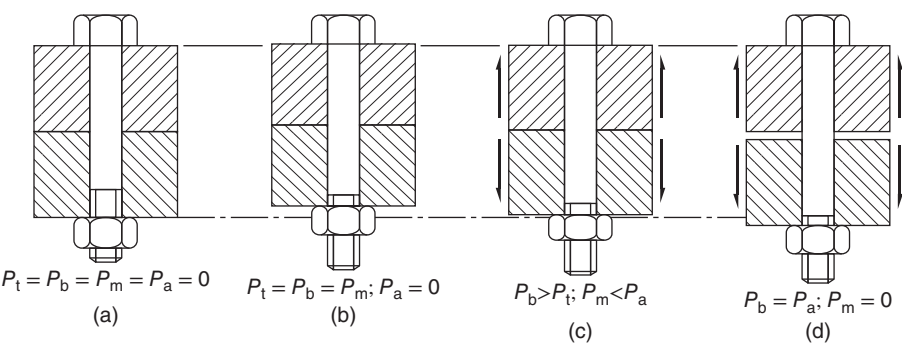


Figure 6.11 Possible bolted joint configurations: (a) structure is resting, (b) a pretorque is applied, and structure withstands a constant load P_t (mean value), (c) structure is subjected to external (working loads) without reaching the load P_t due to pretorque, (d) working load becomes higher than pretorque, and there is separation of the members (Source: ESDU 2005 [17]. Reproduced with permission of ESDU).

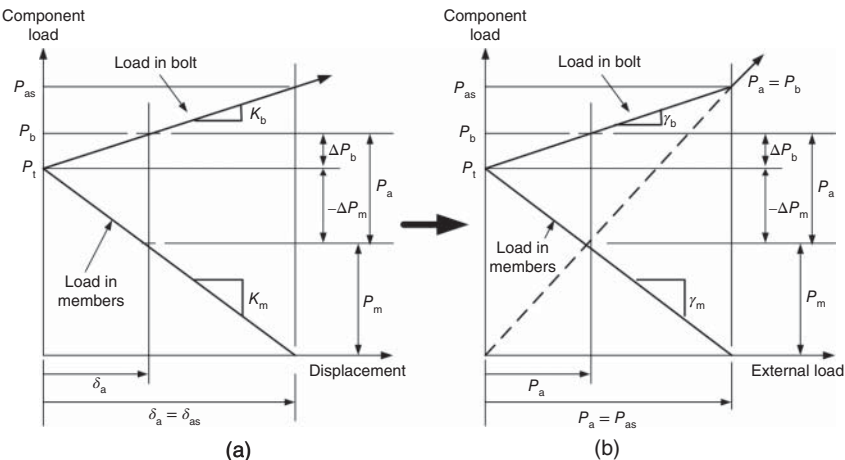
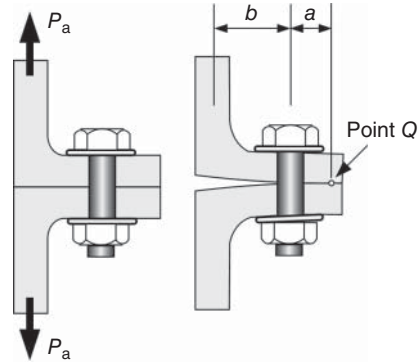


Figure 6.12 Diagrams relating component loads with (a) joint global displacement and (b) applied loads (Source: ESDU 2005 [17]. Reproduced with permission of ESDU).

- There is a point when the applied load reaches the separation of both surfaces ($P_a = P_{as}$), and after this point, the load of the members becomes zero and the bolt load is the same as the applied load, as shown in the right plot.

Hence, when a tension joint is designed, the pretorque must be calculated such that P_{as} is not reached under limit load conditions. Considering that the maximum bolt load is given by the pretorque load and the maximum working load, often this critical condition defines the bolt material and diameter.

Figure 6.13 Schematic representation of the prying effect.



6.4.1 Prying Effect

For nonsymmetric tension joints, when loads are applied, the system bends, and this leads to an increase in the bolt load P_b due to the *prying effect*. Figure 6.13 shows schematically this effect, where as long as the load is applied and even without separation of the members, the joint tends to rotate around a certain point of contact, indicated as Q in the figure. In a first approximation, the bolt load may be estimated by the simple hinge equations, as shown in Equations 6.5 and 6.6.

$$P_b = P_a(1 + b/a) \quad (6.5)$$

$$R = P_a(b/a) \quad (6.6)$$

where R is the reaction at the contact point. However, this contact (or rotation) point changes with the load increase, and this behavior is a function of the plate stiffness, which is mainly related to its thickness.

It is possible to estimate the increase of P_b due to the prying effect, by analytical [17] or numerical methods, although ideally the pretorque and contact between members have to be taken into account, which leads to more sophisticated numerical analyses. This estimation becomes essential, considering that the prying effect leads to more load being added to the bolts.

6.4.2 Fatigue Behavior of Tension Joints

Experimental results [11] have shown that an increasing pretorque improves the fatigue life of the system. This is more evident for the members, because they are under a compressive mean stress state when the system is resting, and when the system works, the cyclic stresses will vary around this compressive mean value.

However, like the members, the bolts also present longer fatigue lifes for increasing pretorque values. The reason for this is somehow less obvious: the fluctuation to which the bolt is subjected is significantly lower than the overall

system fluctuation. In other words, even if the bolt is under a tensile mean stress state, the values of the stress amplitude will be significantly smaller than that corresponding to a joint without pretorque (see Figure 6.12(b) for a correlation between the applied and bolt loads), and from the fatigue standpoint, often the material response to cyclic loads leads to more damage when it is subjected to high stress amplitudes than when it is subjected to high mean stress with lower amplitudes.

6.4.3 Methods for Estimation of Contact Area and Member's Stiffness in Tension Joints

While the bolt stiffness is easily calculated through its material properties and dimensions, many methods have been proposed over the years to estimate the contact area and stiffness of the joint members. Summarizing, these methods involve: the actual stress profile in the region of influence around the bolt, the pressure cone proposed by Roetscher [18], and cylinder with an effective area, whose stiffness properties are equivalent to the actual pressure system.

For design purposes, the stiffness calculation of tension joints based on these approximate methods may be found in the literature [19] or in databases supplied by international standard associations. Further, the stiffness can be obtained with better accuracy for any geometrical conditions by means of numerical methods such as FEM. Figure 6.14 presents an example of numerical analysis, where solid elements with axisymmetric boundary conditions and contact between bolt and plate surfaces were used to obtain the stress profiles along the radial section. Once the mean compressive area is known, the member stiffness calculation is straightforward.

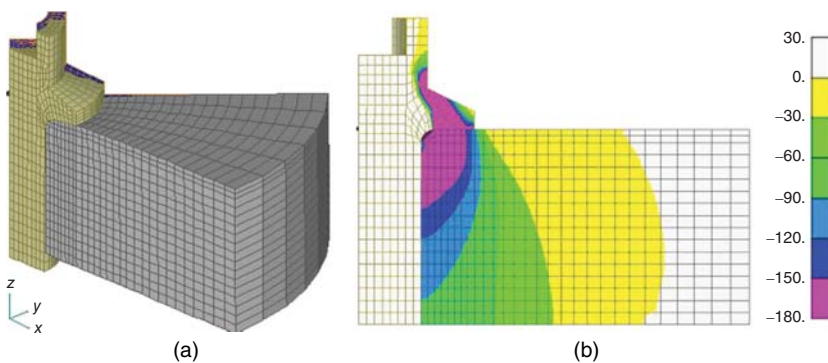


Figure 6.14 Example of FEM analysis in bolted joint. (a) Model with solid elements and axisymmetric boundary conditions, (b) compressive stress fringes (Z stress), stresses in MPa.

6.5 Tolerances in Joint Design

Ideally, every structure should be assembled perfectly, without deviations from the nominal design values. However, deviations from nominal values are often observed during assembly of the actual parts. The dimensional control becomes more difficult for larger structures, and here, the overall structure stiffness becomes a key parameter in order to allow appropriate assembly and to avoid future problems due to residual stresses.

There are many types of tolerance problems to be evaluated during assembly of a joint:

- (a) Hole and fastener size and positioning
- (b) Hole perpendicularity
- (c) Surface misalignment due to offset between members during assembly
- (d) Surface misalignment due to wedges between members during assembly

Figure 6.15 shows some typical scenarios to be evaluated through geometrical dimensioning and tolerancing (GD&T) methods. In Figure 6.15, a hypothetical joint with three fasteners is presented. Looking from the top (Plane A), three cases are presented where the hole center alignment, hole diameter, and bolt diameter must be controlled in order to assure appropriate assembly conditions. Further, the side view (Plane B) presents two scenarios characterizing a parallel gap between the internal surfaces and an angular gap. Both conditions

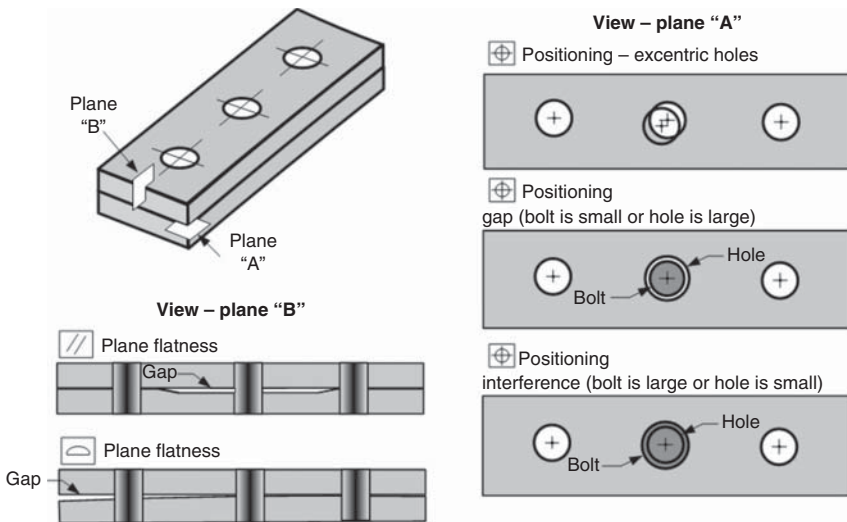


Figure 6.15 Some considerations about tolerances during joint assemblies.

will lead to enforced assembly unless these gaps are controlled. There is another requirement not shown in Figure 6.15 with respect to the hole straightness after drilling, where in this case, perpendicularity between the surfaces and the holes along the thickness must also be controlled.

For a tension joint connecting two parts of an aircraft primary structure, if the bolt holes at each part are drilled separately, usually a very tight radial and positioning tolerance is required, for example, 0.1 mm in the radial direction. Otherwise, the bolt becomes somehow loose or does not transfer the load appropriately.

Surface or plane misalignments are usually not acceptable as well, because when the bolt torque is applied, unexpected residual stresses not foreseen during the joint design may arise. There are various approaches to avoid or to coexist with such misalignments, such as the following:

- (a) Whenever possible, to design appropriate tooling that avoids large errors in critical joint locations
- (b) To change the assembly process such that drilling and surface facing are performed at the final stages. However, this is often undesirable because it leads to tailored assemblies, where the parts cannot be replaced later.
- (c) To introduce solid or liquid shims into void spaces, assuring total contact between both surfaces

Simulation of the assembly process can be carried out as well. Such analysis could help to check the structure flexibility against the possible gaps that could arise during assembly. The residual stresses obtained through analysis can be added to the expected structural working loads.

6.6 Materials

As previously discussed, a mechanically fastened joint is basically composed of at least two parts, joined by fasteners.

These parts may be metallic sheets, plates, casting, sintered or extruded parts, composite materials, or even hybrid materials, such as fiber metal laminated (FML) (e.g., Glare™ and Arall™ [20]) hybrid materials, which show some similarity to metallic materials' behavior, with some improved properties, such as damage tolerance – crack propagation with discontinuity through the layers – corrosion and fire resistance, and lower specific weight. Dissimilar metal–composite joints are also very common in aircraft structures.

Fasteners are a particular kind of mechanical hardware. They will be described in detail in the following paragraphs. Focus will be on the following aspects: the mechanically fastened parts, the fasteners, and the interaction between fasteners and joined parts.

Joints and their components are designed to avoid failure, mainly to avoid abrupt and catastrophic failures. Ideally, a joint and the process used to make it must produce a connection as strong as the parts that are joined [21]. Despite that, there are some special conditions that allow an aircraft component to be designed to bear small defects, such as small cracks, since they are usually predictable, detectable, repairable, and can have their growth controlled within the parts' maintenance plan. Parts are classified according to their structural responsibility and denoted as primary for higher responsibilities and as secondary for lower responsibilities, and this must be included and considered in the failure criteria allowance.

As presented in Figure 6.2, some examples of typical failure modes under quasistatic loading are: net area tension, shear tear-out (fastener or plate), and plate bearing. There are many others more complex failures due to the action of buckling, dynamic loads, vibrations, corrosion, and so on, but all of them are basically related to the former ones.

Due to the importance and reliability of the aeronautical parts, some properties, especially physical and mechanical properties, are fundamental for the success of the assembly working and safety, and it demands some experience and familiarity for those properties' specification.

In simple terms, the aircraft validation has specific final product tests, made not only in coupons but also in real prototypes that simulate real loads and test them for static, dynamic (fatigue, crack propagation, etc.), and buckling results, among others. For specific material allowance, an extensive test campaign is conducted to achieve several aspects of the material behavior and their mechanical properties.

6.6.1 Material Properties

We can list, briefly describe, and make relevant comments on some of the most important properties for aircraft material joints that must be considered in the materials' selection, design, and analysis [21]:

- *Hardness*: It refers to the ability of a material to resist abrasion, penetration, cutting action, or permanent distortion. Hardness may be increased by cold working the metal and, in the case of steel and certain aluminum alloys, by heat treatment. Hardness and strength are closely associated properties of metals [21]. Hardness of a material can be considered as a crude measure of its strength [22]. It is a very important parameter for metallic raw material production control and for some metallic parts controlling under manufacturing process, such as heat treatment.
- *Strength*: One of the most important properties of a material is strength, which is the ability of a material to resist stress without breaking. The type of service load or stress affects the strength it exhibits [21]. Strength may be associated to the form of load application, such as tensile strength,

compressive strength, shear strength, or may be associated to the level of load application, such as ultimate strength and yield strength. The strength parameters, such as ultimate, yield, elongation, among others, can be taken from static tests, extensively made for a material validation in an allowable campaign.

- *Density (specific weight)*: Density or specific weight is the weight obtained for a unit volume of a material. Density is an important issue to take into account when choosing a material to be used in the design of a part in order to maintain the proper weight and balance of the aircraft [21]. It is very important when it is considered together with the strength, giving us the *specific strength* of a material, which is a real understanding of the strength by unit of weight of a material (i.e., the strength-to-weight ratio). This is of ultimate importance when selecting base materials and fasteners, in order to reduce the weight of an aircraft structure. For instance, a trend for the development of new alloys is to switch from simple aluminum alloys to aluminum–lithium alloys, due to their lower specific weight. The marriage of Li to Al offers the promise of substantially reducing the weight of aerospace alloys, since each 1 wt% Li added to Al reduces density by 3% and increases the elastic modulus by about 6% [23]. Composite materials are very important due to their lower specific weight, when compared to metal alloys.
- *Malleability*: A material is malleable when it can be mechanically formed into shapes without cracking or without initiating cracking, breaking, or leaving some other detrimental effects. This property is necessary in sheet metal that is worked into curved shapes, such as cowlings, fairings, or wingtips [21]. The degree of malleability can influence the forming process chosen, making the process leaner. Materials easier to mechanically work may need simpler manufacturing process, lowering the manufacturing costs this way.
- *Ductility*: It is the property of a material that allows it to be permanently drawn, bent, or twisted into various shapes without breaking. Ductile metals are greatly preferred for aircraft use because of their easier forming and higher resistance to failure under shock loads [21]. Some materials for aeronautical manufacturing, such as some aluminum alloys in this case, show high ductility for some specific heat treatment condition, so it can be heat treated to another final condition, eventually with better mechanical properties.
- *Elasticity*: Elasticity is the property that enables a material to return to its original size and shape when the force that causes the changes of shapes is removed [21]. To keep a metal in its elastic state, higher loads surpassing the material's elastic limit must be avoided, to prevent plastic deformation. Some components are desirable with high elastic properties, due to their working function (e.g., components requiring spring effect).
- *Toughness and fracture toughness*: Toughness is the ability of a material to withstand tearing or shearing, allowing it to be stretched or otherwise

deformed without breaking. This is a desirable property in aircraft metals working under tensile loads [21]. The fracture toughness is the ability of a material with an initial crack to resist failure by fracture. In other words, it is the capacity of a material to absorb significant energy before rupture. Material toughness must also be considered and evaluated for lower temperatures, when this material property may decrease, whereby the material can become brittle. The increase in brittleness is considered dangerous and undesirable for a structure that must be resistant and safe from abrupt and catastrophic failure.

- *Resistance to crack growth:* This material behavior describes the susceptibility to crack propagation. Some conditions interfere in the crack propagation of a material, such as chemical composition, mechanical properties, manufacturing process, heat treatment, kind and level of service loads, part geometry, stress disposal, and arrangement on a part, among others. It is a requirement for safety that the base material shows low rates of crack growth and that this behavior could be predicted. Those predictions are based on the mechanical testing results. From those observations, maintenance intervals are defined for the aircraft. Generally, crack inspection is made with visual inspection or even with some nondestructible tests such as liquid penetrant test. Some inspections during the aircraft life cycle are made, such as ground inspections, searching for nonconformities such as small cracks, while other nonaccessible areas (the so-called blind areas) are inspected with the aid of special resources, such as the industrial rigid borescopes.
- *Fatigue resistance:* It is the material strength under cyclic loading. The life of a component, considering fatigue life, is characterized by some very well-defined events: (1) crack nucleation, which may last for about 60–80% of a component life; (2) crack propagation, which may last for the remaining 20–40% of the component life; and (3) finally, the failure (collapse). The cyclic load for fatigue mechanism must have a loading repeating at least in any moment during the airplane service life, with some tensile levels, because pure compressive loads do not lead the material to fatigue failure. In simple terms, only compressive load does not start the fatigue process (crack initiation). Some materials can soften during the cyclic loading application, others may harden, and some others can show both behaviors during their lives, depending on a series of factors and their combination, including load intensity, temperature, load cyclic frequency, material microstructure, heat treatment, and so on. Those softening and hardening behaviors may influence the total life of a component.
- *Conductivity:* Conductivity is the property that enables a material to carry heat or electricity. In aircraft materials, electrical conductivity must be considered in conjunction with bonding, to eliminate radio interference [21]. Generally speaking, materials with organized (crystalline) microstructures have more susceptibility to conductivity than the ones with amorphous

structure. For instance, there is a correlation for aluminum alloys between electrical conductivity and hardness. In this case, the electrical conductivity and hardness are inverse proportionally affected by the level of crystallinity of microstructure (for instance, the average grain size).

- *Thermal expansion*: Refers to contraction and expansion as a result of heating or cooling. It affects the design and tolerance of a part [21]. This material property is important for controlling the dimensions and geometries, and for that reason, some tight-fitting manufacturing processes may need a controlled environment (e.g., temperature control). Thermal expansion plays an important role in designing and analyzing stress distributions in dissimilar materials assemblies, due to the dissimilar thermal expansions. In typical commercial aircraft jets, some common flight temperatures range from less than -40°C up to more than 60°C , and some parts with heating, such as engine parts, may reach much higher temperatures ranges. Those typical ranges of temperature induce harsh changes in material expansion and contraction. Obviously, different materials expand or contract differentially. For this reason, clearances, gaps, reinforcements, overload factor, or any specific design resource in the connection between the dissimilar parts must be taken into consideration. Due to different coefficients of thermal expansion, one part may have its dimensions increased or decreased more than the other, leading to misalignment between the holes and fasteners. Fasteners must have some degree of freedom to avoid overstressing and final joint failure, or these overloads must be properly considered in the stress analysis and design of the joined parts. Moreover, the fasteners probably must be overdimensioned for safety reasons, having reinforced dimensions, which sometimes is not a desired solution, due to the increase of components' weight.
- *Creep resistance*: Creep is the property of a material that leads to its permanent deformation due to an application of an elastic load, at a determined temperature, during a period of time. Creep strain is directly proportional to the load, time, and temperature for which a material is subjected. Once the material is susceptible to creep mechanism, it will relax tension during the creep process, and its initial shape cannot be recovered, thus being permanently deformed. As a thumb rule, we can say that normal creep temperatures range from 0.4 to 0.7 of the material melting point (for metals). An emblematic case is the rotor blades' creep degradation in the aircraft engines. Due to the extreme forces caused by the engine high rotation and the extreme temperatures incoming from the combustion chambers, those parts are strongly susceptible to creep. It is not difficult to understand the high importance of those parts and their relevance to structural integrity. Since this is a critical problem, some big evolution has been made to mitigate creep failure, such as the development of new creep resistance.
- *Stiffness*: Stiffness is a particular and very important property of a component and does not depend only on the material property. It includes the strength

responsible to avoid a part's high deformation and deflection. It depends on part geometry and material properties. This topic requires great design ability to take the maximum advantage of the component, that is, the balance of minimizing the component cross section and maximizing its strength. Stiffness is an important property when considering the aerodynamic loads; therefore, a good design must consider the aeroelasticity criteria to prevent the component from fluttering (aeroelastic instability).

- *Corrosion resistance:* It is the ability of a material to withstand losing mass during a chemical reaction with the environment. Some parameters such as environment, material, protection, material condition (heat treatment, content elements, composition, thermal conductivity, etc.), among others, may influence the susceptibility of a material regarding corrosion. Normally, metals are found in the form of ores in nature, in a stable energy state. Their processing into metallic alloy forms is a rearrangement of its components and microstructure in a way that enhances its susceptibility to react with other elements, because of their increased energy condition, so metal tends to rearrange back into a near-ore form (such as oxides or salts), with a more stable energy balance. Nonmetallic materials (polymers and ceramics) are also susceptible to corrosion, but in this context, it is more commonly referred to as the term degradation. Carbon-fiber-reinforced polymers dissimilarly combined to metals may accelerate corrosion by galvanic (contact) corrosive reaction. This is explained by the high electrical conductivity of the carbon fiber reinforcement, which will support ionic flow, accelerating the galvanic corrosion process. For dissimilar metal-CFRP fastened joints, special protection must be applied. Examples are the use of sealants at the overlap contact area.

There are other material properties, but not so relevant at this moment for a mechanical fastened joint. Therefore, they are not going to be discussed here. On the other hand, corrosion is an important aspect to be extensively considered in the joint design for aircraft parts. Corrosion and protection will be addressed into more detail in the next section.

6.6.2 Corrosion and Protection

There are several types of corrosion and related aspects that are very important for aircraft design and must be considered during material selection and joint design. These may include the following:

- *General corrosion:* Or uniform corrosion, is the most common kind of corrosion and appears in extensive areas, easily recognizable, causing the thinning of the part. It occurs only when the material is exposed to an electrolyte (e.g., moisture from condensed water vapor).
- *Galvanic corrosion:* it is a type of corrosion that occurs when dissimilar materials are close enough to change particles, where the more noble

material (cathode) corrodes at a slower rate and the less noble material (anode) corrodes at a faster rate than they would in case of a separate exposition to an electrolyte. Galvanic corrosion is directly proportional to the difference in the material's electrical (electrochemical) potentials; it occurs in the presence of an electrolyte, where the electrical current flows between the metals, until their potentials tend to be equal.

- *Galvanic series* (see Table 6.2): Consists of a practical ordered list of materials with potentials close to each other, according to their proximity in the list. As nearer they are in the list, the lesser corrosive the match becomes, while the opposite is also true. A hint for practical design is to choose close materials for joints, and fasteners as well, minimizing the effects of galvanic corrosion, whenever it is possible. Moreover, some caution for corrosion protection should be taken. There is a series of design rules for selecting materials and protecting them in joined structures to avoid and protect from galvanic corrosion. Some issues regarding surface protection will be discussed in the following paragraph. One good practice for protecting materials from a galvanic match is to by anodic protection, that is, the fastener must be recovered with a metallic plating from a metallic material in the galvanic series, near the part material, which is less active in the galvanic match.
- *Stress corrosion crack (SCC)*: It is a special and complex condition of corrosion, related to the stress applied into a material. SCC arises due to the combination of the stress application, the environment condition (e.g., electrolyte), and the material susceptibility to corrosion. Some heat-treatable aluminum alloys are very susceptible to SCC, such as the AA7XXX series, for

Table 6.2 Examples of galvanic material series for reducing galvanic corrosion.

Anodic	Magnesium
	Zinc
	Pure aluminum
	Aluminum 5xxx series
	Cadmium
	Aluminum 2xxx series
	Aluminum 7xxx series
	Alloy steel 8740
	Stainless steel (active)
	Nickel (active)
	Nickel (passive)
	Stainless steel (passive)
Cathodic	Graphite

example, the AA7475-T6 alloy. In most of the cases, SCC in aluminum alloys is predominantly intergranular. According to the electrochemical theory, this type of corrosion requires a condition along grain boundaries where they become anodic to the rest of the microstructure, so that corrosion propagates selectively along the grains. Intergranular (or intercrystalline) corrosion selectively attacks the grain boundaries or closely adjacent regions without considering the attack of the grains themselves.

- *Pitting corrosion*: It is a kind of corrosion very well characterized by the presence of small holes, deeper than larger, occurring randomly. Pitting corrosion generally occurs due to the chemical breakdown of the surface protection, caused by scratches or nicks, or other protection surface shortcoming; the corrosion spreads in the holes across the thickness.
- *Corrosion fatigue*: Is a sort of corrosion defined by the cyclic loads and the corrosive environment. Corrosion fatigue causes the failure of a material due to significantly lower stresses than the material strength. The cyclic loads are always renewing the cracks' opening while the corrosive environment influences the nucleation of new cracks or increases the growing rate of the present cracks.
- *Intergranular corrosion*: Metallic materials are arranged into granular structures. The grain is the metallurgical unity of a crystalline structure. Grains are separated by a distinct phase, the grain boundary. The granular corrosion is the corrosion caused by different phases of materials with high potential between the grain and the grain boundary. As seen before, intergranular corrosion normally attacks the grain boundaries or the surroundings. When certain materials are submitted to a specific thermal process, for example, they become more susceptible to intergranular corrosion. That can be seen as a good example in some stainless steel alloys, where the material "sensitizing" causes steel and chromium oxide precipitation in the grain boundaries.
- *Crevice corrosion*: It is caused by a confinement of a corrosive electrolyte, composed of ions such as chlorides. The corrosion reaction takes place and is very similar to the appearance of pitting corrosion. Certain assembly and components that may confine the electrolyte, such as gaskets, fasteners, washers, screws, and other close-fitting mountings, turn favorable for the initiation of this kind of corrosion.
- *Filiform corrosion*: It is a very specific and peculiar kind of corrosion that appears on some surface protected with organic coating film. When the protection is damaged causing a surface defect and a local protection failure, corrosion reaction starts and grows into filiform patterns, under the coating. Some conditions including moisture and acidity must be present in the site to start the reaction. It leads to the coating degradation and may expand out from the initial corrosion site. The aspect of the corrosion products is very characteristic, looking like thin filaments, and its major prejudice is

not the material weakening, but the appearance of the surface and coating degradation.

- *Fretting corrosion*: Also known as friction corrosion, wear oxidation, among other terminologies, this kind of corrosion is also very peculiar. Fretting corrosion is usually caused by the combination of a tribological process and corrosion. These phenomena are characterized by the relative motion between the parts involved, with small displacements, generating small relative movement or vibration. Therefore, there is material removal from the substrate. Correct parts lubrication and control of the amplitude of the parts motion are some measures to avoid or control fretting corrosion.

There are other less common types of corrosion, such as erosion corrosion and concrete corrosion, but not relevant for aircraft design and mechanical fastening.

By bearing the corrosion requirements in mind and considering that the life of an aircraft can range from 10 to 30 years, the established strategy consists in applying the best protection system to avoid future component damages. In a fastened joint, the parts shall be protected individually with a primary defense (anodizing or other coating such as sulfuric, chromic, or boric–chromic adonization, chemical conversion, cadmium plating, cladding, aluminum coating (ion vapor deposited (IVD)) and passivation, sealing, etc.), a secondary defense (such as epoxy primers), and sometimes a topcoat (polyurethane painting system) and immediately before the joining, in the interface area between the parts, interfay sealant (chromate based or others) may be applied for avoiding contaminations and electrolyte entrapment. After the fastener is installed, a fillet sealant is applied all over the interface area perimeter. Later on, in the assembly stages, corrosion inhibitor compounds are applied. Each fastener is also protected when needed, with interfay sealant itself before being installed. These coatings provide improved corrosion protection and paint adhesion. Alodine covers chrome, nonchrome, and anodizing technologies and serves numerous markets including the beverage industry, architecture, automotive, aerospace, electronics, and others.

6.6.3 Material Selection

Taking into account all those properties and requirements, we can list a range of materials eligible for application in the civil aircraft structures, including mechanical fastened joints.

They are mainly aluminum alloys for structural parts such as the skins for fuselage, wings and empennages, and the reinforcements for those structural parts, such as stringers, spars, ribs, supports, and fittings in general. They can be manufactured by forming processes of rolled sheets, plates and extruded parts, machining process, and in fewer cases, by casting process.

Table 6.3 Aluminum alloy series and respective alloy elements.

Aluminum alloy series	Main alloy element
1XXX	Pure aluminum ^{a)}
2XXX	Cooper
3XXX	Manganese
4XXX	Silicon
5XXX	Magnesium
6XXX	Magnesium and silicon
7XXX	Zinc
8XXX	Other elements
9XXX	Unused

a) For the 1000 series, the last two digits mean the hundredths of 1% above the 99% designated (AA1100: 99% pure aluminum; AA1130: 99.30 pure aluminum, etc.) [21].

The main series of aluminum alloys used in aircraft industry are the AA1XXX, through AA9XXX series. See Table 6.3 for the main aluminum alloy series and their main alloy elements.

The most widely used aluminum alloys in the aircraft industry are the AA2XXX and AA7XXX series, followed by the AA6XXX and AA5XXX series. The AA2XXX alloy series is widely used in the skin components and stringer reinforcements, showing good strength and good toughness, with good performance where crack propagation is required, while AA7XXX alloy series is typically used for high-resistance applications, such as fittings, spars, ribs, stringers, showing good static strength. The main content in the AA2XXX series alloys is the cooper element (Cu), which in association with aluminum is the basis of the precipitate formation and growth, within the aging dynamics. For the AA7XXX series alloys, the main content is the zinc element (Zn), which in association with aluminum is the basis of the precipitate formation and growth, within the ageing dynamics.

Staley and Hunt [24] made a brief review of the aluminum use during the twentieth century, as well as they made some findings in the current era and observation of future trends for the aircraft industry. Headed by the decreasing cost needs, the future of aluminum considers products from better wrought and forged materials, high-temperature age-formed materials, superplastic materials, castings, new alloys generation such as the third-generation aluminum–lithium and aluminum–magnesium–scandium alloys.

Steel is also very much used in specific cases for the aircraft design, where no other alloy can reach the established requirements including strength, temperature, corrosion, and so on, with the same benefit ratio, in terms of cost, workability, availability, and affordability. The use of steel in aircraft design is somehow averted due to the high density of this material, so its use must be justified by the balance of the requirements and needs.

For the aeronautical application, this group can be divided into some more subgroups, as, for instance, carbon steel alloys (plain carbon), alloy steel, and stainless steel.

Carbon steel alloys (or simply, plain carbon alloys) are used for some small parts or hardware where strength and costs are the main requirements, without compromising the final whole aircraft weight.

Alloy steel is used in critical structural parts, with high-level requirements, like bolts in the wing and fuselage links, for example, with high-strength responsibility. Chromium contents in alloy steel improve their strength. Chromium and nickel are the base elements for the stainless steel, which show high corrosion resistance. Vanadium and molybdenum associated with chromium and high carbon contents can increase the strength, toughness, wear, and fatigue.

Stainless steel alloys are very much used for specific applications where high temperatures or corrosion susceptibility or both are present. The main contents are the chromium and nickel elements, as mentioned before. One typical aircraft application of this kind of steel is in the engine exhaust collectors, other is in the pylon structures, for instance. The most used stainless steel alloys are the PH series such as the 13-8, 15-5, 17-7, 18-8, corrosion-resistant steel (CRES – a series of stainless steel specific for high-temperature applications), A-286, for instance.

Titanium alloys are also widely used for aircraft manufacturing, for fuselage and wing skins, firewalls, frames, fittings, and so on. They are also much used for fastener manufacturing (bolts, such as the hi-loks and lockbolts) and for military aircraft machined components, where costs can be mitigated and justified due to the number of aircraft produced and due to the high mechanical properties required. Among the metallic materials, titanium is the one with the best strength-to-weight ratio, and it seems very similar to stainless steel in appearance, with good resistance to stress corrosion crack and with a corrosion resistance similar to that of the 18-8 stainless steel. There are three basic types of titanium, and they can be classified as alpha, beta, and a combination of both, according to their microstructure and properties.

Other nonferrous metallic alloys, such as magnesium alloys, copper alloys, high nickel alloys, are not so relevant when considering their use in terms of quantity in aircraft structural applications. Besides that, magnesium has shown high improvement in the last few years. Some increase in the mechanical properties may turn it into a real possibility for aircraft application in

the next years. Magnesium alloys may be a promising material for aircraft applications due to their lowest density among the structural metals. A few applications with no structural responsibility are already being used in aircraft nowadays.

The use of polymer composites has increased in the last generations of civil airplanes, such as the Airbus 350 and the Boeing 787. The main classes of thermoset composite materials in aircraft structures are glass, carbon, and aramid fibers reinforced mainly with epoxy and polyester resins, and some other not so usual polymeric resins, such as phenolic ones (not usual). Other recent examples of new polymer matrix are the polyetherimide, polyphenylenesulfide, and polyamide 6. These are thermoplastic matrixes, which are weldable composites, therefore allowing for repair. Due to its melting and reconsolidation properties, these composites are recyclable, increasing the component sustainability. Composite materials have increased application in aircraft components due to their excellent mechanical properties when compared to metallic materials: strength, aeroelasticity, low density, lower susceptibility to dynamic damage and fatigue.

When combining metal with composites, we can mention FMLs, such as GlareTM and ArallTM, which are made by a defined number of aluminum sheets alternated with other glass fiber plies impregnated with polymeric resins. FMLs usually present higher damage tolerance than the individual base materials; however, they are usually slightly weaker than a composite laminate with the same thickness.

The selection of a material and an alloy regarding their properties takes into account the balance of the properties to attend the joint requirements. Whether there is an enhancement of some properties, generally it comes with a match decreasing of others. Examples are the high strength and low malleability, which make the proper material selection a challenging task.

Material selection must involve extensive knowledge about the final application, as well as the consideration of related factors, such as temperature, vibration, besides corrosion, protection, mechanical properties, loading, weight, among others. Therefore, it is not an easy task to consider all those items for a proper material and fastener selection.

6.7 Fasteners

“Fastener” can be described as a specific type of mechanical hardware for mechanically joining parts. As an example, we can list rivets, bolts, screws, pins, studs, nuts, collars, washers, nut plates as general fasteners. There are some special fasteners within these groups, which are the pin/collar system series, lockbolt series, rivets (solid and blind rivets), blind bolts, which bring together the characteristics of bolts and blind rivets, among others.

Because of its small size, the importance of mechanical hardware is usually underestimated, despite its relevancy to the safety and efficiency of the aircraft operation.

Mechanical fastening is a process that involves the technology of joining elements by the use of fasteners, and its financial feasibility increases when it reaches an automated process. The automation of fastener installation, specifically drilling and riveting, makes the process highly competitive when compared to the other processes such as welding. Moreover, no other technology is very efficient when quick disassembling (nondestructive) is necessary, for repairs, for instance.

Fasteners can be described and selected according to several and complementary criteria. It is important to collect and establish details as much as possible about the joint requirements, so a proper fastener specification can be obtained for such applications. “Proper fastener selection and application is the first step toward building reliable hardware; therefore, a standard approved parts list consisting of a limited number of types and styles will result in optimum performance, reliability, maintainability, and economy” [25]. As it was discussed in material the selection section of this chapter for joined parts, the same can be applied for fastener selection. Important aspects must be concomitantly considered, including fastener material, load nature and level, corrosion aspects and then protective finishing, work temperature, mechanical properties, lightning, more specifically fatigue aspects, torque application, work vibration, weight, accessibility, installation, and so on.

Although an intrinsic issue, it is important to mention that the proper dimensional specification of the joint will lead to a proper selection and specification of the fastener. Thickness and hole tolerances may be evaluated in order to guarantee the proper fastener specification. Interface sealing and torque application (clamp force) must be taken into account when specifying the joint fasteners.

One first classification of fasteners is based on the nature of the application, regarding the continuity or permanency of the fastener installation, that is, for permanent or temporary joints. For permanent joints, fasteners can be installed once, and so for their removal, a mechanical process, such as drilling, must be applied, causing their destruction. On the other hand, for temporary joints, we can use removable fasteners, which can be removed and, in some cases, reinstalled.

Accessibility is another very important criterion to be considered. Where the hole can be accessed from both sides, “head” and “tip,” solid rivets or bolts could be installed. When there is only one-side access, called blind access, blind rivets or blind bolts may be chosen; or if there is a need for creating two-side access, doors can be specified, adequate for the design and structure requirements, such as inspection doors on aircraft wings. The use of nut plates is an option allowing the use of screws in blind access holes, since there is the allowance of the structural requirements. The preference for riveting is noted

when permanent joints must be manufactured with a large number of rivets, which makes the process less time-consuming than with any other fastener.

Another important issue to be considered when selecting fasteners is the nature of load applied on the joint. Basically, a joint must transfer shear or tension loads or even a combination of both. This correct analysis and specification must be made to allow the proper definition of the fastener. Depending on the load nature, the fastener classification will determine the kind of fastener head, its material, general dimensions, among other particularities, such as dimensions, which may be different from the shear to the tension fastener.

Material selection for fasteners not only depends on design criteria mentioned before (load nature and level and mechanical properties, lightning, fatigue requirements, weight, etc.) but also depends on the kind of application. For high-structural-strength application, such as critical wing–fuselage joining, high-strength bolts must be selected. Other aspects such as corrosion and temperature must be considered in the fastener material selection. In Table 6.4, a summary of fastener materials, related finishing or plating, work temperature, strength, and some basic information is provided. That brings other subclassifications for fasteners, according to their material (i.e., aluminum fasteners, titanium fasteners, steel fasteners, etc.), protective coating, and plating (surface-treated fasteners, such as electrochemical passivation, metallic plating fasteners, IVD-coated fasteners, etc.), working temperature (high-temperature fasteners, low-temperature fasteners, with some specific temperature ranges), among others.

Plating and coating must be considered concomitantly with material selection. It depends on the materials of the joined parts and also on the fastener materials, besides temperature and working environment. A very commonly used coating is cadmium plating, for aeronautical aluminum parts. Cadmium plating is accomplished by an electrolytic process, which may result in an undesirable fastener embrittlement by hydrogen contamination. To avoid this effect, parts must be heat treated for 23 h at 375°F (190°C), within 2 h after the plating [26]. There is a strong recommendation to avoid cadmium use due to its health problem implications. There other alternative platings based on zinc, nickel, chromium, silver, phosphate coatings, epoxy coatings, IVD aluminum platings, among others.

Table 6.5 presents the characteristics of standard plating and coating used for fasteners, as well as some proprietary coatings such as Stalgard®, Sermatel W®, and SermaGard®.

From the point of view of corrosion protection, some drivers may be considered, in a simple and logical sequence [27]:

- Fastener material preferably must be galvanically similar to the joined parts material.
- Fastener must be isolated with passivated coating (or titanium fastener material).

Table 6.4 Summary of fastener materials.

Material	Surface treatment	Useful design temperature limit (F)	Ultimate tensile at room temperature (ksi)	Comments
Aluminum ^{a)}	Anodizing, cadmium plating, aluminium IVD	–85 to +200	Up to 65	n.a.
Carbon steel	Zinc plate	–65 to 250	55 and up	n.a.
Alloy steels	Cadmium, nickel, zinc, or chromium plate	–65 to limiting temperature of plating	Up to 300	Some can be used at 900°F
A-286 stainless	Passivated (MIL-S-5002)	–423 to 1200	Up to 220	n.a.
17-4 PH stainless	None	–300 to 600	Up to 220	n.a.
17-7 PH stainless	Passivated	–200 to 600	Up to 220	n.a.
300 series stainless	Furnace oxidized	–423 to 800	70–140	Oxidation reduces galling
410, 416, and 430 stainless	Passivated	–250 to 1200	Up to 180	47 ksi at 1200°F; will corrode slightly
U-212 stainless	Cleaned and passivated (MIL-S-5002)	1200	185	140 ksi at 1200°F
Inconel 718 stainless	Cadmium plate or passivated (QQ-P-35)	–423 to 900 or cadmium plate limit	Up to 220	n.a.
Inconel X-750 stainless	None	–320 to 1200	Up to 180	136 ksi at 1200°F
Waspalloy stainless	None	–423 to 1600	150	n.a.
Titanium	None	–350 to 500	Up to 160	n.a.

a) Experimentally determined.

Source: Barret 1990 [26]. Reproduced with kind permission of NASA.

- Choose a fastener with a similar metal plating (cadmium-plated or alloy-steel fastener materials installed in aluminum structure).
- Finally, use sealant for fastener installation, faying surface, and fillet sealing.

The lubrication of fasteners (thread and shank) also deserves some special attention, and the next paragraphs on this topic are based on the “Fastener Design Manual,” from [26].

Table 6.5 Summary of plating and coating.

Type of coating	Useful design temperature limit (F)	Remarks
Cadmium	450	Most common for aerospace fasteners
Zinc	140–250	Self-healing and cheaper than cadmium
Phosphates:		
Manganese	225	
Zinc	225–375	Mildly corrosion resistant but main use is for surface treatment prior to painting. Another use is with oil or wax for deterring corrosion
Iron	400	
Chromium	800–1200	Too expensive for most
Silver	1600	Most expensive coating
Black oxide (and oil)	300 ^{a)}	Ineffective in corrosion prevention
Preoxidation (CRES) fasteners only	1200	Prevents freeze-up of CRES threads
Nickel	1100	More expensive than cadmium and zinc
SemaGard and Sermatel W	450–1000	Dispersed aluminum particles with chromates in a water-based ceramic base coat
Stalgard	475	Proprietary organic and/or organic–inorganic compound used for corrosion resistance and lubrication (in some cases)
Diffused nickel–cadmium	900	Expensive and requires close control to avoid hydrogen damage
Chemical conversion ^{b)}	200	One of the most simple process applications and one of the less expensive processes. It is widely used for 2000 aluminum alloys sheets, including for the aluminum clad sheets
Anodizing ^{b)}	200	Widely used for 7000 aluminum alloys, generally after machining process. May slightly interfere in the component fatigue life (decreasing)

a) Oil boiling point.

b) Experimentally determined.

Source: Barret 1990 [26]. Reproduced with kind permission of NASA.

The most common thread lubricants for fasteners are oil, grease or wax, graphite, and molybdenum disulfide. Oil and grease are the most common types of thread lubricants, and the working temperature depends on the oil or grease melting point, because above that, they can boil off. In addition, they are good for application with corrosion prevention.

Graphite is sometimes referred to as “dry graphite,” but it is not really dry. It is a fine carbon powder that needs moisture aggregation (usually by water or oil) to become a lubricant. Otherwise, it will turn into an abrasive medium. Therefore, the working temperature of a graphite-lubricated joint must be within the oil melting temperature. Thus, graphite must be avoided to bolted joints when those limitations are not respected.

Molybdenum disulfide is one of the most popular dry lubricants. It can be used in vacuum environment, and the limit temperature is about 750 °F (399 °C), when it turns into molybdenum trisulfide, which is an abrasive rather than a lubricant.

6.7.1 Design Criteria

This section presents a brief description of design recommendation and rules regarding mechanical fastening:

Simple rules:

- For weight reduction purposes, a basic rule is to keep as much less fasteners as possible in a structured. To fulfill the structural requirements, they must accomplish the maximum spacing between fastener rows (less proneness to catastrophic failure).
- For weight reduction purposes, use the best strength–weight ratio, considering the specific weight of the following materials: aluminum (2,70), titanium (4,75), and steel (7,18), in g/cm³.
- Rivets, pins, and lockbolts are identified in 32nd of an inch diameter (1/32"). Bolts are identified in 16th of an inch diameter (1/16"). All of them are identified in 16th of an inch (1/16") length.

Simple recommendation:

- Do not use bolts smaller than 1/4" diameter on a structural joint.
- When possible, use the same fastener with the same diameter, grip, and length in one joint, to minimize the chance of fastener and joint grip mismatching and fastener mating mismatching (pin and collar or bolt and nut exchanging).
- The bolt head may be installed upward, so in case of nut loosening, it minimizes the loosening of the fastener or some F.O.D. (foreign object damage) in the structure and aircraft systems.
- The fastener positioning should be at a point that facilitates the visualization and maintenance.
- For purposes of minimizing lightning sparks, the fastener head tip is preferred in wet area (fuel tank), instead of the nut tip; as less parts are in contact, it minimizes the chance of electrical sparking.
- Rivet material shall have less strength in shear than the material pack joint. The rivet-formed head (upset head) shall be installed against the thickest or hardest material in the joint.

- For aluminum casting and composite material joints, a proper washer may be installed in the rivet upset head side, to avoid the process damaging of the joint.

Some traditional fastener manufacturer and supplier groups for aeronautical products are: Monogram Aerospace Fasteners, LISI Aerospace, Hi-Shear Corporation, Alcoa Fastening Systems (AFS), Bristol Industries, Allfast Incorporated, Cherry[®] Aerospace, and SPS Technologies, among a lot of so important others. It is not intended to provide and discuss the details of the different commercially available fasteners in this chapter, as there is an uncountable amount of products for a multitude of applications.

6.8 Summary and Final Remarks

In this introductory chapter on mechanical fastening, we have revised the main concepts and theories involving mechanically fastened joints in aircraft structures. By using typical terminology adopted in the aircraft structural design community, we described the general joint structural design considerations. The chapter offers engineers and engineering students an overview of the main types of joints found in primary aircraft structures: the shear joints and the tension joints. Focus has been on addressing the stress distribution and geometrical influence of fasteners, member geometries and structure. A brief comparison of the mechanical behavior and residual stress distributions in adhesively bonded and welded joints in comparison to mechanically fastened joints was carried out, showing the limitations and advantages of each joining technique. Furthermore, tolerances and assembly issues of mechanically fastened joints were addressed, whereby design best practices were suggested to decrease assembly-related residual stresses. Finally, we revised on the main fastener materials and types and discussed the main recommendations and practices when manufacturing mechanically fastened joints for aircraft structures.

This chapter is primarily devised to provide an overview of the process physics and mechanical performance of mechanically fastened joints in aircraft structures. These theories are generally valid for similar and dissimilar material joints to a certain extent. The readers willing to deepen their knowledge in mechanical fastening are advised to refer to the textbooks cited in this chapter.

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7

Mechanical Fastening of Composite and Composite–Metal Structures

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7.1 Introduction

The competition between metals and polymer composites as the main material for primary structural applications in the aeronautical and automotive industries requires optimal designs that fully exploit the capacities of each material. One of the limitations of polymer composite materials is their relatively low joint efficiency [1], which is defined as the ratio between the joint notched and the unnotched strengths. Such a low efficiency results from the limited capability that polymer composites have to relieve the stress concentrations that occur at the bolt-bearing region of the joint.

While ingenious solutions to increase the joint efficiency, such as the use of hybrid metal–composite local reinforcements [2, 3], have been recently proposed, there is the need to address both the overdesign of composite bolted joints that results from the use of overly simplistic analysis methods and the excessive cost of the experimentally based design of composite bolted joints. It is clear that the simulation of the complex failure mechanisms that occur in polymer composites is by no means an easy task. The failure mechanisms of polymer composites include the separation between plies (delamination), fiber tensile failure, fiber kinking, and transverse matrix cracking. In addition, polymer composite materials are frictional materials [4] whose response under certain loading conditions depends on the strain rate [5].

The failure of composite bolted joints results from the accumulation of different subcritical failure mechanisms, and the specific failure mode, which can be bearing, net tension, shear-out, or cleavage (Figure 7.1), depends on the joint geometry and the lay-up used [1].

Small ratios between the specimen width (w) and the hole diameter (d) result in net-tension failure, and small ratios between the end distance (e) and the hole diameter promote shear-out failure.

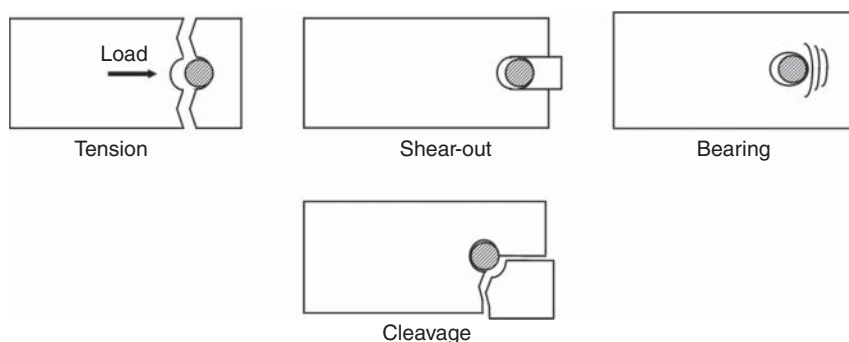


Figure 7.1 Typical failure modes in composite bolted joints.

Different methods have been proposed to predict the failure load and the failure mode of composite bolted joints. The use of failure criteria is appropriate to predict the onset of the failure mechanisms and to identify the type of failure; however, failure criteria alone cannot predict the final failure of a composite bolted joint that results from the propagation of different failure mechanisms up to a critical dimension. Therefore, the most widely used methods for the prediction of the failure load and failure mode of composite bolted joints are based on the application of a failure criterion at a characteristic distance from the hole boundary. Such methods are modifications of the point-stress and average-stress models proposed by Whitney and Nuismer [6, 7]. The identification of the joint failure mode is based on the location where the failure criterion is satisfied.

Alternatively, nonlinear finite-element methods with appropriate material models that simulate the onset and propagation of the different failure modes are used [8–10]. While the existing methods provide reasonably accurate predictions of the failure load and mode of composite bolted joints, there are some difficulties that need to be addressed. The first one is the fact that the methods based on the point-stress and average-stress models require the empirical calibration of the characteristic distance used in the failure criterion. The measurement of this adjusting parameter is required for different failure modes, geometries, and lay-ups. The second difficulty is that, generally, the nonlinear finite-element methods do not account for the interaction of the different failure mechanisms and for the pressure-dependent response of the composite material. As a result, a model based on one set of material properties may yield good predictions for one joint configuration, but not for other configurations where some joint design parameters, for example, the clamping pressure, are modified.

Based on the previous observations, the objectives of this chapter are to present new analysis models for composite bolted joints. Taking into account that the design of a complex composite structure is based on different phases

that include preliminary design and optimization and detailed analysis, two analysis models with different levels of complexity are presented.

The first one, based on finite fracture mechanics (FFMs) [11–13], provides solutions based on independently measured material properties in a few seconds. Therefore, the FFMs model is quite suitable for preliminary design and optimization of composite structures.

The second method presented in this chapter is based on a recently developed three-dimensional plastic-damage model for polymer composites implemented in a nonlinear finite element (FE) code [14]. Both methods presented are applicable to both composite-to-composite and composite-to-metal hybrid joints.

7.2 Semianalytical Method for the Design of Composite Joints

The FFMs approach to the prediction of failure is based on the assumption that crack propagation requires the simultaneous fulfillment of stress-based and energy-based criteria [11–13]. Generally, the FFMs criterion is given as [13]:

$$\begin{cases} \frac{1}{l} \int_R^{R+l} \sigma(x) dx = X_L^T \\ \frac{1}{l} \int_R^{R+l} K^2(a) da = K_C^2(\Delta a) \end{cases} \quad (7.1)$$

where l is the crack extension at failure, R is the radius of the hole, a is the crack length, X_L^T is the unnotched laminate strength, $\sigma(x)$ represents the stress distribution along the failure plane, K is the stress intensity factor and $K_C^2(\Delta a)$ is the laminate's fracture toughness, which may be a function of the increment in crack length (Δa). Taking into account that the application of the FFMs criterion is based on the propagation of a dominating macro crack, this criterion is appropriate to predict the net-tension or shear-out failure modes. However, the FFMs model is not applicable for the prediction of the bearing failure mode (Figure 7.1). This noncatastrophic failure mode is characterized by a permanent deformation of the hole that results from the accumulation of different failure mechanisms in the several plies of a composite laminate. Therefore, it is not possible to clearly define a macro crack that determines the ultimate failure of the joint.

7.2.1 Prediction of Net-Tension Failure

Figure 7.2 shows the geometry of a composite laminate subjected to a bolt-bearing load, which is represented by a sinusoidal pressure distribution, $p(\theta)$. The FFMs criterion requires solutions for the stress distribution along the

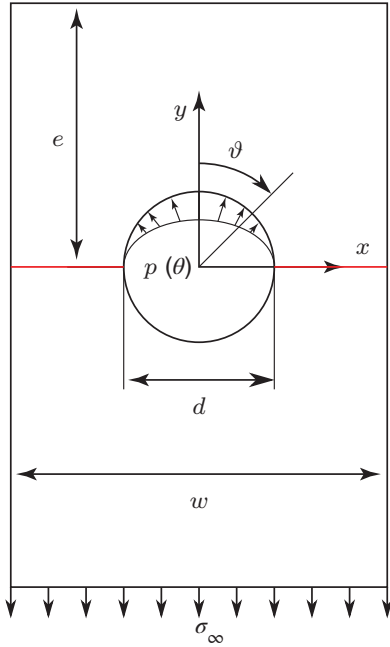


Figure 7.2 Geometry and loading of the composite lap.

net-tension plane and for the stress intensity factor of two symmetric cracks that emanate from the hole boundary along the net-tension failure plane.

Introducing the nondimensional variables $\xi = \frac{2}{w-d} \left(x - \frac{d}{2} \right)$ and $\xi^* = \frac{2}{w-d} \left(a - \frac{d}{2} \right)$ it is possible to rewrite Equation 7.1 as:

$$\left\{ \begin{array}{l} \frac{1}{l} \int_0^l \sigma_{yy}(\xi) d\xi = X_L^T \\ \frac{1}{l} \int_0^l K_I^2(\xi^*) d\xi = K_{IC}^2(\Delta \xi^*) \end{array} \right. \quad (7.2)$$

While there are semianalytical methods available for the prediction of the stress distribution, the solution for the stress intensity factor for generic laminates requires a numerical approximation.

The procedure proposed consists of the numerical calculation of the functions $\sigma_{yy}(\xi)$ and $K_I(\xi)$ using linear elastic FE models. The superposition of the stress field is used to compute the stress distribution along the net-tension plane. Considering $\phi(\xi, \varpi)$ as the nondimensional scaling function of the stress distribution for a unit remote load applied to a composite bolted joint with a unit hole diameter and of unit thickness, the stress distribution for a general remote load and diameter reads:

$$\sigma_{yy}(\xi, \varpi) = \sigma_b \phi(\xi, \varpi) \quad (7.3)$$

where σ_b is the bearing stress defined as $\sigma_b = \frac{P}{dt}$, and ϖ is a nondimensional parameter defined as the ratio between the specimen width and the hole diameter, $\varpi = w/d$.

The nondimensional function $\phi(\xi, \varpi)$ is defined using parametric linear-elastic FE models of composite bolted joints of unit thickness and unit hole diameter, subjected to a unitary remote load. ABAQUS [15] CPS8 elements are used in the parametric FE models to predict the stress distribution along the net-tension plane. Based on the FE results, $\phi(\xi, \varpi)$ is approximated by a polynomial of the form $\phi(\xi, \varpi) = \sum_{i=1}^M \sum_{j=1}^N \Phi_{ij} \xi^{i-1} \varpi^{j-1}$. The components of the matrix Φ_{ij} are given in [16].

A similar procedure is used to define the function $K_I^2(\xi^*)$. The linear-elastic FE models represent two symmetric cracks that emanate from the hole boundary, and the corresponding stress intensity factor is computed for different crack lengths from the J -integral calculated using ABAQUS [15].

Considering $\psi(\xi, \varpi)$ as the nondimensional scaling function of the stress intensity factor for a unit remote load applied to a composite bolted joint with a unit hole diameter and thickness, and with two symmetric cracks along the net-tension plane, the stress intensity factor for a general remote load and diameter reads:

$$K_I(\xi^*, \varpi) = \sqrt{d} \sigma_b \psi(\xi^*, \varpi) \quad (7.4)$$

Based on the FE results, $\phi(\xi, \varpi)$ is approximated by a polynomial of the form $\psi(\xi^*, \varpi) = \frac{\sqrt{\xi^*}}{1-\xi^*} \sum_{i=1}^M \sum_{j=1}^N \Psi_{ij} \xi^{*i-1} \varpi^{j-1}$. The components of the matrix Ψ_{ij} are presented in [16].

Using (7.3) and (7.4) in (7.2), the resulting system of nonlinear equations may be solved for the bearing strength and for the crack length at failure.

The model presented in the previous points is validated by comparing its predictions with tests performed using Hexcel's IM7/8552 carbon–epoxy laminates. The $[90/0/+45/-45]_{35}$ lay-up with a nominal thickness of 3 mm is used. The ply elastic properties of IM7/8552 are shown in Table 7.1.

E_1 and E_2 are respectively the ply longitudinal and transverse Young's modulus, G_{12} is the ply shear modulus, and ν_{12} is the ply major Poisson's ratio.

The laminate unnotched strength is $X_L^T = 845$ MPa [14], and the laminate fracture toughness is approximated by the following Gompertz function [17]:

$$K_{IC}(\Delta a) = K_{IC}^p e^{\log(K_{IC}^i/K_{IC}^p)} e^{-b\Delta a} \quad (7.5)$$

Table 7.1 Ply elastic properties.

E_1 (GPa)	E_2 (GPa)	G_{12} (GPa)	ν_{12} (-)
171	9.1	5.3	0.32

where K_{IC}^i is the initiation fracture toughness, K_{IC}^p is fracture toughness corresponding to steady-state crack propagation, and b is a shape factor calculated from the R -curve. The R -curve for the material and lay-up used here is obtained as follows: compact tension (CT) tests are performed in cross-ply $[90/0]_{8s}$ laminates to measure the R -curve of the 0° plies for cracks that propagate perpendicularly to the fiber direction [18]. Having measured the R -curve for the 0° plies, the R -curve of the lay-up under investigation is calculated using a mechanical model that relates the fracture toughness of the 0° plies to that of a multidirectional laminate [19], assuming that the length of the fracture process zone is the same. Based on this procedure, the remaining material properties required for the model are obtained as: $K_{IC}^i = 36.7 \text{ MPam}^{-1/2}$, $K_{IC}^p = 42.8 \text{ MPam}^{-1/2}$, and $b = 1.27 \text{ mm}^{-1}$.

The experimental tests are conducted following the American Society for Testing and Materials ASTM D-5961 standard [20]. Three different width-to-diameter ratios are used: $\varpi = 1.5, 1.75$, and 2.0 . The hole diameter is 6 mm for all tests, and a 2.2 N m torque is applied to the washers to represent finger-tight bolted joints. Figure 7.3 shows the specimens corresponding to the three different geometries used after being loaded up to failure. The net-tension failure mode is observed in all the specimens tested. This means that the FFMs model is applicable to predict the strength of the specimens tested.

Table 7.2 compares the model predictions with the experimental results.

An excellent agreement between the model predictions and the experimental results is obtained. However, it should be noted that the model needs to be complemented with a prediction of the transition between the net-tension and bearing failure modes. This prediction can be performed using appropriate modifications of the conventional point- or average-stress models or by means of the more advanced numerical models that will be described in the following section.

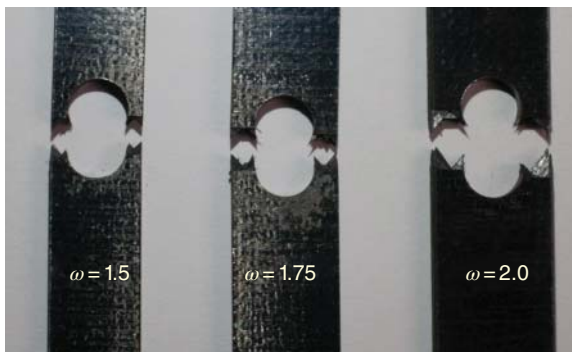


Figure 7.3 Failed joints with different w/d ratios.

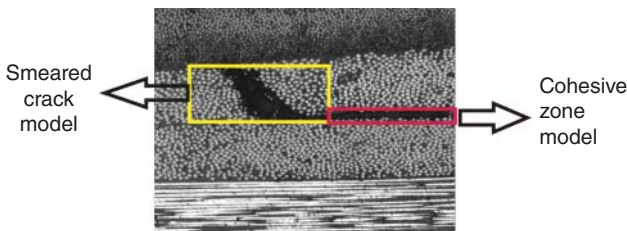
Table 7.2 Experimental and predicted joint strengths.

Specimen	Strength (MPa) (exp.)	Strength (MPa) (FFMs model)	Error (%)
NT1	322	310	−4
NT2	466	435	−7
NT3	527	550	+4

7.3 Numerical Method for the Design of Composite Joints

A large number of numerical models have been proposed to simulate the onset of the different failure mechanisms that occur in composite bolted joints, as well as their propagation up to a critical dimension corresponding to the structural collapse of the joint. Initially, models that use a simple reduction of some ply elastic properties to represent the ply failure mechanisms were developed [9]. These models yielded reasonable predictions, but the element size needs to be calibrated to ensure that the computed energy dissipation matched the actual energy dissipated as a result of the propagation of different failure mechanisms. In addition, most of the models developed to simulate failure in composite bolted joints did not account for the delamination failure mechanism. While neglecting delamination is acceptable for bolted joints with through-the-thickness pressure applied by washer, this is not the case for pinned joints that tend to delaminate.

The methodology to simulate failure in composite bolted joints under general loading conditions proposed here consists of the use of cohesive elements [21, 22] at the interfaces between the plies to simulate delamination and a recently proposed three-dimensional smeared crack model to simulate ply failure mechanisms [14], see Figure 7.4.

**Figure 7.4** Modeling strategy.

The concept of the smeared crack model is easily explained using a simple one-dimensional example. Consider the propagation of a transverse crack under pure transverse tension (mode I loading), as shown in Figure 7.5.

Under pure transverse tension, the crack plane is perpendicular to the applied stress, and it is bridged by unbroken matrix ligaments. Such bridging mechanisms have been experimentally observed, see [23]. As the crack opening, defined by ω , increases the traction transferred by the crack faces, t , decreases until a traction-free crack is created. This relation between the traction and crack opening can be represented by a simple linear softening law, as shown in Figure 7.6.

The linear softening law is given as:

$$t(\omega) = Y_T \left(1 - \frac{\omega}{\omega_f} \right) \quad (7.6)$$

where Y_T is the transverse tensile strength, and ω_f is the crack opening corresponding to a fully formed crack. The area under the traction–crack opening relation is the fracture toughness, G_C , which implies that the crack opening

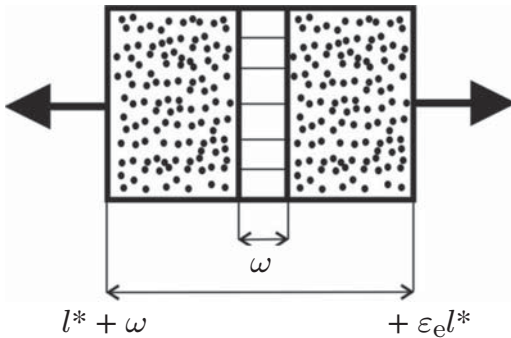


Figure 7.5 Schematic representation of bridged transverse crack.

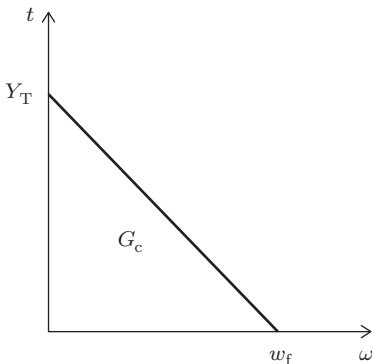


Figure 7.6 Linear traction–displacement jump relation.

corresponding to a fully formed crack is given as:

$$\omega_f = \frac{2G_c}{Y_T} \quad (7.7)$$

The total strain of the representative volume element shown in Figure 7.5 reads:

$$\varepsilon = \varepsilon_e + \frac{\omega}{l^*} = \varepsilon_e + \varepsilon_c \quad (7.8)$$

where ε_e is the elastic strain of the undamaged material, ε_c is the cracking strain, and l^* is the original dimension of the representative volume element in the direction perpendicular to the crack plane. Applying the equilibrium equation (see Figure 7.7):

$$\sigma = E(\varepsilon - \varepsilon_c) = t(\omega) \quad (7.9)$$

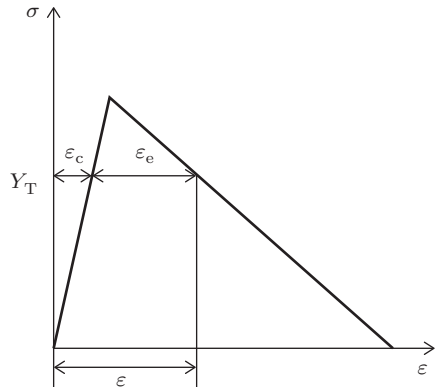
Using (7.7) and (7.8) in (7.9), the resulting equation may be solved for the displacement jump:

$$\omega = \frac{2(E\varepsilon - Y_T)l^*G_c}{2EG_c - Y_T^2l^*} \quad (7.10)$$

Having defined ω from the applied strain, the calculation of the stress is trivially performed using the cohesive law shown in (7.6).

It is interesting to relate the smeared crack model with the previous approaches where the failure mechanisms of composite laminates were represented by degrading some components of the stiffness matrix, in the spirit of continuum damage mechanics. For the simple case previously presented, the effect of the transverse matrix crack is represented by the use of a reduced Young's modulus (E_d) as: $\sigma = E_d\varepsilon$, see Figure 7.8.

Figure 7.7 Total strain, elastic strain, and cracking strain.



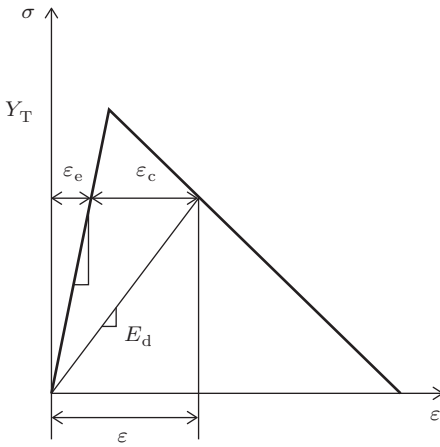


Figure 7.8 Damaged elastic modulus.

The relation between the smeared crack model and the continuum damage model is given as:

$$\sigma = E_d \epsilon = E(\epsilon - \epsilon_c) \quad (7.11)$$

Using (7.8) and (7.10) in (7.11):

$$E_d = \frac{Y_T(2G_C - \epsilon Y_T l^*)}{(2EG_C - Y_T^2 l^*)\epsilon} E \quad (7.12)$$

Equation (7.12) defines the degradation factor of the transverse Young's modulus. It should be emphasized that the degradation factor is no longer empirical, but it is now defined as: $\frac{Y_T(2G_C - \epsilon Y_T l^*)}{(2EG_C - Y_T^2 l^*)\epsilon}$.

The previous exercise also identifies a critical length for l^* , which corresponds to the maximum admissible element size in a FE model: E_d must be greater than or equal to zero; imposing this condition to Equation 7.12 results in the following restriction to the element size $l^* \leq \frac{2EG_C}{Y_T^2}$. An alternative interpretation of this condition is that the total strain must be equal to or greater than the cracking strain (see Equation 7.11), which means that the snap-back of the softening stress–strain relation shown in Figure 7.8 is not allowed.

In composite bolted joints, the crack plane may have different orientations, and it may be subjected to mixed-mode loading conditions. Therefore, there is the need to generalize the previous one-dimensional smeared crack model to general loading conditions. We propose a fully three-dimensional smeared crack model that is based on the following building blocks [14]:

- Fully three-dimensional, pressure-dependent failure criteria that predict failure onset and the orientation of the crack plane [24]. After the crack plane orientation and its associated coordinate frame (crack frame) are defined, they remain fixed and all the computations are performed in the crack frame.

- Mixed-mode cohesive law that accounts for the dependence of the fracture toughness on the mode ratio and for loading/unloading conditions. Appropriate modifications of the mixed-mode cohesive law that was developed to simulate delamination [21, 22] are used.

The full details of the three-dimensional smeared crack model used to simulate ply failure mechanisms are described in [14]. The simulation of delamination is performed using cohesive elements [21, 22] placed in the interfaces between the plies. Both the smeared crack model and the cohesive model are implemented in ABAQUS Explicit [15] by means of user-defined material models coded in VUMAT [15] subroutines.

The FE models use one volumetric three-dimensional element per ply thickness (ABAQUS C3D8R elements with reduced integration [15]) and cohesive elements along all the interfaces of the composite laminate. The steel washer and steel bolt are also simulated using C3D8R elements.

Two different mesh densities were used, see Figure 7.9: in the vicinity of the hole, a refined mesh where the typical dimension of the FEs is $0.5 \times 0.5 \times 0.125 \text{ mm}^3$ is used. In the elastic region, where no damage takes place, a coarse mesh is used. The connection between the two nonconforming meshes is established by means of multipoint constraints.

The loading imposed to the model consists of a first step where a 22 MPa clamping pressure is applied to the washer. The joint is then loaded in tension by applying a velocity to one end of the laminate while keeping the bolt fixed.

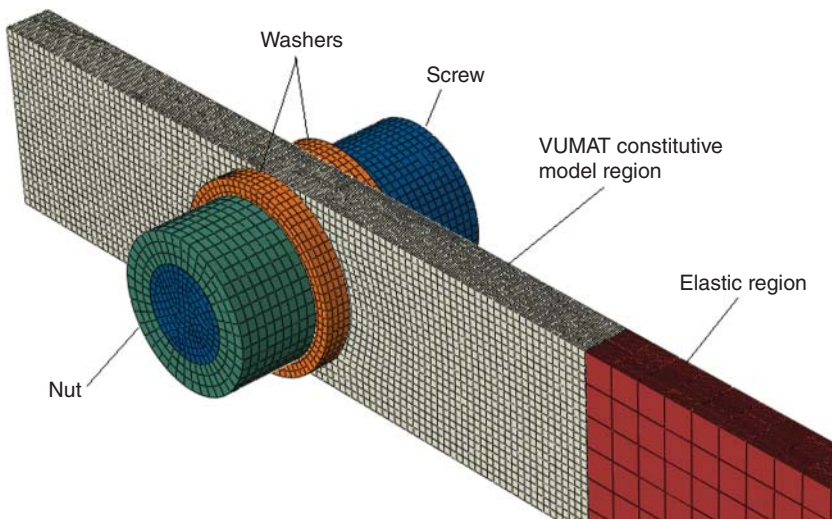


Figure 7.9 Mesh of bolted joint.

Table 7.3 Ply strengths and interlaminar fracture toughness.

X_T (MPa)	X_C (MPa)	G_{Ic} (kJ/m ²)	G_{IIc} (kJ/m ²)	η (-)
2806	1200	0.277	0.788	1.6

Table 7.4 Ply translaminar fracture toughness.

G_{1+}^i (kJ/m ²)	G_{1+}^{ss} (kJ/m ²)	l_{fpz} (mm)	G_{1-}^i (kJ/m ²)
94.1	134.7	3.4	47.5

The geometry and material properties used correspond to those of the NT3 net-tension specimen shown in Table 7.2. The additional material properties required for the FE model are presented in Tables 7.3 and 7.4.

X_T and X_C are respectively the tensile and compressive longitudinal strengths. G_{Ic} and G_{IIc} are respectively the mode I and mode II interlaminar fracture toughness, and η is the mixed-mode interaction parameter [21, 22]. G_{1+}^i and G_{1+}^{ss} are respectively the initiation and steady-state propagation values of the tensile translaminar fracture toughness, l_{fpz} is the length of the corresponding fracture process zone, and G_{1-}^i is the compressive translaminar fracture toughness.

Figure 7.10 represents the damage variable associated to fiber fracture after the final failure of the specimen. Failure along the net-tension plane and damage due to the bolt-bearing load are observed.

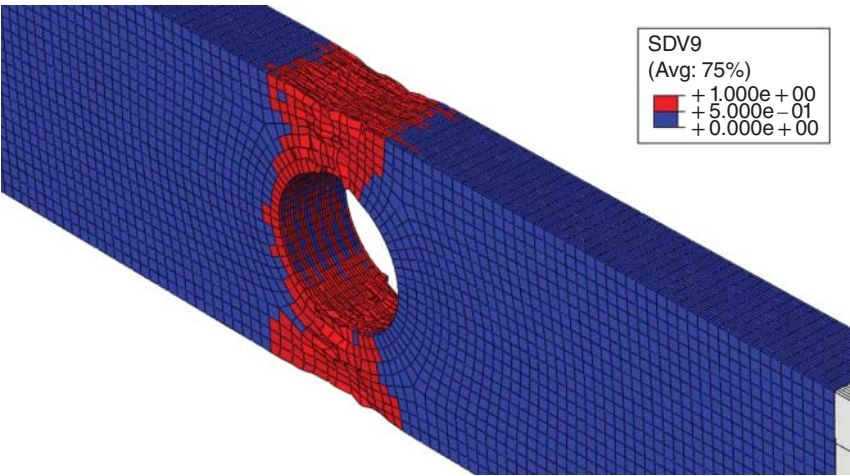


Figure 7.10 Damage variable for the fully failed joint.

The predicted peak value of the bearing stress in 519 MPa is a value that correlates well with the experimental results (mean value of the bearing stress of 531 MPa). The corresponding error of -2.4% is an improvement over the predictions obtained using the FFM model (error of 4.3%).

7.4 Conclusions

The models presented in this chapter indicate that it is currently feasible to perform accurate predictions of the strength of composite bolted joints with different levels of detail and cost.

The simple models based on FFMs yield sufficiently accurate predictions for the preliminary design of composite structures. The absolute value of the maximum error obtained using the FFM model is 7% , which is quite remarkable taking into account that the model is based on independently measured material properties defined at laminate level. The FFM model yields predictions in a few seconds, which means that this model is appropriate for the optimization of large-scale structures. The cost of using the FFM is very small – the only material properties required are the *R*-curve of the laminate and its strength; in addition, no expert users nor large computing power is needed to run the model.

However, the FFM model is only applicable when a macro crack dominates the fracture process. This means that it is suitable to predict net-tension and shear-out failure modes, but not the bearing failure mode that is characterized by the permanent deformation of the hole as a result of the accumulation of subcritical failure mechanisms. The strength prediction of bolted joints that fail by bearing requires appropriate modifications of the point- or average-stress models, of nonlinear FE models.

The modeling strategy based on three-dimensional finite-element models that use cohesive elements along the interfaces between the plies to simulate delamination and a smeared crack model to simulate ply failure is an alternative method to predict the strength of composite bolted joints.

The smeared crack model accounts for the directionality of the cracks and for crack propagation under general mixed-mode loading conditions. In addition, the smeared crack model provides a clear physical meaning to the reduction factors used in previous damage models: the components of the stiffness tensor that are affected by damage and the corresponding reduction factor are a function of the orientation of the crack, of the applied strain, of the material properties, and of the characteristic element length. An upper bound for the size of the FE that can be used is also identified from the formulation of the smeared crack model.

The accuracy of the prediction of the net-tension failure load using the nonlinear finite-element model is better than that of the analytical

model. In addition, the nonlinear finite-element model enables the prediction of residual strength and the identification of the several subcritical failure mechanisms. However, these improvements come with the cost of large computing times and of a larger number of experimental tests to measure the required material properties, which are defined at ply level

Both methods presented here are also applicable to composite-to-metal hybrid joints, therefore very useful with regard to the increasing trend in the design of high-performance hybrid structures.

Acknowledgments

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8

Friction Riveting of Polymer–Metal Multimaterial Structures

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8.1 Introduction

In recent years, the use of polymer–metal hybrid structures has increased in industrial applications, mainly due to improved performance and weight reduction. The application fields range from automotive, aerospace, shipbuilding, railway, and civil engineering. Joining of polymer–metal multimaterials is still a new research topic, and only little information is available. Messler Jr [1, 2] has reported that the joining methods used for multimaterial structures are mechanical fastening, adhesive bonding and several welding processes (the latter only for similar joints within a structure). Furthermore, other techniques such as hybrid joining methods (adhesive bonding combined with mechanical fastening or welding), polymer staking, and plastic injection over molding in perforated metallic parts are currently being explored [3, 4].

Friction Riveting (FricRiveting) was developed and patented by the Helmholtz-Zentrum Geesthacht (HZG) in Germany in 2007 as a method of joining hybrid metal–polymer structures [5, 6]. This method is based on the principles of mechanical fastening and friction welding, where a cylindrical rivet is used to join metallic and/or thermoplastic parts through frictional heat. When compared to other manufacturing techniques used for hybrid joints, such as adhesive bonding, friction riveting does not require complex surface preparation or curing agents and requires a shorter process time. Regarding the use of rivets, this method eliminates the need for predrilling, reducing the number of process steps and reducing the stress concentration due to the presence of the notch effect related to through-holes. This technique allows for the manufacture of joints with high mechanical performance [7].

FricRiveting has the potential to fulfill the technology and market needs of polymer–metal multimaterial structures. The technique has recently attracted the attention of the international R&D joining community, winning different engineering and scientific awards (see list of awards and prizes at the end of the chapter). The Friction Riveting (FricRiveting) technique is a new alternative spot joining process developed for polymer–polymer and polymer–metal multimaterial structures. Frictional heat during the process is mainly generated by the friction between the tip of the rotating rivet and the polymer (or composite) substrate in the friction zone. In the basic configuration of the technique, a cylindrical metallic rivet is used to join one or more thermoplastic or thermosetting components by means of plasticizing and deforming the tip of the rotating rivet through frictional heating. Advantages of this new technique are short joining cycles, minimal sample preparation, and the absence of environmental emissions. Friction-riveted joints have enhanced mechanical performance. The feasibility of FricRiveting has been demonstrated on several material combinations, ranging from unreinforced polymers to carbon- and glass-fiber-reinforced composites with aluminum or titanium, as shown in Table 8.1:

Table 8.1 Studied material combinations for FricRiveting (Status from December 2017).

Material combination	AA 2024-T351	AA 6056-T6	Ti gr. 2	Ti gr. 3	Ti6Al4V	Stainless steel S321
Polyetherimide (PEI)						
Polycarbonate (PC)						
40% short glass-fiber-reinforced polyetheretherketone (40CF-PEEK)						
30% short-glass-fiber-reinforced polyamide 6 (30GF-PA6)						
Glass-fiber woven-reinforced polyetherimide (GF-PEI)						
Carbon-fiber-reinforced polyetheretherketone (CF-PEEK)						
Pultruded glass-fiber-reinforced thermoset polyester (GF-P)						

Gray shadings indicate proven feasible material combinations

8.2 FricRiveting: Principles of the Technique

The concept of FricRiveting is based on the principles of mechanical fastening and friction welding, where joining energy in the form of frictional heat is supplied by the rotational movement of one of the joining partners (normally a cylindrical rivet). The friction riveting process can be performed for various joint configurations, such as lap joints using a metallic rivet or metallic inserts in polymer parts. The principles of this process can be better understood for the manufacture of metallic-insert joints in thermoplastics. Initially, the to-be-joined parts are fixed on the equipment over a backing plate. The next stage is the early joining stage, in which the rivet is rotated and pressed against the surface of the polymeric component (Figure 8.1(a)); the high rotation and applied axial force heat the polymer by friction, resulting in a thin molten/softened layer around the tip of the rivet (Figure 8.1(b)). As the rivet penetrates into the thermoplastic matrix, the molten/softened thermoplastic is expelled as flash outside the joint area. Due to the high temperature and low thermal conductivity of the thermoplastic, the local temperature at the tip of the rivet rises considerably, nearing the plasticizing temperature of the metal (between 60% and 95% of the melting temperature). At this time, the rivet's rotational speed (RS) is decreased, and the axial force is increased – the so-called forging force is applied; the rivet expels the remaining softened material toward the flash, encountering the resistance of the cold volume of the thermoplastic. The plasticized tip of the rivet is thereby deformed, assuming a larger diameter than the initial diameter (Figure 8.1(c) and (d)). After cooling under pressure, the joint is consolidated (Figure 8.1(e)). The increase of axial force can be skipped depending on the selection of joining parameters controlling heat generation. In other words, the forging phase in FricRiveting can be avoided if the heat input is high enough to allow the plasticizing of the rivet tip. This could be advantageous, for instance, to enable the use of joining equipment with smaller pay loads [7].

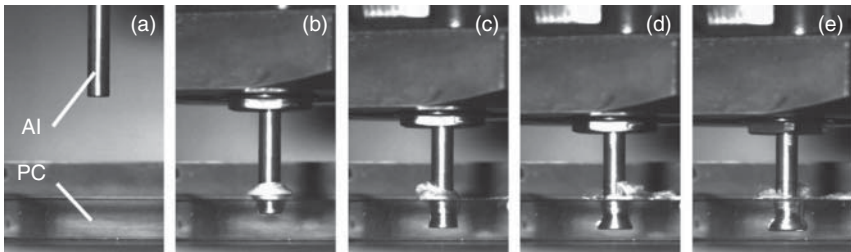


Figure 8.1 Friction riveting process scheme as illustrated for “point-on-plate” joints. (a) Positioning and clamping of joining partners. (b) Rotational insertion of the rivet into the polymeric base plate. (c) Rotational breaking followed by (d) Rivet forging. (e) Joint consolidation (Source: Rodrigues *et al.* 2014 [8]. Reproduced with permission of Elsevier).

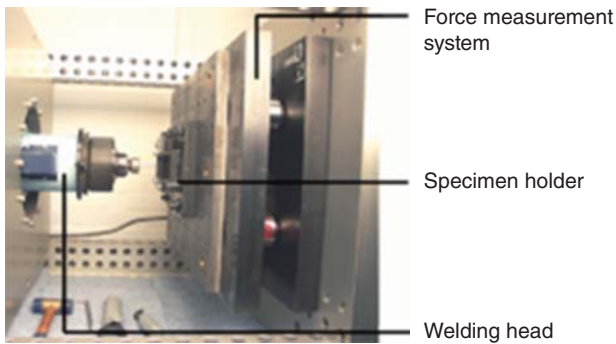


Figure 8.2 Scheme of joining equipment RSM 400 used for FricRiveting.

8.2.1 Joining Equipment and Procedure

Generally speaking, any joining equipment that is able to provide rotation and spindle translation (i.e., CNC and milling and friction welding machines) could be adapted for FricRiveting. The joining equipment originally used in the process development of FricRiveting consists of a commercially available friction welding machine (RSM 400, Harms & Wende, Germany) with a coupled force measuring system for torque measurements (Kistler GmbH, Germany). Figure 8.2 shows the joining equipment described earlier [7].

More recently, a new research-purpose machine for FricRiveting – The RNA (H. Loitz-Robotik, Germany) – has been developed at the Helmholtz-Zentrum Geesthacht. The new gantry machine consists of a commercially available high-speed friction welding system (RSM 410, Harms & Wende GmbH, Germany) assembled in an automated portal structure. The RNA is equipped with triaxial force sensors, torque sensors, and an integrated position sensor (Figure 8.3). The basic differences between the RNA equipment and its predecessor, RSM 400 are: the process can be controlled either by or a combination of time, force, displacement, rotational speed, and angle while in the previous joining equipment, the process was controlled only by time. The new equipment allows the process to be divided into five controlled setup steps (the first three are related to the friction, the fourth is the intermediate phase while the fifth is related to the forging phase), while in the previous one, the joining parameters setup occurs in two time-controlled steps (friction and forging) [9]. The new gantry system also enables the fabrication of components up to about one m³.

8.3 FricRiveting: Process Parameters and Variables

Process parameters and variables in FricRiveting are analogous to the ones found in friction welding [10] and spin welding [11], due to their process

Figure 8.3 The RNA FricRiveting equipment.



similarities. Parameters can be understood as controllable input data while variables as resulting process outputs. The main process parameters that influence the thermal, mechanical, and microstructural properties of joints manufactured by FricRiveting are: rotational speed (RS), joining time (JT), and joining pressure (JP). The effective combination of these parameters with the properties of the materials to be joined (thermal conductivity, thermal expansion, and melt viscosity, among others) determines the quality of anchoring of the metallic rivet following deformation in the thermoplastic matrix and hence the mechanical performance of the joints [12]. The main process variables in the process are the Heating Time, Burn-Off, Burn-Off Rate, Temperature, and Frictional Torque. Process variables and parameters will be described in the next sections.

8.3.1 Process Parameters

Rotational Speed (RS) is the angular velocity of the rotating cylindrical rivet. It is important to the temperature development and associated phenomena (control of the viscosity of the molten polymer, thermal defect formation, etc.), assisting in the frictional heating generation. **Joining Time (JT)** comprises the sum of the friction time (t_0 – FT segment in the diagram of Figure 8.4) and the forging time (FT – FoT segment in the diagram of Figure 8.4). Friction time and forging time (FoT) are set by the operator. Besides being a control factor for joining speed, the JT influences the level of volumetric defects in the polymeric part, related to thermomechanical processing, by controlling the

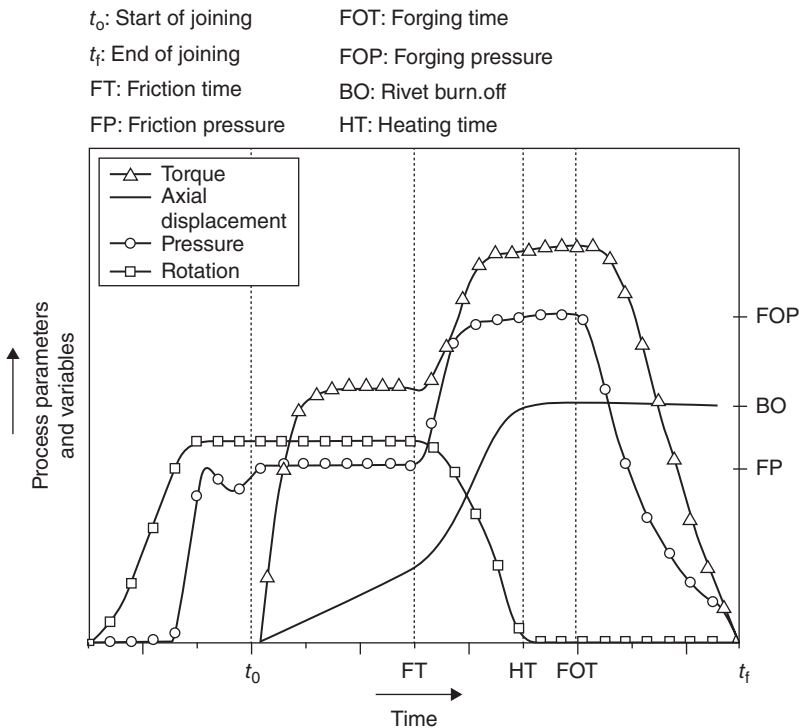


Figure 8.4 Schematic experimental monitoring diagram with the main FricRiveting process parameters and variables (e.g., for the time-controlled process variant).

amount of heat energy supplied to the molten polymeric film. Joining Pressure (JP) is the addition of the friction pressure ($t_f - \text{FP}$ segment in the diagram of Figure 8.4) and the forging pressure (FP – FoP segment in the diagram of Figure 8.4). FP and FoP are also set by the operator. JP is related to the normal pressure distribution on the rubbing surfaces. Similarly to RS and JT, it also contributes to heating of rubbing surfaces and joining speed control, although its main role lies in controlling rivet forging and consolidation phases [7]. The more recent RNA equipment uses force instead of pressure, thus the parameter will be analogously called Joining Force.

8.3.2 Process Variables

Heating Time is the interval related to the initial contact between rivet and the base element (touchdown), and the moment when RS becomes null ($t_0 - \text{HT}$ segment in the diagram of Figure 8.4). This variable is representative of the heat input regime and provides a good estimate of material plasticizing level and

an indication of the amount of material thermal-induced structural changes (e.g., polymer degradation, physical aging, and metal annealing phenomena). **Burn-Off** is a variable associated with the insertion level of the rivet into the base components and its consumed (deformed) length. It is obtained from the axial displacement monitoring curve, from the rivet touchdown up to the end of the joining course ($t_f - \text{BO}$ segment in the diagram of Figure 8.4). Along with the heating time, this variable provides a good estimate of the plasticizing level of the rivet achieved during joining. **Burn-Off Rate** can be seen as the average joining speed during FricRiveting. It is calculated from the quotient between the rate of burn-off and heating time, providing an average approximation to the real joining speed.

Average Process Temperature provides an estimation of changes in viscosity of the softened/molten polymer, which is, together with the deformation rate, directly linked with frictional conditions and heat generation. This is a key variable when explaining microstructural and properties changes within the joint area. Frictional Torque is not only a tool for estimating the rheological behavior of molten polymer and plasticizing of rivet material, but also a way of identifying process anomalies and defects regarding equipment malfunction and poor surface finish (e.g., remaining finishing fluids changing frictional condition) of joining components prior to joining [7].

8.4 FricRiveting: Process Phases and Heat Generation

FricRiveting can be decomposed in terms of process phases related to different stages of heat generation and rivet axial displacement over JT. This analogy is typically adopted in other friction-based joining techniques such as in spin welding of plastics [13]. The process can be divided into five main phases as schematized in Figure 8.4. In Phase-I, Coulomb Friction (solid friction) is the mandatory heating mechanism; Coulomb friction is also found in the initial phases of other friction joining methods [14]. Solid friction in FricRiveting is believed to be of the polymer–polymer type, where the harder asperities of the metal penetrate the softer polymer, instead of being smeared off. Usually, the dynamic friction coefficient decreases with rotation and time due to polymeric film transfer on the metal surface (generally for ductile polymers) or because of tiny split polymeric particles, which act as lubricant [15] (generally for glassy polymers). During this very short phase, rivet axial displacement is virtually zero (see P-I, Figure 8.5).

The friction regime changes from the solid to the molten state when frictional heating is high enough to soften/melt the polymer in the faying surfaces. It will mark the beginning of the Unsteady-State Viscous Dissipation or Phase-II (see P-II, Figure 8.5). In the course of this phase, frictional heating is mainly due

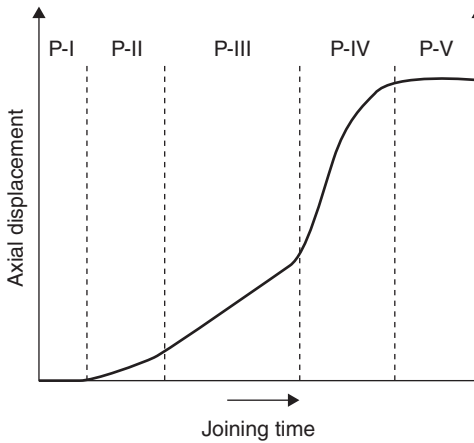


Figure 8.5 Process phases in FricRiveting: P-I: Coulomb Friction (solid friction). P-II: Unsteady-State Viscous Dissipation. P-III: Steady-State Viscous Dissipation. P-IV: Rivet Forging Phase. P-V: Consolidation Phase.

to internal shearing in the molten polymer owing to chain disentanglement [16]. During this phase, the rivet starts penetrating the polymeric base plate, whereby axial displacement increases nonlinearly with time.

In Phase-III, or Steady-State Viscous Dissipation phase, there is a balance between the melting rate of the polymer underneath the rotating tip of the rivet and the molten material (expelled as flash) outflow rates. Consequently, the axial displacement increases linearly with time. By the end of this phase, the desired level of plasticizing in the rivet tip is achieved, and the process is ready for the forging phase [17].

In the Rivet Forging Phase or Phase-IV, rotation is decelerated (motor braking) and axial pressure (forging pressure) is increased. As a result, the thickness of molten polymer layer is strongly reduced below the rivet tip. There is an augment in flash volume due to the extra amount of squeezed material. Note that the flash material can be either extracted postjoining or accommodated into re-entrances placed in the head of the rivet. In this way, a sharp increase in axial displacement rate (see change in the curve slope of region P-IV, Figure 8.5). The plasticized tip of the rivet is then deformed, leading to a paraboloidal pattern [17].

In the Consolidation Phase or Phase-V, the joint solidifies under constant pressure. The displacement reaches a maximum, becoming constant (see P-V, Figure 8.5). Molten polymer consolidates around the rivet, creating a bonded interface [17].

Based on the description of the heat generation during the different process phases, Amancio-Filho and dos Santos [18] proposed an analytical model for the heat input in friction-riveted unreinforced joints:

$$Q_{\text{total}} = \left[\left(\frac{2}{3} \cdot \mu \cdot P(r) \right) + \frac{\eta \cdot V_{\text{max}}}{H} \right] \cdot V_{\text{max}} \quad (8.1)$$

where μ is the kinematic friction coefficient, $P(r)$ the normal pressure distribution over the frictional area, η the molten polymer viscosity, $V_{\max}$ the maximal tangential speed of the rivet obtained from the angular speed (ω) and the radius of the undeformed rivet (R): $\omega = V_{\max}/R$, and H is the average width of the consolidated polymeric layer [18].

This model correlates the joining process parameters with the solid-friction and rheological properties of thermomechanically processed thermoplastic polymer. However, this analytical model does not account for the complex behavior of heat generation in thermoset polymers and woven-fiber-reinforced composites. In this case, solid friction takes place during the whole joining course and is influenced by matrix type and reinforcement (for composites).

The mechanical energy input is often used to estimate the heat generation during friction-based welding processes for thermoplastics [13, 19] and metals [20–23]. Considering the process similarities for friction riveting, this approach can be used in FricRiveting. The mechanical energy input is the energy introduced to the joint due to the FricRiveting process according to Equation 8.2 [20, 21], consisting of two terms. The first term corresponds to the energy accumulated due to the torque (M) and the angular velocity (ω) – that is, due to rivet rotation – and the second term corresponds to the energy due to the axial force (F) and the axial displacement (x) – that is, the rivet insertion. The first two terms can be simplified because term 2 (translational energy part) can be usually neglected since its contribution to the mechanical energy input is generally about 20 times lower than the contribution of term 1 [24]. The mechanical energy input was calculated according to the simplified form of Equation 8.2 [24]. Considering that M is a result of the material changes – that is, reflects changes in frictional properties – during the process, this equation can also be applied to estimate the process-dependent heat generation for thermoset-based and woven-reinforced composite parts.

$$E_{\text{mech}} = \int M \times \omega dt + \int F \times (x_0 - x_1) dt \sim \int M \times \omega dt \quad (8.2)$$

8.5 Thermal History

Thermal history in FricRiveting is dependent on heat transfer and mass transport as molten polymer is squeezed out. In other words, thermal history is a result of the balance between heat input and heat outflow. In this manner, not only thermal conductivity but also rheological properties of the molten polymer, such as molten viscosity, plus the level of frictional heating being supported by RS, JT, and JP will influence process temperature.

The temperature evolution during FricRiveting was evaluated for different base material (BM) combinations. Temperatures are usually measured on the fresh expelled molten polymer flash by means of an infrared (IR) camera.

It is well known from other friction-based welding techniques [25] that the direct measurement of the real temperature on the rubbing area is very complicated due to the relative motion of both matting surfaces. Considering the short heating phases present in FricRiveting and the low conductivity of the polymer, it is fairly acceptable to assume that the temperature in the flash material is about the same as in the rubbing area. In this way, the average peak temperatures measured by IR thermography can be considered as the average temperatures in the rubbing area.

For AA 2024-T351/PEI joints, average peak temperatures as measured by thermocouples and IR thermography were within 300–500 °C. Figure 8.6 shows the schematic thermometric and thermographic measurement systems used to monitor process temperature for a of PEI-Aluminum 2024 joint produced with 21,000 rpm, 3 s, and 11 bar [7]. Figure 8.6 shows that thermocouples' average peak temperatures were within an approximate range of 30–65 °C for H1, H3, and H4 and 350 °C for H2. The highest measured temperatures were observed for the thermocouples H2 and H3, the nearest measurement points to the rivet center and rubbing area. This indicates that temperature tends to increase when approaching the main source area of heat generation, the faying surfaces, as expected [7].

Similar trends were also reported for other thermoplastic–metal joints: Polycarbonate (PC)Aluminum 2024 (Figure 8.7(a) [8]), 40% short-carbon-fiber-reinforced Polyetherketone (PEEK-40CF)/Ti Grade 3 (Figure 8.8(b) [24]), 30% short-glass-fiber-reinforced Polyamide 6 (PA 6-30GF)-Aluminum 6056-T6 (Figure 8.9 [9]), glass-fiber woven-reinforced polyetherimide (GF-PEI) – Ti Grade 2 (Figure 8.10 [26]).

The measured experimental temperature range is approximately 60–95% of the melting point of the metallic rivet and is normally within the thermal alteration range of the thermoplastic part. In this way, both complex structural metallurgical changes in the rivet and macromolecular alterations in the polymer are expected to take place. The latter, however, is generally of lesser extension as the exposure of the molten matrix to the degradation temperatures is very short [12].

In the case of thermoset-based polymer and woven-fiber-reinforced composites, heat generation is rather more complex. Considering that the thermoset matrix does not melt due to its cross-linked structure, from the current theories, heat by viscous dissipation is thought to be absent. Heat is mainly a result of solid–solid friction (Coulomb friction) between the metallic rivet and matrix and reinforcement in the case of thermoset composites. For instance, Borba *et al.* [27] observed maximal process temperatures in the expelled flash material varying from 400 to 700 °C for pultruded glass-fiber-reinforced thermoset polyester (GF-P) – Ti-6Al-4V joints. In this case, thermal decomposition of the matrix was present in a thin volume

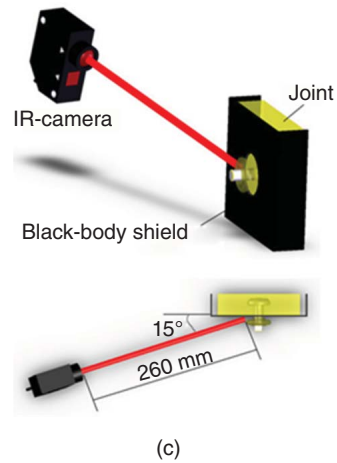
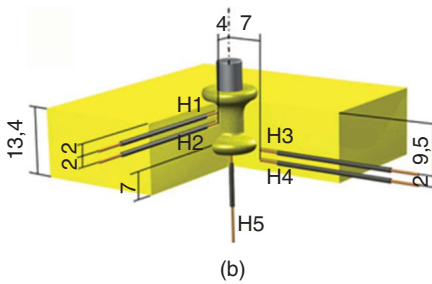
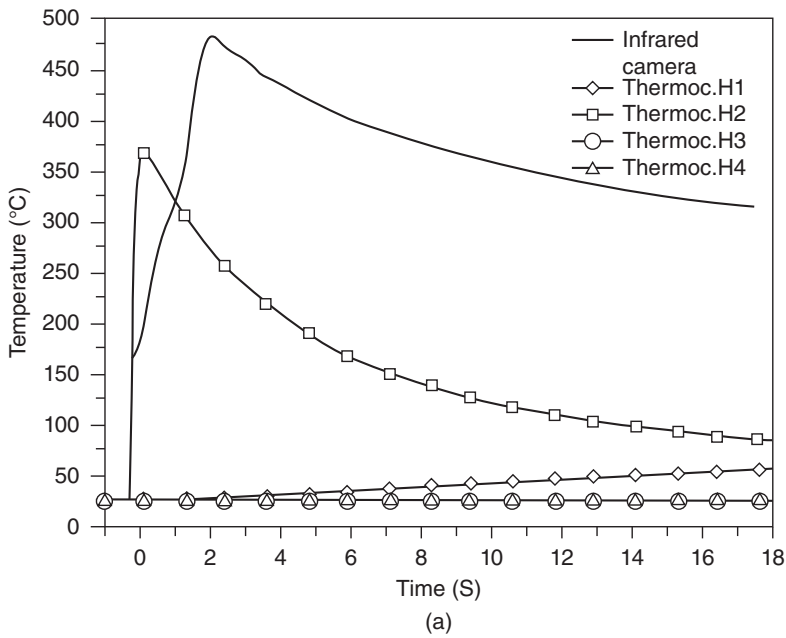


Figure 8.6 (a) Average experimental peak temperatures obtained for a 21,000 rpm, 3 s, 1.1 MPa Al 2024-T351/PEI friction-riveted joint, (b) Schematics of thermocouple disposition, (c) IR camera positioning.

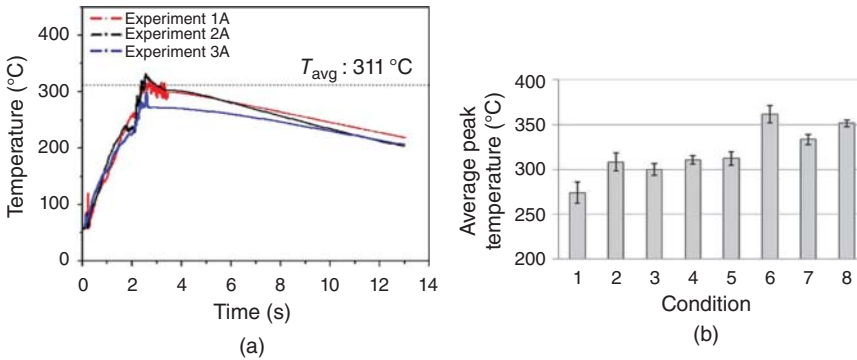


Figure 8.7 (a) Example of the process temperature history of FricRiveting joints measured in polymeric flash material for three samples produced with 18,000 rpm, 3 s, and 1.10 MPa. (b) Average peak temperatures for PC/AA 2024-T351 joints (Source: Rodrigues *et al.* 2014 [8]. Reproduced with permission of Elsevier).

around the rivet, as the measured temperatures were well above the thermal degradation range for the polyester matrix (390 °C) [27, 28].

8.6 Microstructure

In the previous sections, process temperature was shown to induce metallurgical and physical–chemical changes in the joining parts. Nondestructive techniques (NDT) combined to destructive analytical techniques, such as optical microscopy are normally used to evaluate joint microstructural changes.

The use of X-ray radiography is a typical NDT tool used to allow the observation of macroscopic defects and reveal geometrical changes in the tip of the plastically deformed rivet. Altmeyer *et al.* [24] radiographed short-carbon-fiber-reinforced PEEK/Titanium grade 3 friction-riveted specimens with X-ray prior to mechanical testing. They measured the rivet penetration depth (T) and the widening of the rivet tip (w), in addition to searching for volumetric defects, ensuring the joint quality. The X-ray measurements revealed the exact measurements of the anchoring zone at the center of the joints. An example X-ray scan used to conduct measurements is shown in Figure 8.11. Adding this nondestructive testing step can provide information regarding the relationship between the shape and size of the anchoring feature and the mechanical performance [24]. Part of the referenced work is presented in Chapter 12.

Synchrotron Computed Microtomography (μ CT) has been shown to allow precise NDT evaluation of 3D volumetric microstructural features of PEI/Al 2024 joints [7]. Figure 8.12 shows the internal 3D structure of the joint. Three rendered volume segments could be separated by means of computer image

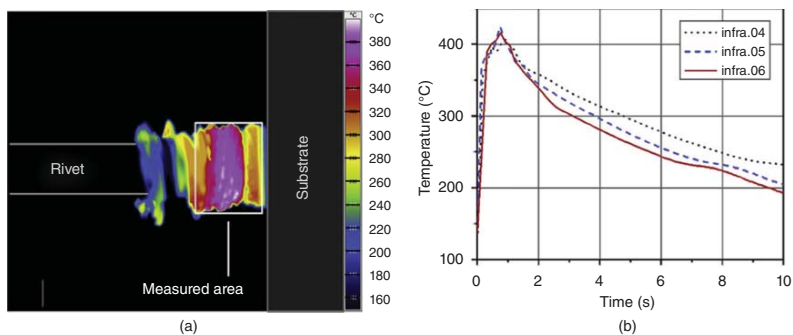


Figure 8.8 (a) Thermogram (IR snapshot) showing the temperature distribution of the softened PEEK composite flash material being expelled from the joining region and pushed to the surface and the location of the measurement area. (b) Example of process temperature evolution during FricRiveting a metallic-insert CF-PEEK/Ti gr. 3joint (Source: Altmeyer *et al.* 2014 [24]. Reproduced with permission of Elsevier).

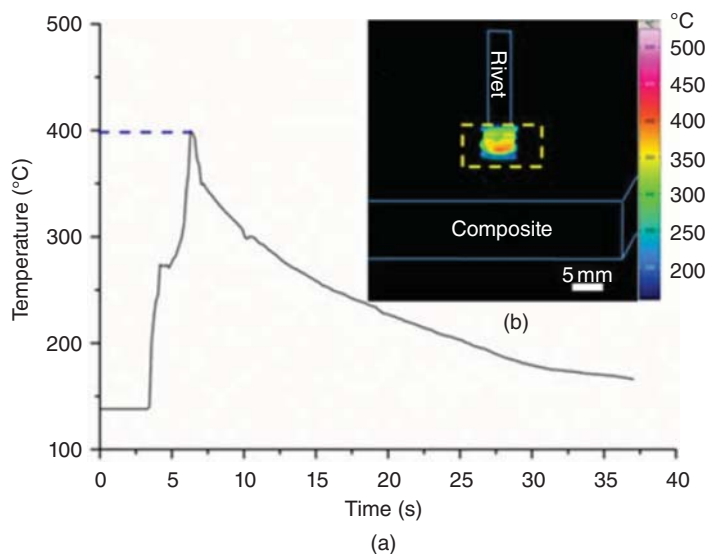


Figure 8.9 (a) Evolution of the maximum temperature during the process measured by infrared thermography. (b) thermogram showing the maximum temperature of the softened composite flash material being expelled out of the composite plate, the dash-lined rectangle is the measuring area for friction-riveted GF-PA6/AA 6056-T6 joints (Source: Proenca *et al.* 2015 [9]. Reproduced with permission of Society of Plastic Engineers).

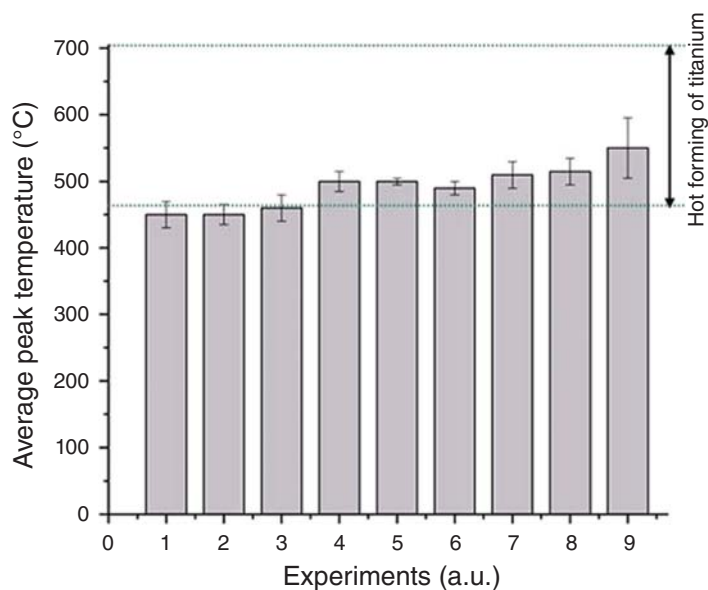


Figure 8.10 Monitored average peak IR temperatures of GF-PEI/Ti gr. 2 friction-riveted joints (Source: Blaga *et al.* 2015 [26]. Reproduced with permission of Elsevier).

Figure 8.11 X-ray image of a metallic-insert joint (Source: Altmeyer *et al.* 2014 [24]. Reproduced with permission of Elsevier).

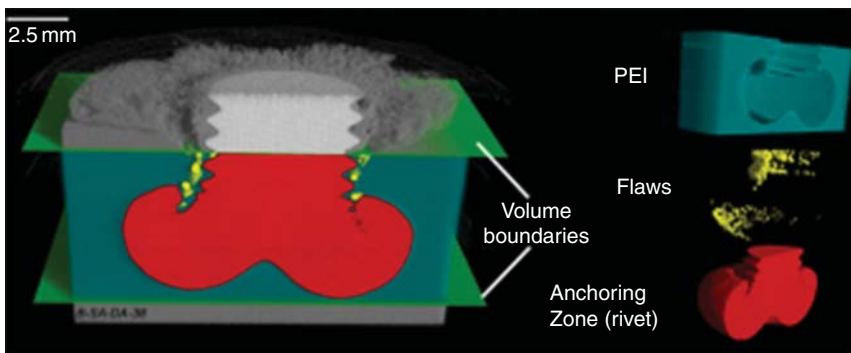
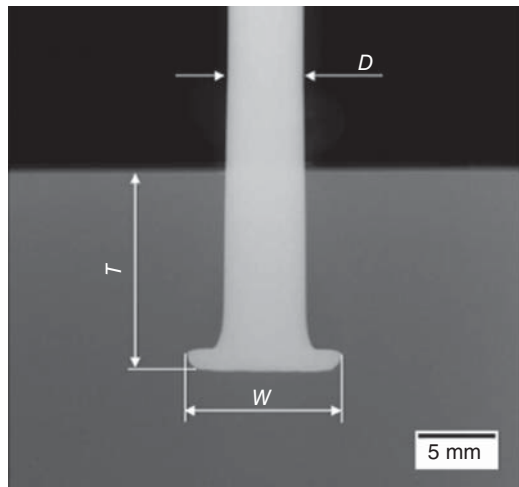


Figure 8.12 μ CT results showing a metallic-insert joint type in PEI/Al 2024. Three different segmented volumes are represented from the top-right-hand side to the bottom of the picture: the nontreated PEI volume, the PEI volume containing thermomechanical treated polymer and the deformed tip of the Al 2024-T351 rivet (the anchoring zone) (Source: Amancio-Filho 2007 [7]. Reproduced with permission of SAGE Publications).

analysis. The unchanged PEI volume (see Figure 8.12) displayed the greatest volume; a second volume with intermediary size contains the deformed tip of the rivet (also called the anchoring zone). A third very small volume containing thermomechanical flaws (voids related to slightly thermodegraded material, volatile evolution, and imprisoned air) was observed in the PEI volume mainly around the nondeformed rivet shaft.

Within these three 3D volumes, there are four typical microstructural zones for thermoplastic-based polymers: the polymer heat-affected zone (PHAZ), the polymer thermomechanically affected zone (PTMAZ), the metal heat-affected zone (MHAZ), and the metal thermomechanically affected zone (MTMAZ)

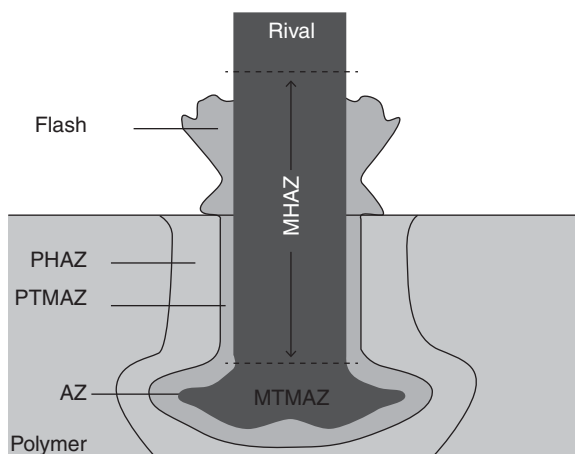


Figure 8.13 Schematic representation of the typical microstructural zones found in FricRiveting joints (Source: Adapted from Amancio-Filho 2007 [7] and Amancio-Filho 2011 [29]).

located in the rivet anchoring zone (AZ). The AZ is related to the volume, where the deformed tip of the rivet is located. Due to the severe plastic deformation, it generally assumes a paraboloidal pattern (see Figures 8.13 and 8.14(a)) whose external diameter is larger than the original rivet diameter. This region mainly bears the imposed loading through mechanical interference (anchoring) with the polymeric base element. For alloys having higher melting points, frictional heating may not be enough to induce full plasticization of the rivet tip and to form a well-pronounced AZ. In this case, fitments or protrusion elements added to the rivet would contribute to increasing rivet anchorage. Figure 8.13 shows an illustrative scheme of the microstructural zones in a FricRiveting joint [7, 29].

The microstructural zones in FricRiveting can be better understood by microscopy. Figure 8.14 presents a metallographic cross section of the joint produced with 21,000 rpm, 3 s, and 1.1 MPa. A first observation is the identification of two different interfaces: a metal–polymer and a polymer–polymer (see dashed lines schematized in Figure 8.14(d)). While the metal–polymer interface is composed by the formed interface among MTMAZ in the anchoring zone and the PTMAZ, the polymer–polymer interface is defined by the formation of a weld line between the PHAZ and the consolidated polymer of the PTMAZ. From the previous work [7], it is assumed that interfaces are mainly held by adhesion forces. Investigations are being carried out in order to further comprehend these bonding mechanisms.

In the MHAZ, the rivet metal structure is only heat treated. In this zone, different annealing phenomena may take place, for example, static recovery, static recrystallization, and overaging, as well as some hardening mechanisms, such as aging and reprecipitation, depending on the alloy type, heating time, and maximum process temperature. The microstructure may not visually change, although the structure is rather modified.

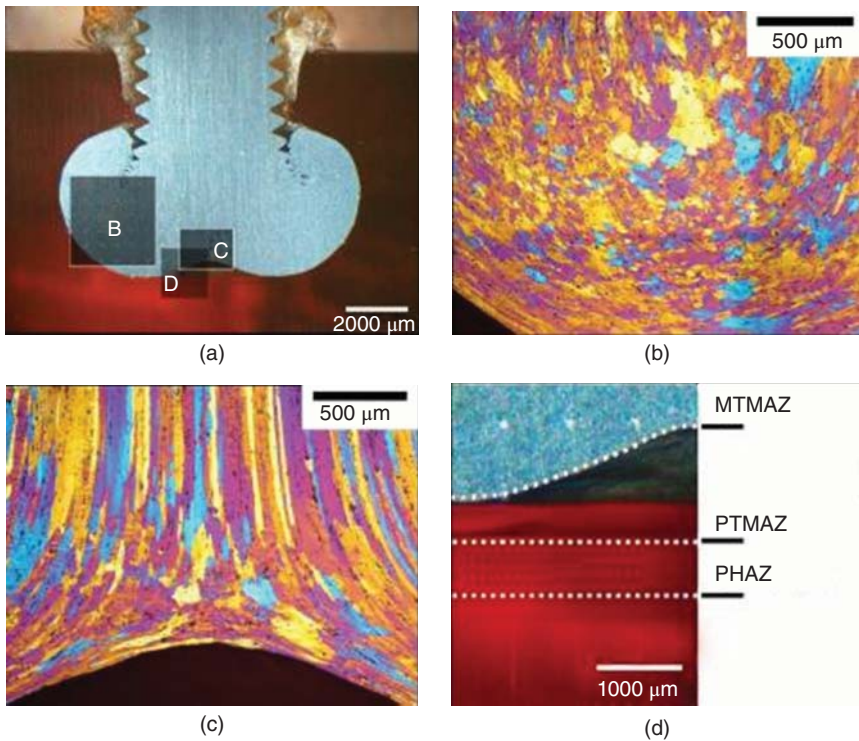


Figure 8.14 Microstructural features of a cross-sectioned metallographic insert-type FricRiveting joint on PEI/Al 2024-T351 (21,000 rpm, 3 s, and 1.1 MPa): (a) nonetched polished LOM macrograph; (b) detailed photo of region B in (a) showing the partially dynamically recrystallized grain distribution in the MTHAZ (electrolytic etching with Barker solution – 200 mL distilled water, 5 g fluoboric acid 35%); (c) magnified view of the region C in (a) showing base material grain realignment to the material flow direction in the forged plasticized rivet metal; (d) detailed micrograph of marked are D in (a). The transition zones (interface lines) for MTMAZ, PTMAZ, and PHAZ are indicated by dashed lines (Source: Adapted from Amancio-Filho 2007 [7] and Amancio-Filho 2011 [29]).

On the other hand, the MTMAZ microstructure changes as a result of heating and severe plastic deformation. In this zone, the plasticized rivet material can undergo dynamic recovery and/or recrystallization (or even reprecipitation in precipitation hardenable alloys) depending on whether critical temperatures and deformation are attained for a given period. Large-scale melting is usually not observed, although incipient melting might occur at the faying surfaces. In aluminum alloys, microstructure investigated by optical microscopy is normally characterized, by partially grain refinement, characteristic of dynamic recrystallization and recovery (Figure 8.14(b)), and BM grain realignment to the flow direction of forged plasticized material (Figure 8.14(c)) [7, 29].

In the PHAZ (Figure 8.14(d)), process temperature did not reach the polymer softening point (polymer glass transition temperature, T_g , or melting point, T_m). Generally, this zone cannot visually be identified by microscopy for amorphous polymers, such as PEI. Nevertheless, the polymer structure is rather affected by heat and consequently local mechanical properties as well. Examples of these possible changes are: induced crystallization/growth of crystallites in the case of semicrystalline polymers and physical aging for amorphous polymers.

The PTMAZ is formed as a consequence of shear deformation and heat. It comprises the thin molten polymer layer consolidated around the rivet. As PTMAZ forms a weld line with PHAZ, it can generally be visualized by microscopy (see Figure 8.14(d)). In this zone, temperature is well above T_g or T_m , frequently reaching the polymer thermomechanical degradation temperature [7, 29].

The presence of voids and flaws associated with high temperatures and deformation in the PTMAZ (see Figure 8.16) can be originated from volatile formation, such as absorbed or structural water, from entrapped air, due to high molten viscosity of some polymers, from cooling shrinkage related to differences in linear coefficient of thermal expansion, and from thermal degradation. These flaws can be detrimental to mechanical strength and should be reduced by optimizing the process parameters. For the most thermoplastics, although flaws in the PTMAZ are prone to exist, they do not extensively affect the overall mechanical performance of the joint (as demonstrated in [7]). Additionally, local mechanical properties in the PTMAZ can be influenced by some preferential polymer chain orientation and thermal residual stresses.

In some cases, additional microstructural subfeatures can be found. Altmeier *et al.* [30] analyzed the microstructure from short-fiber-reinforced PEEK/Ti grade 3 joints. They identified three different microstructural zones in the rivet: two different metallic thermomechanically affected zones (MTMAZ 1 and MTMAZ 2) and a metal friction zone (MFZ). Four different zones in the composite partner were also identified, namely composite base material (CBM), composite heat-affected zone (CHAZ), composite thermomechanically affected zone (CTMAZ), and composite stir zone (CSZ) (see Figures 8.15 and 8.16).

The diameter of the rivet inside the composite increases from the initial diameter D of 5 mm to D_1 of 5.4 mm, which is also known as “barreling” [31]. This deformation behavior is most likely explained as the result of applying an axial force to the rivet during FricRiveting [30].

8.6.1 MTMAZ 1

Due to the increase in low-angle boundaries (LAB) fraction (or decrease of high-angle boundaries (HAB) fraction) in areas “a” and “b” compared to the

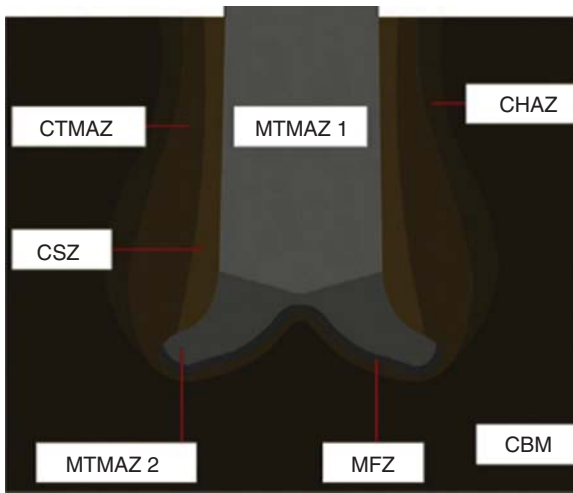
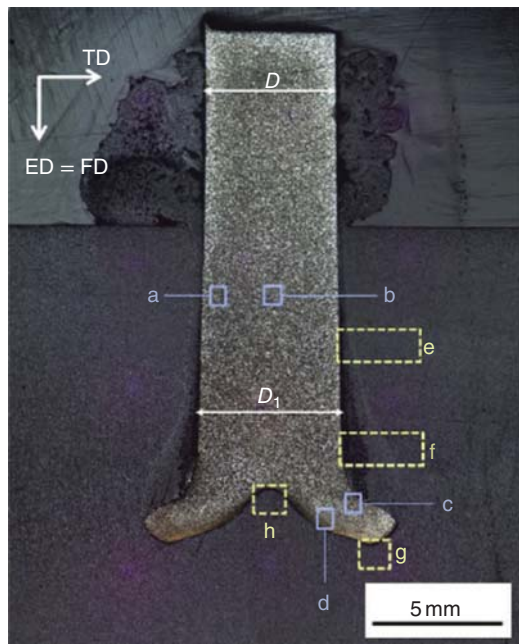


Figure 8.15 Schematic representation of the microstructural zones in friction-riveted metallic-insert joints. Composite base material (CBM), composite heat-affected zone (CHAZ), composite thermomechanically affected zone (CTMAZ), composite stir zone (CSZ), metal thermomechanically affected zone 1 (MTMAZ 1), metal thermomechanically affected zone 2 (MTMAZ 2), and metal friction zone (MFZ) (Source: Altmeyer *et al.* 2015 [30]. Reproduced with permission of Elsevier).

Figure 8.16 Macrographs of a CF-PEEK/Ti gr. 3 metallic-insert joint, indicating the areas of interest (a, b, c, and d in the metallic part of the joint and e, f, and g in the composite part) where detailed microstructure data was acquired. (TD, ED, and FD are transverse, extrusion, and forging directions, respectively) (Source: Altmeyer *et al.* 2015 [30]. Reproduced with permission of Elsevier).



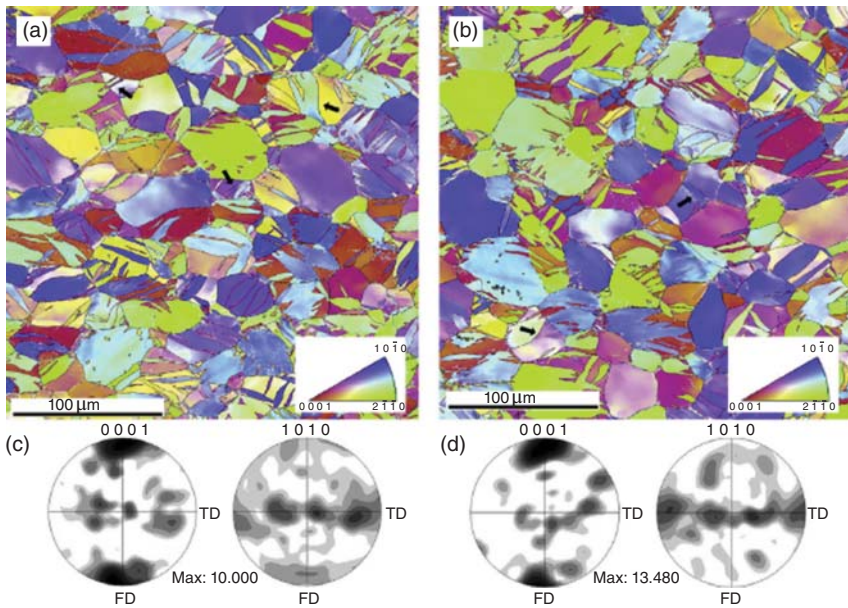


Figure 8.17 EBSD maps showing microstructure of regions a (a) and b (b), and {0001} and {1010} pole figures extracted from region a (c) and b (d) (Source: Altmeyer *et al.* 2015 [30]. Reproduced with permission of Elsevier).

LAB fraction (or HAB fraction) in the BM (5–29%) (see [30]), the formation of 85° $\bar{1}210$ twin boundaries (tension twin) and the orientation of c -axis (i.e., roughly parallel to the forging direction), it is concluded that regions “a” and “b” were thermomechanically affected by compression in the forging direction during the process (Figure 8.17 and 8.18). Thus, regions “a” and “b” were classified as metal thermomechanically affected zone 1 (MTMAZ 1).

8.6.2 MTMAZ 2

The absence of 85° $\bar{1}210$ twin boundaries indicates that this region experienced substantially high deformations and temperatures [31, 32]. Besides that, the grain size in this region was reduced to $4.7\ \mu\text{m}$. These data are evidence that the region experienced plastic deformation due to the geometrical effect of strain and was exposed to elevated temperatures during the FricRiveting process. The analysis of this area leads to the conclusion that area c is an MTMAZ (MTMAZ 2), which is consistent with the results of previous studies [7, 29].

By the observation of Figures 8.16, 8.19, and 8.20, Altmeyer *et al.* [30] identified three different microstructural zones in the polymer partner: CHAZ, CTMAZ, and CSZ. Whereas in the CHAZ, the only difference from the BM

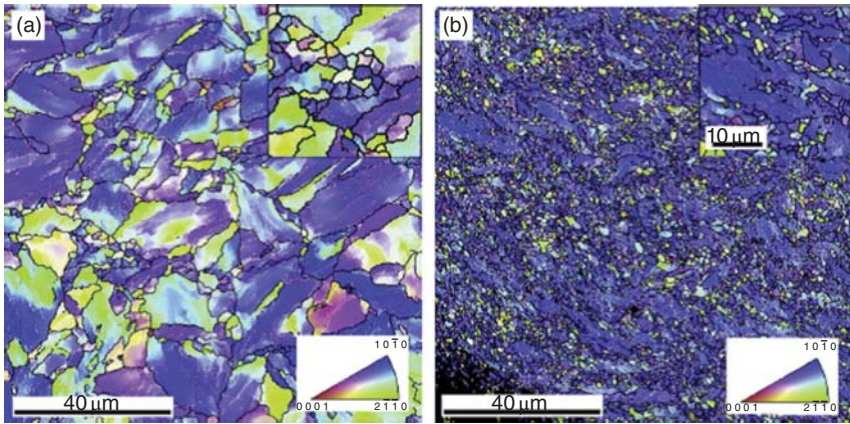


Figure 8.18 EBSD maps showing microstructure of regions c (a) and d (b) (Source: Altmeyer *et al.* 2015 [30]. Reproduced with permission of Elsevier).

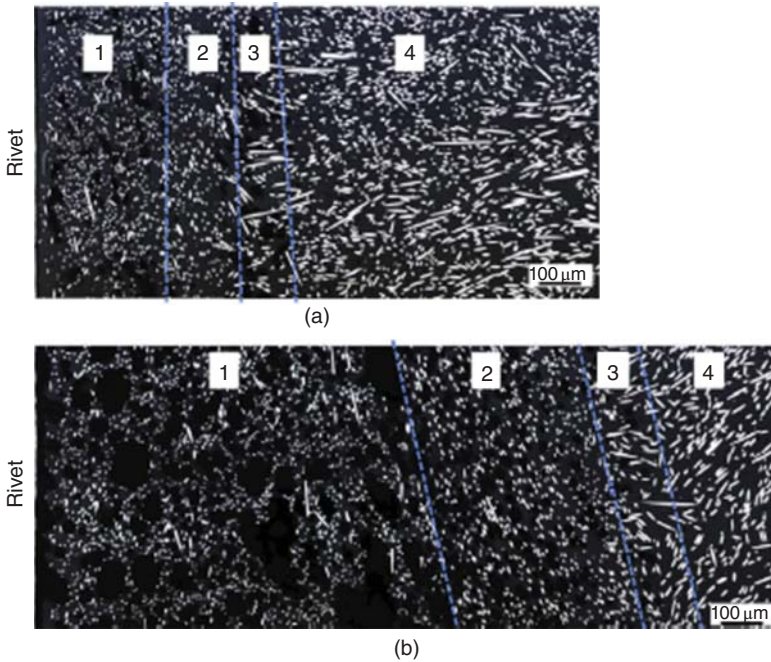


Figure 8.19 Micrograph with higher magnification of (a) area e and (b) area f (see Figure 8.16 for exact position).

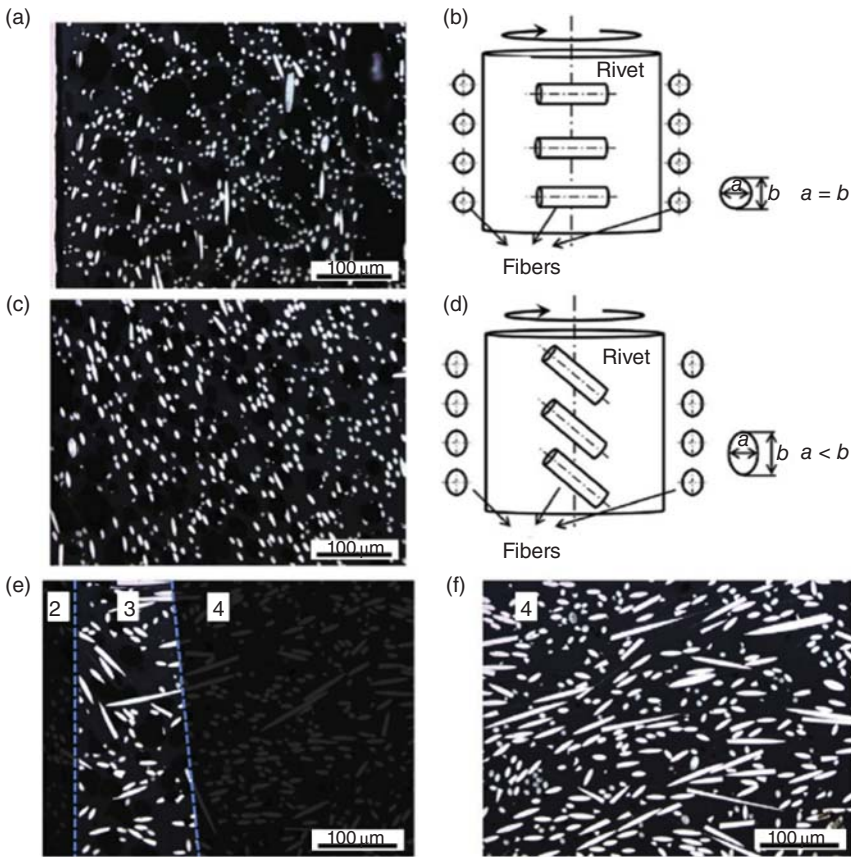


Figure 8.20 Composite stir zone (CSZ): (a) enlarged image of Region 1 identified in Figure 8.19; (b) schematic of the fiber orientation in this region; composite thermomechanically affected zone (CTMAZ): (c) enlarged image of Region 2 in Figure 8.19 and (d) schematic of the fiber orientation in this region; (e) enlarged image of region 3; (f) enlarged image of region 4 (Source: Altmeyer *et al.* 2015 [30]. Reproduced with permission of Elsevier).

was the presence of pores, CTMAZ and CSZ also presented fiber reorientation in specific directions, indicating material plasticization. Although fibers have been realigned in both CTMAZ and CSZ, the direction of the fiber is not the same in each region. While in CSZ, most of the fibers appear as circular dots, indicating that they are oriented in a direction perpendicular to the observation plane, in CTMAZ, they are oriented in a different direction. This indicates that in CSZ, material flowed in a circular movement around the rivet (stirring), causing the fibers to be realigned parallel to the rivet as it rotated as shown later in Figure 8.20(b).

The four regions of PEEK with different morphologies are presented in Figure 8.19, which represents an enlarged map of areas e (Figure 8.19(a)) and f (Figure 8.19(b)).

As it can be observed comparing the porosity in Figure 8.20(a), (c), (e), and (f), the porosity decreases as the distance from the rivet increases. In addition to this porosity variation, it is possible to notice that the fiber orientation is different in these regions. While Regions 3 and 4 do not show any predominant fiber orientation (Figure 8.20(e) and (f)), fibers are aligned in Regions 1 and 2, Figure 8.20(a) and (c).

Figure 8.20(b) represents the fiber orientation around the rivet, detected in Region 1. This orientation indicates that the material flowed in a circular movement around the rivet (stirring). Thus, this region was called composite stir zone (CSZ). The fiber orientation in Region 2 is slightly different from the one presented in Region 1. The helical realignment may have appeared due to two factors: rotation of the rivet and upward movement of the softened composite being suppressed by the rivet. Therefore, Region 2 was classified as CTMAZ. In Region 3, although no fiber realignment has been observed, the porosity fraction is higher than in the BM, see Figure 8.20(e). For these reasons, Region 3 was classified as CHAZ. Region 4 presented the same porosity fraction/density and fiber orientation of the BM (Figure 8.20(f)), being classified as BM.

The microstructure of long-fiber-reinforced polymer composites follows a similar trend as for the unreinforced and short-fiber-reinforced polymers. Blaga *et al.* [33] showed by light optical microscopy and laser scanning confocal microscopy that friction-riveted joints on woven-reinforced PEI/titanium grade 2 joints present no thermal damage but reorientation of the cut fibers of the composite woven. The glass-fiber-reinforced woven was shifted upward from its original plane by approximately 45° , same direction of the polymeric material flow (Figure 8.21(c)).

Recently, Borba *et al.* [27] has demonstrated the feasibility of FricRiveting in thermoset composites. They investigated glass-fiber-reinforced thermoset polyester/Ti-6Al-4V joints and observed the important role played by RS in the mechanical strength of the friction-riveted joints. The microstructural zones in both metal and composite have been preliminary studied in [28] and will be the subject of deeper investigations in future publications by the author.

8.7 Physical–Chemical Changes in the Polymeric Material

Although the FricRiveting process is characterized by short heating times (typically from 0.5 to 10 s), high process temperatures are achieved, which are generally in the degradation range of the polymeric partner. Considering that the

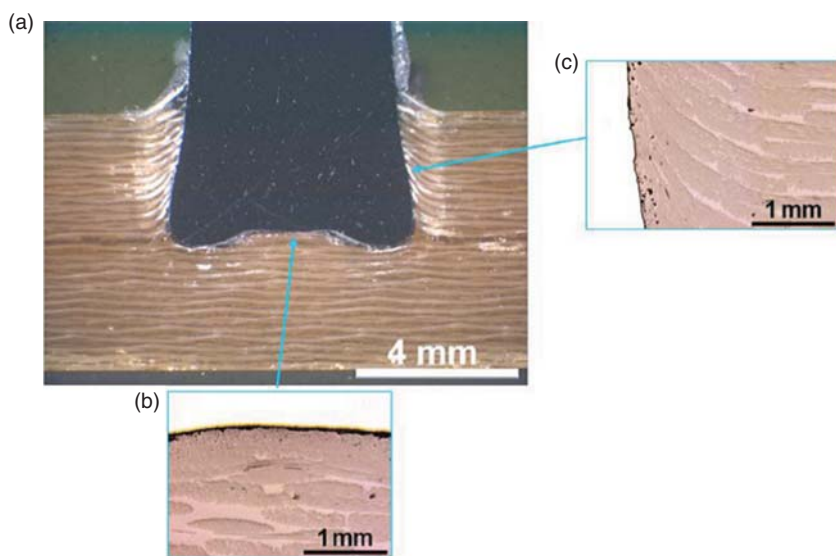


Figure 8.21 (a) Example of a cross-sectional view of a friction-riveted PEI-GF/Ti gr.2 joint; (b) and (c) details of the volumetric thermal flaws around the rivet (Source: Blaga *et al.* 2013 [33]. Reproduced with permission of Elsevier).

thermal degradation is directly coupled with mechanical performance in plastics, this should be avoided or minimized in order to guarantee joint mechanical integrity in FricRiveting [12]. Amancio-Filho *et al.* [12] studied the influence of RS on the thermal degradation behavior of PEI. They demonstrated that, although RS plays an important role in controlling the temperature change, heating rate, and heating time, it has a weak influence on the thermal degradation of engineering thermoplastics being friction riveted by aluminum rivets. The μ CT analysis also helped to confirm that the RS has only a limited influence on thermally degraded material formation (generally $<1\%$ of volumetric defects in the joint) in the studied parameter range for this combination of materials. This behavior can be associated with the shorter measured heating times ($t < 3$ s), although the good stability of the thermoplastic PEI certainly contributes to the low level of thermal degradation.

Average process peak temperature evaluated by IR thermography is presented in Figure 8.22. Figure 8.22(a) shows a sharp increase in the flash temperature within the first 3 s (the setup JT for these samples) indicating high heating rates. Slower cooling rates for these joints are associated with the natural cooling under room temperature. Figure 8.22(b) presents the peak temperatures extracted from the temperature versus time curves in Figure 8.22(a). A clear trend of increasing temperature with the RS could be observed as a result of the friction energy being transformed into heat and

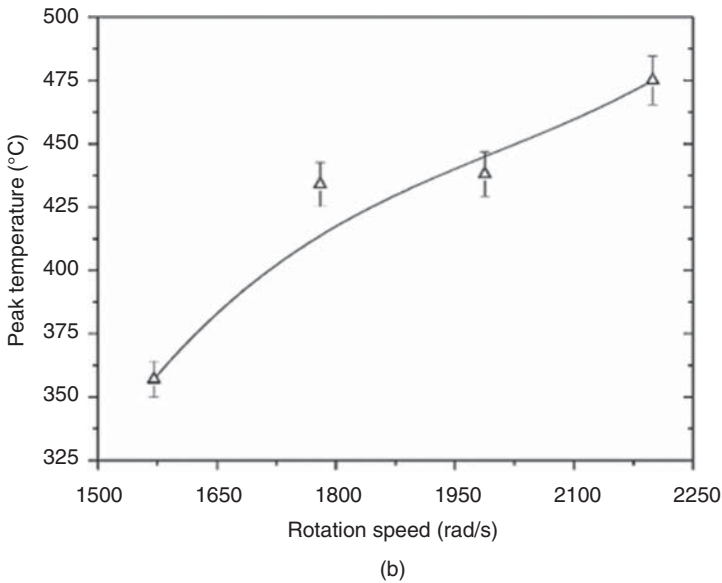
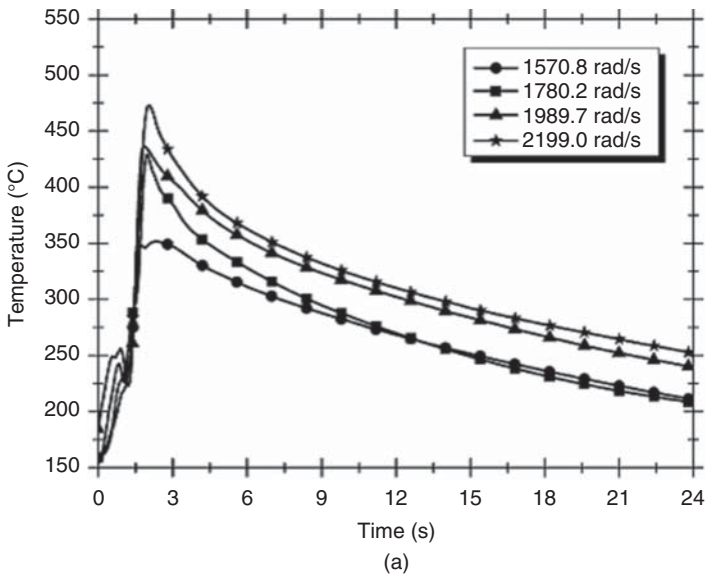


Figure 8.22 (a) Infrared temperature measured during Friction Riveting for the investigated joints. (b) Plot with the peak temperatures extracted from (a), versus rotational speed (Source: Amancio-Filho *et al.* 2008 [12]. Reproduced with permission of Elsevier).

the low thermal conductivity of the polymer. Peak temperatures for the tested rotation speeds were well above the average glass transition temperature of the PEI polymer (221 °C) approximately varying in the range of 350–475 °C, which is within the initial range of thermal degradation by chain scission for the studied polymer [12, 34–36].

Molecular weight (MW) measurements by size-exclusion chromatography (SEC) showed that the main thermal degradation mechanism is chain scission, with a minor decrease in MW (10% for M_n and 5% for M_w at RS = 2199 rad/s), considered irrelevant for the mechanical performance of the joints. This thermal degradation mechanism (chain scission) was expected because of the measured peak temperatures (around 350–475 °C), which are above the range of the chain scission thermal degradation regime for PEI polymer [12, 34–36].

To establish the nature of thermal degradation mechanisms taking place during joining, specimen MW distributions (Figure 8.23(a)) and polydispersity (Figure 8.23(b)) were analyzed. Only a slightly broader curve was measured after processing, with a slightly higher content of low-MW polymer, when elevating RS (Figure 8.23(a)). Taking into account this observation and the process temperature, it is possible to conclude that the main thermal degradation mechanism taking place during joining is chain scission instead of cross-linking. A cross-linked structure would preferentially lead to an increase of MW [37]. This behavior can also be confirmed by the increase of polydispersity with RS (Figure 8.23(b)), probably owing to the presence of smaller scissioned entities [38].

Sanner *et al.* [39] studied the effect of different MWs on PEI's mechanical properties. For a molecular range varying from $M_n = 15,660$ to $22,820$ g/mol and $M_w = 36,640$ to $52,300$ g/mol, the tensile strengths at temperatures ranging from 23 to 140 °C were not strongly altered. Considering that untreated PEI used in this work had average MWs similar to those studied by those authors ($M_n = 26\,300$ and $M_w = 52\,200$ g/mol), the decrease of about 10% for M_n and 5% for M_w ($M_n = 22\,250$ and $M_w = 49\,650$ g/mol) in average MW measured by SEC can be considered as irrelevant to the overall tensile strength. The experimental values of MW for the studied samples lay either above or within the MW range of PEI where tensile strength behaves independently of this property [12]. The irrelevance of the decreasing MW in the behavior of global mechanical strength for the studied joints was demonstrated elsewhere [7].

8.8 Mechanical Performance

8.8.1 Joint Local Mechanical Properties

The microhardness distribution in a friction-riveted joint is dictated by structural changes associated with the thermal and mechanical processing,

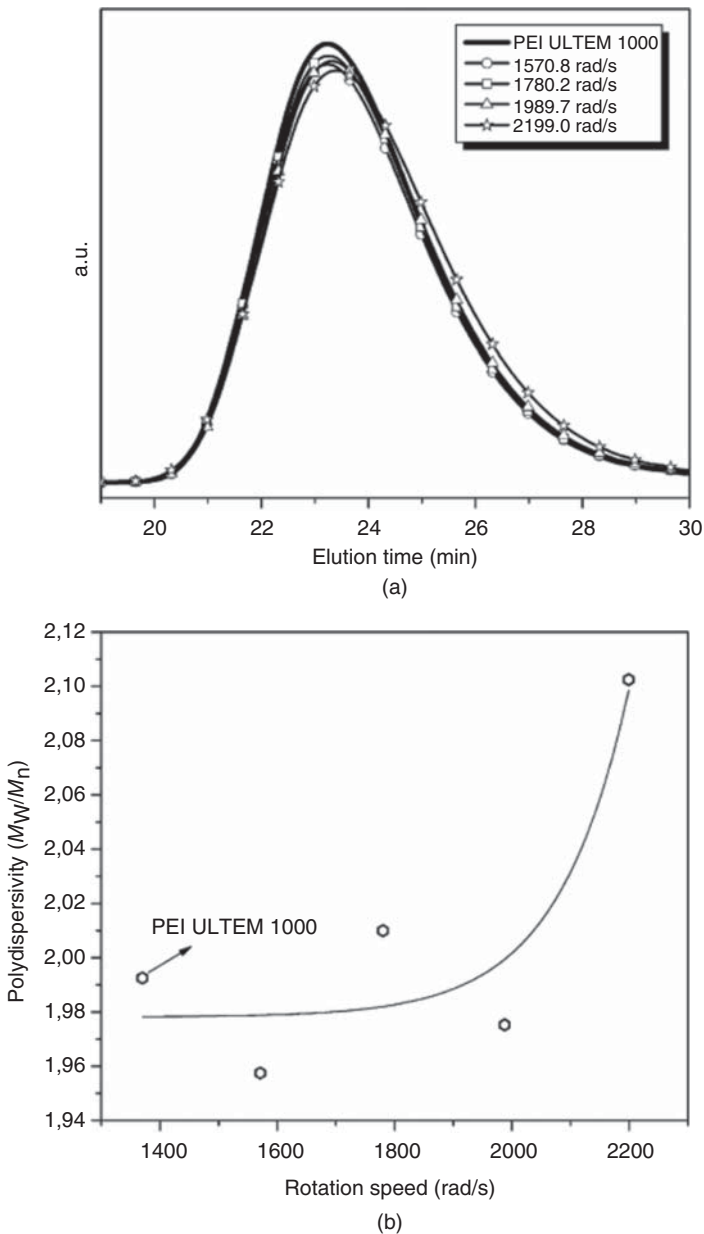


Figure 8.23 (a) Size-exclusion chromatography (SEC) molecular weight distributions for investigated rotation speed specimens. (b) Influence of rotation speed on SEC polydispersity of studied specimens (Source: Amancio-Filho *et al.* 2008 [12]. Reproduced with permission of Elsevier).

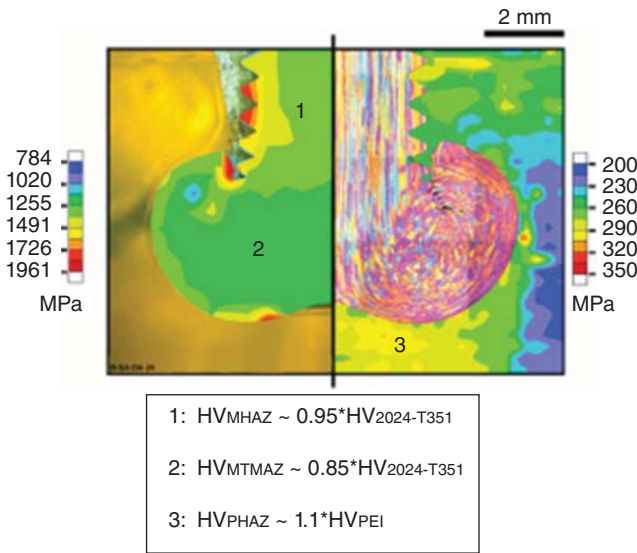


Figure 8.24 Schematic representation of microhardness distribution of a PEI/Al 2024 FricRiveting joint (Source: Amancio-Filho 2007 [7]. Reproduced with permission of SAGE Publications).

as discussed in the previous section. Figure 8.24 presents a schematic overlaying of experimental results on microhardness mapping of a FricRiveting point-on-plate joint on PEI 13.4 mm extruded plates/Al 2024-T351 M5-threaded drawn rivets.

Generally speaking, an overall decrease in microhardness in comparison to the BM can be observed in the metallic rivet portions inserted in the polymer, and an overall increase in the polymer volume around the rivet. The decrease in microhardness at the MHAZ is usually smaller than at the MTMAZ ($HV_{MHAZ} \approx 95\%$ and $HV_{MTMAZ} \approx 85\%$ of BM AA 2024-T351 average hardness value), as presented on the left-hand-side microhardness map of Figure 8.24, locations 1 and 2. While in the MHAZ, no visual changes in microstructure are seen, in the MTMAZ, there is some grain refinement, or dynamically recrystallized grains as demonstrated in [7, 17]. This supports the lower microhardness values, where annealing was more extensive in the rivet regions near to the faying surfaces (i.e., the rivet tip within the anchoring zone).

However, overmatching in the microhardness distribution may also occur depending on the alloy used for the rivet. Borba *et al.* [27, 40] showed for glass-fiber-reinforced thermoset polyester/Ti-6Al-4V joints that local mechanical strength can increase due to thermomechanical processing. The anchoring zone, which is exposed to high temperatures and shear rates, might present hardness values superior than those of the BM [27, 40]. In these

regions, due to heat and forging effects, the material suffers microstructural and morphological changes, which might lead to the hardness increase observed. These changes occur due to the temperature levels that are reached during the process (near beta transition level), and the high cooling rate, which create conditions to the formation of acicular structures by diffusional (Widmanstätten) or diffusionless (metastable α' martensitic) transformations [27, 28, 40]. leading to hardening of the local mechanical resistance. In the mentioned study, two different joining conditions were compared, with two different Rotational Speeds (RS 9000 and 10,000 rpm, with constant parameters FT 1.2 s, FoT 1.2 s, FP 0.6 MPa, FoP 1.0 MPa), as presented in Figure 8.25, where the differences in microhardness due to thermomechanical process-related changes in microstructure can be observed.

8.8.2 Joint Global Mechanical Performance

8.8.2.1 Tensile Strength

The volumetric ratio (VR) [33], a simplified analytical model describing the anchoring efficiency of the rivet, was demonstrated to be directly proportional to the tensile strength of the joint and therefore an adequate analytical model to describe the joint mechanical performance. The VR takes into account, on a simplified geometrical basis, the ratio of the volume of dislocated polymeric material, the volume of the deformed metallic rivet, and the volume of the remaining polymeric resistive material over the deformed shape of the rivet [41, 42]. Figure 8.26 A1–A3 show the cross-sectional macrographs for the specimens analyzed in the case study PEI-GF/Ti gr. 2; Figures 8.26 B1 and B2 show the geometrical simplifications and a 3D view of the interaction volume of the polymer used to calculate the VR, where W is the maximal diameter of the deformed rivet, H the insertion depth of the metallic rivet into the CBM, $(H - B)$ the height of the remaining composite material over the deformed rivet shape, B the height of the anchoring zone, and D the undeformed rivet diameter.

The VR represents the rivet anchoring efficiency in friction-riveted joints and can be analytically expressed by the model shown in Equation 8.3. VR varies between 0 and 1 and is dimensionless:

$$VR = \frac{(H - B)(W^2 - D^2)}{W^2 \cdot H} \quad (8.3)$$

Similar trends in anchoring efficiency and mechanical performance under tensile loading were observed by Rodrigues *et al.* [8] for polycarbonate (PC)/aluminum 2024-T351 alloy and Proenca *et al.* [9] for short-fiber glass-reinforced polyamide (GF30-PA6)/aluminum 6056-T6 aluminum alloy metallic-insert joints. The variation of VR and their related joint ultimate tensile forces (UTF) for the two case studies (material combinations) is presented in Table 8.2:

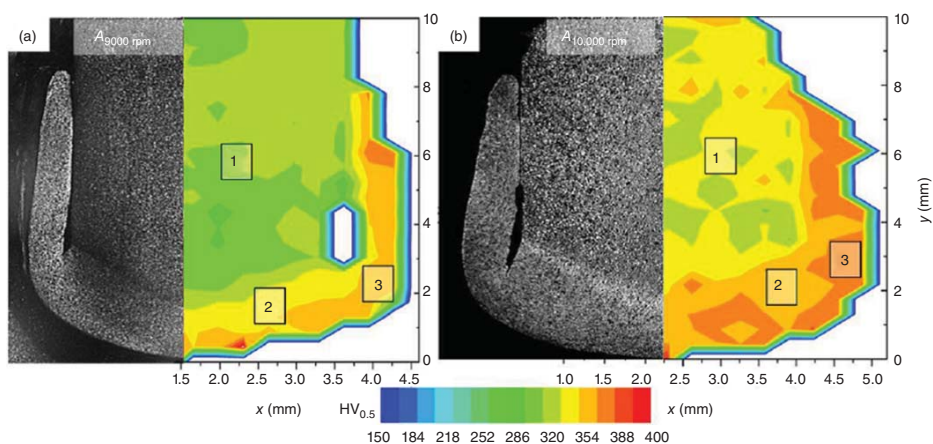


Figure 8.25 Typical hardness map superimposed over the macrograph of the transversal section of joints Ti-6Al-4V/P-FV rivets joined in conditions with RS 9000 rpm (a) and RS 10,000 rpm (b) (the microstructural zones described in [28] are: Region 1 – MHAAZ, Region 2 – MTMAZ₁, Region 3 – MTMAZ₂). (Source: Borba 2015 [28]. Reproduced with permission of UFSCar).

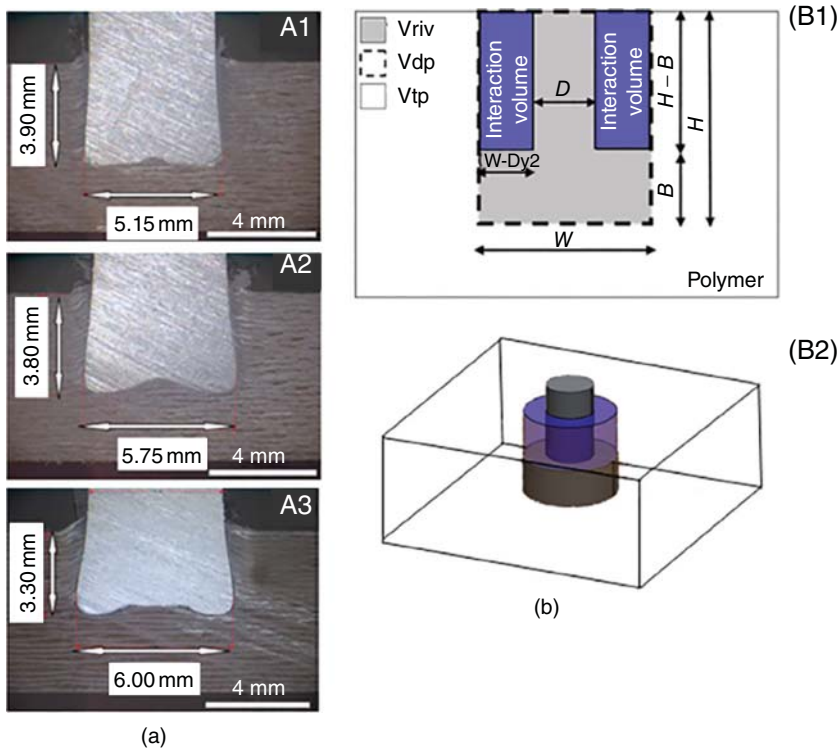


Figure 8.26 (a) Cross-sectional views of the selected PEI-GF/Ti gr. 2 joints (Specimens A1, A2, and A3); (b) geometrical simplifications of the anchoring zone (B1) and a simplified 3D model (B2) depicting the polymeric volume of interaction (*Source: Blaga et al. 2013 [33]. Reproduced with permission of Elsevier.*

Table 8.2 Examples of anchoring efficiencies for selected studied material combinations for FricRiveting^a.

Material combination/ joining condition	Volumetric ratio (VR) (–)	Ultimate tensile force (UTF) (N)
AA 2024-T351/PC	0.49 ± 0.02	6659 ± 62
AA 2024-T351/PC	0.59 ± 0.03	8040 ± 195
AA 2024-T351/PC	0.68 ± 0.01	8540 ± 182
AA 6056-T6/GF30-PA6	0.50 ± 0.05	3500 ± 250
AA 6056-T6/GF30-PA6	0.72 ± 0.02	4900 ± 150

a) Joining conditions and calculations are to be found in [8] and [9].

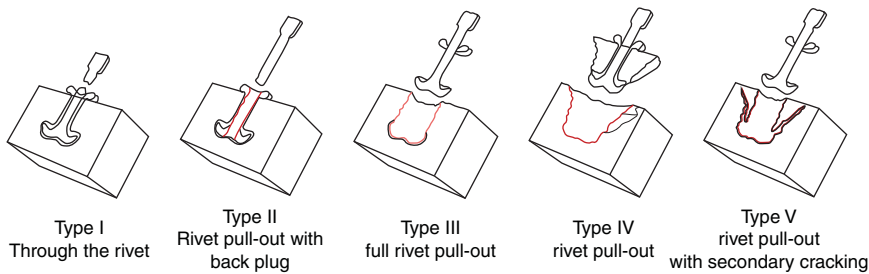


Figure 8.27 Current description of failure modes under tensile loading in FricRiveting. (Source: Amancio-Filho *et al.* 2008 [43]. Reproduced with permission of Elsevier).

Amancio-Filho *et al.* [43] reported five types of failure during tensile testing, as shown in Figure 8.27:

- *Through the rivet (Type I)*: ductile fracture occurring in the metallic rivet outside of the joint. The maximum tensile strength of the joint is equivalent to that of the metallic rivet.
- *Rivet pull-out with back plug (Type II)*: a fracture in which the crack nucleates at the deformed tip of the rivet in the anchoring zone. The rivet is later pulled out from the polymer, leaving part of the anchoring zone (“the back plug”) embedded. The tensile strength is usually good but lower than that of the metallic rivet.
- *Full rivet pull-out (Type III)*: a fracture typically observed in joints with high ductility (e.g., polycarbonate) or in cases in which the tip of the rivet was only slightly deformed (low mechanical anchoring) during the forging process. In this type of fracture, the crack starts around the anchoring zone of the polymer, and the rivet is completely removed, leaving an orifice with a diameter similar to that of the deformed end of the rivet. The mechanical strength of joints exhibiting this type of fracture is generally less than the strength of the Type I and II joints.
- *Rivet pull-out (Type IV)*: a type of failure observed in joints with large deformations at the tip of the rivet but with small insertion depths, leaving the anchoring zone close to the surface of the polymer. In this type, the crack also nucleates in the polymer around the top of the anchoring zone and propagates toward the posterior surface of the polymer. A cone of polymer forms around the anchoring zone of the rivet detached from the joint.
- *Rivet pull-out with secondary cracking (Type V)*: a more complex type of failure under tensile strength that is not yet well understood. In this type of failure, nucleation is observed at multiple sites in the polymer around the anchoring zone. In the early stages of propagation, the crack seems to exhibit the same behavior as Type IV failure, but the rivet is pulled out of the joint as observed in Type III failure. As with specimens failing by Type IV, specimens failing by Type V have medium-to-low tensile strengths.

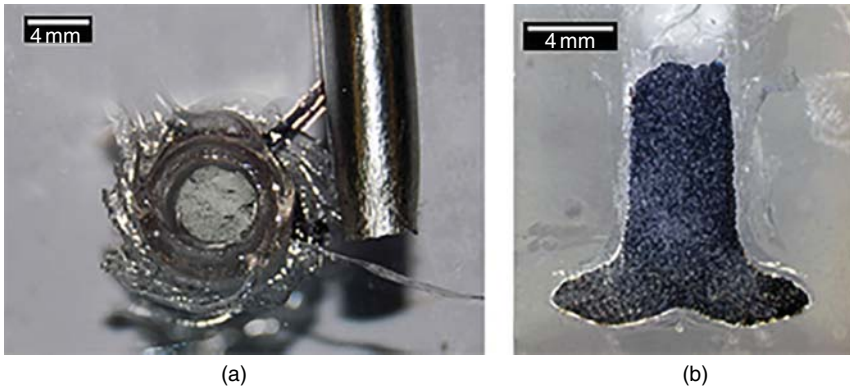


Figure 8.28 Type I fracture observed in joining condition 8. (a) Specimen after T-Pull testing. (b) Cross-sectional view of the fractured specimen (please refer to [8] for detailed descriptions of joining conditions and failure modes).

An example of Type I failure mode is presented in Figure 8.28 for a AA 2024-T351/polycarbonate friction-riveted joint (joining parameters: RS 21,000 rpm, JT 3 s, JP 1.1 MPa) [8]:

The joints performed in the selected condition developed Type I fractures with high mean UTF values (8540 ± 182 N). This type of fracture is desirable for friction-riveted joints and structures as it is a ductile-type final fracture (cup/cone) occurring externally to the polymeric matrix, within the metallic rivet [8].

Figure 8.29 displays two examples of Type I and Type III failure modes for selected conditions of AA 6056-T6/GF30-PA6 friction-riveted joints [9]:

8.8.2.2 Lap Shear Strength

In Figure 8.30, the average tensile stress–strain curve for the unreinforced PEI plaques and the average lap shear stress–strain curve for the single-riveted specimens (5 mm PEI plaques single-riveted by $\Phi 5$ mm Al 2024-T351 rivets) can be observed. Rivet penetration in the bottom plate was about 2 mm on average, and the width of the deformed rivet tip was about 9.5 mm [7].

Lap shear tests were performed on overlap joints for the evaluation of the lap shear strength, according to the standard ASTM D 596. Bearing stresses (lap shear stresses) were calculated using Equation 8.4 [44]:

$$\sigma_i = \frac{P_i}{(k \times D \times h)} \quad (8.4)$$

where σ_i is the bearing stress in megapascal at a given data point, P_i is the load in newton at a given data point, k is a force per hole factor (1.0 for single-riveted systems), D is the diameter of the hole in millimeters in the polymeric plates

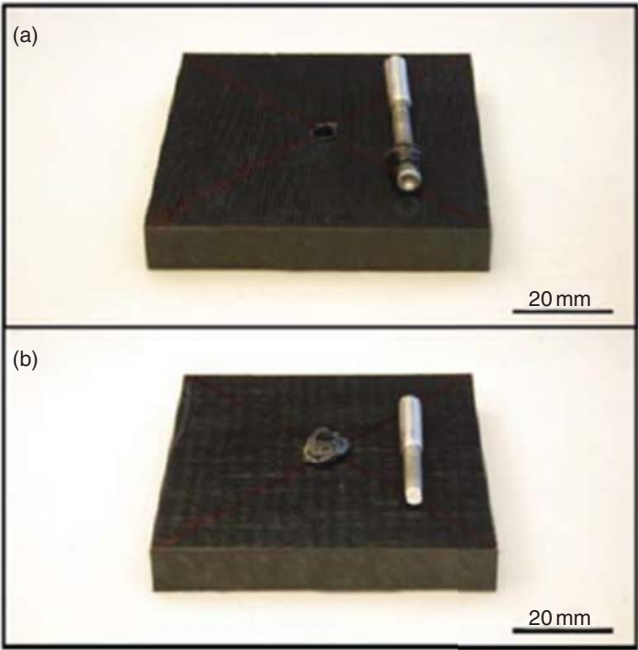


Figure 8.29 (a) Rivet pulled-out failure mode and (b) through the metallic rivet failure mode [9] (please refer to [9] for detailed descriptions of joining conditions and failure modes).

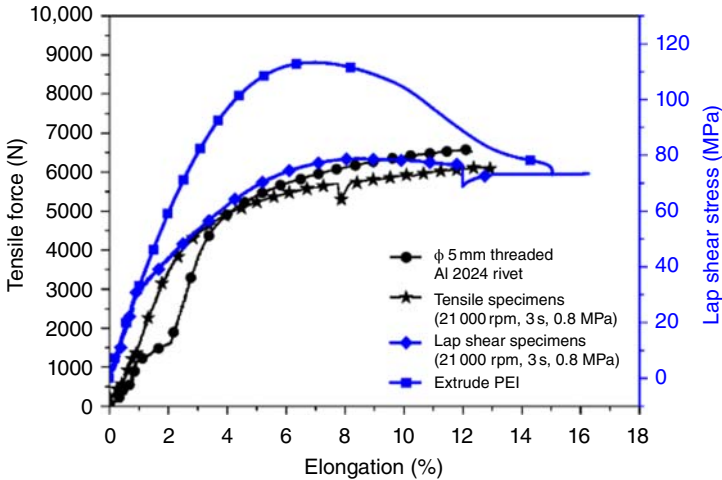


Figure 8.30 Average results of five tested specimens for tensile and lap shear testing of direct-friction-riveted joints on Al 2024-T351/PEI (joined at 21,000 rpm, 3 s, 0.8 MPa). The average curves for the pristine PEI extruded plaques and for the M5-rivets are provided for comparison (Source: Amancio-Filho 2007 [7]. Reproduced with permission of SAGE Publications).

(in this study, an average nominal value of 6 mm was selected), and h is the thickness of the joined plate in millimeters. Strain of the tested joints was measured through the change in length per unit of the original length using an extensometer [44].

Both pristine PEI and friction-riveted PEI plaques failed in a brittle manner. Figure 8.31(a) shows a fractured flat tensile specimen of pristine PEI, where almost no necking could be observed. It a hint about the material brittleness.

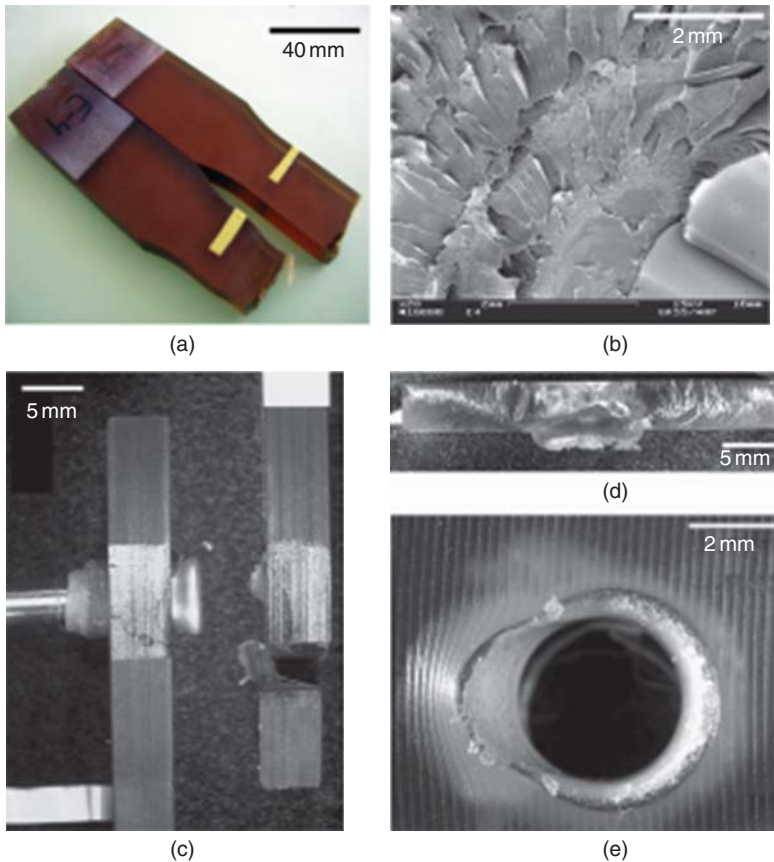


Figure 8.31 (a) Fractured tensile specimens for the pristine PEI plaques. (b) Fracture surface of the specimens in (a). Multiple crazing patterns of the fractured specimen in (a) indicating brittle fracture mechanisms. (c) Overview showing a fractured lap shear specimen on PEI/Al 2024. (d) Detailed view of the lower plaque in (c). (e) Plastic deformation (bearing) locally occurring in the polymeric volume around the rivet (not shown in the photo). Joint produced with RS: 21,000 rpm, JT:3 s, and JP:0.8 MPa (Source: Amancio-Filho 2007 [7]. Reproduced with permission of SAGE Publications).

Additionally shown in Figure 8.31(b), multiple crazing patterns (micro cracks typically found in brittle polymers) supported the brittle fracture assumption.

Brittle final failure was also found to occur in the lower PEI plaque (Figure 8.31(c) and (d)). Although primary failure was initiated by bearing (local plastic deformation) around the rivet in both PEI plaques (Figure 8.31(e)), final cracking took place by net tension (catastrophic brittle failure) in the lower PEI plate. The primary bearing failure is a satisfactory failure mechanism desired for riveted plastic joints, considering that the final fracture by net tension can be avoided through regular joint inspections [7].

The average ultimate lap shear strength achieved values of 77.5 ± 0.1 MPa, corresponding to about 70% of the strength of the single PEI plate (111.7 ± 1.9 MPa). This is a fairly good result considering that PEI is highly notch-sensitive and the hole created by the rivet into the polymeric plate acts as a stress concentrator [7].

In a recent study by Altmeyer [45], carbon-fiber woven-reinforced PEEK laminate/titanium grade 3 friction-riveted single-lap joints were compared with state-of-the-art mechanically fastened reference joints (lock-bolted joints) produced and provided by Airbus, Germany. Prior to this, the friction-riveted joints were optimized by means of design of experiments (work referenced in Chapter 12). Furthermore, design recommendations for single-lap joints were proposed. Since the lock-bolted joints provided by Airbus consisted of titanium Ti-6Al-4V (an alloy about four times stronger than titanium grade 3), the comparison with FricRiveting was focused on the joint efficiency. The joint efficiency is the ratio between the joint ultimate lap shear strength and the rivet tensile strength. The friction-riveted Ti gr. 3/CF-PEEK joints thus reached a joint efficiency of about 20% higher than that of Ti-6Al-4V lock bolts. The general overview of the compared joints and the results of this study are shown in Figure 8.32.

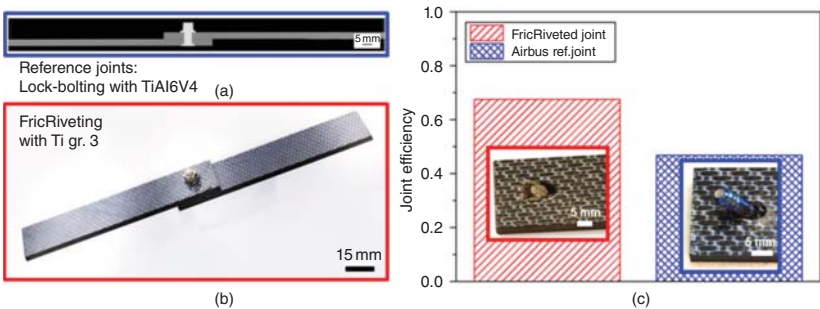


Figure 8.32 (a) X-ray tomography scan of lock-bolted Ti-6Al-4V reference joint, (b) CF-PEEK friction-riveted joints surface view, (c) compared joint efficiencies and failure mode of lock bolts and friction-riveted joints (Source: Altmeyer 2015 [45]. Reproduced with permission of HZG/Christian Schmid).

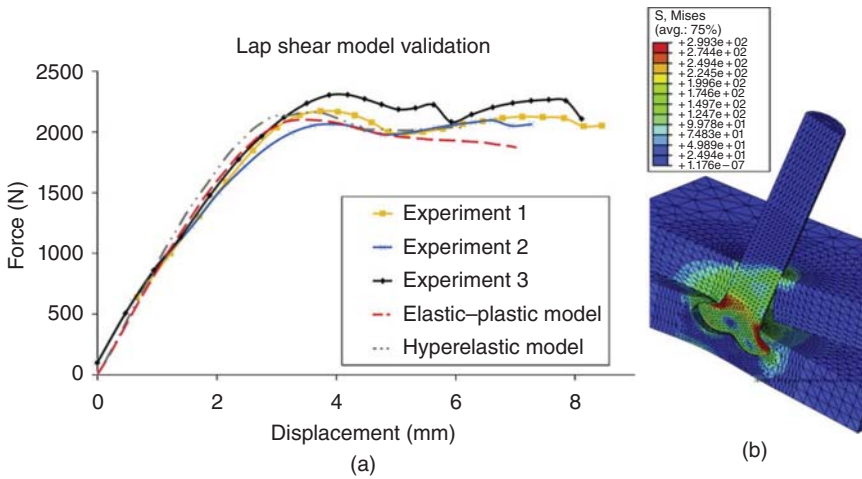


Figure 8.33 Experimental and modeled lap shear testing results. (a) Force–displacement diagrams and (b) finite element analyses showing stress distribution of a modeled lap shear joint.

Borges *et al.* [46] obtained good results from modeling the mechanical behavior of PEI/AA2024-T351 aluminum alloy lap shear joints produced by Friction Riveting. These results were obtained through FEM using elastic–plastic and hyperelastic models for the PEI and a hybrid elastic–plastic/Johnson–Cook model for the aluminum alloy. Although the material models chosen for PEI do not take into account the viscoelastic (chain relaxation) component typical of plastics under mechanical load, they were able to represent the joints' mechanical behavior within a 10% deviation limit (Figure 8.33). According to the authors, these models can be used for rivet geometry optimization. Furthermore, the models can be improved for a more thorough understanding of the joint formation and performance, with inputs such as friction coefficients, self-contacts, and contact distributions [46].

Blaga *et al.* [26] investigated glass-fiber-reinforced PEI (GF-PEI)/Ti gr.2 friction-riveted overlap joints and compared with state-of-the-art bolted joints of similar geometries (Figure 8.34). The overlapped joining part consisted of an aluminum AA2198 plate of 3.2 mm thickness. The overlap assembly was fastened with bolt nuts and washers, for both friction-riveted and bolted joints. The friction-riveted joints (within the following parameter ranges: RSs of 6000–20,000 rpm, JTs of 1.9–4.2 s, and JPs of 0.6–1.0 MPa) presented comparable or even higher strengths to the bolted joints. Under the optimized condition (experiment 7), friction-riveted overlap joints could achieve bearing strengths of up to 20% (ultimate lap shear forces of 5000 ± 750 N) higher than those of bolted connections, as shown in Figure 8.34. Both friction-riveted joints and bolted joints failed via shear of the metallic rivet. The intended

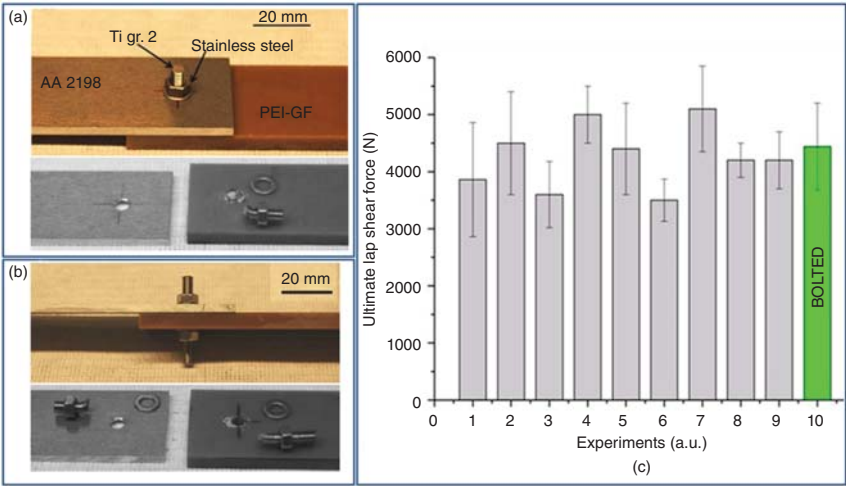


Figure 8.34 (a) Detailed view of the friction-riveted assembly and rivet shear failure; (b) bolted lap shear configuration and rivet shear failure; (c) average ultimate lap shear force results of DoE experiments and bolted joints (Source: Blaga *et al.* 2015 [26]. Reproduced with permission of Elsevier.).

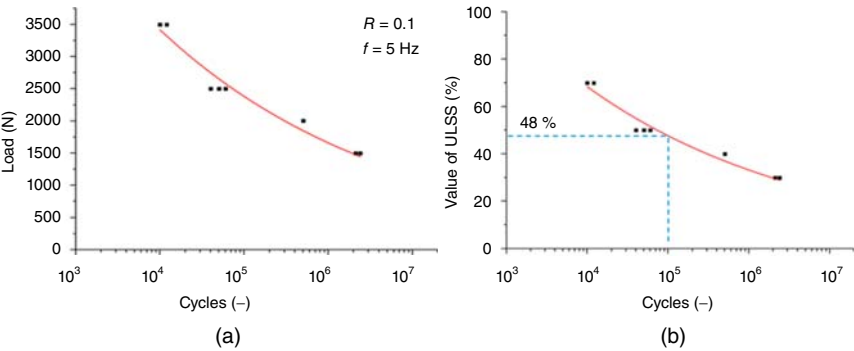


Figure 8.35 (a) S – N curve for selected friction riveting of Ti gr. 2/GF-PEI and AA 2198 overlap joint; (b) fatigue strength after 10^5 cycles.

application proposed in this work was the use of friction-riveted joints for lightweight emergency bridge structures [26].

The dynamic mechanical performance of the joints investigated in [26] has been addressed in a preliminary study, which is going to be published in a separate manuscript. Figure 8.35(a) displays the S – N curve of one selected friction-riveting joining condition (corresponding to the optimized joint from [26]), with the fatigue strength at 10^5 cycles determined in Figure 8.35(b). The aircraft industry recommends fatigue strengths of 30–40% of the ultimate

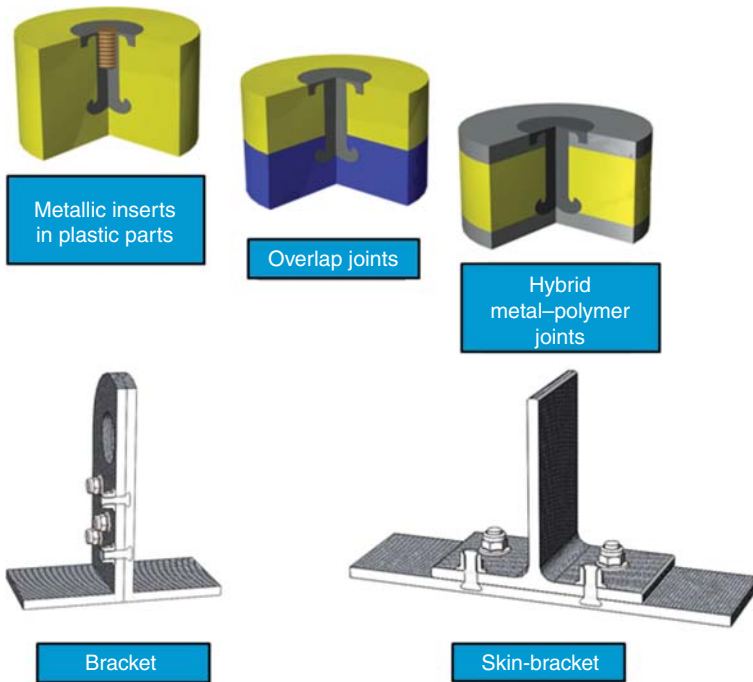


Figure 8.36 Possible FricRiveting joint geometries and applications (Courtesy of S.T. Amancio-Filho and N. Z. Borba, Helmholtz-Zentrum Geesthacht, Germany).

load for the commissioning of new mechanical fastening techniques for future structures at 10^5 cycles [47], which situates friction-riveted joints in this preliminary study above these average recommended levels, showing a high potential for future applications.

8.9 Envisaged Applications

Through its proven feasibility for a large variety of material combinations and the high mechanical performances achieved, FricRiveting shows a great potential for applications in a multitude of engineering structures, spanning from automotive, aeronautics to civil engineering. Several possible joint geometries for structural applications are proposed in Figure 8.36.

8.10 Conclusions

FricRiveting is an innovative joining technique for polymer–metal and composite–metal hybrid structures. The feasibility of the technology has

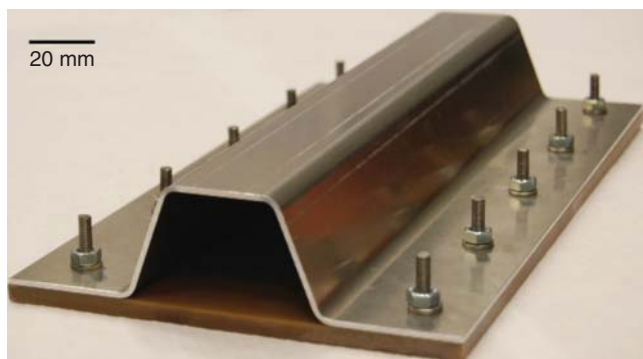


Figure 8.37 Friction-riveted skin–stringer component (aluminum omega-stringer and woven GF-PEI composite skin).

been demonstrated for several material combinations including unreinforced and fiber-reinforced polymer similar and dissimilar metal-composite joints. By thorough investigation of the process thermal history, microstructural changes, physical–chemical changes, local and global mechanical properties, the process has been described and characterized for all the studied material combinations. Sound joints were achieved, with good quasistatic mechanical performance (tensile and lap shear) and cyclic (lap shear), similar-to-better compared to state-of-the-art joining technologies, such as bolting. No extensive thermal flaws were identified in the polymers nor in the polymeric matrix of the composites. Current R&D efforts are being concentrated in applying advanced design of experiments and finite element modeling to better understand the influence of joining parameters on joint formation, heat generation, and mechanical performance. Process optimization is a central topic as well, whereby new process variants, such as force control with variable and multiple RS subphases are being investigated in close cooperation with the industry, for instance, to allow the direct FricRiveting of metals to composites (i.e., without predrilling the metal part prior to joining). Moreover, scaling-up is a subject of importance in FricRiveting; current investigation has demonstrated that the FricRiveting technology can be used to manufacture skin–stringer components for future aircraft applications (Figure 8.37). These exciting results and generated knowledge will be published in the near future. This will help to increase the technology readiness level of FricRiveting, helping in accelerating its transfer to the industry.

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List of Awards and Prizes Received by Works on FricRiveting

- **Best Paper Award** of the “Joining of Polymers and Composites Special Interest Group” for the paper: “Preliminary Analytical Modeling of Heat Input in Friction Riveting,” at ANTEC 2016 – Annual Technical Conference of the Society of Plastics Engineers (SPE), 23.05.2016, Indianapolis, USA
- **“Rising Star, Category: Joining Techniques,”** Automotive Circle International, Automotive Engineering Expo 2015, Nürnberg, 09.06.2015, Germany
- Winner of the **“Early Career Researcher’s Competition 2013”** (Winner: Dr-Ing. Lucian-A. Blaga, advised PhD Student) for the work “Friction Riveting of Glass Fiber Reinforced Lightweight Emergency Bridges, Advanced Composites in Construction ACIC 2013, Network Group for Composites in Construction (NGCC),” 10th–12th September 2013, Belfast, UK
- **“Airbus PhD Day 2012s speaker award”** (Runner-up: Ms Julie Altmeyer, advised PhD student) for the work “Fundamental characteristics of friction riveted multi-material joints: effect of process parameters on joint formation and performance,” 22nd October 2012, Getafe, Spain
- **Granjon Prize 2009 of the International Institute of Welding, Category A:** Joining and Fabrication Technology, Singapore
- **Best Student Presentation Award of the “Joining of Plastics and Composites Special Interest Group”** for the paper: “FricRiveting a new technique for joining thermoplastics to lightweight alloys” about my PhD work, at ANTEC 2008 – Annual Technical Conference of the Society of Plastics Engineers (SPE), Milwaukee, USA, 3rd May 2008
- **NORDMETALL-Stiftung “Preis der Metall- und Elektroindustrie 2008”:** best PhD work of 2007, Hamburg, Germany.

9

Staking of Polymer–Metal Hybrid Structures

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9.1 Introduction

Mechanical interference between two materials has been one of the most used methods of assembly. The classical mechanical joining process is through use of extra joining components such as fasteners. Mechanical fastening is a well-understood method, widely applied in virtually all structures. Although very flexible, fasteners can be substituted by other interference mechanisms; these mechanical attachment processes are carried out using interlocking and can be classified as rigid (e.g., dovetail and grooves), elastic (e.g., snap fits) or plastic (e.g., clinching, staking) [1].

A notably widespread method of mechanically joining dissimilar components is staking. Staking is a mechanical attachment between two joining parts by the use of plastic deformation from one of them. It has been used since the 1950s, but only in the 1980s, it developed to its current technological level [2]. Its basic configuration is illustrated in Figure 9.1(a). The joining parts consist of a thermoplastic workpiece with an integral protruding stud, and a second workpiece with a through-hole in which the stud fits in. The second workpiece can be manufactured of any material, but this chapter focuses on metal–polymer joints. Figure 9.1(b) shows the action of a forming tool shaping the stud into a stake, which entraps the two parts together as displayed in Figure 9.1(c). This is a simple method that can join thermoplastic polymers to other materials, such as metals, printed circuit boards, and other chemically incompatible polymers.

Staking is widely applied on nonstructural joints in automotive industry, such as trims, fairings, bodywork, and printed circuit boards of electronic components [2, 3]. Due to the necessity of integral studs on the thermoplastic workpiece, staking is usually performed for injection- or compression-molded

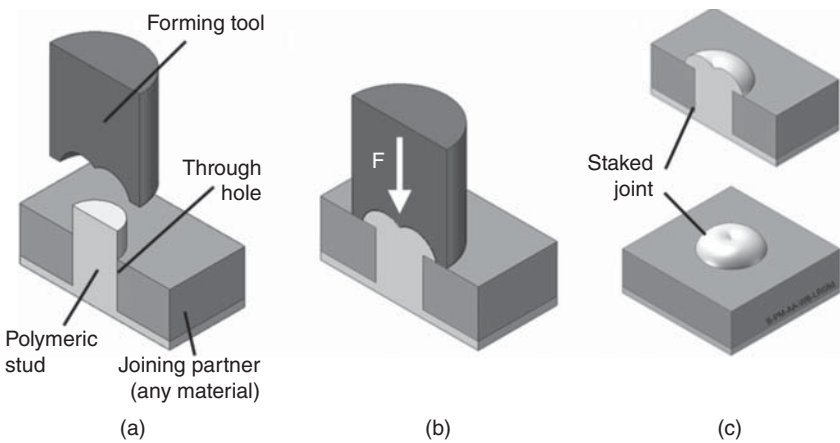


Figure 9.1 Basic configurations of joining parts for staking processes.

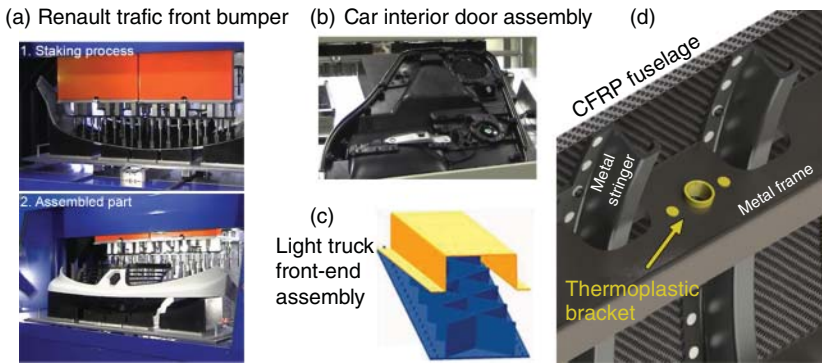


Figure 9.2 Applications of staking processes: (a) Renault Traffic front bumper assembly (Reproduced with permission of PHASA [12]); (b) car interior door assembly (Reproduced with permission of PHASA [13]), both performed by PHASA Limited (www.phasa.co.uk); (c) light truck front-end assembly (Source: Leaversuch 2003 [14] Reproduced with permission of Plastics Technology Magazine); and (d) potential application for fiber-reinforced brackets in aircraft structures.

parts, in which these geometries are easily achieved and add little to tooling costs. Industrial-scale staking applications are performed through automatized equipment, which can join dozens of stakes per cycle. Although most applications are not aimed at load-bearing structures, new advanced staking-based processes have been aiming for these assemblies, such as injection clinching joining (ICJ) [4–6], friction staking [7, 8], ultrasonic upsetting [9, 10], and thermoclinching [11]. Figure 9.2 shows examples of current and potential applications for staking processes.

Advantages of staking processes are [15, 16]: (1) can join dissimilar materials; (2) short joining cycles; (3) no extra parts needed, reducing weight; (4) low investment; (5) recycling friendly; and (6) can have loose tolerances for low-strength applications. Limitations are: (1) permanent assembly; (2) shape recovery from relaxation; (3) appearance (large stake heads); (4) longer cycles for thermally resistant materials; and (5) two-sided accessibility.

Traditionally, staking methods are a simple way of joining dissimilar materials and are not deeply investigated in a scientific aspect. This chapter intends to introduce the state of the art in staking methods and show an overview of the observed joint formation and microstructural changes, as theorized according to our experience in the field. Additionally, an introduction to design concepts, characterization, and mechanical behavior of staked joints helps the reader in deciding strategies for the development of such methods.

9.2 Types of Staking Processes

The two basic types of staking processes are based on the energy supply used for deformation of the stud into a stake: cold staking and hot staking. Cold staking processes are carried out without or with external heating, however, always at temperatures below the relevant transition of the polymeric partner – glass transition temperature (T_g) for amorphous polymers and crystalline melting temperature (T_m) for semicrystalline polymers. Hot staking processes use a heat source to soften or melt the polymeric stud above its T_g or T_m , reducing the viscosity and facilitating deformation. Figure 9.3 summarizes the main techniques of each of these types.

The characteristics of each of these processes will be mainly dictated by the energy transfer mechanism that takes place. On all variations, mechanical energy is needed to form the stud into a stake. Additionally, on hot staking processes, a heat source is used to help soften the material and reduce the magnitude of the mechanical energy in joining. The nature of each of the energy transfer mechanisms will influence in different ways the material and joint properties, as well as the duration of the joining cycles. The next sections contain short descriptions of each of the listed processes and the main characteristics affecting joint and material integrity.

9.2.1 Cold Staking

This type of staking process is characterized by plastic deformation of the polymer, below transition temperatures. It uses a tool to apply a forming force above the yielding strength of the polymeric stud, transforming it into a stake with a geometry corresponding to the tool shape. Cold staking can be performed with any material malleable enough to withstand the high

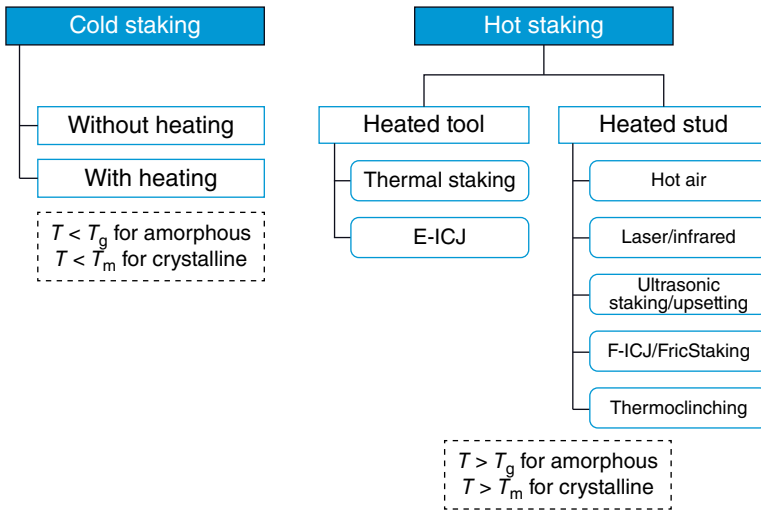


Figure 9.3 Summary of types of staking processes.

deformation levels without breaking or cracking; therefore, it is usually not suitable for composites and brittle polymers.

Recovery effects are a relevant issue in cold staking. Cold forming leaves residual stresses in the deformed volume. After tool retreat, a time-dependent relaxation takes place at the stressed volume, changing the macromolecules configuration to a low energy state. In practical terms, it means that a “spring-back” effect takes place [15], so that the stake slowly loses its designed shape and can deteriorate the clamping force of the stake head on the joining partner. A strategy to reduce this effect is to hold the stake under pressure of the tool after forming, inducing the polymer to recover while still on the desired shape. Service temperature of the assembly must be considered, since higher temperatures will accelerate the recovery effect for cold-staked plastics. As a result of the recovery issues and cracking at higher deformation rates, cycle times to obtain strong cold staking joints can be greatly extended.

These limitations cause cold staking to be mainly applied in nonstructural parts or low-load-bearing assemblies. Cold staking is therefore not technologically as relevant as hot staking, the latter being preferred for high-end applications. The rest of this chapter is mainly focused on hot staking applications.

9.2.2 Hot Staking

Hot staking processes use thermal energy to soften the material prior to formation by heating the stud or during formation by using a heated tool. Short joining cycles require fast heating rates, which in hot staking may lead to thermal degradation of parts of the joined material. The process must be optimized

to obtain sound joints with no significant degradation of the joining parts, even when using very fast heating rates.

Hot staking has some advantages over cold staking: (1) lower forming forces; (2) avoids recovery effects by reducing/eliminating residual stresses; and (3) softened material can better fill the hole clearance. Although in general, the performance is better, there are drawbacks on the use of extra heating: (1) higher energy consumption; (2) thermal degradation risks; and (3) holding/cooling times needed for consolidation. The next sections briefly describe the main types of hot staking processes. Most processes are carried out as pictured in Figure 9.1 with the addition of a heating source.

9.2.2.1 Thermal Staking

The forming tool is electrically heated above polymer's T_m or T_g , and compressed air is used to cool down the system before tool removal. Since the tool is never cooled, there is high energy consumption, and care must be taken to avoid thermal degradation. Thermal staking is better suited for high-temperature reinforced thermoplastics [15].

9.2.2.2 Hot Air Cold Staking (HACS)

Streams of hot air are delivered with manifolds or nozzles over the polymeric stud to soften or melt it; next, a cooled forming tool shapes the stake and cools it down at the same time. The use of very hot air shortens cycles, but increases energy consumption. Typical temperatures range between 150 and 400 °C. During process development, the engineer must control the forming speeds according to temperature development, so that the polymer is soft enough to be formed by the cold tool without cracking. This is one of the most used staking processes in the automotive industry and can be easily automatized [2].

9.2.2.3 Infrared and Laser Staking

These processes use electromagnetic radiation to heat the polymeric stud prior to forming. Equipment costs are higher than those of other staking technologies. A device is used to converge the photonic radiation beams (infrared or laser) to the region of interest (stud), afterward forming the stud into a stake [17]. There are two variations for this process depending on the configuration of the device: single tool or dual tool. A single tool has both the functions of converging the beam to the stud and forming it afterward. Glass lenses are used for converging the beam, and they also form the stake head. This configuration is usually costlier because of the excessive wear on the lenses when used as a forming surface, especially for reinforced plastics. The dual tool uses a two-stage tool, composed of a beam source and a forming head. In this case, the infrared/laser source heats the stud, and afterward, the tools or workpieces change position and the forming head creates the stake. In this way, a more suitable forming tool can be used, but the cycles are slightly longer due to tool

change [17]. Due to the nature of this energy source, infrared/laser staking only works on materials opaque to the radiation used. Carbon black is commonly applied in small quantities to the plastics for this purpose. It must be evenly distributed over the stud volume, to avoid nonuniform heating of the stud.

An innovation of this process is the “through-transmission” laser staking, in which a transparent polymer part is positioned over the opaque (absorbing) polymer stud. As the beam heats the top of the stud, and the lens presses the transparent part against the opaque stud, a weld is formed between them, thus transforming the transparent part into the stake head. This process reduces tool wear for the single tool variation by the use of a softer material as the transparent component [18].

Recently, a derived method of infrared staking was developed as the proprietary “Infrastake” technology [19, 20]. Infrastake uses a closed chamber with reflective walls, which apply the radiation more efficiently than a single beam on the stud. This process can achieve very low forming forces using low energy consumption (35 or 100 W lamp) and short joining cycles due to simultaneous forming and cooling.

9.2.2.4 Ultrasonic Staking

An ultrasonic source is used to vibrate the forming tool, which when in contact with the polymeric stud will soften/melt it through the heat generated by intermolecular and interface friction. The nature of this mechanism creates only a thin molten layer on the contact surface; therefore, most of the stud volume is in a stiff nonyielding state. Understanding and optimization of the process are needed to control joining pressure and oscillation amplitude, in order to match the melting rate of the plastic and avoid cracking the stud due to excessive pressure on a solid material [15]. Optimized processes can be very fast, with only a few seconds of joining time. In addition, the recovery effect is not very intensive due to the small molten volumes.

When using reinforced plastics, tool wear can be an issue. Hahn and Finkeldey [21] reported excessive wear of titanium tools after 100 joints of PP-40%GF. Additionally, ultrasonic staking is not recommended for self-lubricating plastics as UHMWPE (ultrahigh-molecular-weight polyethylene) and polytetrafluoroethylene (PTFE), since the frictional heating is not efficient for these materials. Although it is a very efficient method for single stakes, large multistaked structures can be challenging for a single ultrasonic source to join, since spacing and height differences between stakes must be accounted for [15, 22].

9.2.3 Advanced Staking Processes

New innovative staking-based technologies are being developed aiming at secondary and tertiary load-bearing structures. The following sections shortly describe the main aspects of these methods.

9.2.3.1 Injection Clinching Joining (ICJ)

ICJ is a staking-based technology, including principles of injection molding, mechanical fastening, and staking. This method was recently developed for polymer–metal structures [4], aiming to improve the mechanical performance of a staked joint while also reducing stake head size. The strategy of ICJ staked joints is to use cavities inside the through-hole of the metallic partner and push (or inject) the softened/molten polymeric material of the stud inside of these cavities. This makes a more efficient use of the stud volume and improves the mechanical anchoring between the parts. This concept can be applied to any staking method, but it is currently being developed as two variants, using electrical and frictional heating. Figure 9.4 displays the basic principle of ICJ.

For production of optimized ICJ joints with filled cavities, a systematic study should be done for the specific material and cavity geometry to be used. For any chosen heating method, the process parameters should be adjusted so that the stud has low viscosity during the forming step; otherwise, the cavity filling will be incomplete and the joint inefficient. Expenses from experimental runs can be reduced by using computational fluid dynamics (CFD) models to define rheological conditions for successful cavity filling. The cavities in the joining partner are preferably machined, although they can also be molded if it is a polymeric material.

Electrical-Heating ICJ (E-ICJ) Electrical-heating injection clinching joining (E-ICJ) is the original concept of the ICJ process. With the parts preassembled, it uses electricity to heat a hot case, which surrounds the free length of the stud. After a heating time, a punch-piston coaxial to the hot case applies pressure on the softened/molten polymeric stud and pushes it into the cavities of the joining partner, allowing it afterward to cool under pressure.

Flush-stake-head joints using a chamfer-and-thread cavity present improved performance when efficient cavity filling is achieved [23]. If the application

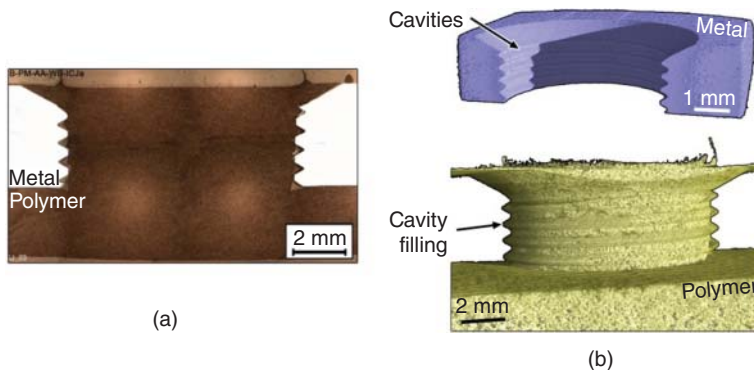


Figure 9.4 Principle of injection clinching joining: (a) cross section of a joint using cavities in the metallic partner and (b) μ CT imaging of both parts showing cavity filling of the polymer.

allows, a larger stake head can also be designed to further improve the joint strength. Current experience with E-ICJ shows that for an efficient cavity filling, joining cycles of 1 min or more may be required for engineering thermoplastics [5], since the polymer's low thermal conductivity hinders a fast overall softening of the stud.

Friction-Based ICJ (F-ICJ) F-ICJ was developed as a faster alternative to E-ICJ [6, 24]. The equipment used for F-ICJ can be an adapted drilling or milling machine capable of high rotational speeds and of applying controlled axial forces. A rotating nonconsumable tool generates frictional heat when it touches the polymeric stud. This softens/melts the top layers of material, and as the tool feeding continues, squeeze flow continuously occurs and further volumes of the stud are softened/molten. In a consolidation step, the rotational speed is decreased to a stop, and further axial pressure is used during a short cooling time. The joining cycles range from 3 to 20 s depending on stud volume and cavity profile [24].

As in other staking technologies, the forming tool shape will dictate the final stake-head shape (see Section 9.4). For F-ICJ, the tool shape also has an influence on the heat input profile along the formed stake. Because of the low thermal conductivity of polymers, it is important that the tool is able to heat a large volume of material and push it to the surrounding cavities. One way to improve heating efficiency is to use a conical-pin tool similar to the ones used in Friction Stir Welding (FSW). Although more efficient heat input is achieved with a pin tool, it forms a hollow stake. A hollow stake may have decreased mechanical performance, but when well designed can create strong joints and improve strength-to-weight ratios [2, 6]. Therefore, the tool should be designed according to the material's thermal conductivity and joint geometry, so that sufficient cavity filling can compensate the formation of a keyhole.

9.2.3.2 Friction Staking (FricStaking)

Friction Staking is a recently developed process using the same machinery and three-part tool set as from the Friction Spot Joining process (FSpJ, see Chapter 3) [7, 8]. The system and joining sequence are shown in Figure 9.5.

The pin and sleeve move independently, and the clamping ring prevents loss of flash to the surroundings; these features allow control of the stake-head geometry. FricStaking can notably improve heat input at the stud while still creating a solid stake head, which also makes it suitable for ICJ configuration using cavities. Strong joints can be created even using small flat stake heads [7].

9.2.3.3 Ultrasonic Upsetting

Aiming to overcome joint formation issues with common ultrasonic staking, Brückner *et al.* [9, 10] developed ultrasonic upsetting. In this method, a solid stud with a tubular section on the top is used. The tool comprises a shoulder and a pin, with the pin fitting into the tubular section of the stud. The ultrasonic

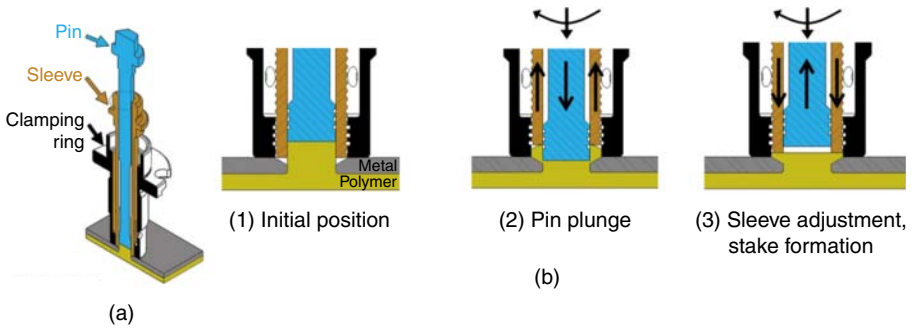


Figure 9.5 FricStaking: (a) tool system and preassembled parts and (b) joining sequence.

vibration heats up and softens only a selected volume inside the tubular stud through action of the pin; meanwhile, the shoulder applies pressure on the walls of the tubular stud deforming the softened polymer. The process achieves sound joints without risk of damage to joining parts in cycles of 1.5–3.0 s [25].

9.2.3.4 Thermoclinching

Traditional staking methods are feasible with molded discontinuous-fiber-reinforced thermoplastics. The new thermoclinching technology [11, 26] creates form locks similar to hollow stakes with laminated composites. A notch is created on the composite and aligned with a through-hole on the metallic partner. After a heating-up phase, a conical forming tool pushes the softened composite through the hole, and a forming die shapes the material on the other side as a stake head. After heating, joining cycles last less than 1 s, but may require long cooling times for effective consolidation.

9.3 Characteristics of Staked Joints

Formation of the stud into a stake via hot or cold conditions will have effects on the joining parts, which can be relevant for joint properties. The core to an efficient process, which obtains a sound joint, is to be able to soften the material in short times without causing damage or degradation and to be able to achieve the desired stake shape after cooling. For this purpose, the mechanisms that occur during joint formation must be understood, as well as the resulting microstructure.

9.3.1 Joint Formation

Figure 9.6 schematically shows the phenomena that typically occur during a staking joining process. After heating the preassembled stud (Phase 1), it presents a softened/melted volume above T_g or T_m (dark gray area), as well as

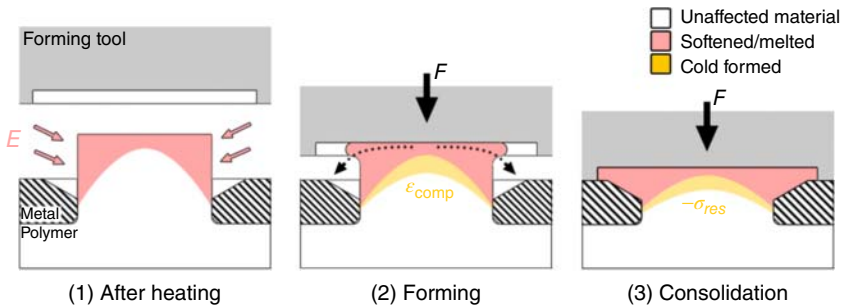


Figure 9.6 Joint formation steps in a hot staking process.

a volume, which was not significantly affected by heating (white area) and is still solid. The size and precise shape of the softened/melted volume depends on the heating method and parameters used. For most traditional methods such as HACS, infrared, and laser staking, it corresponds to a concave shape as shown in Figure 9.6 [2]. For ultrasonic- and friction-based processes, the softened/melted volume might be thinner [15], but generally the observed effects will be similar to the ones described here.

Following this point, the forming step (Phase 2) takes place with a forming tool pressing this softened/melted volume, pushing it to the surroundings in a squeeze flow as shown by the dotted arrows. The shape of the tool will determine the material flow and the final geometry of the stake head. The hydrostatic pressure on the flowing volume is transferred to the solid polymer, which experiences a certain degree of compressive deformation below its transition temperatures (light gray area). Low joining forces will apply only elastic deformation; however, high forces may cause plastic deformation at this region. Process parameter control is critical to avoid plastic deformation in this volume, in which case it becomes a cold-formed volume with compressive residual stresses.

During consolidation (Phase 3), the softened or molten material gradually cools down until it is solid, while the rest of the stake experiences stress relief under pressure. In case of a cold-formed volume, the residual stresses from plastic deformation initiate recovery effects. This might result in micro cracks in the polymer and loss of clamping force due to relaxation. In more extreme cases where the force applied is too large, stud root cracks may form, which will be problematic during service. This problem may arise when the tool feeding rate is significantly higher than the stud melting rate [15].

The final joint structure has large dependence on the heat input induced during joining. The heat input has an inverse correlation with the viscosity of the polymer. A low heat input will create a small or insufficiently softened or melted volume so that no significant squeeze flow is achieved, and joints can only be created through cold forming with high joining forces. On the other

hand, excessive heat input may exceedingly reduce molten viscosity, which can lead to loss of stud material as flash to the surroundings of the joint, and/or possibly degradation of the material. The process parameters must therefore be adjusted to obtain the desired structure, without inducing defects or extra production costs.

For cold staking, the softened or melted region described in Figure 9.6 does not exist, and the stake is mainly composed of a cold-formed volume with high levels of residual stresses.

9.3.2 Microstructure

A process as hot staking induces microstructural changes in the joining parts, most notably on the polymeric partner as a result of the mechanical and thermal energy input. Figure 9.7(a) schematically shows the microstructural zones in a consolidated joint, composed of a thermomechanically affected zone (TMAZ), a heat-affected zone (HAZ), a plastically deformed zone (PDZ), and an unaffected base material (BM). The TMAZ, HAZ, and BM are always present at hot-staked joints, whereas the PDZ is only observed when the axial forces during joining create plastic deformation and local residual stresses.

The TMAZ is a volume where heat lowers the viscosity, and deformation induces flow of the material. Molecular chains and reinforcements are oriented in the flow direction, for example, as seen from the carbon fibers in Figure 9.7(b) for F-ICJ-joined PEI-CF. At the TMAZ, fibers are oriented in the tool rotation

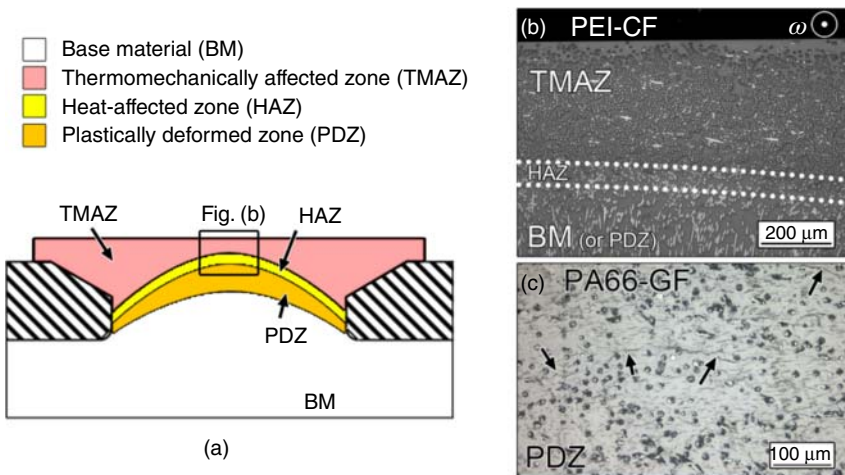


Figure 9.7 (a) Microstructural zones in a staked joint; (b) example of boundaries from the TMAZ, HAZ, and BM (or PDZ) in an F-ICJ joint of carbon-fiber-reinforced PEI; and (c) example of the PDZ in an E-ICJ joint of glass-fiber-reinforced PA66. The black arrows show microcracks.

direction (ω , perpendicular to the page), while at the HAZ and BM, they are oriented in the original direction (vertically in this image). This volume may also contain defects such as voids and fiber breakage. In amorphous polymers, the microstructure will be formed by a frozen state of the oriented chains, which can result in anisotropy of properties. For semicrystalline polymers, a recrystallized structure with chain reorientation is created. The new crystalline configuration will depend on the heat development and the cooling regime.

A HAZ is a transition zone between the TMAZ and BM. It suffers no plastic deformation of material flow, but is heated – still below T_g or T_m – through conduction from the TMAZ. Clear boundaries of the HAZ are usually difficult to define. As a result of the low thermal conductivity of polymers, only a thin layer of material is expected to suffer a certain level of change in properties from heat conducted from the TMAZ. Theoretically, the properties in a HAZ can be changed through certain levels of physical aging [27] or structural water elimination, but its effects on a global level are still to be understood.

A yielded volume of polymer may be present when high axial forces are used, consisting of a PDZ (Figure 9.7(c)). On a macro level, this volume is often similar to the base material, but micro cracks and a small degree of reorientation of reinforcements and polymer chains can occur due to the strain from hydrostatic pressure imposed by the tool. Additionally, the plastic deformation can significantly change the local properties through the creation of free volume in amorphous polymers and partial fracture of crystallites in semicrystalline materials [28, 29].

Depending on joint geometry and forming tool design, as the TMAZ material flows, it will form a polymer–polymer interface with the stake shaft (Figure 9.8(a)). The flowing material possesses a high level of thermal energy, and the contact between the two surfaces leads to heat exchange between the surfaces. Figure 9.8(b) shows such an interface for a low-heat input condition of an F-ICJ joint, with the TMAZ and BM microstructures evident from the fiber orientation direction. An interface without intimate contact between TMAZ and BM is observed, in a similar manner to a residual weld line. Figure 9.8(c) shows a joint produced with higher heat input, where different fiber orientations are still visible; however, no discontinuity is observed. In this case, the heat exchange between TMAZ and BM promoted interdiffusion of polymer chains, leaving a seamless interface [30]. The latter is the most desired, since the gaps between the zones are notches and may act as crack initiators and stress concentrators.

At the interface between the TMAZ and the joining partner, any possibly relevant effects will depend on the nature of the other workpiece. In a polymer–metal or polymer–ceramic interface, no significant thermomechanical or thermal effects occur. Some level of heat transfer from the TMAZ may occur, but usually it is not sufficient to alter the microstructure of structural metals. If another polymer is used whose matrix is compatible to the original

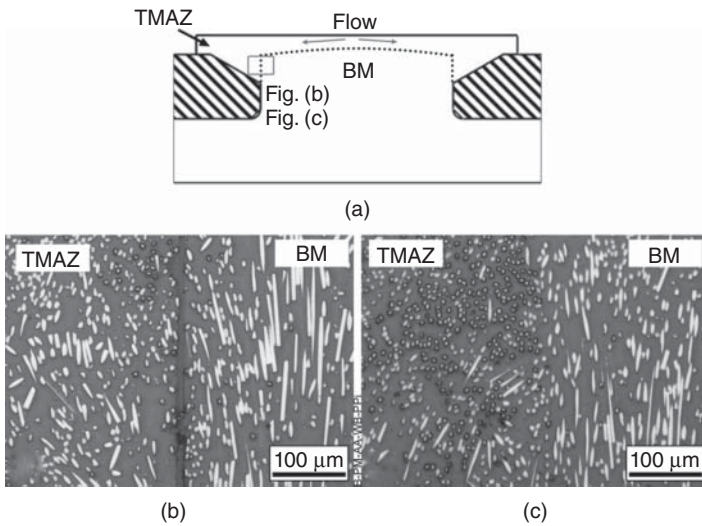


Figure 9.8 (a) Formation of polymer–polymer interfaces; (b) visible residual weld line; and (c) healed interface.

stud material, some degree of interdiffusion may take place in the same manner as the weld line healing shown in Figure 9.8. For incompatible or thermoset resins, no relevant effect from heat transfer is expected.

Although in some of these cases, extra micromechanical anchoring and adhesion may be present, no significant contribution to mechanical performance is expected at a global level from such effects. The interaction between the polymeric part and the other workpiece is intended only for mechanical interference between the materials. In fact, many staking applications use a large hole clearance to reduce production costs and do not rely on any interaction between joining parts other than mechanical interference. This results in looser joints, usually acceptable for light-duty applications.

9.3.3 Defects

Defects in staking joints are usually a result of excessive heat input during processing. Typically, volumetric flaws such as pores, residual weld lines, and micro cracks appear in the TMAZ or its boundaries. These flaws can act as stress concentrators, contributing to crack nucleation and growth, and increase the susceptibility of the joint to chemical agents. Even though a high heat input can contribute to polymer–polymer interface healing, it also compromises the mechanical integrity: porosity, possible thermal degradation, and loss of material as flash to the surroundings impair the joint's mechanical performance.

Changes on the joining partner are usually undesired in staking processes. Ideally, the heat flow and forming effect only act on the stud to transform

it into a stake, without affecting the joining partner. However, unintended heat transfer or deformation may occur in some cases. On a metallic partner, usually conductive heat transfer from the molten polymer is not significant for microstructural changes, but stray hot air flow and laser beams can create HAZs, and inadequate feeding of ultrasonic or rotating tools can induce thermomechanical work on its surface. Depending on the metal and degree of energy input, small regions of the metal can experience precipitate dissolution, grain growth, and recovery or recrystallization. For ceramic materials, stray heating from hot air or radiation usually is negligible, but tool crashing might lead to accelerated tool wear and/or local damage to the ceramic partner's surface. In the case of other polymeric joining parts, the effects can be more significant, with thermomechanical work or heating effects causing the same effects described in Section 9.3.2 for the stake material. However, changes in the joining part are not of utmost importance for the overall performance of the joint, since failure of staked joints takes place in the stake region (see Section 9.5).

9.3.4 Characterization of Local Properties

Changes in microstructure due to the thermal and mechanical effects are a result of local changes in the material properties. It can be useful to explore the property changes over a joint's volume, in order to understand the extension of the affected zones, the cause of the change in properties, and to correlate the changes with global joint behavior. In the polymeric stake's TMAZ and HAZ, for example, reorientation of chains, residual stresses, differential crystallinity, and breakage/clustering/reorientation of reinforcements are typical effects. Such microstructural changes can be detectable through local mechanical testing and physical–chemical characterization.

9.3.4.1 Local Mechanical Properties

The mechanical properties at specific regions of the stake can be evaluated through microhardness testing. The small size of the indenter allows the testing to obtain property change along the microstructural zones and identify polymer–polymer interfaces. This sort of investigation helps to elucidate the global mechanical behavior in joining technologies.

A simple analysis can use indentations on the stake head and see the differences in value compared to the base material. Such a configuration is illustrated in Figure 9.9(a) with five profiles from the top of the stake head toward the bottom plate, for an aluminum–polyetherimide (PEI) hollow-stake F-ICJ joint. The measured values for lines L1–L3 are displayed in Figure 9.9(b) compared to average BM microhardness. It is clear that up to 2000 μm below the top surface, the local strength is below BM levels. Figure 9.9(c) displays a contour plot using the five profiles from Figure 9.9(a); here a more graphical view of the boundaries between microstructural zones can be obtained. As previously explained,

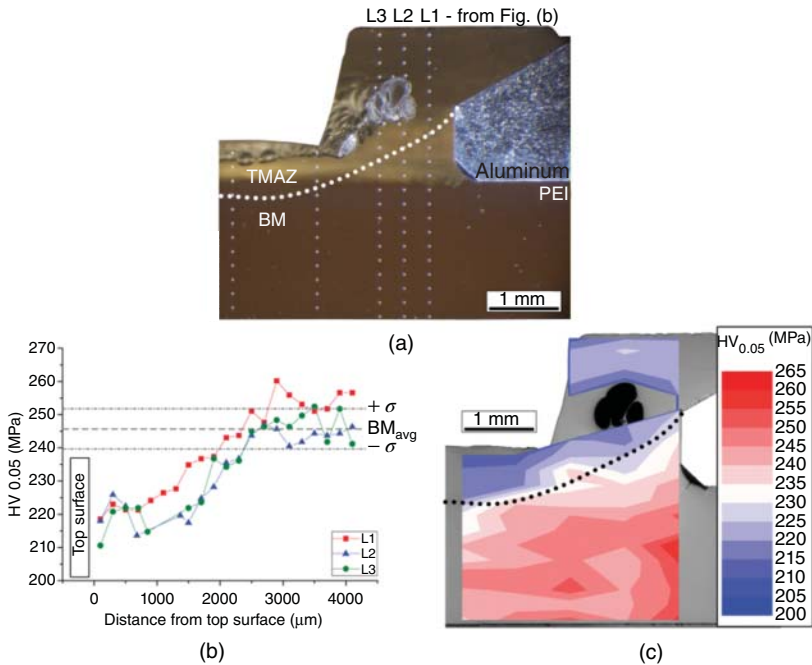


Figure 9.9 Local mechanical properties of staked joints: (a) example of indentation profiles on a hollow PEI stake, stereomicroscopy image; (b) microhardness of profiles L1–L3 from (a), compared to PEI–BM properties; (c) contour plot of the five profiles from (a) overlaid on a light-optical microscopy image. The dotted lines in (a) and (c) show the boundary between TMAZ and BM.

the difference in properties can be traced to microstructural changes in the polymer; in the case of amorphous polymers such as PEI joined by F-ICJ, this may be an influence of increased free volume from fast cooling, chain reorientation from the tool rotating action, or partial thermal degradation. However, for each material combination and staking process, new hypotheses should be investigated.

An even more refined analysis can be done with instrumented nanoinindentation hardness. Such tests are more time-consuming than regular microhardness; however, not only the resolution of a region can be greatly improved, but also a vast amount of extra information about the local properties can be obtained. The provided load–depth curves give additional details of the viscoelastic behavior of the analyzed zone.

9.3.4.2 Physicochemical and Structural Properties

Analytical techniques can help to investigate the causes of the property changes on the microstructural zones. For these analyses, a small sample usually must

be extracted from the region of interest. This can be sliced with the help of a scalpel or a microtome or ground to a fine powder. It is important during the extraction process to minimize any additional heat input or residual stresses on the material, in order not to affect the following measurements.

Changes in molecular weight as a result of degradation can be directly measured using size-exclusion chromatography (SEC). Changes in molecular composition, density, and local stresses can be identified through Raman spectroscopy or Fourier-transform infrared spectroscopy (FT-IR), by comparing to the fingerprint spectra of the base material. With state-of-the-art equipment, these techniques are mostly nondestructive, being able to analyze polished surfaces of a cross section and provide a mapping of the spectra with high spatial resolution.

Based on the entangled network models, the energy to activate decomposition and thermal transitions in a polymer will reduce when the molecular weight is decreased [29]. Techniques such as thermogravimetric analysis (TGA) and differential scanning calorimetry (DSC) can help to evaluate the local changes by comparing thermal transitions to the usual values of a base material. With TGA, changes in the TMAZ of a stake can be evaluated by comparing the curves of the processed material with the fingerprint curve of the base material. Lower temperatures of the onset of decomposition are an indication of the presence of smaller chains, indicating certain levels of degradation. The same principle can be applied to DSC tests, where the onset of melting peaks and glass transition temperatures are shifted to lower temperatures compared to the base material if the structural changes were significant. Using DSC, it is also possible to measure the crystallinity in a microstructural zone, which can help to elucidate the local mechanical properties. DSC can be used for oxidation induction time (OIT) tests, which evaluate the susceptibility of the material to thermo-oxidative degradation [21]. A processed material with partial degradation and therefore more free radicals will be more susceptible to oxidation compared to the pristine base material.

9.4 Design Considerations for Staked Joints

As in most joining processes, an application-driven engineering approach is useful in joint design. The requirements of the final product will dictate the materials selection and therefore the staking process that is most fit for this material, which will yield the necessary strength that the structure requires. However, costs must be considered for a successful project. In this sense, dimensional tolerances have a high influence both on costs and on joint strength. Loose tolerances are economic, but produce weaker staked joints. A well-designed staked joint should be able to use loose tolerances, and still keep a tight joint, by exploring the joining process, material flow, postprocessing properties, and the combination of stud, stake, and forming tool geometries.

The material selection for the application can restrict the staking process to be used for the assembly. Mostly, this is due to incompatibility between material properties and the nature of heat transfer mechanism [15]. For example, transparent plastics are inefficient for infrared/laser staking, and the self-lubrication of PTFE and UHMWPE hinders frictional heat generation.

As reported by Rotheiser [15], some design criteria have to be defined in order to choose a functional staking process for a given application. One should define the type and level of loading on the assembly as a strength requirement so the joint geometry and the distribution of stake points can be defined. In addition, the use of a backing surface has to be considered for assembly according to the applied joining forces.

Once the design criteria conditions are defined, geometry optimization of a stake should be performed to define which are the loading levels that one single stake can bear. At this point, stud and through-hole geometries, as well as forming tool design, are defined. Finite-element method (FEM) can be used to analyze stressed regions in the resulting structure prior to production and, if necessary, altering the original design. For light-duty structures, in which the stakes are not used for substantial load bearing, the design criteria can be more flexible, adding more stakes if necessary, without the need of a critical analysis of geometries and load distribution.

The mechanical behavior of the joint will be dictated by the load levels and the final geometry of the stake. The stud, the through-hole, and the forming tool geometries are the main factors that control the stake geometry and therefore its performance. Therefore, design considerations of these factors need special attention.

9.4.1 Through-Hole Design

The machined hole in the joining partner must fit the design criteria for the assembly. This can be met in combination with design of the stud and the final stake geometry. Since the molten/softened polymer will be forced against its surface, the joining partner can also be designed to improve joint performance. Through-holes can be simple, of constant diameter, which will usually require a large stake head to achieve high mechanical resistance. Chamfered holes are also commonly used to obtain more esthetically pleasant, shallower stake heads, but their shape can also lead to stress concentrations and lower strength. Cavities inside the hole can be used to improve the mechanical anchoring [4, 23]. Naturally, any modifications to a simple through-hole increase manufacturing costs.

Although for metallic components, machining is the most obvious and well-known way of producing the cavities, for specialized applications where tolerances and complex cavities are of utmost importance, metal injection molding (MIM) is an interesting alternative. Not only the tolerances are more stable, but very complex and fine geometries can be achieved with such MIM technology. Nevertheless, this approach is only to be considered for

very specialized uses, due to the high costs and technological know-how. For polymeric parts, complex geometries can be obtained through molding processes. Ceramic workpieces are preferably sintered in the final shape. Additive manufacturing of metal (e.g., through selective laser melting or sintering) parts can be selected as a rapid prototyping tool. Such tools can help to validate FE stress analysis models, avoiding unnecessary high-cost capital investment to fabricate injection molds for MIM parts.

9.4.2 Stud Design

The stud on the plastic partner can be manufactured in a number of ways, preferably by injection molding or compression molding, but also possibly through machining a thick plate or additive manufacturing. Although some of these processes can have their advantages in terms of investment and part cost, injection molding and compression molding are the most cost-effective and with best tolerance control, needing only a high initial investment for the molds and machinery. Each manufacturing process creates a certain configuration of reinforcement distribution, which can be taken advantage of. For example, an injection gate placed at the top surface of a stud will orient fibers parallel to stud length, improving tensile strength at this region in the stake length direction.

Injection molding has the problem of sink marks and molded-in stresses in the stud as a result of thermal gradients, but these can be avoided or reduced by using hollow studs or limiting stud diameter to 50% of the plate thickness, provided that the joints are still complying to the strength requirements [15]. To assure this compliance, compensations in stud volume – height for solid studs and wall thickness for hollow ones – can increase joint strength while avoiding undesired malformations. Hollow studs also allow lower joining forces, even heating, and faster cycles, without jeopardizing joint strength [2].

Other than the topics of solid or hollow design and stud volume (diameter and height), the geometry of the base of the stud is of high importance for load bearing during joining and in service [15]. A design as shown in Figure 9.10(a) should be generally avoided due to the sharp angles; a rounded stud base is a preferred design to avoid stress concentrations. The use of a surface radius (Figure 9.10(b)) requires a matching chamfer on the bottom surface of the joining partner, increasing the costs. The recessed radius design (Figure 9.10(c))

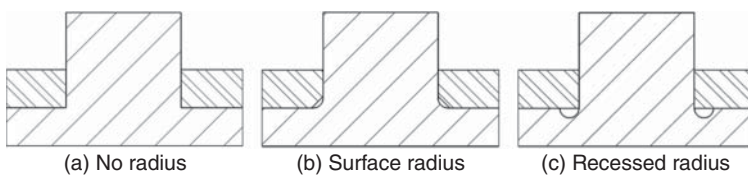


Figure 9.10 (a) No-radius design; (b) surface-radius design; and (c) recessed-radius design.

requires no changes in the through-hole, but the reduction of thickness of the base plate also creates a stress intensity factor for other loading conditions.

9.4.3 Stake Head/Forming Tool Design

Final shape of the stake is dictated by both the through-hole and forming tool geometries. Typical stake head shapes are rosette, dome, hollow, flush, and knurled (Figure 9.11). Staked joints usually rely on a large stake head to improve strength, with rosette and dome stakes being the simplest higher-strength designs. Rosette stakes have the advantage of low forming forces and good joint strength, but there are issues with alignment and wear of the tip of the tool. Dome stakes form large heads, giving looser tolerances on forming, and reduce wear of the tool in comparison to rosette stakes. Hollow stakes require low forming forces and are used for molded parts that require larger diameters, retaining good strength. In this case, the wall thickness is an important factor to be considered according to the type and level of loading to be endured. The knurled head has flexibility in alignment and appearance, and also medium strength, being suitable for light-duty applications. Flush configuration has good appearance, but the smaller head can have lower strength if not optimized. Other particularities of these stake head geometries have been more extensively described in [15, 31].

The geometry of the stud prior to forming and of the final stake, as well as the through-hole shape, are important for the mechanical performance of staked assemblies. FEM can be used to predict the in-use behavior of the structure and observe different load-bearing mechanisms depending on the stake shape. Chen and Wu [32] used FEM to simulate head pull-out tests for stakes made of poly(methyl methacrylate) (PMMA), using a through-hole without cavities. The objective was to investigate the ratio of height to radius of the stake head, which would avoid head failure, thus causing necking on the stud inside the hole. In their study, such a condition would occur with a ratio of 0.625, so that the joint would need a certain volume of the stake head to shift the stress concentration to the stud instead of the head. If the application requires

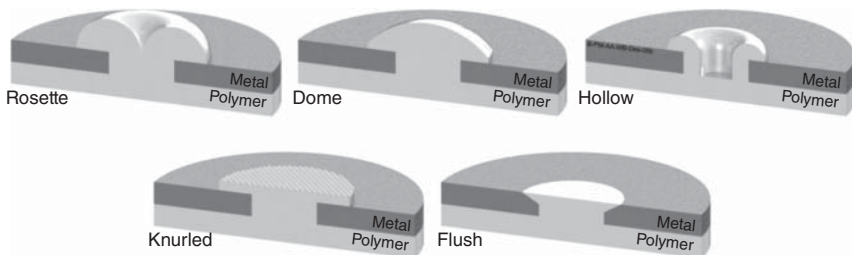


Figure 9.11 Stake head geometries.

smaller stake heads, this can be done by improving the design through the use of internal cavities on the partner or fine hole clearances.

Relying on mechanical anchoring in internal cavities is a strategy still not largely explored in staking technologies, but recent studies have shown that it can be significantly advantageous for staked joints [5, 23]. The usual means of improving strength has been increasing stake head volume, which is a successful method. The combination of a controlled stake head size and designed cavities for mechanical anchoring can enhance the overall performance of staked joints. Such a design can be achieved by understanding the material behavior during processing (see Section 9.3.1), taking advantage of material flow and properties of the different microstructural zones. Figure 9.12 shows an example of different joint configurations combining these two factors and their mechanical behavior under lap-shear loading.

Figure 9.12 shows three lap-shear joints with the same failure modes but different performance and microstructure. Figure 9.12(a) shows a flush stake head and no cavity filling, showing low strength. Figure 9.12(b) shows a joint with only partial cavity filling and a flat stake head (similar to the knurled geometry in Figure 9.11), presenting improved strength. Finally, the joint shown in Figure 9.12(c) has a small flush stake head as that in Figure 9.12(a), but with complete cavity filling and achieving high strength. The larger stake head shown in Figure 9.12(b) is stronger than that in Figure 9.12(a) due to the

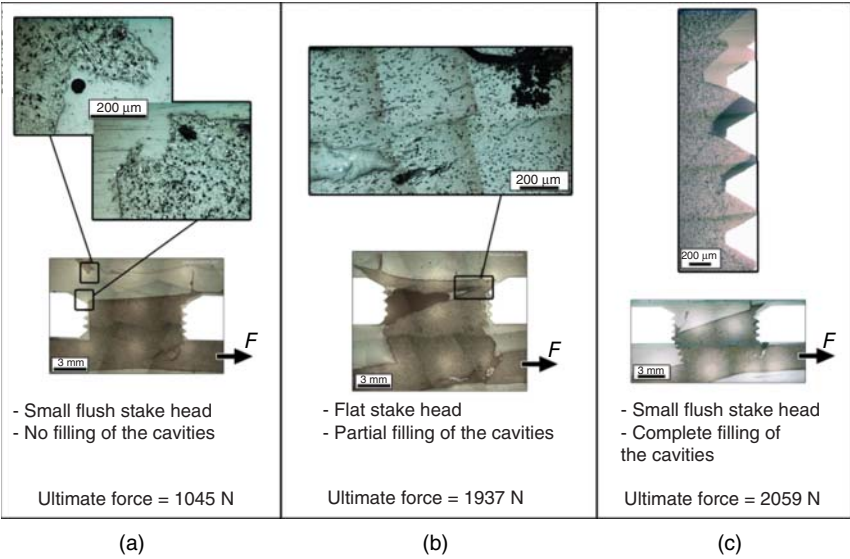


Figure 9.12 Staked joint configurations and mechanical behavior: (a) small head and no cavity filling; (b) large stake head and partial cavity filling; and (c) small stake head and complete cavity filling. (Source: Abibe 2011 [33]. Reproduced with permission of UFSCar.)

crack propagation path showed in the details. The joint shown in Figure 9.12(c) shows even higher strength as a result of the complete cavity filling. Therefore, in designing a joint configuration, it is clear that joint strength can be improved by both stake head geometry and cavity filling [23, 33].

9.5 Mechanical Behavior of Staked Joints

Connections such as staked joints have similar purposes as fastened joints, but are used as economic alternatives in applications that require weight and cost savings and that require load levels bearable by stakes. Mechanical testing of staked joints has no specific standard from the international organizations. The most frequent tests shown in research papers are lap-shear and cross-tensile tests [7, 9, 20, 21, 23, 24, 32, 34]. Both lap-shear and cross-tensile testings are mostly performed with customized testing fixtures and geometries. Some works have used standardized tests [7, 21, 24, 34], with cross-tensile testing following ISO 14272 [35] (originally for resistance welding of metals) and lap-shear testing following ASTM D5961 [36] (originally for bearing of fastened composite joints). Although these standards are idealized for other joining technologies, they have been frequently adapted for other types of joining processes [21, 32, 37–40]. A comprehension of the mechanical behavior in these configurations is useful for an improvement of joint design. Figure 9.13 illustrates the forces acting on staked joints in lap-shear and cross-tensile configurations. These are shown for a flush hollow stake in a metal–polymer joint, but can be used for further understanding of other stake geometries and material combinations.

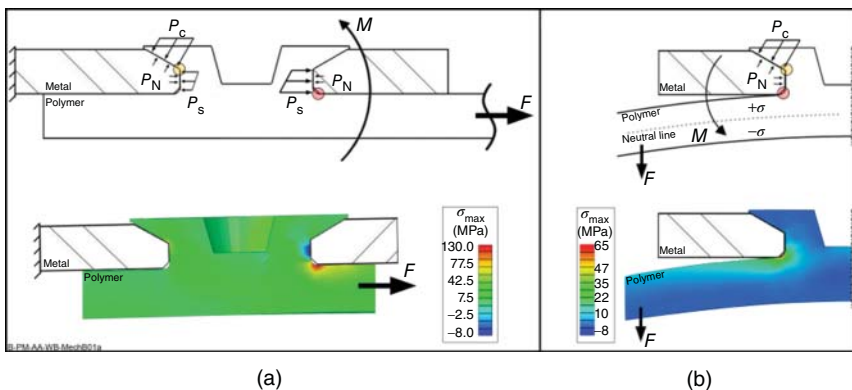


Figure 9.13 (a) Forces acting in lap-shear loading and stress concentrations analyzed through FEM and (b) forces acting in cross-tensile loading and stress concentrations analyzed through FEM. The dark gray and light gray circles indicate preferential crack nucleation points in each case.

Figure 9.13(a) illustrates the forces acting on lap-shear loading of a hollow-staked joint and the maximum principal stresses resulting from an FEM simulation of the test. The combination of contact pressures of the stake on the walls p_s leads to an eccentricity that causes the bending moment M , responsible for secondary bending in such cases [41]. These forces combined with the contact pressure on the chamfer p_c the reaction pressures p_N yielding stress concentrations on the stake base (dark gray disk) and stake head (light gray disk). These regions are also visible in the FEM model. The out-of-plane displacement resulting from secondary bending forces the rotation of the stake and therefore plays a large role in the failure behavior. Generally, the maximum force of a joint in lap-shear testing occurs before the crack at the stake head (orange disk) grows to a critical size; afterward, the force is concentrated at the stake base crack (red disk) leading to stake pull-out or secondary bending failures. Joints of ductile polymers usually fail with a slow rivet pull-out failure. Brittle materials break at the base plate as a result of secondary bending. For further information on the mechanical behavior of staked joints under lap-shear loading, please refer to [23].

In cross-tensile tests, one of the plates is fixed, while the other plate is displaced, thus transferring the loads to the spot-like joint of the specimen. Figure 9.13(b) shows the forces acting on the joint during cross-tensile loading and the stress concentration at the polymer part. Pulling the base plate downward causes a bending moment M and a contact pressure on the chamfer p_c , and reaction pressures p_N . The bending moment also creates tensile ($+\sigma$) and compressive ($-\sigma$) stresses at the base plate. As seen in the FEM analysis, the stresses are mainly concentrated at the base plate (red disk), which leads to a failure by bending of the base plate without damage to the stake head. However, in actual joints, a failure by head pull-out is frequently observed as a result of volumetric flaws and discontinuities at the TMAZ of the stake [21, 24]. A FEM model accounting for such issues would probably show higher stresses at the position of the stake head (orange disk).

Larger multispot assemblies can experience much more complex stress states, but single-stake testing at coupon level assists in the understanding of failure modes and possible stress concentration issues. The analysis from Figure 9.13 shows that a large stake head improves strength by reducing p_c (larger area); internal cavities with mechanical anchoring hinder the effect of the bending moment M in lap-shear joints and reduce p_c in cross-tensile joints by increasing the contact area.

9.6 Final Remarks

Staking technologies have been originally developed for light-duty applications, and its possibilities explored within this framework. More recent development

with advanced staking methods such as ICJ, friction staking, ultrasonic upsetting, and thermoclinching gives a new outlook to this scenario. This chapter focused on the description of the unexplored materials science and engineering aspect of staking technologies. The features of joint formation and design are useful for engineers to tailor the process according to the application and cost limits of a product or project.

List of Abbreviations

BM	base material
CFD	computational fluid dynamics
DSC	differential scanning calorimetry
E-ICJ	electrical-heating injection clinching joining
F-ICJ	friction-based injection clinching joining
FEM	finite-element method
FSW	friction stir welding
FT-IR	Fourier-transform infrared spectroscopy
HACS	hot air cold staking
HAZ	heat-affected zone
ICJ	injection clinching joining
MIM	metal injection molding
OIT	oxidation induction time
PA66	polyamide 66
PDZ	plastically deformed zone
PEI	polyetherimide
PMMA	poly(methyl methacrylate)
PP	polypropylene
PTFE	polytetrafluoroethylene
SEC	size-exclusion chromatography
T_g	glass transition temperature
TGA	thermogravimetric analysis
T_m	crystalline melting temperature
TMAZ	thermomechanically affected zone
UHMWPE	ultrahigh-molecular-weight polyethylene

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Part III

Joining Processes Based on Direct-Assembly Methods

10

Injection Overmolding of Polymer–Metal Hybrid Structures

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10.1 Basics of Polymer–Metal Hybrid Technology

In the traditional automotive manufacturing practice, a choice has to be typically made between the use of metals and plastics for various structural and nonstructural applications. This paradigm is gradually being shifted with the introduction of polymer–metal hybrid (PMH) structures in which metals and polymers are integrated in a singular component/subassembly. The main motivation behind the introduction of the PMH technology into the automotive manufacturing practice is to, through the application of a system-level approach, combine various requirements of several adjacent components into a singular component/subassembly (typically consisting of a metal-stamping core and plastic injection-molded overcoat containing multiple ribs) in order to deliver a customer-specific solution/function [1–10]. When the integration of metals and polymers into a single component/subassembly is done successfully, greater system-level benefits are obtained relative to those attained through simple merging/joining of the proximal parts. In fact, several patented PMH design/manufacturing technologies have already proven their ability to allow the automotive original equipment manufacturers (OEMs) and their suppliers to engage flexible assembly strategies, decrease capital expenditures, and reduce labor required to manufacture a vehicle.

An example of a PMH automotive body-in-white (BIW) load-bearing component is depicted in Figure 10.1(a) and (b). The component in question is generally referred to as the “*rear longitudinal beam*,” which connects, on the front end, to the rocker panel, in the middle to the shock tower, while at the rear end, it connects to the rear cross beam. The traditional all-steel design of this component is displayed in Figure 10.1(a) and includes three subcomponents: (1) main U-shaped channel beam; (2) a reinforcement plate; and (3) a cover plate. The latter two subcomponents are spot welded to the first one. It should

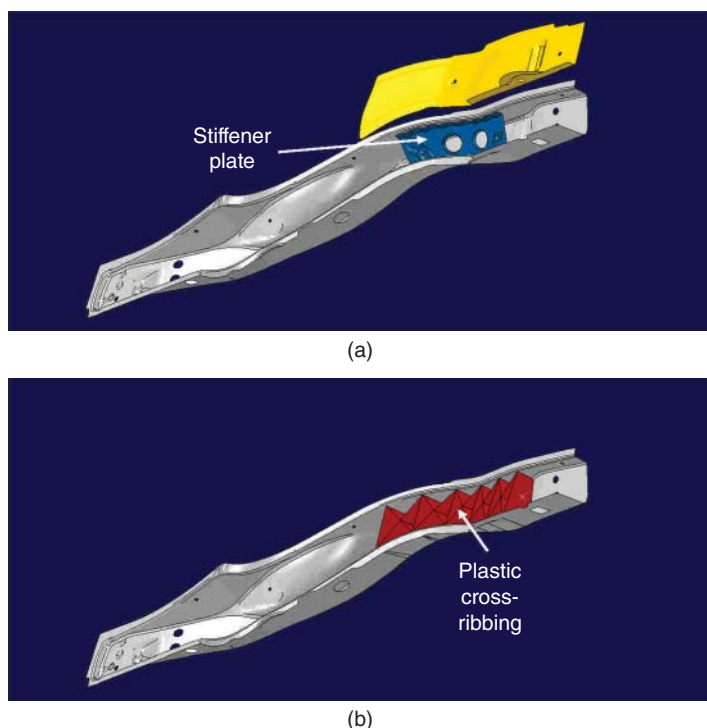


Figure 10.1 An example of the: (a) all-metal and (b) polymer–metal hybrid (PMH) load-bearing automotive component.

be noted that the cover plate is slightly shifted in Figure 10.1(a) in order to reveal the location of the reinforcing plate. The PMH rendition of the same component is depicted in Figure 10.1(b). The reinforcement plate has been replaced with an injection-molded thermoplastic cross-ribbed substructure, while the thickness of the cover plate (not shown in Figure 10.1(b) for clarity) is reduced.

The basic concept utilized in all PMH technologies is illustrated in Figures 10.2(a) and (b). An open-channel thin-wall sheet-metal component can readily buckle under compressive load, Figure 10.2(a). With very little lateral support, provided by a thin-wall cross-ribbed injection-molded plastic subcomponent, Figure 10.2(b), the buckling resistance (and the stiffness) of the component can be greatly increased (while the accompanying weight increase is relatively small).

Among many technical and economic benefits associated with the use of the PMH technologies, the following appear to be the most important: (1) reduction of the number of components; (2) production of the integrated components ready to assemble; (3) weight reduction compared to the traditional all-metal solutions; (4) additional design and styling freedom;

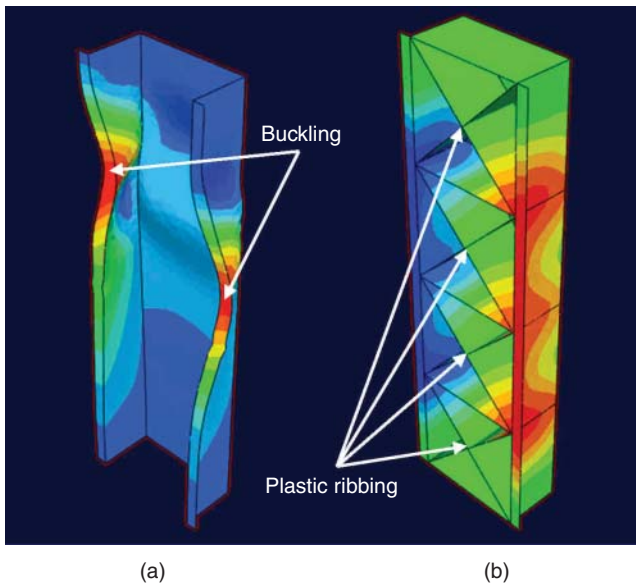


Figure 10.2 The basic concept utilized in the PMH technologies. Buckling in an open-channel all-metal component in (a) has been prevented by a rib-like plastic substructure in (b), which provides the needed lateral support.

(5) production of in-mold features such as brackets, bosses, and attachment points; (6) safety improvement due to lowered center of gravity of the vehicle; (7) a major (several-fold) increase in the bending strength of stamped metal sections. This effect is well understood and is attributed to the plastic sub-component, which forces the metal to maintain its cross-sectional properties throughout the loading cycle and delays the onset of failure due to localized buckling; and (8) improved damping in the acoustic range (relative to their all-steel counterparts, often as high as four times lower than the initial decibel reading measured in a simple hammer-strike test).

The first publicly reported case of the introduction of the PMH technology into the automotive manufacturing practice is the PMH front end of the Audi A6 [11]. This component was produced by Ecia, Audincourt, France, by injection overmolding a sheet-metal stamping with a cross-rib-shaped structure made of elastomer-modified polyamide PA6-GF30 (type: Durethan BKV 130 from Bayer). Through direct mechanical testing of the PMH front end, it was demonstrated that the two constituent materials (i.e., metal and plastics) act synergistically to impart the mechanical performance to the PMH component, which is not found in either of the two materials, per se.

Over the last few years, there has been an accelerated trend to replace all-steel structures in automotive front-end modules with their PMH counterparts.

Besides their use in automotive front-end applications, PMH structures are finding an increased use in other automotive (e.g., instrument-panel and bumper cross-beams, door modules, tailgates, etc.) and nonautomotive (e.g., appliance housings, bicycle frames, etc.) applications. In addition to an increased use of the PMH structures in automotive and nonautomotive applications, new PMH technologies are being developed as alternatives to the classical overmolding method first established by Bayer/Ecia [11, 12].

10.2 Classification of PMH Technologies

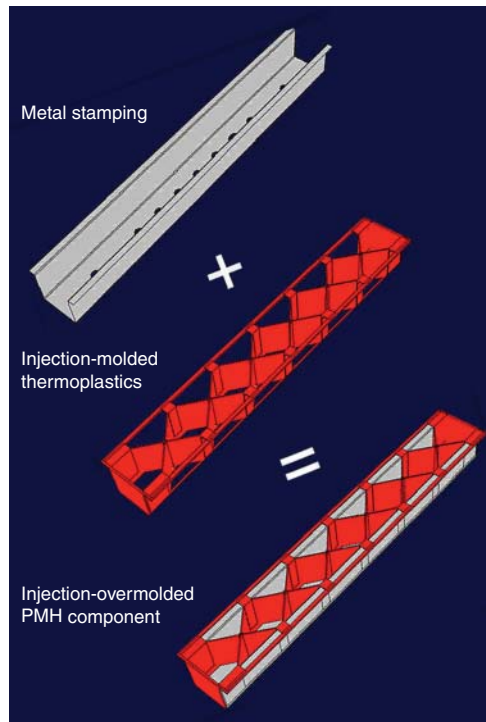
Examination of all PMH technologies currently being employed in the automotive and nonautomotive industries suggests that four major categories can be defined: (1) Injection overmolding technologies; (2) metal overmolding technologies combined with secondary joining operations; (3) PMH technologies involving adhesive bonding; and (4) direct-adhesion PMH technologies. Each of these categories of PMH technologies is briefly described next.

10.2.1 Injection Overmolding PMH Technology

This process was originally introduced and patented by Bayer [12]. The process involves the following steps: (1) sheet-metal blanks are stamped to obtain desired (typically, U) shape of the metal inserts; (2) flared through-holes are punched into the metal inserts; (3) inserts are next placed in the injection-molding die; and (4) injection molding is used to overmold the metal inserts with a cross-ribbed integrated structure made of 30% short glass-fiber-filled polyamide (i.e., nylon-6). In this process, tight interlocking between the metal insert and the short-fiber-filled nylon cross-ribbed structure and the attainment of a superior combination of the PMH component stiffness and buckling resistance are achieved through: (1) formation of rivets from the molten nylon, which penetrated the insert through-holes; and (2) overmolding of the U-shaped insert flanges. A solid model of the simplified PMH component (consisting of an overmolded cross-ribbed nylon structure and a U-shaped metal stamping) produced by injection overmolding is displayed in Figure 10.3.

In the earlier renditions of this PMH technology, the overmolded cross-ribbed structure was limited to (extruded) two-dimensional geometries, due to the fact that an injection-molding press normally opens in only one direction. In more recent renditions of this PMH technology, side motion of the tooling has been added, which enabled fabrication of multidirectional ribbed structures. These modifications in the cross-ribbed reinforcing nylon structure were found to yield significant improvements in the load-bearing capability of the PMH component relative to its U-shaped steel-stamping counterpart.

Figure 10.3 Exploded and integrated views of a prototypical injection overmolded (simplified) load-bearing automotive body-in-white (BIW) PMH component.



10.2.2 Metal Overmolding PMH Technology

This PMH technology was originally developed and patented by Rhodia [13] and employed for the front-end module of a 2004 light truck intended for the South American market. The manufacturing process involves the following main steps: (1) a U-shaped steel stamping is placed in an injection mold and the underside of the stamping coated with a thin layer of short-fiber-filled nylon; (2) a separate injection-molding operation is used to fabricate a cross-ribbed nylon structure; and (3) finally, in a secondary operation, the overcoated metal stamping and the cross-ribbed nylon structure are joined using ultrasonic welding. The main benefits of this PMH technology are as follows: (1) potential for the fabrication of closed-section PMH components, which, due to the continuous nature of the metal/polymer bond lines, exhibit a high load-bearing capability; and (2) enabling functional integration such as cable housings and air or water channels due to the hollow core of the PMH structure. In a more recent rendition of this PMH technology, gas or water injection molding is employed in order to produce a stiffer, thinner coating for enhanced load-bearing capabilities and increased functional integration.

Another, low-cost rendition of this technology is the so-called Plastic–Metal Assembling process, also developed and patented by Rhodia [14]. The process comprises three main steps: (1) within the first step, a U-shaped steel stamping with punched holes and a nylon injection-molded component, which contains columns or heat stakes that can lock into the stamping holes, are produced separately; (2) next, the two subcomponents are brought into contact while ensuring that the nylon columns penetrate the stamped holes; and (2) finally, the two subcomponents are joined using ultrasonic welding or heat staking to form locking rivets from the ends of the plastic columns.

10.2.3 Adhesively Bonded Polymer–Metal Hybrid Structures

This type of PMH technology was developed and patented by Dow Automotive [15] and introduced in 2003 in prototype form for a Volkswagen front-end module. This process involves three main steps: (1) a separate fabrication of the metal stamping and the injection-molded cross-ribbed structure made of short-glass-fiber-filled polypropylene; (2) application of Dow's proprietary low-energy surface adhesive (LESA) to the subcomponents to be joined using high-speed robots; and (3) curing of the resulting adhesive joint. Often, to help maintain alignment of the subcomponents during curing of the adhesive, snap features are designed into the subcomponents.

The main additional advantages of this PMH technology are as follows: (1) the acrylic-epoxy adhesive LESA does not require cleaning or other types of pretreating of the low surface-energy polypropylene; (2) adhesive bonding creates continuous bond lines, minimizes stress concentrations, and acts as a mechanical buffer, which absorbs and delocalizes contact stresses between the metal and polymer subcomponents; (3) adhesively bonded PMHs enable the creation of closed-section structures, which offer high load-bearing capabilities and the possibility for enhanced functionality (e.g., direct mounting of air bags in instrument-panel beams or incorporation of air or water circulation inside door modules); and (4) through proper selection of the LESA grade, adhesively bonded PMH structures can be optimized with respect to their stiffness, buckling resistance, toughness, adhesion strength, and cure time.

10.2.4 Direct-Adhesion Polymer–Metal Hybrid Technology

This PMH technology is currently being developed [16, 17] and is aimed at addressing some of the main limitations of the aforementioned three PMH technologies. Among a number of limitations/shortcomings of the current PMH technologies, the following appear to be most noteworthy: (1) the injection overmolding process requires the presence of through-holes in the metal stamping for the formation of the interlocking rivets. However, the presence of these holes may compromise the functionality and/or mechanical performance (or even structural integrity of the critical load-bearing)

PMH components; (2) the injection overmolding process also requires overmolding of the metal-stamping flanges for effective metal-to-polymer interlocking. Since these flanges are often needed for spot-welding purposes, their overmolding may not be allowed; and (3) in the case of adhesively bonded PMH structures, the high cost of the adhesive, the relatively long curing time, and the limited ability of the adhesive to withstand aggressive chemical and thermal environments encountered in the paint shop during BIW pretreatment and E-coat curing may be considered as potential limitations.

Within the direct-adhesion PMH technology, the joining between the metal and thermoplastic subcomponents is attained through direct-adhesion of injection-molded thermoplastic cross-ribbed structure to the metal without the use of interlocking rivets, overmolded edges, or structural adhesives [1]. Within this PMH technology, to attain a desired level of polymer-to-metal adhesion strength, advantage is taken of various mechanical, physical, and chemical phenomena and processes. As reported in Ref. [2], polymer-to-metal direct-adhesion technologies can be classified as:

- (a) Technologies relying on surface roughness length-scale polymer/metal mechanical interlocking phenomena (e.g., [18–20]). While still of a mechanical interlocking character, the mechanism of plastic-to-metal joining in the case of these PMH direct-adhesion technologies is distinct from that found in the standard *insert overmolding* process, which relies on the shrink-fit phenomenon and special undercut geometrical features for good polymer-to-metal load transfer. In the case of direct-adhesion PMH technologies relying on polymer/metal mechanical interlocking phenomena, polymer/metal interlocking occurs by the infiltration of the micron-size roughness features of the metal substrate by the molten plastic and, upon solidification, the formation of mechanical micron-size interlocks. It is well established [18–20] that for successful polymer/metal joining, metal subcomponent preheating is extremely critical. It was suggested in [2] that metal subcomponent preheating can be effectively achieved by integration of an induction heater into the injection-molding mold;
- (b) Technologies employing in-coil or stamped-part metal priming with adhesion promoters (e.g., [21, 22]). The most frequently used primer is silane, which, owing to its amino and vinyl functional groups, acts as a “*coupling agent*,” which promotes adhesion between inorganic (metallic, in the present case) and organic (polymeric, in the present case) materials. For silane to act as an adhesion promoter, its organoreactive moieties must be in contact with polymer and metal, which is achieved by coating the metal substrate with silane just prior to injection molding of the polymer [23]. It is generally believed that the silane coupling reactions take place in the following sequence: (1) hydrolysis of the alkoxy group, which results

in the production of hydrogen; (2) formation of the hydrogen bonds at the polymer/metal interface; (3) interfacial condensation of the functional groups; and finally, (4) interfacial chemical reactions with the polymer and metal resulting in the formation of interfacial bonds [24];

- (c) Technologies based on chemical modifications of the injection-molding thermoplastic material for enhanced adhesion to metal (e.g., [25, 26]). Efforts have been reported in the open literature involving modification of either polymerized thermoplastic material (through the formation of polymer blends, [25]) or at the monomer level (through direct changes in the monomer chemistry). In the work reported in Ref. [10], polyamide was chemically modified by blending it with self-ordering poly-(ester amide) block copolymer (a hot-melt adhesive-like material). The resulting polymer blend was found to exhibit an exceptionally high adhesion strength (>20 MPa) even in the cases in which metal surfaces were not precleaned and were left covered with drawing compound/oil prior to injection overmolding.

An example of chemical modification of the thermoplastic resin for enhanced polymer/metal adhesion was reported in Ref. [26]. In this work, the effect of direct addition of various concentrations of styryl silane to styrene (monomer) resin on the ability of the resin to directly bond to aluminum upon polymerization was investigated. It was found that the concentration of styryl silane in styrene resin affected the following: (1) the thickness of the polymer-to-metal bonding interface; (2) the polymer/metal adhesion strength; and (3) the bond-strength sensitivity to the presence of moisture. In addition, the results revealed that metal-surface preparation via either chromic–sulfuric acid etching or phosphoric acid anodization is highly critical for attaining good polymer-to-metal adhesion. This observation was rationalized by the role of etching surface treatment in ensuring that a sufficient density of binding sites is available to provide grafting or tethering of the polymer interfacial layer to the metal surface.

The thickness of the interfacial layer, which is controlled by the concentration of silane in the styrene monomers, has been found to have a dominant effect on the adhesion strength. Specifically, when the thickness of the polymer interfacial layer becomes comparable with the average distance between the polymer-chain entanglement points, the polymer-to-metal adhesion strength attains its maximum value;

- (d) The so-called *clinch-lock* PMH technology [8], which utilizes some ideas from the spot-clinching sheet-metal mechanical fastening/joining process. Specifically, stamping is used to produce shallow millimeter-size *dove-tail*-shaped impressions/indentations into the metal subcomponent/stamping. These impressions ensure that the subsequently injection overmolded thermoplastic subcomponent is securely anchored to the metal

subcomponent. The joint provides effective metal/polymer connectivity by at least two distinct mechanisms: (1) mechanical interlocking and (2) enhanced adhesion due to an increased metal/polymer contact surface area; and

- (e) Other approaches aimed at enhancing polymer-to-metal direct adhesion through physical and chemical modifications in the metal subcomponent surface [27–30]. For example, in Ref. [29], Openair[®] plasma is utilized to modify metal subcomponent surface via the combination of the following mechanisms: (1) surface cleaning, for example, the removal of organic contaminants; (2) ablation, which removes weakly bonded surface layer; and (3) potential chemical modification due to enhanced surface reactivity and the potential for the occurrence of surface chemical reactions.

10.3 Mechanisms for Polymer/Metal Joining

It is well established (e.g., [1]) that structural/functional performance of a PMH component depends greatly on the extent of load transfer through the polymer/metal interfaces, which, in turn, is controlled by the mechanism and strength of polymer-to-metal joining. Hence, it is important to identify and understand the nature of this joining across the four aforementioned groups of PMH technologies.

10.3.1 Injection Overmolded PMH Structures

In this case, polymer-to-metal load transfer is carried out through purely mechanical component length-scale polymer/metal joints. These joints rely on the operation of shrink-fit phenomena and on the formation of mechanical interlocks promoted by the presence of special undercut geometrical features within the metal subcomponent.

10.3.2 Metal Overmolded PMH Structures

In this case, it is the interface between the coating and the metallic subcomponent that plays a critical role in the load transfer, since the interface between the injection-molded polymeric subcomponent and the coating, for example, after ultrasonic welding, is effectively seamless. As it will be discussed next, in conjunction with the direct-adhesion PMH technologies, there are a number of potential (mechanical and chemical) polymer/metal adhesion mechanisms.

10.3.3 Adhesively Bonded PMH Structures

In this case, polymer-to-metal interface is replaced with a thin-layer structural interphase. Due to the presence of a large number density of interfacial

covalent bonds, the interphase layer is typically stiff and strong and enables almost complete load transfer between the two PMH components.

10.3.4 Direct-Adhesion PMH Structures

As reviewed earlier, there are several direct-adhesion PMH technologies, and they rely on different polymer/metal joining mechanisms. For example, one class of direct-adhesion PMH technologies relies on the formation of surface roughness length-scale polymer/metal mechanical interlocks (e.g., [18–20]), which are formed as a result of the infiltration of the surface roughness features of the metal substrate by, and subsequent solidification of, the molten plastic. On the other hand, in the case of the direct-adhesion PMH technology, which employs metal surface priming, as in the case of cold-rolled mild steel-stamping primed with amino silane and overmolded with poly-(vinyl chloride), PVC [21], the polymer/metal joining mechanism is rationalized as follows: (1) amine hydrochloride complexes appear to form by protonation of amino groups of the silanes with HCl that was liberated from PVC during the onset of thermal dehydrochlorination; (2) furthermore, quaternization or nucleophilic substitution of labile pendant allylic chloride groups by amino groups on the silanes takes place, thus grafting PVC onto the amino silanes. It was determined that PVC having β -chloroallyl groupings along its chains showed better adhesion with steel precoated with amino silanes; and (3) interdiffusion of the polymer phase and the silane phase was also found to be critical in obtaining good adhesion.

10.4 Computational Engineering Analyses of PMH Technologies

In order to assess the potential of PMH technologies for use in load-bearing automotive BIW structural components, various multidisciplinary computational methods and tools have been utilized by a number of researchers, designers, and manufacturing engineers (e.g., [1, 31]). The analyses cover the following aspects of the PMH component design, fabrication, performance, and end-of-life considerations:

- (a) Application of the engineering design optimization methods and tools to the design of an automotive BIW PMH component, which meets functional requirements (e.g., those related to stiffness, strength, and buckling resistance) while accounting for the component manufacturability constraints;
- (b) Detailed computational fluid dynamics numerical simulation of the filling (including flow-induced changes in fiber orientation), packing, and cooling stages of the injection-molding process used to fabricate

PMH short-fiber-filled polymeric subcomponent; and an anisotropic thermoviscoelastic computation of the thermally and pressure-induced (in-mold) stresses in an injection-molded short-fiber-filled polymeric subcomponent;

- (c) Structural mechanics analysis (based on the use of multilayer shell elements) of shrinkage and warping caused by the relaxation of the in-mold stresses after polymeric-subcomponent (in the case of metal overmolding or adhesive-bonding-based PMH technologies) or hybridized-subcomponent (in the case of injection overmolding and direct-adhesion-based technologies) ejection from the injection-molding mold; and
- (d) Structural mechanics analysis (including the effect of adhesion-based load transfer between metallic and polymeric subcomponents) of the PMH component stiffness and strength under several simple monotonic loading modes and under creep.

In the remainder of this section, a brief overview of these computational analyses is provided.

10.4.1 PMH Component Design and Optimization

Due to ever-more restrictive lightweight targets and the demands for shortened product development time-scale in the automotive industry, a continuous need has arisen for an integration of advanced computer-aided optimization methods into the overall component/subassembly design process. This is particularly true in the case of structural load-bearing PMH BIW automotive components. In most cases, the design of the load-bearing PMH components is driven not only by stiffness and buckling-resistance requirements but also by strength requirements (e.g., to obtain the required performance in side-impact collisions).

Automotive BIW structural PMH components are typically designed using the following finite-element-based two-step engineering design-optimization procedure: (1) topology optimization is performed first to obtain a general idea about an optimal configuration of the BIW component in question, which ensures mass-efficient load paths; and (2) the component topology obtained in (1) is next interpreted to form an engineering design, which is then optimized under real functional requirements, using nonlinear finite-element-based, detailed size- and shape-optimization methods and tools. Within these optimization procedures, geometrical and material aspects of the PMH component are treated as design variables, the objective functions are defined in terms of the functional performance requirements (as typically quantified by the required levels of stiffness, strength, or buckling resistance) while constraints are generally associated with component manufacturability, material compatibility with the BIW manufacturing process chain, cost, and

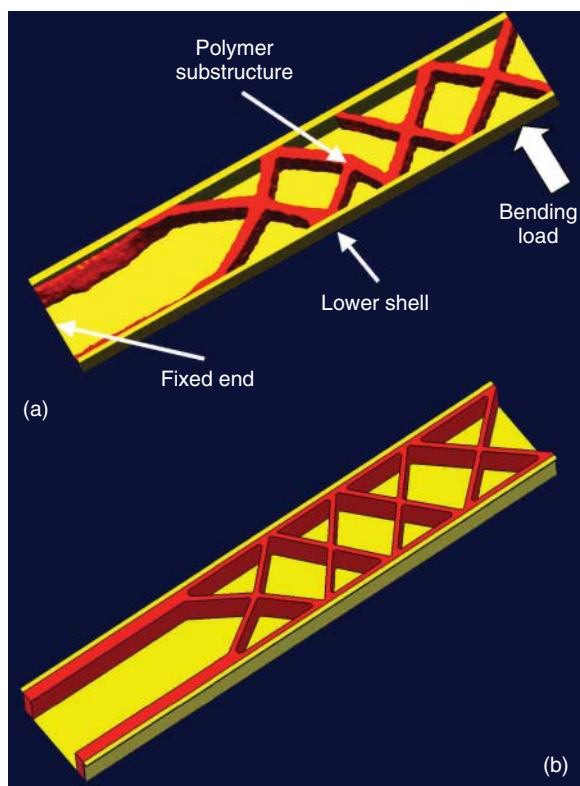


Figure 10.4 Results of strength-based: (a) topology; and (b) detailed-design optimization procedures for a simplified automotive BIW structural PMH component.

so on. Examples of the results obtained using strength-based topology and detailed-design optimization procedures for a simplified automotive BIW structural PMH component are shown in Figure 10.4(a) and (b), respectively.

10.4.2 Modeling and Simulations of the Injection-Molding Process

Thermoplastic injection molding is a widely used manufacturing process for producing parts/components with a high degree of geometrical complexity. A typical injection-molding process involves four distinct stages: (1) *filling* of the mold with molten thermoplastics; (2) *packing* – injection of additional material into the mold under high pressure to compensate for the cooling-induced volumetric shrinkage of the material; (3) *cooling*, which gives rise to the solidification of the material residing in the mold; (4) *ejection* of the solidified part/component from the mold. During the filling, packing, and cooling stages of the injection-molding process, the material is subjected to complex thermomechanical loading, which gives rise to the changes in local specific volume (density), component shape as well as to the development of the in-mold stresses within the component. In other words, while

the (thin-wall) component resides in the mold, it is constrained by the mold causing internal stresses to develop within the component during solidification of the melt and subsequent cooling. Upon ejection, these stresses relax causing distortion/warping and further shrinkage of the molded component. Further warping and shrinkage of the component may occur during cooling to room temperature of the ejected molded component.

To take into account the fact that, usually, most of the injection-molded plastic subcomponents are made of short-fiber-filled thermoplastics, and hence, may possess a heterogeneous, nonisotropic material, the following injection-molding process simulation subanalyses are generally carried out: (1) identification of the optimal placement and the number of thermoplastic-melt injection points; (2) mold filling; (3) melt-flow-induced changes in the fiber-orientation distribution; (4) mold packing; and (5) in-mold stresses. These subanalyses are briefly reviewed next.

10.4.2.1 Optimal Placement and Number of Injection Points

Before simulations of the injection-molding process can be carried out, the optimal placement and the number of injection points (gates) have to be determined. To determine an optimum number and location of the gates, a constrained optimization analysis is typically employed within which optimum values of the objective function (the degree of balanced flow, which ensures that regions within the mold that are furthest away from the gate(s) are filled at approximately the same time [32]), is attained through the selection of the number and location of injection points (design variables) while meeting the constraints imposed by: (1) the component geometry; (2) the properties of the thermoplastic melt; (3) the specified injection-molding process parameters; and (4) injection-molding feasibility (i.e., successful filling of the sections associated with the minimum plastics-wall thickness).

10.4.2.2 Mold-Filling Analysis

Earlier computational efforts reported in the literature were mainly focused on predicting pressure and temperature distributions within the mold cavity and melt-front advancement during mold filling [33–38]. More recent computational efforts, on the other hand, have also addressed postfilling phenomena such as flow-induced changes in the fiber-orientation distribution and the development of in-mold stresses within the component [39, 40]. Furthermore, while the early efforts employed mainly empirical material and melt/mold interaction models (e.g., [40]), the more recent computational investigations employed more physically based material models and contact algorithms (e.g., [41]).

Within the mold-filling analysis, the three basic conservation equations, that is, the mass, momentum, and energy conservation equations, are integrated spatially and temporally using a (typically explicit) numerical scheme. In the

case of semicrystalline polymeric materials, the aforementioned partial differential equations have to be combined with an additional (differential or algebraic) equation defining the rate of crystallization.

It should be noted that, when a mold-filling computational analysis involves short-fiber-filled thermoplastics, the melt-flow local field is generally assumed to be independent of the orientation distribution of the fibers. On the other hand, the flow field causes reorientation of the fibers and changes in their local orientation distribution. Strictly speaking, the exclusion of the effect of fiber orientation on the local flow field is justified only in the case of injection molding of the thin-walled components, in which the fibers are oriented nearly parallel to the plastic-wall midplane and, hence, their interaction with the melt flow is limited [42–47]. The conditions that have to be satisfied in order for the influence of the fiber distribution function on the flow to be neglected can be found in Ref. [48].

Since injection molding of PMH subcomponents or overmolding of the PMH components involves melt flow through thin mold-cavity channels, through-the-thickness variations in pressure are generally neglected, and the melt flow is treated to be of a Hele–Shaw flow character [35]. Consequently, mold-filling analysis is simplified and involves not the direct solution of the governing conservation equations but rather a solution of the pressure-based (elliptical partial differential) Hele–Shaw flow equation [35]. When solving the Hele–Shaw flow equation, the shear-rate, pressure, and temperature dependencies of the material viscosity must be specified. This is typically done using the Cross model [41]. Furthermore, the following boundary conditions are typically used in conjunction with the Hele–Shaw equation: (1) either the inlet-flow rate or the pressure boundary conditions are defined at the injection points; (2) a zero-pressure condition is defined on the advancing flow front; and (3) a zero-normal-pressure gradient is specified over the mold-cavity surface. While these boundary conditions do not generally ensure a *no-slip* condition over the mold-cavity surface (and may allow the fluid to *slip*), the resulting inaccuracies in the velocity-field predictions are typically found not to be significant [49]. Since the Hele–Shaw flow equation considers only the flow parallel with the local midplane, it does not account for the fountain flow and may lead to inaccuracies in the temperature and fiber-orientation predictions. These inaccuracies are generally mitigated using one of the local approximations [37].

One of the results of the mold-filling analysis is determination of the instantaneous location of the flow front. Typically, the flow front is tracked by discretizing the mold cavity into a large number of control volumes and by determining the state of filling of each control volume. An example of the mold-filling analysis results showing spatial distribution of the local filling time for the case of a vehicle PMH front-end module is shown in Figure 10.5. (Five

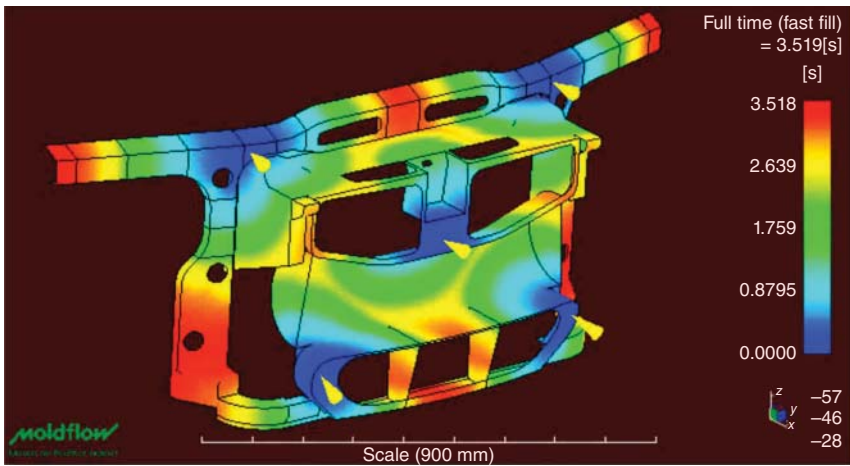


Figure 10.5 An example of the mold-filling analysis results showing spatial distribution of the local filling time for the case of a vehicle PMH front-end module. (Please note that metallic stampings are not visible since they are placed within the mold cavity.)

injection ports are marked as white cones.) It should be noted that metallic stampings are not visible since they are placed within the mold cavity.

Another result of mold-filling analysis is the temporal evolution of the temperature field. This result is obtained by numerically solving the energy conservation equation in which the heat convection and viscous dissipation terms from a previous time step are treated as source terms during the current time step. Furthermore, to account for high rate of heat conduction through the metal subcomponent (in the case of injection overmolding and direct-adhesion PMH technologies) or over the injection-mold internal surfaces (in the case of metal overmolding and adhesion-bonding PMH technologies), time-dependent, uniform temperature-based boundary conditions (determined using a separate boundary element analysis [50]) are employed. It should be noted that the use of this boundary condition assumes temperature continuity at the polymer/metal and polymer/mold interfaces. In other words, the effect of interfacial heat conductance is neglected.

10.4.2.3 Flow-Induced Fiber-Orientation Distribution Analysis

As mentioned earlier, melt flow through the mold cavity causes reorientation of the fibers and changes in their local orientation distribution. For accurate predictions of the shrinkage and warping of an injection-molded component made of short-fiber-filled thermoplastics, knowledge of the flow-induced fiber-orientation distribution throughout the component is critical (e.g., [51–53]). Since most commercial short-fiber-filled thermoplastics commonly used for injection molding can be characterized as semi- or highly concentrated

suspensions, fiber/fiber interactions and the associated spatial constraints to the fiber motion may significantly affect the final fiber-orientation distribution in the injection-molded component. Typically, fiber/fiber interactions are accounted for computational analysis of the injection-molding process through the use of the Folgar–Tucker model [51]. In this model, an isotropic symmetric second-order fiber/fiber interaction tensor is introduced in the diffusion term of the equation of motion for an isolated fiber in a Newtonian fluid [54]. The components of this interaction tensor, as a function of the initial fiber-orientation distribution, fiber aspect ratio, the number density of fibers in the suspension, the melt properties, and the shear–strain magnitude, are assessed using direct numerical simulations of fiber/fiber interactions within simple-shear flow [52]. In these simulations, short-range interactions are quantified using a lubrication model [55] while long-range interactions are calculated using a boundary element method [55].

Once the components of the interaction tensor are determined for a given short-glass-filled thermoplastic polymer melt, they are used, throughout the mold cavity, within an anisotropic rotary diffusion equation to define the local rate of change of the fiber-orientation distribution function as quantified by the second-order fiber-orientation distribution tensor. Time integration of this rate of change gives temporal evolution of the fiber-orientation distribution function during mold filling.

10.4.2.4 Mold-Packing Analysis

As mentioned earlier, mold packing involves injection of additional melt into the mold under high pressure to compensate for the cooling- and solidification-induced volumetric shrinkage of the material. While the packing phase of the injection-molding process is governed by the same conservation equations as the filling phase, an additional equation, the equation of state, must be defined in order to include the effect of melt compressibility. The equation of state typically used in mold-packing analysis defines a functional relationship between the pressure, specific volume, temperature, and cooling rate.

It should be noted that the presence of the cooling rate term in the equation of state enables modeling of various phase transformations (such as freezing, crystallization, and ductile-to-glass transition) accompanying the packing process. Furthermore, it should be noted that various material properties such as volumetric thermal expansion coefficients and compressibility, and their temperature and pressure dependencies, are derived from the equation of state.

10.4.2.5 In-Mold Stress Analysis

There are two main sources for in-mold stresses in injection-molded components: (1) viscoelastic deformations of the thermoplastic material during filling/packing can give rise to the development of the so-called *flow-induced* in-mold stresses; and (2) restrictions to the (often inhomogeneous) cooling-

and solidification-induced shrinkage of the polymer due to the mold walls and the applied packing pressure may lead to the generation of the so-called *thermally and pressure-induced* in-mold stresses. It is generally assumed that the flow-induced in-mold stresses are relatively small and that they are readily relieved while the component resides in the mold at high temperatures prior to ejection. Consequently, they are typically neglected in an in-mold stress analysis. As far as the thermally and pressure-induced in-mold stresses are concerned, they have been extensively investigated computationally (e.g., [56–65]). These investigations clearly revealed the effects of mold-wall constraints and thermoplastic material properties on the extent and distribution of the in-mold stresses.

Computation of the in-mold stresses entails the knowledge of high-fidelity material models (in particular, the time-dependent portion of the material model). This is related to the fact that as the injection-molded component begins to cool inside the mold, the relaxation time of the thermoplastic material starts to increase and to approach the in-mold component resident time. Due to the small magnitude of the attendant in-mold strains, the thermoplastic material behavior can be satisfactorily represented using an anisotropic linear thermoviscoelastic material model (e.g., [66, 67]). Typically, within such models, the viscous portion of the material response assumes interchangeability between the time and temperature effects. In other words, materials are assumed to be *thermorheologically simple*. It should be noted that this assumption may not be fully justified in the case of short-fiber-filled polymers used in PMH structures. For amorphous polymers, this time/temperature interchangeability is generally represented using the Williams–Landel–Ferry (WLF) equation [68]. On the other hand, for semicrystalline materials, this interchangeability is based on an Arrhenius-type expression (e.g., [1]).

As established earlier, since the thermoplastic material used in PMH components is typically filled with short fibers and the flow causes the orientation distribution function to deviate from a random one, the material locally behaves anisotropically. To quantify anisotropic aspects of the material behavior from the knowledge of the polymeric melt and fiber properties as well as from the knowledge of the fiber-orientation distribution function, one typically employs one of the micromechanics-based homogenization procedures. A brief discussion of these procedures is presented in the next section.

Once the appropriate material model has been constructed, temporal evolution of the in-mold stresses can be determined by carrying out a time-dependent thermoviscoelastic structural analysis. Within this analysis, the temperature field is imported from the filling and packing analyses.

To simplify in-mold stress analysis, the following assumptions/simplifications are typically used: (1) through-the-wall-thickness normal stress is locally constant in the through-the-thickness direction; (2) as long as through-the-wall-thickness normal stress is compressive, the injected polymer is considered to

be in contact with the metal subcomponent/mold; (3) locally, a component is fully constrained within the midplane, and hence, the only nonzero strain component is the one in the through-the-thickness direction; and (4) metallic subcomponent/mold is assumed to be rigid.

The in-mold stress analysis is typically carried out under the following stress-based boundary conditions:

- (a) When the component resides in the mold and the injected material contains both a solid outer layer and a liquid core, the through-the-thickness normal stress is set equal to the negative fluid pressure; or
- (b) When the component resides in the mold and the injected material has completely solidified, the component may either be in contact with the metal subcomponent/mold or be separated from it. In the first case, the through-the-thickness normal stress is determined using the condition that the average through-the-thickness normal strain is zero. In the latter case, the through-the-thickness normal stress is set to zero.

10.4.2.6 Micromechanics-Based Derivation of the Effective Material Properties

As established earlier, glass-filled polymeric materials used in PMH components become anisotropic during mold filling due to flow-induced changes in the (initially random) orientation distribution of the fibers. Typically, micromechanics-based homogenization models are utilized to derive (anisotropic) elastic and thermoelastic properties of fiber-filled thermoplastic materials used in PMH technologies from the knowledge of the properties of the constituent fiber and matrix materials and the known fiber-orientation distribution function [69]. It is generally assumed that the injection-molded material is transversely isotropic, that is, its properties are equal in the transverse and the through-the-thickness directions. Consequently, the elastic response of such materials is defined by five (temperature-dependent) elastic moduli while the thermoelastic response is defined in terms of two (longitudinal and transverse) linear coefficients of thermal expansion.

The (homogenized and isotropic) elastic and thermoelastic properties of fiber-filled thermoplastics are typically assessed using the following two-step micromechanics procedure: (1) first, the properties of a material, in which the fibers are perfectly aligned, are assessed using a homogenization scheme within which the material at hand is considered as an aggregate of discrete constituent materials (e.g., [69, 70]); and (2) next, an orientation averaging procedure is applied to include the effect of the attendant fiber-orientation distribution on the effective elastic and thermoelastic material properties (e.g., [71]).

10.4.3 Ejected-Component Shrinkage and Warping Analysis

While the injection-molded material resides in the mold, it is constrained and cannot distort. However, after ejection, the component can undergo shrinkage

and warping. On the other hand, in the case of an ejected PMH component, the thermoplastic subcomponent remains somewhat constrained by its adhesion to the metallic subcomponent/stamping.

The same thermoviscoelastic structural mechanics analysis used to determine in-mold stresses is often employed in order to analyze shrinkage and warping of a polymeric subcomponent (in the case of metal overmolding or adhesive-bonding-based PMH technologies) or hybridized subcomponent (in the case of injection overmolding and direct-adhesion-based technologies) after ejection from the injection-molding mold. Since the shrinkage/warping analysis is generally not carried out within the mold-filling analysis but rather within a separate structural mechanics finite-element program, the spatial (including through-the-thickness) variations in thermomechanical material properties (a material-model definition) and the in-mold stresses (initial conditions) have to be imported from the injection-molding process analysis (where they were originally computed). After the part is ejected from the mold, no external loads are applied to it, and hence, the following boundary conditions are employed: (1) six (three translational and three rotational) degrees of freedom of one of the ejected component material points are constrained in order to prevent uncontrolled rigid body motion of the component; and (2) zero-traction boundary conditions are applied over the ejected-component surfaces.

10.4.4 PMH Component Structural Analysis

The ejected (warped) PMH component, after cooling to room temperature, is subjected to a series of structural (quasistatic and dynamic) finite-element analyses in order to validate its functionality and assess its mass efficiency. This is typically done by comparing the performance of the PMH component against the performance of the corresponding all-metal component, the PMH component is intended to replace. An example of the results obtained in such analyses involving an idealized load-bearing component [10] is displayed in Figure 10.6(a)–(f).

The all-metal rendition of this component (used as a control) consists of a flanged U-shaped stamping and a cover plate (spot-welded) along the length of the flanges, Figure 10.6(a). Within the finite-element analysis employed, each spot weld is modeled as a kinematic constraint distributed over a circular region (corresponding to the spot-weld size) of the contacting surfaces. The resultant closed-box configuration generally provides a good combination of compressive, bending, and torsional stiffnesses and strengths but, in the case of the all-steel construction, the weight of the component is relatively high.

To form a PMH rendition of the simplified load-bearing BIW component, the all-metal control is modified in the following way: (1) the cover plate is eliminated; and (2) a plastic insert consisting of an overlay (mates with the interior of the U-shaped channel) and a series of *cross ribs* is added. To ensure

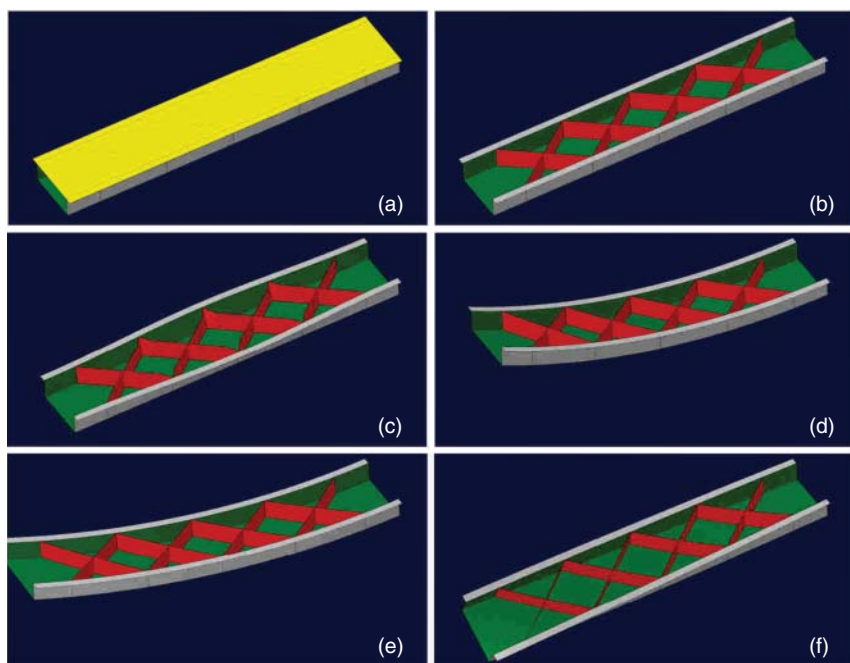


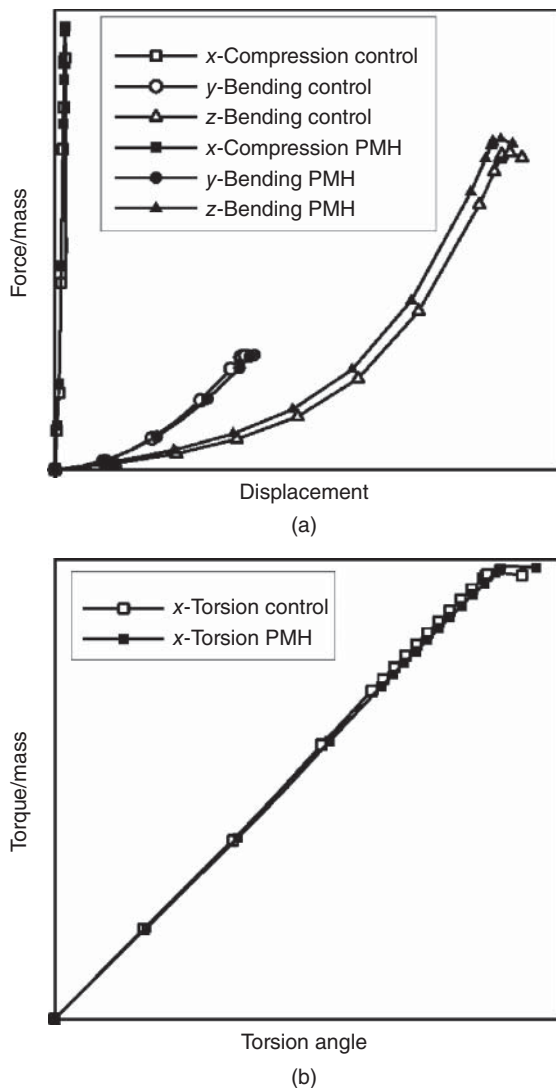
Figure 10.6 Structural analysis of the PMH component: (a) all-metal control; (b) PMH component; (c) axial compression; (d) bending about first transverse direction; (e) bending about second transverse direction; and (f) twisting about the longitudinal direction.

that the plastic insert will not be affected by welding of the U-shaped channel ends to the BIW structure, the length of the insert is set to 80% of the U-shaped channel length and the insert is centered relative to the channel lengthwise. The resulting configuration of the PMH component is shown in Figure 10.6(b). Within the finite-element analysis of the PMH component, adhesion between the metal and the polymer is simulated using specialized cohesive elements, which, through the use of normal and tangential traction–separation relations, enable modeling of the initial loading, the initiation of interfacial damage, damage-induced adhesion stiffness/strength degradation, and the propagation of damage leading to eventual decohesion of the adhering surfaces, for example, [1].

To validate functional performance of the PMH component under quasistatic loading conditions, the control and the PMH component are each subjected to four basic loading modes: (1) longitudinal (i.e., x -axis) compression, Figure 10.6(c); (2) bending about the first transverse (i.e., y -axis) direction, Figure 10.6(d); (3) bending about the second transverse (i.e., z -axis) direction, Figure 10.6(e); and (4) twisting about the longitudinal (i.e., x -axis) direction, Figure 10.6(f). It should be noted that in

Figure 10.6(c)–(f), the four deformation modes are displayed only for the PMH component, for brevity. Structural mass efficiency of the PMH component is assessed by comparing mass-normalized load and torque peak values between the control and the PMH component. An example of such a comparison is given in Figure 10.7(a) and (b). The results displayed in Figure 10.7(a) and (b) suggest that the PMH component outperforms its all-steel counterpart relative to x -compression and z -bending load-bearing resistances while the two are on par relative to their y -bending and x -torsion strengths.

Figure 10.7 Typical results pertaining to:
(a) mass-normalized force versus displacement; and
(b) mass-normalized torque versus torsion angle for the all-steel (control sample) and PMH component (experimental sample) used for validation of the PMH-component functionality/utility.

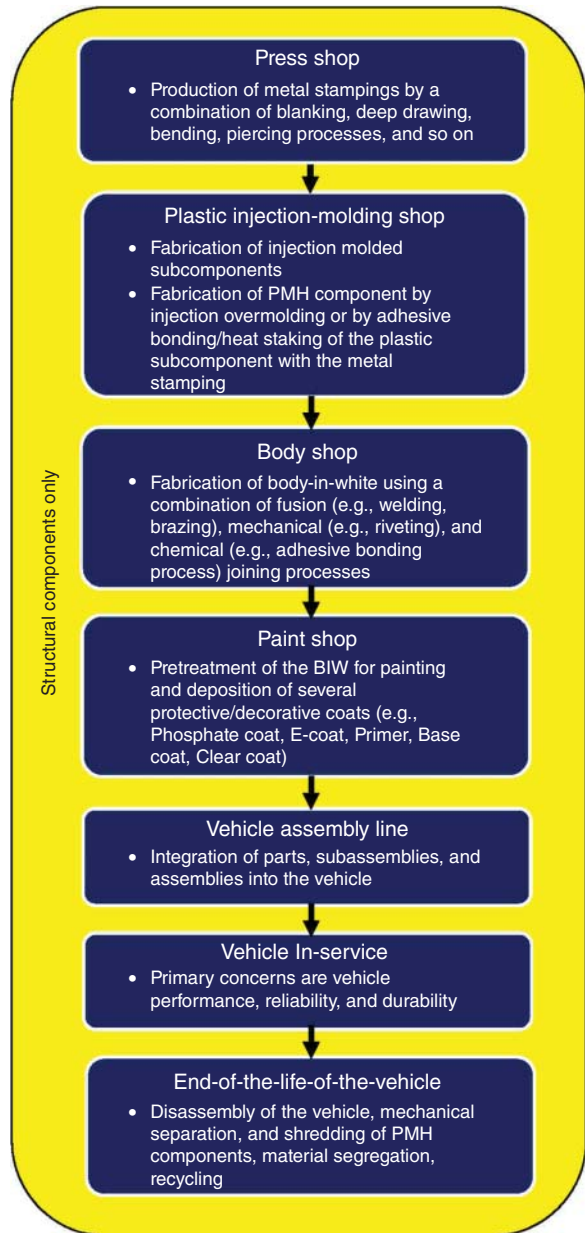


10.5 Compatibility with Automotive BIW Manufacturing Process Chain

When selecting among the previously overviewed PMH technologies for use in various automotive manufacturing applications, consideration is given to the total life cycle (TLC) of the PMH component in question as well as the TLC of the vehicle. The TLC PMH technology selection approach differs from the more conventional manufacturing-process selection approach (e.g., [7]), which primarily emphasizes the issues related to the component function and performance. The TLC approach, on the other hand, considers the potential consequences and ramifications associated with the PMH technology selection to various stages of the vehicle manufacturing process chain, vehicle performance and durability (while in service), as well as the analysis of various End-of-the-Life-of-the-Vehicle (ELV) issues (e.g., disassembly, suitability of the material(s) for shredding, and segregations, potential for economic recycling, etc.). A schematic of the major stages in the life of a BIW component for which the PMH technology is being selected (using the TLC approach) is depicted in Figure 10.8. When considering potential consequences of the PMH technology selection relative to the automotive BIW manufacturing process chain, one of the key issues is compatibility of the selected PMH technology with the main manufacturing-process steps, which include: (1) metal-subcomponent manufacturing by stamping in the press shop (see Figure 10.8) shop; (2) PMH component or thermoplastic subcomponent manufacturing in the injection-molding shop; (3) BIW construction by various joining processes in the body shop; and (4) BIW pretreatment and painting in the paint shop. It should be noted that, as indicated in Figure 10.8, bolt-on (i.e., nonstructural) and load-bearing (i.e., structural) BIW components have somewhat different manufacturing history. These differences are caused by the fact that, since structural components are integrated into the BIW frame in the body shop, they have to pass through (and be compatible with the requirements of) the paint shop. On the other hand, it is not necessary for bolt-on components to pass through the paint shop since they can be directly attached to the painted BIW frame in the assembly shop.

As an example of a BIW manufacturing-process chain requirement for the PMH technology, one may take the case of selection of the material for the polymeric subcomponent. In the body shop, the key functional requirement for the polymeric subcomponent material is its ability to withstand welding-induced high-temperature exposures. On the other hand, the main functional requirements for the polymeric subcomponent material relative to its compatibility with the BIW paint shop deal with the ability of the selected material to withstand mechanical, thermal, and chemical attacks without degrading and without contaminating the paint baths.

Figure 10.8 Key life-cycle stages for a typical PMH automotive BIW component.



10.6 Concluding Remarks

In this chapter, a comprehensive overview of the key aspects of injection overmolding technologies used in automotive BIW structural applications has been provided. Specifically, the following aspects of injection-molding technologies have been discussed: (1) fundamental concepts related to synergistic polymer/metal interactions; (2) classification of the technologies; (3) basics of polymer/metal adhesion and load transfer; (4) application of computational engineering methods and tools for process and product-performance simulations; and (5) compatibility of different injection-molding PMH technologies with the automotive BIW manufacturing process chain. It has been argued that, while the utilization of the PMH technologies in the manufacture of automotive BIW may yield benefits related to lower vehicle weight, improved fuel economy and cost, vehicle end-of-life issues must be considered when selecting these technologies. Specifically, increasing emphasis on sustainability, dwindling material supplies, increasing producer responsibility, product take-back legislations, and marketing of recycled material-content claims require consideration of the issues such as product refurbishment, product disassembly, materials extraction and procurement, and end-of-life product management.

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11

Ultrasonic Joining of Lightweight Alloy/Fiber-Reinforced Polymer Hybrid Structures

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11.1 Introduction

Modern lightweight design in transport structures has become a matter of success for Original Equipment Manufacturer (OEMs). Due to strict environmental policies, designers and engineers feel compelled to select lighter, recyclable, and sustainable materials to reduce fuel consumption and emission [1, 2]. Examples of lightweight materials used in transport structures are high-strength steels, magnesium, aluminum, titanium, polymers, and fiber-reinforced polymer [3, 4].

In this context, the combination of fiber-reinforced polymers with lightweight alloys for the fabrication of multimaterial or hybrid structures provides an innovative spectrum of materials with excellent mechanical properties, physical–chemical resistance, and lightweight. However, joining of lightweight alloys to fiber-reinforced polymers (LM-FRP) is complicated due to their physical–chemical dissimilarity resulting in lower mutual miscibility [5]. Therefore, there is an open niche for new, high-performance and cost-effective joining technologies for this purpose [6]. Recently, new joining methodologies have been proposed such as Friction Spot Joining [7–9] (Chapter 3), Friction Riveting [10] (Chapter 8), injection overmolding [11, 12] (Chapter 10), and welding-based techniques such as laser [13, 14] (Chapter 5), induction [15, 16] (Chapter 4), and metal ultrasonic welding [17, 18]. These new approaches aim at reducing the limitations of state-of-the-art adhesive bonding (i.e., long curing times and extensive surface pretreatments) and mechanical fastening methods (i.e., elevated stress concentration due to the through-hole and increase in weight related to fasteners).

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The strength of LM-FRP joints is typically dictated by the type of adhesive forces and mechanical interlocking mechanisms, normally restricted to a thin layer at the joint interface. Therefore, in LM-FRP rivet-free joints, the load-transfer performance and out-of-plane mechanical properties are usually lower than those of the respective base materials. In order to overcome this drawback and fully explore the positive individual properties of the lightweight alloys and polymer composites in the hybrid structure, new joining methodologies leading to strong bonding mechanisms at the interface are often required.

Through-the-thickness reinforcement of the joining parts has been recently applied to increase the mechanical performance and particularly improve damage tolerance of LM-FRP joints. The reinforcements are normally integrated on the metallic part surface, while the composite is added layer by layer through co-curing or vacuum infusion process [19, 20]. Examples of technologies that have been reported to produce metallic 3D surface features (e.g., pin-like anchoring zones) include the Surfi-Sculpt™ [20–24], Additive Layer Manufacturing (ALM) [25], and welding-based methods such as Cold-Metal Transfer (CMT) [19] and Arc Percussive Micro-Welding (APMW) [26].

The Surfi-Sculpt™ treatment is based on electron beam technology [24], which locally melts the metallic surface to create holes and protrusions, upon which the composite is laminated. When the Surfi-Sculpt™ structured joint is combined to composite laminates, the technology is also known as *Comeld*™ [20]. This approach has been demonstrated to increase the joint load-carrying capability and energy absorption prior to failure. Although the laser-based Surfi-Sculpt™ variant has been recently introduced as a better alternative [23], most of the technological knowledge relies on the electron beam variant. This process variant operates under vacuum, which restricts the size of the metallic part. Furthermore, the joint assembly by composite laying-up makes the *Comeld*™ technology time-consuming. The welding-based technologies (CMT and APMW), as well as the ALM, make use of fusion welding to weld or deposit layers to generate micropins on the metallic part surface. Therefore, as fusion-based processes, problems such as solidification cracking, hydrogen embrittlement, and evaporation of alloying elements are expected. Other limitations of these metal surface structuring technologies are generally restricted freedom of reinforcement geometry, reduced geometrical reproducibility of the individual pins, and limited production rates (especially in the case of ALM).

This chapter introduces a new approach to manufacture future damage-tolerant and crash-resistant LM-FRP hybrid structures. The new Ultrasonic Joining (U-Joining) technology [27] combines the use of ultrasonic energy to join surface-structured metallic parts produced by metal injection molding (MIMStruct [28]) to fiber-reinforced thermoplastic composites. Joining is accomplished through mechanical interlocking (due to the through-the-thickness reinforcement present on the metallic surface) and adhesion forces created by a thin molten polymer layer consolidated

at the metal/composite interface. The first part of the chapter describes the MIMStruct manufacturing route and principles of the joining process, as well as its potential applications. In the second part, a case study on MIM-structured Ti-6Al-4V parts joined to glass-fiber-reinforced polyetherimide (GF-PEI) was used to demonstrate the U-Joining feasibility. Selected results on temperature measurement, microstructural features, and mechanical performance of the hybrid joints are presented. Finally, advantages and limitations of this new joining technology are briefly discussed.

11.2 MIMStruct Manufacturing Route

The MIMStruct manufacturing concept is based on the metal injection molding, which allows the production of surface-structured metallic parts with excellent dimensional tolerance, surface finish, and high reproducibility [29]. The MIMStruct concept was patented by Helmholtz-Zentrum Geesthacht in 2010 [28]. It consists in using a polymer–powder feedstock to injection mold a surface-structured metallic part, which will be suitable for further connection with polymeric and composites materials after a sintering process. Figure 11.1 presents a schematic illustration of the manufacturing route using a binder based on a wax–polymer combination.

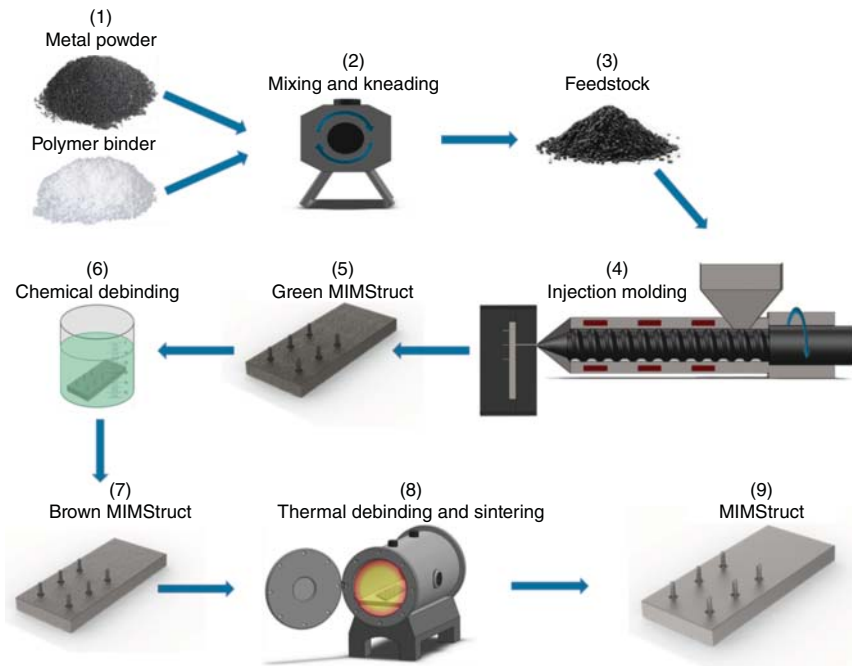


Figure 11.1 MIMStruct manufacturing route.

In the beginning of the manufacturing cycle (Step 1), the polymeric binder is mixed and kneaded with the metallic powder (Step 2) to produce the feedstock (Step 3). The polymeric binder works as a medium for shaping and holding the metal particles together, adjusting the feedstock viscosity for the processing by conventional injection molding (Step 4). The injection mold cavity presents a negative oversized shape of the desired MIMStruct to accommodate part shrinkage during the sintering [29]. The injection-molded part is named “green” after being removed from the mold (Step 5). Following that, it undergoes a two-stage debinding process known as “*chemical and thermal debinding*” (Steps 6 and 8, respectively).

The chemical debinding intends to remove the nonpolar content (normally paraffin or low-molecular-weight polymers) of the binder system, responsible for pseudoplasticity of the feedstock, through solvents. This stage results in an open microporous structure [29] held structurally by a polar chain polymer; after this stage, the piece is named “brown” (Step 7) and is ready for the thermal debinding. During this stage, which is carried out under a controlled inert atmosphere, all remaining polymer binder is removed from the MIMStruct part (Step 8). The last stage consists in sintering (also represented in Step 8 for simplification) the brown part under a high vacuum atmosphere and high temperatures, resulting in a ready-for-joining MIMStruct part (Step 9).

11.3 U-Joining: Principles of the Process

The U-Joining concept combines the previously described manufacturing route to produce the MIM-structured part, to be joined to a composite part, with ultrasonic energy. This synergic combination of technologies enables the production of strong hybrid structures in a new, faster, and more effective manner. The joining process can be divided into five stages, as represented in Figure 11.2.

Firstly, the parts are fixed between an anvil and a sonotrode, with the pin-like elements of the MIMStruct in contact to the composite surface (Figure 11.2(1)). Secondly, the sonotrode tip touches the upper part of MIMStruct, and static pressure is applied perpendicularly to the contact surfaces (Figure 11.2(2)). In the third stage, the sonotrode starts to vibrate in a back-and-forth motion parallel to the contact surface with high frequencies (usually between 20 and 40 kHz) and amplitudes usually varying between few micrometers up to 52 μm . The interaction between the pins – set in motion by the ultrasonic vibration and composite surface – produces frictional heat, locally increasing the temperature at the interface between metallic pins and composite.

The pressure and the mechanical vibration of the sonotrode are kept constant during a specific joining time, which combined with the frictional heat generation induce softening or melting of the polymeric matrix located right below the pins. This will allow insertion of pins in the composite to start (Figures 11.2(3)).

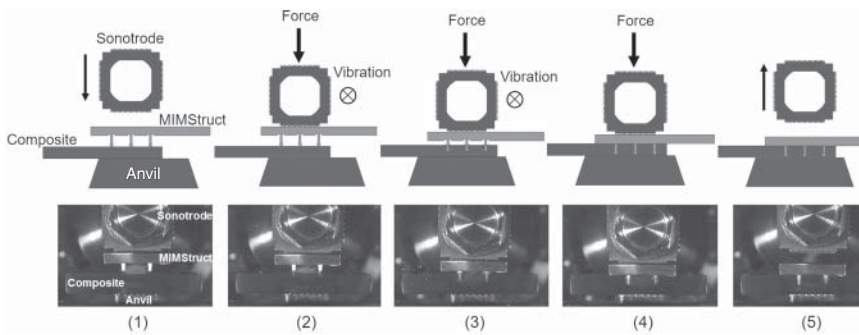


Figure 11.2 Schematic representation of the U-Joining process. (1) Positioning of joining parts; (2) Application of ultrasonic vibration and axial force; (3) Softening of the polymer by frictional heat at the interface and onset of pin insertion; (4) Polymer consolidation; and (5) End of the process and sonotrode retraction.

The inserting pins will expel a certain volume of softened or molten polymer out of the joining area. The expelled molten polymer will be further consolidated at the joint interface. After the complete insertion of the pins, the base of the MIMStruct plate hits the upper surface of the polymer composite. In this way, the squeezed molten polymer wets the MIMStruct surface, and adhesion forces between the materials are created (Figure 11.2(4)). Consequently, a joint strongly held by a combination of mechanical interlocking and adhesion forces is accomplished. In the last stage (Figure 11.2(5)), the static pressure is removed and the sonotrode is retracted from the joined hybrid structure.

The association of the mechanical interlocking and adhesive forces will increase the out-of-plane mechanical properties of the joints. By adding special anchoring features in the protruding pins, such as conical, spherical, convex, or arrow-like heads (Figure 11.3), mechanical performance of the hybrid joints can be improved under complex loading modes, as reported in the literature [19]. Furthermore, the through-the-thickness reinforcement effect created by the pins over the bonded interface of the joints will help to slow down the crack-propagation rate, avoiding catastrophic failure.

Apart from the positive gain regarding mechanical properties, the use of ultrasonic energy induces a very efficient transformation of mechanical work into thermal energy [30, 31] and decreases the joining time, since the assembly joining cycle times are very short (usually <2 s). Therefore, one may expect

Figure 11.3 Possible reinforcement elements' shapes.



a decrease in the final assembling costs in comparison to the complex and time-consuming state-of-the-art lamination processes.

11.3.1 Process Parameters

The U-Joining process parameter and variants are similar to the ones described for ultrasonic welding of metals [32] and thermoplastics [30, 31]; these are directly related to the type of joining control mode selected. The basic process parameters are relatively simple and may include joining energy (E_j), sonotrode oscillation amplitude (A_0), joining force (F), clamping force (CF), and joining time (t).

The conventional control modes available for U-Joining depend on the capability of the specific equipment and may include joining by energy, time, collapse, and peak power. These control modes are selected mainly as a criterion to control the duration of the applied ultrasonic vibration. The market-available microprocessor-controlled ultrasonic welding machines permit precise control of the joining parameters, usually operating in a “closed-loop” system [30]. This allows the feedback parameters recorded by the system to adjust the joining parameter during the process. For instance, when the U-Joining is carried out under energy-controlled mode, the ultrasonic vibration is maintained constant until the preset value of energy is achieved, regardless of the initial setup of the remaining joining parameters. Similarly, it also occurs when time, collapse, or peak power modes are chosen; the ultrasonic vibration is kept constant until a determined preset time, collapse distances, or power peak value is achieved during the joining, respectively.

Joining by energy control is a preferable strategy for U-Joining to better control the heat generation and mitigate the polymer matrix degradation. Moreover, the joining system can easily compensate surface irregularities arising from the fiber-reinforced woven. In this control mode, the energy delivery is adjusted during the joining by the integral of power over time [30, 31]. Therefore, the parameter Joining Time is a dependent variable automatically adjusted by the joining system to deliver the required amount of energy [32]. The energy delivery in the energy-controlled mode can be described as:

$$E_i = P x t_i \text{ (J)} \quad (11.1)$$

where P is the power and E_i is the energy delivered at an instant t_i . The power required to start and keep the sonotrode vibration depends on the preset joining force (the normal force applied parallel to the sonotrode vibration), sonotrode oscillation amplitude, and material's kinematic coefficient of friction. When E_i reaches the specified joining energy (E_j), the sonotrode vibration is stopped. Some implications can be drawn from this equation. For a fixed value of setup joining energy, the higher the oscillation amplitude and joining force, the higher is the power required by the system and, as a consequence, the

shorter will be the joining time. Nevertheless, the combination of parameters' amplitude (A_0) and force (F) controls the power required to maintain the sonotrode oscillation during U-Joining, which affects the energy consumption. Moreover, one must take into consideration the stiffness and mechanical properties of the pin-like elements when selecting the applied force, in order to avoid their deflection by plastic deformation.

The E_f is directly proportional to the heat generation and can be associated to phenomena such as changes in the viscosity and volume of the molten polymer, thermal-related defect formation (e.g., polymer degradation), as observed for other frictional-based joining processes [33, 34]. In order to control the postjoining process, modern ultrasonic systems usually allow the monitoring and recording of sonotrode displacement and supplied power; this control data can be used to evaluate the process reproducibility and joint quality.

11.3.2 Process Phases

The U-Joining can be segmented into five different process phases according to changes in process parameters and heat generation. A typical U-Joining process phase diagram is schematically presented in Figure 11.4. Since process temperatures are usually well below the annealing or phase transformation temperatures of high-strength and heat-resistant lightweight metals, such as titanium alloys, the MIMStruct metal part generally does not undergo metallurgical transformation during the process. Therefore, the physical and chemical transformations are normally restricted to the polymeric part.

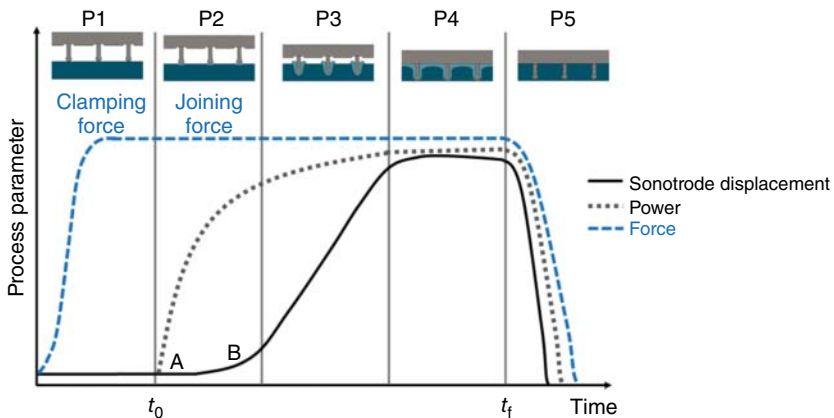


Figure 11.4 U-Joining process phases. P1 – Accomplishment of contact between MIMStruct pins and the surface of the composite; P2 – Coulomb friction and unsteady-state viscous dissipation; P3 – Steady-state viscous dissipation; P4 – Complete pins insertion and creation of adhesion forces at the joint interface; and P5 – Joint consolidation.

In the first process phase (Figure 11.4, Phase P1), the force curve abruptly increases due to the application of the normal force, whereby physical contact between the pin surface and composite is accomplished. In a usual joining procedure, clamping and joining forces are kept at the same level; hence, the force curve remains constant over the subsequent process phases.

Once the sonotrode vibration starts (Phase P2), a sharp increase in the course of the power curve is observed, while the sonotrode displacement slightly starts to increase. At this phase, electrical power is required to start the sonotrode vibration. The initial heat mechanism in this phase, which is believed to last a fraction of seconds due to the high frequency of vibration, is based on Coulomb [35, 36] or solid friction phenomenon (Region A in P2). In this short period, surface microasperities of both parts are plastically deformed, generating heat, as known from other friction-based techniques [37–39], but sonotrode displacement is usually not detected. Moreover, as the polymer matrix is a viscoelastic material subjected to sinusoidal strain, energy is also dissipated through intermolecular friction, which can be described by the polymer loss modulus (out-of-phase modulus) [40]. The combination of these mechanisms will increase the temperature and locally melt a small volume of polymer matrix right below the pins. The created small volume of molten polymer will accommodate and allow the MIMStruct pins to begin penetrating the composite. Owing to the transition from solid to viscous heat dissipation, the heat generation becomes unstable, by which the sonotrode displacement (i.e., pin penetration) increases nonlinearly with time (Region B in P2). Therefore, P2 is usually called unsteady-state viscous dissipation phase [33].

In the third phase (Phase P3), heat generation becomes stable and a larger volume of molten polymer matrix is formed around the vibrating pins; it is assumed that, due to a balance between polymer melting and molten material outflow rates (a squeeze flow of molten polymer is created), heat input and heat outflow are equalized. Accordingly, a steady-state viscous dissipation regime is achieved, which will result in a linear displacement of sonotrode. During this phase, the heat is purely generated and maintained by viscous dissipation from the internal sinusoidal straining in the molten polymer. In case of fiber-reinforced polymers, pins will most likely displace the load-bearing fibers; however, some local fiber damage may occur during this phase. As the pin insertion depth increases into the composite, pins' vibrational motion is restrained by the adjacent material. To compensate the losses in motion, the U-Joining system needs to increase the electrical power continuously to maintain sonotrode vibration constant, as shown in Figure 11.4, at P3.

By the end of Phase P4, the complete penetration of the pins is achieved and the metallic surface of the MIMStruct part is wetted by the squeezed molten polymer. During this phase, cooling (e.g., forced air) may be applied or not, to consolidate the molten polymer creating adhesive forces between the joining

parts. In this phase, the joining process is finished and a holding pressure may be used in order to compensate for polymer shrinkage.

Finally, at Phase P5, the sonotrode is retracted from the joint, whereby all curves return to zero. It is worth emphasizing that the process diagram described in Figure 11.4 corresponds to an ideal joining condition; alterations in the process parameter used, as well as materials (e.g., reinforced and unreinforced polymers), can change the shape of the described curves.

11.3.3 Process Variants

The U-Joining was firstly demonstrated through the use of the metal ultrasonic spot welding as the source of ultrasonic energy. Parallel vibration mode of the sonotrode allows higher power to be generated and delivered to the joining area. Moreover, as pointed out by Wagner *et al.* [34] for the conventional (i.e., without a through-the-thickness reinforcement) metal ultrasonic welding of aluminum sheets to carbon-fiber-reinforced polymers (CFRP), parallel oscillation allows direct contact between the bearing load fibers and metal to be created without excessively damaging the carbon fibers. When the metallic surface must be preserved from the marks leads which resulted from the contact with the joining tool, the perpendicular sonotrode vibration variant normally applied in conventional polymer ultrasonic welding may be alternatively used. In this case, the sonotrode vibration should be applied on the surface of polymeric part perpendicular to the joining area. This is aimed to support the transformation process of ultrasonic energy into thermal energy by internal friction in the polymeric part [41]. The two U-Joining process variants are presented in Figure 11.5.

11.3.4 Potential Applications

Structures designed for applications in the transport sector require high damage-tolerant and crash-resistant metal/composite joints. Therefore, the U-Joining concept offers a wide field of potential applications. The examples presented in Figure 11.6 comprise application in body-in-white structures in automotive, such as local reinforcement of metallic structures with composite elements to increase crashworthiness (e.g., hybrid metal–composite B-columns, Figure 11.6(a)). Other examples are envisaged for primary aircraft structures (Figure 11.6(b)), as well as metallic connectors in construction or repair of fiber-reinforced polymer structures applied in lightweight roofs and façades (Figure 11.6(c)).

11.4 Case Study on Ti-6Al-4V/GF-PEI Joints

The material selection for the case study was based on its relevance for application in transportation lightweight structures. Four- and six-pin hybrid

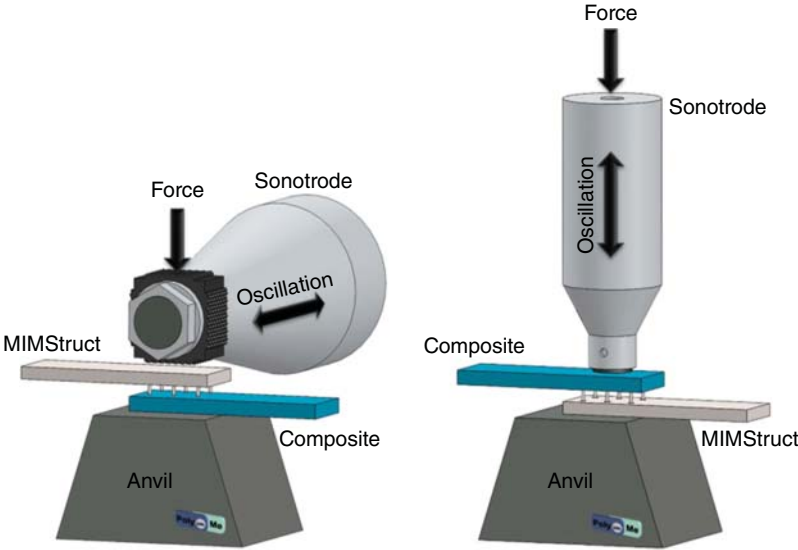


Figure 11.5 U-Joining process variations. Parallel and perpendicular vibrations of the sonotrode in relation to the joining area.

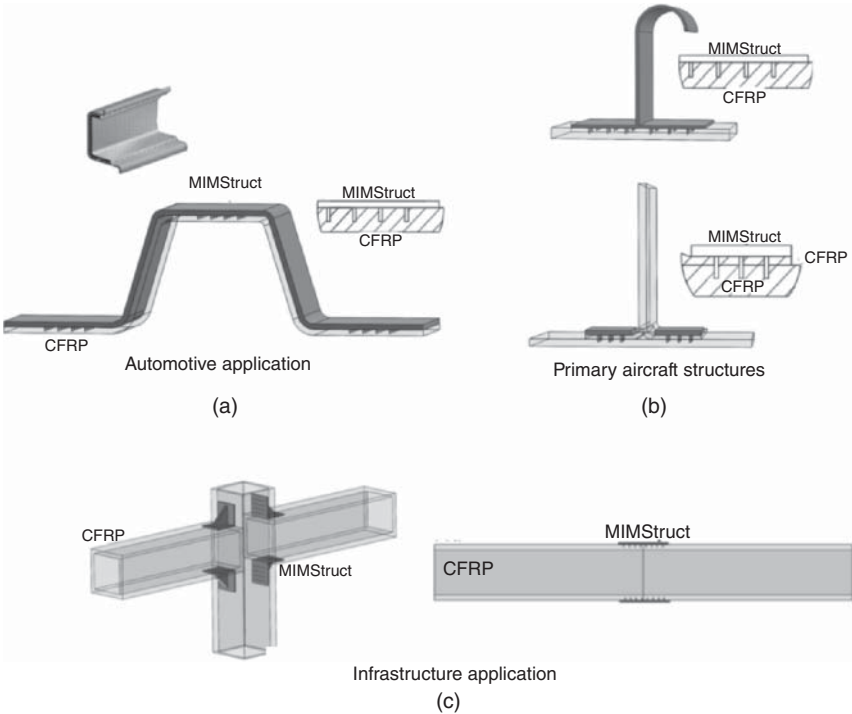


Figure 11.6 Examples of potential application for the U-Joining technique in automotive, aerospace, and infrastructure sectors.

joints were produced through the U-Joining technique and evaluated in terms of microstructure, local, and global mechanical performance.

11.4.1 Materials

11.4.1.1 MIMStruct Part

The MIM-structured part of the hybrid joints consists of a Ti-6Al-4V alloy. Prealloyed and gas-atomized spherical powder from TLS Technik was used to prepare the feedstock, in which the powder particles had an average diameter smaller than 45 μm and grade 23 according to ASTM B348-13 [42]. The titanium powder was mixed with a binder system (60 wt% paraffin, 35 wt% polyethylene–vinyl-acetate copolymer, and 5 wt% stearic acid) in a ratio of 1:9, kneaded and subjected to a homogenization step carried out in one injection-molding cycle without filling the mold in ARBURG 320-S machine. The main MIMStruct process parameters are summarized in Table 11.1.

Chemical debinding was carried out in a Lömi EBA-50 chemical debinding system in a cycle of 900 min reaching the maximum temperature of 40 °C. Thermal debinding and sintering were performed in the same cold wall furnace (Xerion XVAC) at 600 and 1300 °C, respectively. A controlled atmosphere of argon was used during thermal debinding and high vacuum during the sintering.

Figure 11.7(a) illustrates as-sintered four- and six-conical pin MIMStruct parts used in this work, while Figure 11.7(b) shows the detailed geometry of the conical pins. The microstructure of the produced MIMStruct is typical for an α - β Ti-6Al-4V alloy manufactured by metal injection molding [43]. It exhibits

Table 11.1 Metal Injection-molding process parameters.

Volumetric parameters	Dose 10 ccm	Flow 15 ccm/s	Part volume 10.5 ccm	Filling time 2 s
Injection parameters	Injection pressure 440 bar	Switchover time 0.5 s		
Holding time and pressure	Holding time 1 1 s	Pressure 1 200 bar	Holding time 2 1 s	Pressure 2 50 bar
Barrel temperature	Zone 1 80 °C	Zone 2 100 °C	Zone 3 120 °C	Nozzle 120 °C
Mold temperature	Zone 1 55 °C	Zone 2 60 °C		

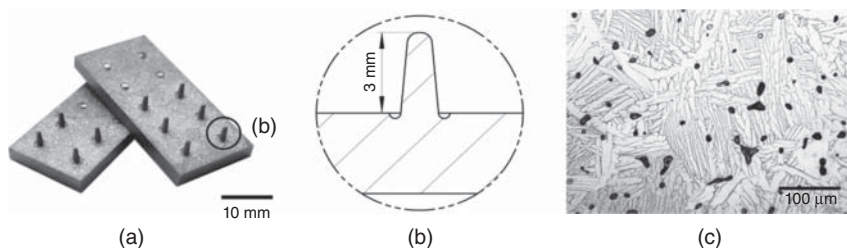


Figure 11.7 As-sintered Ti-6Al-4V MIMStruct part (a), round-tip conical pins detail (b), and Ti-6Al-4V microstructure (c).

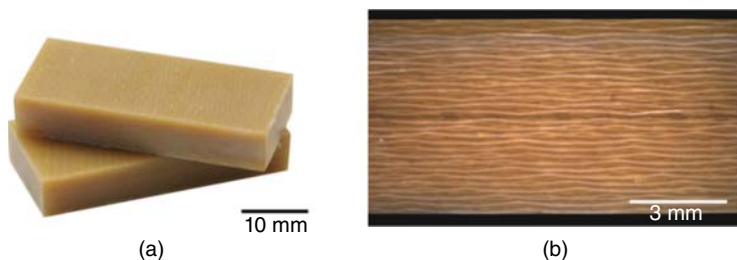


Figure 11.8 (a) GF-PEI composite part and (b) microstructure of the GF-PEI composite laminate.

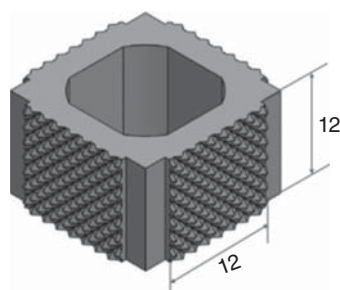
a fully lamellar α -colony microstructure and β -phase distributed in the grain boundaries as shown in Figure 11.7(c). A residual porosity of $4.4 \pm 0.7\%$ was measured by digital image using the software Image J.

11.4.1.2 Composite Part

6.35 mm-glass-fiber-reinforced polyetherimide (GF-PEI) laminates with fiber content of 50 vol% in weight and ply stacking sequence $[0,90]_{14}$ from TENCATE were used. GF-PEI is an amorphous polyetherimide thermoplastic composite qualified by Airbus and Boeing for primary and secondary structural applications [44]. Plates measuring $15 \times 35 \times 6.35$ mm were machined to produce the joints (Figure 11.8(a)). The microstructure of GF-PEI is presented in Figure 11.8(b).

11.4.1.3 Joining Procedure

The joining parts were fixed in an overlap configuration, and the ultrasonic vibration was applied over the MIMStruct upper surface parallel to the joining area as described in Figure 11.1. Joints were produced in a commercially available metal ultrasonic welder (Ultraweld L20, Branson Ultrasonics). The joining equipment allows, when operated under the energy control mode, precise control of Joining Energy (E_j), Clamping Pressure (CP), Joining Pressure (JP), and Sonotrode Amplitude Oscillation (A_0). The frequency of sonotrode oscillation was fixed at 20 kHz.

Figure 11.9 Sonotrode tip (joining tool).

Table 11.2 Joining parameters.

Pins N°	E_j (J)	CP (bar)	JP (bar)	A_0 (μm)	Amp. threshold [J]
4 6	2200	1.03	1.03	32/52	500

The joining tool consisted of a standard steel sonotrode tip (Figure 11.9) with knurled surface (12×12 mm). The joining parameters used in this case study were previously optimized within a range of E_j : 1500–3500 J, CP: 0.7–1.7 bar, JP: 0.7–1.7 bar, A_0 : 16–52 μm in a separate project. The optimized set of joining parameters is listed in Table 11.2.

The threshold to amplitude switching was 500 J, which means in other words, the moment the joining system delivered this amount of energy, the amplitude increases from 32 to 52 μm . The resultant joining cycles were 1.42 ± 0.03 and 1.21 ± 0.28 s for the four- and six-pin joints, respectively.

11.4.2 Process Temperature

The process temperature was monitored using a high-speed infrared thermo-camera model ImageIR® 8300 hp (Infratec, Germany) on the range of 150–700 °C. Temperature was measured at the joining partners' interface in the region marked with a dash-lined in Figure 11.10(a). Therefore, the cutoff point for the measurement was defined by the complete pin penetration (i.e., the full touching down of the lower surface of the MIM-structured part on the upper composite surface). Thus, the peak temperature in this region may not refer to the maximum temperature reached during the process. Figure 11.10(b) shows the thermographic temperature profile obtained through the described experimental procedure. The curves are composed of the maximum temperature recorded for each thermogram acquired during the joining.

The maximum temperature measured for the four-pin joints at the interface was 374 ± 8 °C. The values for the six-pin joints were slightly higher with

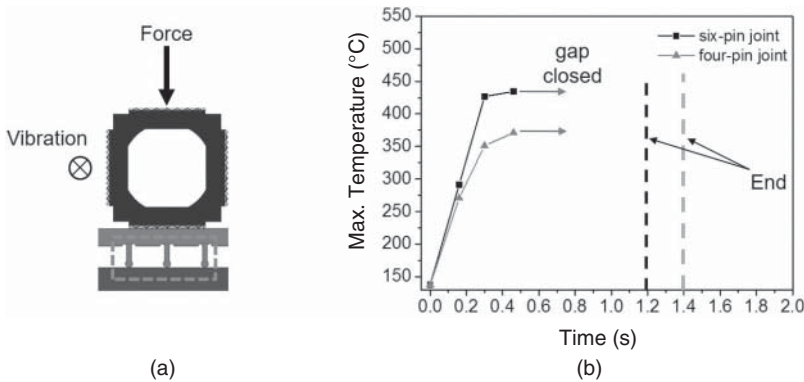


Figure 11.10 Temperature measurement area assessed by infrared thermography (a) and temperature profile (b) at the interface between the MIM-structured and GF-PEI parts for the four- and six-pin joints.

$460 \pm 63^\circ\text{C}$. Such temperatures at the interface may induce the formation of cross-linking in the PEI matrix, which has been reported to occur at a temperature ranging from 320 to 380°C [45–47]. This is the first and dominant degradation mechanisms of the PEI according to Kuroda *et al.* [45], while the chain scission (second mechanism) starts above 400°C , with a maximum rate of decomposition around 510 – 540°C [45, 47]. As the joints have experienced this temperature for only tenths of a second, one may expect that polymer matrix degradation is not extensive, as observed in other friction-based processes [47, 48]. A systematic evaluation of the U-Joining process-related physical–chemical changes in the polymer matrix is in progress and will be reported in a separate publication.

11.4.3 Microstructure of the U-Joining Joints

Figure 11.11 presents a typical joint’s cross-sectional view, showing a complete insertion of the pins in the composite part. Heat and strain rate associated with the pin feeding create a composite thermomechanically affected zone (TMAZ). The TMAZ is a thin layer formed at the joint interface and distributed around the pins, whose geometry is strongly influenced by the joining parameters. This zone is characterized by melt and mixture of the polymer matrix with pieces of broken fibers produced during the perforation of the glass-fiber woven by the vibrating pins. In addition, some internal flaws can be found, which may be related to thermal degradation, entrapped air bubbles in the viscous molten polymer, structural water evolution, and fiber deconsolidation as typically seen in other polymer welding processes [49, 50].

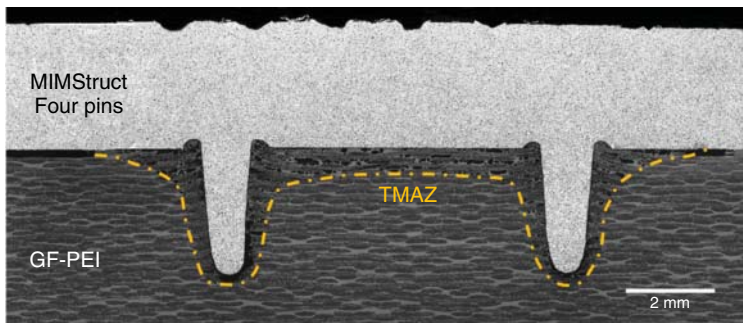


Figure 11.11 Cross-sectional view of the typical hybrid joint.

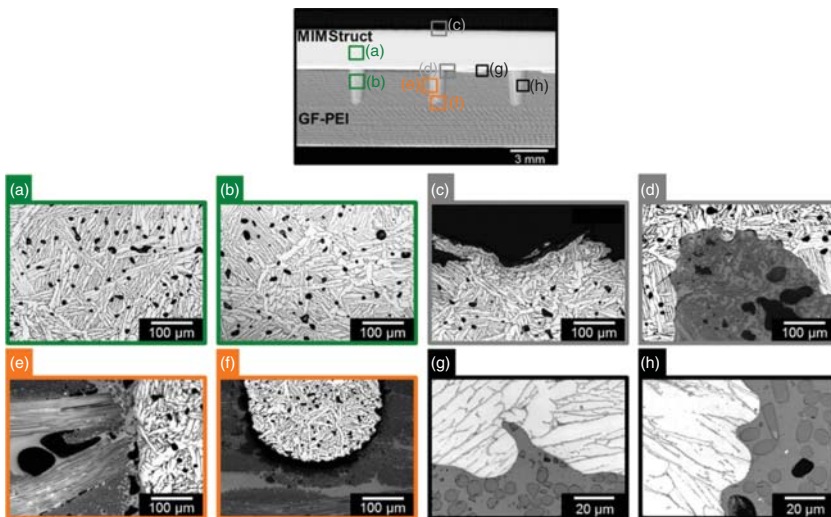


Figure 11.12 X-ray tomography and detailed micrographs of a six-pin ultrasonically joined Ti-6Al-4V/GF-PEI hybrid joint. (a) Ti-6Al-4V microstructure at MIMStruct half thickness, (b) at pin region, (c) at the surface contact between MIMStruct and sonotrode tip, (d) detail at an undercut region, (e) pin/composite interface, (f) pins tip/composite interface, (g) laser microscopy image of pin/composite interface and (h) MIMStruct bottom surface/composite interface. Microstructure of Ti-6Al-4V was revealed by Kroll etching reagent (96 mL H_2O , 6 mL HNO_3 and 2 mL HF).

The joints' microstructural features were similar under the two investigated conditions. Therefore, the six-pin joint was selected for further microstructural characterizations (Figure 11.12). Since the process temperature was well below the β -phase transition temperature ($\beta_t \approx 995^\circ\text{C}$ [51]), no thermal-induced microstructural changes in the Ti-6Al-4V part are expected in comparison to base material. This can be observed in the MIMStruct part (Figure 11.12(a))

and pin area (Figure 11.12(b)). However, a very thin area on the MIMStruct surface showed plastically deformed microstructure (Figure 11.12(c)), which is mainly caused by the contact between the MIMStruct surface and the sonotrode tip (tool marks).

During the steady-state viscous dissipation phase, the pins penetrate the glass-fiber-reinforced composite woven; as a result, a squeeze flow of molten polymer matrix and pieces of broken fibers is formed. The broken fibers, embedded in the molten polymer, flow upward and fill the pins' undercut (Figure 11.12(d)) at the pin base in the MIMStruct part. After complete penetration of the pins, the glass-fiber woven around the pins are reoriented upward from the original plane, according to the material flow, as shown in Figure 11.12(e).

The presence of thermal flaws was also observed in the composite TMAZ, including voids and fiber delamination (Figure 11.12(e)), which are probably related to the differential contraction of fibers and molten matrix during joint consolidation. Figure 11.12(f) shows the interface of composite–pin tip with final insertion depth within a glass fiber bundle. As a combined result of reduced amount of resin and differential contraction between the titanium pin and glass fibers, a poor bonding contact between the materials was observed. The presence and distribution of these flaws can directly affect the mechanical performance, especially under cyclic loading, and may induce delamination of the composite and premature failure. Therefore, these should be avoided or kept to a minimum. As experienced with the development of other welding and joining processes, it is expected that the current efforts to optimize the heat input in U-Joining process may eventually reduce the occurrence of these defects.

Despite the formation of thermal flaws in the TMAZ, a very intimate contact between the composite and MIMStruct was observed all over the metal–composite interface. The molten polymer tends to fill the opened residual pores distributed along the surface of the pins (Figure 11.12(g)) and at the joint interface (Figure 11.12(h)). Consequently, mechanical interlocking mechanism is achieved at microscale, which can positively contribute to the bonding resistance, therefore, increasing the out-of-plane mechanical properties of the joints.

11.4.4 Local Mechanical Properties of MIMStruct Part

The local mechanical property of the Ti-6Al-4V part was assessed by Vickers microhardness in the cross-sectioned MIMStruct part (base material) and joints (Figure 11.13). By comparing Figure 11.13(a) and (b), no apparent process-related changes in hardness distribution were detected. These results are in agreement with the microstructural observation, previously discussed, where no visual microstructural changes induced by the process were seen in the Ti-6Al-4V part.

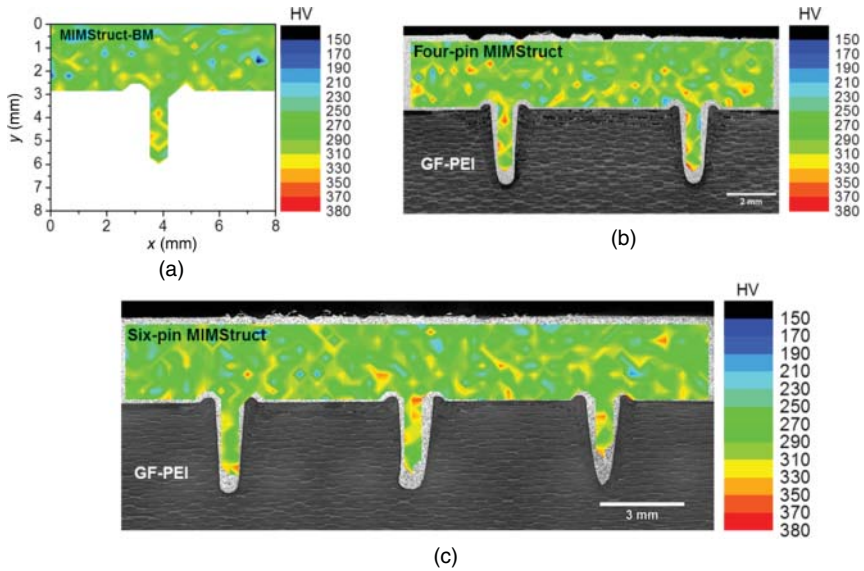


Figure 11.13 Vickers microhardness maps of the MIMStruct base material (a), four- (b) and six-pin (c) MIMStruct parts after joining. Indentations spacing were set to 0.3 mm in x and y directions and, 0.2 kgf load was applied for 10 s for each indentation.

Although no process-related changes were observed in the microhardness maps, large variations of the hardness values are present all over the parts. These variations are mainly related to the residual porosity. Titanium parts produced by MIM typically present residual porosity varying from 3% to 4% [29]. When indentations are performed over these pores, the hardness values tend to vary, as seen in the microhardness maps. The residual pores may be closed by an additional hot isostatic pressing (HIP) stage during the manufacture of the MIMStruct, resulting in practically 100% dense material [29].

11.4.5 Global Mechanical Properties of the U-Joining Joints

Global mechanical properties of the hybrid joints were evaluated through a customized lap shear test. Tests were carried out using a Zwick/Roell universal testing machine with a load capacity of 100 kN with a constant crosshead speed of 2 mm/min at room temperature. Two different overlap areas were used, according to the number of pins. Figure 11.14 schematically shows the customized lap shear samples and its respective dimensions in millimeters.

The lap shear force versus displacement curve for the optimized four- and six-pin joints and non-reinforced reference joints produced with the same process conditions and overlap area are shown in Figure 11.15. As expected, the increase in the macromechanical interlocking significantly increases the

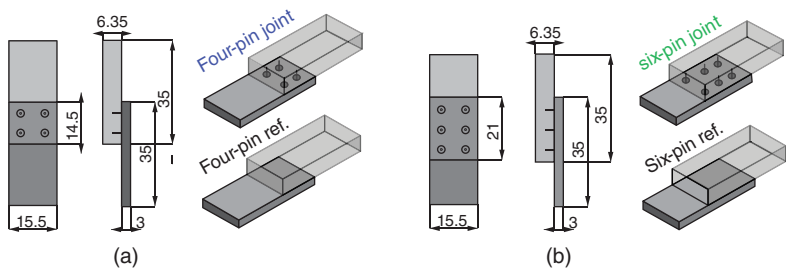


Figure 11.14 Geometry of the lap shear specimens: (a) four- and (b) six-pin joints and their respective non-reinforced reference.

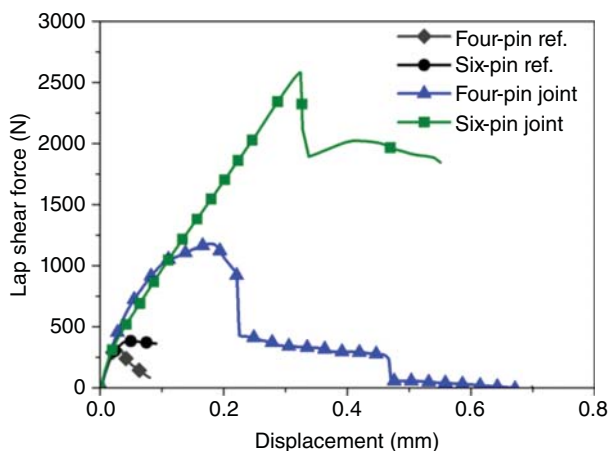


Figure 11.15 Lap shear force versus displacement curves for the non-reinforced reference, four- and six-pin hybrid joints. Curves display the replicates with the highest ULFS.

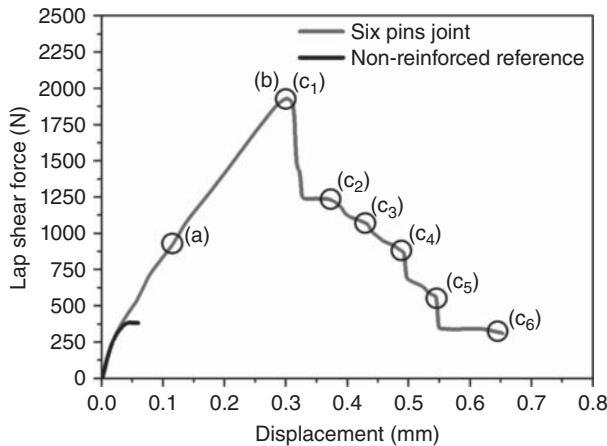
strength of the reinforced joints in comparison to non-reinforced reference. Moreover, the raise in pin number also created a positive effect on the joints' strength. The six-pin joint endured a maximal ultimate lap shear forces (ULSF) of about 5.5 times higher (ULSF of 2011.4 ± 530.7 N) than that of the reference (ULSF of 368.7 N), while the four-pin joint showed 2.4 times increase (ULSF of 831.7 ± 336.2 N). Although further process optimization is required to decrease the rather large standard deviation (Table 11.3), the positive contribution of the addition of pins to the fracture toughness can be clearly seen in Figure 11.15, whereby the addition of pins seems to have increased the damage tolerance of joints in comparison to the references.

The six-pin joint (replicate R1, Table 11.3) was selected to study the failure regime during the lap shear testing. This specific replicate was selected once it presented well-defined transitions in stiffness (Figure 11.16) due to the joint

Table 11.3 Compilation of the lap shear testing results.

MIMStruct	Replica	ULSF (N)	ULSF Avg $\pm$ Std
Non-reinforced reference (overlap area 14.5×15.5 mm)	R1	389.2	368.7
	R2	–	
	R3	348.1	
Non-reinforced reference (overlap area 21×15 mm)	R1	333.7	373.4 ± 35
	R2	403.5	
	R3	383.1	
Four-pin joint	R1	1180.3	831.7 ± 336.2
	R2	509.4	
	R3	805.5	
Six-pin joint	R1	1927.7	2011.4 ± 530.7
	R2	2579.0	
	R3	1527.51	

ULSF, Ultimate Lap shear force; Avg, average; and Std, standard deviation.


Figure 11.16 Load-bearing behavior of a six-pin joint (replicate R1), where (a) corresponds to elastic regime, (b) to adhesion failure, (c_1) to breakage of the first row of pins, and (c_2 – c_6) to sequential failure of the remaining pins.

damaging during the test. At the first stage (Stage (a)), deformation takes place in the elastic regime with both bonding mechanisms – pins' mechanical interlocking and adhesion forces – contributing to the load bearing. When the applied load surpasses the mechanical resistance of the consolidated polymer layer, adhesive failure takes place (Stage (b)) and a crack starts propagating.

At this point, load is mainly born by the pins. In Stage (c_1), the failure of the first row of pins takes place and a sharp decrease in stiffness is observed. The remaining stages (Stages c_2 – c_6) correspond to a sequential failure of the MIMStruct pins. Therefore, after the failure starts, the pins slow down the propagation rate (crack-arresting effect) increasing the fracture toughness.

11.4.6 Fracture Surface Analysis

Surface fracture analysis indicated that the joints failed under complex failure mechanisms, involving failure by shear of metallic pins and mixed cohesive–adhesive failure of the composite part. Overview and detailed fractographic images of the composite and metallic parts of a six-pin joint are depicted in Figure 11.17. The composite macroscopic examination revealed that the pins remain completely inserted in the GF-PEI after shear failure (Figure 11.17(a) and (b)); the glass-fiber woven reinforcement was also exposed. 3D-rendered images obtained by the laser confocal microscopy of the fracture surface showed that the shear cracks initiated and propagated in the bottom of the pins, right above the undercut feature, as schematically shown in Figure 11.17(c). Apparently, there is also a layer of darkened polymer matrix spread out on the fracture surface, which may be a result of polymer matrix thermal degradation at the metal–composite interface. From the macro examination of the metal fracture surface (Figure 11.17(b)), it was observed that the molten polymer effectively wetted its surface, thereby evidencing the formation of adhesion forces between the materials. Broken glass-fiber bundles were also identified attached to metal fracture surface.

The fracture micromechanisms were assessed by scanning electron microscopy (SEM) through the composite (Figure 11.18) and MIMStruct (Figure 11.19) fracture surfaces. Figure 11.18(a) depicts a broken pin fractograph obtained from the composite surface and adjacent regions, which were plastically deformed during the load bearing. Therefore, it helps to corroborate the assumption that the pins interlocking and adhesion forces successfully transfer the load from the joints' interface through the thickness of the composite. At this adjacent zone, large amount of broken fibers were exposed (Figure 11.18(b)). These fibers, as previously discussed (Section 11.4.3), remain embedded in the molten polymer; they flowed to the undercut at the base of the pins during the joining process.

Pure adhesion failure regions were identified on the fracture surface of the composite, marked by the white arrows in Figure 11.18(c), which are surrounded by thermal voids created during the joining process. Pulled and intact fibrils connecting the thermal voids can be observed (light gray arrows in Figure 11.18(c)), therefore, indicating ductile microfailure.

The fracture surface of the pins (Figure 11.19(a)) is characterized by a fully ductile pattern developed by nucleation, growth, and coalescence of microvoids

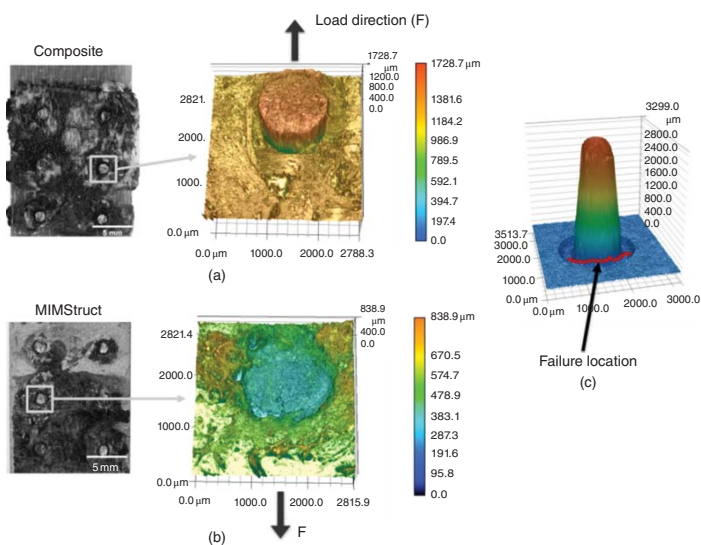


Figure 11.17 Fractographic analyses of the joint after lap shear testing. Composite fracture surface (a), fracture surface of MIMStruct part (b), and a schematic view of the shear failure location in the bottom of pins (c).

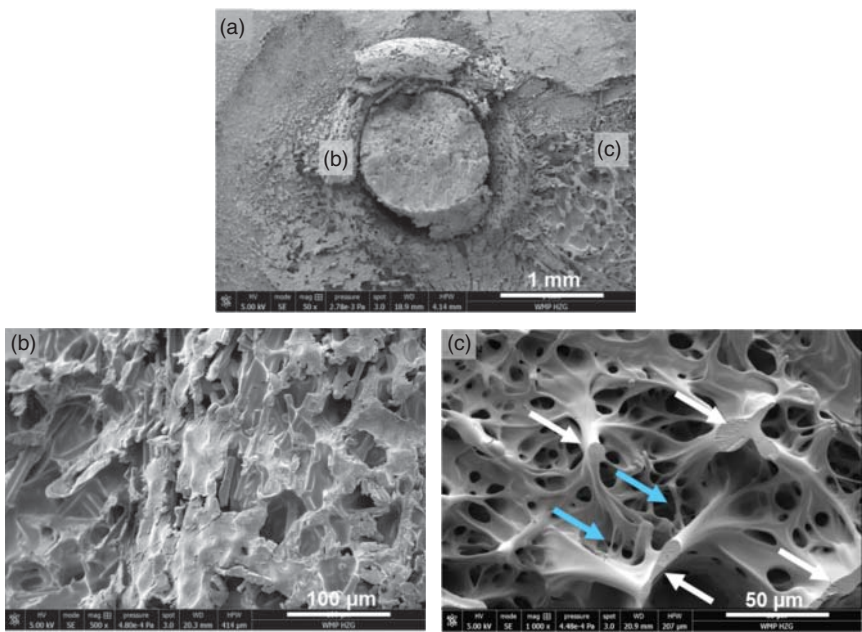


Figure 11.18 SEM analysis of the composite fracture surface. (a) Overview of a pin fracture area, (b) adhesion failure region and, (c) detail of the pin adjacent area.

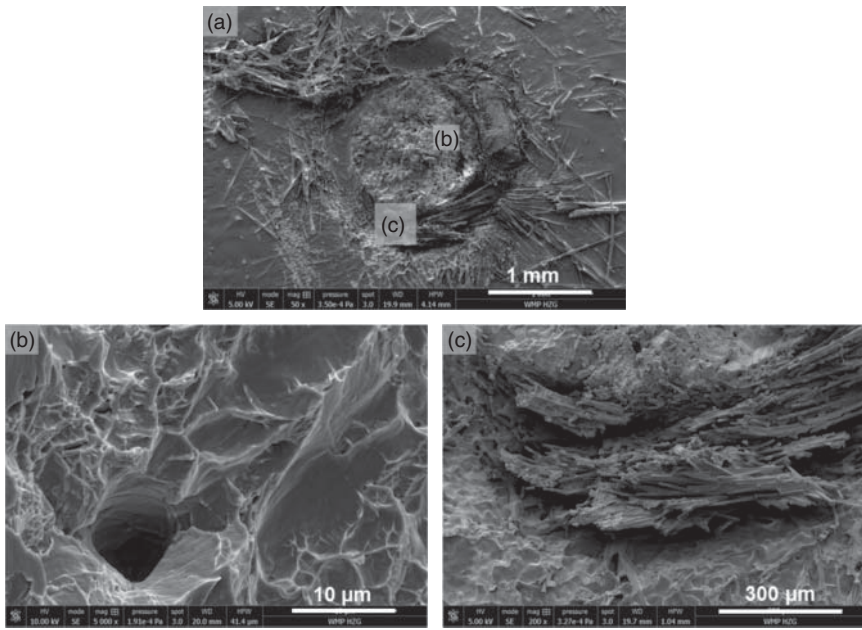


Figure 11.19 SEM analysis of the MIMStruct fracture surface. (a) Overview of a pin fracture area, (b) dimples formation on the pin fracture surface and, (c) cohesive fracture region with fibers attached to the Ti-Al6-4V part.

(Figure 11.19(b)). Due to the considerable amount of porous distributed in the MIMStruct, which typically act as failure nucleating sites, the precise fracture initiation site could not be identified. Furthermore, bundles of attached fibers and composite consolidated layer were also presented on the MIMStruct surface, as shown in the SEM images in Figure 11.19(a) and (c), corroborating the evidence of a mixed cohesive–adhesive microfailure mechanism.

11.4.7 Conclusions

A new approach to manufacture of damage-tolerant and crash-resistant lightweight alloy/composite hybrid structures has been introduced. The joints produced by U-Joining combined mechanical interlocking due to the reinforced elements inserted in the fiber-reinforced polymer and adhesion forces created at the interface metal/fiber-reinforced polymer. Furthermore, the following conclusions could be drawn from the presented case study:

- The process temperature recorded at the joints' interface was inside of the degradation range of the polymer matrix. However, the exposure time was short and the heat generation was localized at the pins' regions.
- The association of the high-frequency oscillation motion and axial force applied during the U-Joining generated enough frictional heat to soften the polymer matrix and, therefore, allow the complete penetration of the pins in the composite.
- Microstructural changes were observed in the GF-PEI including formation of thermal flaws. However, the thermal history does not affect the Ti-6Al-4V microstructure and its local mechanical properties. Moreover, the formation of a very intimate interface between Ti-6Al-4V and GF-PEI was observed.
- In terms of global mechanical properties, the six-pin hybrid joints showed about 5.5 times more ULSF and a significant improvement in fracture toughness in comparison to a non-reinforced reference joint.
- The joints' failure mechanism combines shearing of the metallic pins and mixed cohesive–adhesive failure of the composite.

11.5 Advantages and Limitations

The main advantages of the U-Joining are precise control of the heat generation (which tends to be localized at the pins' region), the joining is clean, safe (no spatter, sparks, or fumes), and faster in comparison to the state-of-the-art technologies. Furthermore, the ultrasonic-based joining techniques are extremely energy efficient, environmentally friendly, safe for the operator, and have the ability of produce hybrid joints without the use of any solvent,

adhesive, or additional materials. After joint consolidation, as demonstrated in this case study, the pins anchored in the composite generate effective bonding mechanisms and act redistributing the mechanical stresses imposed during loading to the composite part. This has a potential impact on the damage tolerance of future hybrid structures.

Regarding limitations, U-Joining is suitable only for overlapped configuration and is not directly applicable to thermoset-based composites (the use of a solid thermoplastic film or adhesive layer may be required under this condition). Moreover, U-Joining hybrid joints cannot be disassembled without local damage of the structure, which may make its inspection difficult.

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Part IV

Design of Experiments and Statistical Analysis in Joining Process Development

12

Factorial Design of Experiments for Polymer–Metal Joining

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12.1 Introduction

Design of experiments (DoE) is a very important engineering tool for process development and optimization. When combined with statistics – for example, analysis of variance (ANOVA) – engineers and scientists can understand and explain the correlations between process parameters (the “factors”) and properties of products, specimens, or new materials, with a high confidence level. Recently, DoE and ANOVA have been increasingly applied in welding and joining as a tool for weld or joint optimization (e.g., to optimize mechanical performance). This chapter presents the basics of DoE and emphasizes on its utility for the development, deeper understanding, and optimization of metal–polymer joining technologies. Factorial DoE are presented and exemplified based on the authors’ and their collaborators previous experience. Case studies presented in this chapter are examples from research works within the Advanced Polymer–Metal Hybrid Structures Group at the Helmholtz-Zentrum Geesthacht, Germany, and its international cooperation projects. For detailed understanding of the theories of DoE, statistical analysis, and ANOVA, we strongly recommend the textbooks by Montgomery [1] and other further complementary texts, such as by Taguchi [2].

12.2 Design of Experiments

The DoE nowadays plays a very important role in both industrial and scientific research. It has the understanding of a given process or activity, by

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controlling the input variables involved and analyzing their effect on the outcome predetermined characteristics, as main objective. In order to collect the maximum amount of data and statistically evaluate the obtained results, a well-planned procedure must be applied so to minimize the amount of resources (i.e., materials, man power and equipment, etc.) used for this purpose. This planning must be as simple as possible, bearing in mind the intended outcome information. A traditional and simplistic approach consists of evaluating the influence of a given factor, input variable of the process, on the evaluated response, outcome characteristic, investigating one-factor-at-a-time (OFAT), maintaining the remnant factors at a constant value. This approach most of the times renders itself costly, due to the amount of resources and time needed to implement such a procedure. Nonetheless, OFAT can be a useful tool for the brief investigation of the unique influence of a single parameter in simple processes or small parameter ranges [1, 3].

The use of a DoE allows cause–effect correlations to be established between the process input factors and the process responses. A DoE should be planned so to allow:

- a reduction or minimization of the number of experiments or units to be performed or produced;
- all input factors must be varied;
- a clear choice of strategy that renders sound data solutions after each experimental sequence.

The potential benefits of DoE for manufacturing and welding processes include [1, 3]:

- improved process yield and stability
- improved profits and return on investment
- improved process capability
- reduced process variability, hence better product performance and consistency
- reduced manufacturing costs
- reduced process design and development time
- heightened work force morale with success in chronic-problem solving
- increased understanding of the correlations between key process inputs and output(s)
- increased business profitability by reducing scrap rate, defect rate, rework, retest, and so on.

There are different categories of factors that affect response variables, based on three key characteristics' magnitudes of influence on response variables, degree of controllability, and measurability (e.g., precision and accuracy). These factors can be explained as follows [1, 3]:

1. Control variables are measurable, controllable, and thought to be (very) influential.
2. Held-constant factors are controlled.
3. Nuisance factors are uncontrolled factors (either they cannot be controlled, or they are allowed to vary).

The main effect of a given factor is defined as the variation produced in the response in question, by the change of that factor's level, its value or magnitude. When this variation is not regular, given that other factors are also varied, the interaction between these factors needs to be verified.

In some processes, there are nuisance factors that cannot be controlled, such as intrinsic experimental errors, environmental conditions, variations in base material batches, equipment architecture and functionality, among others. These can be considered as noise and be statistically treated as experimental errors. These differ from held-constant factors in a manner that they cannot be deliberately set to a constant level for all experimental units. If, however, the level can be selected for any experimental unit, blocking or randomization might be appropriate. If levels cannot be selected (i.e., the levels of the factor are unpredictable, perhaps continuous), then the nuisance factor becomes a covariate in the analysis, which means that it cannot be controlled by the experimenter, but can be observed along with another controllable variable) [1].

If a nuisance factor is not measurable and thought to be very influential, it may also be called an experimental risk factor. Such factors can inflate experimental error, making it more difficult to assess the significance of control variables. They can also introduce bias to the results.

Therefore, to guarantee that the most efficient method is applied to the case at hand, the following questions should be answered:

- How to assess the intended response (e.g., joint tensile strength) and the factors' (e.g., welding or joining parameters) main effects?
- How will the factors be simultaneously varied?
- How many replications of each experiment, units produced, will be needed?
- Which type of data analysis should be performed?
- Which effects' magnitude will be considered significant?

12.2.1 Factorial Design of Experiments

Factorial designs are the basis of the most commonly used experimental designs. On these designs, every possible level–factor combination is performed for each design. For k – number of factors, each with n levels, there are nk combinations. Due to the fact that these designs can become costly depending on the number of experiments needed, they are mainly used where reduced number of factors and levels are necessary.

12.2.1.1 General Description

These types of designs are defined by a set of variables/factors $x_1, x_2, \dots, x_N$, which can assume different values or levels. A factorial design is considered a good design due to the fact that it is both an orthogonal and a balanced design type. In an orthogonal design, the effects of each factor can be determined independently. Balanced refers to the fact that every factor–level combination is present the same number of times on the design plan.

The randomization of the experimental runs, each defined by one specific factor–level combination, contributes to a reduction of possible bias effects, by averaging these throughout the levels of each design factor. This is achieved by randomly assigning each factor–level combination to a position of the experimental run sequence from 1 until N .

The design should also include repetition runs, where each factor–level combination run is performed or observed more than once. From them, a possible standard deviation is determined, accounting for variations due to measurement errors or changes in the experimental environment. This allows a good evaluation of the fitted model's adequacy, without the need to consider one of the factor interactions negligible, so to consider it as the experimental variability. The repetition runs should also be randomized.

The measured effects are defined as variations of the average responses due to different factor–level combinations or experimental conditions. Considering a two-level design (e.g., the minimum and maximum rotational speed values in friction riveting), the measured effects can be described as main effects, two-factor and three-factor interactions. The main effects are the differences between the average responses obtained from the two levels of each factor. An advantage of this DoE is the fact that the results can be analyzed by means of regression.

The factors in this design are used as coded values, meaning that a factor will assume, for the two-level design, either the value -1 or $+1$. The coding of a quantitative variable is possible by applying

$$X_i = \frac{x_i - \bar{x}_i}{\frac{1}{2}\Delta x_i} \quad (12.1)$$

with X_i being the code value of the factor i , x_i is the actual value, $\bar{x}_i$ is the mean value, and Δx_i is the range of the factor.

In the case where all factors are quantitative, the experimenter should perform additional center point experiments, which would be coded 0, in order to evaluate the design for nonlinearities.

12.2.1.2 Analysis of Variance

Considering that the focus of the experimenter is to better understand how certain factors – for example, joining process variable inputs – influence a given response – for example, joint mechanical strength – the main effects of those

factors and the interactions between factors should be evaluated, by assessing statistically relevant differences in averages between several levels of a factor or different groups. An analytical method widely used is ANOVA, performed on the data collected using different DoE. This analysis is performed by separating the observed variability into explainable variability, taking into account the data and regression models, or nonexplainable variability. Making use of sum-of-squares tests, the variability can be explained as originating from individual main effects or factor interactions [3].

ANOVA is based on three key assumptions: a uniformity of variance, property also referred to as homoscedasticity, is verified; the response has a normal distribution; and the experiments are independent. Although in reality the assumption of normal distribution is rarely achievable, the analysis can still give useful information to the experimenter, taking into account the distribution's deviations [1].

12.2.1.3 Interpretation of Results and Design Validation

In a two-level factorial design, the data collected can be analyzed either by means of ANOVA or by regression analysis. The latter is considered fairly simpler, especially due to its orthogonality property [4].

The most used method for analyzing the results of orthogonal designs is the ordinary least squares regression (OLS), which takes into consideration an approximated normal distribution and independent errors. Statistical model validation can be achieved by several classical statistical tests, the following being the most used: the lack-of-fit, to assess the model's adequacy; the *t*-test, which yields the significance of individual model terms; and tests based on the externally studentized residuals [5]. If none of the factors are found to be statistically significant, meaning these have no considerable effect on the response, then changes should be made to the design, for example, increasing the step between the factor levels to broaden the scope of the design.

The *t*-test takes into account the statistic found by dividing a regression coefficient by its standard deviation, which evidences a *t*-distribution having $n - p - 1$ degrees of freedom, p being the number of parameters in the model. Significance is found for model terms with a *p*-value lower than 0.05, when working with a confidence interval of 95% (a condition typically used in welding and joining). In other words, $p = 0.05$ means that there is a 5% chance that the experimenter is working with wrong assumption. Considering a design with replicate experiments, one can apply a lack-of-fit test, due to the fact that these replicates allow the estimation of the pure error. Taking the replicates into account in the regression model, one can find the lack-of-fit sum of squares, SS_{LOF} , by using

$$SS_{\text{LOF}} = SS_{\text{R}} - SS_{\text{E}} \quad (12.2)$$

$$SS_R = \sum_{i=1}^n (y_i - \hat{y})^2 \quad (12.3)$$

$$SS_E = \sum_{i=1}^{n_r} (y_i - \bar{y})^2 \quad (12.4)$$

where SS_R is the residual sum of squares, SS_E is the pure error sum of squares, y_i the i 'th response, $\bar{y}$ the mean value of the replicates, $\hat{y}$ the value given by the model, and n_r the number of replicates. Then the lack-of-fit test statistic, F_{LOF} , can be determined as,

$$F_{LOF} = \frac{MS_{LOF}}{MS_E} \quad (12.5)$$

where MS_{LOF} is the lack-of-fit mean squares and MS_E the pure error mean square. If the value of F_{LOF} is significantly higher than 1, the model is considered not adequate, evidencing lack-of-fit. If the value is much smaller than 1, then it is a case of overfit.

12.2.2 Examples of Factorial Design of Experiments in Joining Process Development for Metal–Polymer Hybrid Structures

DoE is a powerful statistical tool used to optimize welding and joining processes. When coupled with an ANOVA, the DoE elucidates the influence of the joining process on joint characteristics. The welding process parameters and the tool geometry affect the properties of friction stir spot-welded high-density polyethylene sheets, as indicated by Bilici [6, 7]. These researchers investigated the effects of the rotational speed, plunge depth, plunge rate, and dwell time of the tool on the joint lap shear strength. By applying a “one-factor-at-a-time” approach, the authors found that the rotational speed, plunge depth, and dwell time were important when optimizing the ultimate lap shear strength. The dwell time and the rotational speed significantly influenced the lap shear strength, given that increasing the dwell time or rotational speed increased the frictional heat and enlarged the welded areas. Harras *et al.* [8] studied the effect of process parameters using an “one-factor-at-a-time” approach to determine the optimized welding conditions for ultrasonic welding with poly(ether-ether-ketone) (PEEK)–carbon composites. Dashatan *et al.* [9] performed statistical analysis according to a two-level full-factorial design, along with ANOVA and the signal-to-noise ratio, to understand the influence of the tool rotational speed, tool plunge rate, and dwell time on the friction stir spot-weld strength between PMMA and ABS polymeric sheets. They concluded that all of the parameters significantly influenced the lap shear strength of the joints and fracture modes. Although a limited number of DoE and ANOVA studies on similar joints and welds were published, very few studies on metal–polymer joints have been published. This section intends

to exemplify the use of factorial designs of experiments for metal–polymer joining processes. Practical examples from our R&D work were selected for this purpose.

12.2.2.1 Case Study 1 – Full-Factorial Design in Friction Riveting

According to Myers and Montgomery [10], Full-Factorial Design is a design in which all possible combinations of the factor levels are fulfilled. The result from the full-factorial experiments would be more reliable, but conducting the full-factorial experiments is costly and sometimes prohibitive. However, this is a DoE often used in cases where the number of factors is reduced (in welding and joining frequently a number of three to five factors), and a more comprehensive understanding of factor interactions greater than 1 is needed.

The first example of the use of full-factorial DoE in metal–polymer joining process development comes from Friction Riveting (FricRiveting) [11, 12]. FricRiveting is a friction-based spot joining process developed for joining thermoplastics and thermoplastic composites similar and thermoplastic (composite)/lightweight alloy hybrid structures [13–17]. More information on FricRiveting can be found in Chapter 8 – Friction Riveting. Part of the contents of this case study is partially or fully reproduced with authorization from Altmeyer *et al.* [15].

Earlier studies of friction riveting (FricRiveting) using unreinforced polyetherimide (PEI) and aluminum AA2024 [18, 19] have successfully demonstrated the feasibility of metallic-insert joints. One of our previous studies has described the influence of a single FricRiveting process parameter, the rotational speed on the joint microstructure (OFAT design), the local and global strength, and the thermal history [20, 21]. However, no further research was found describing how to tailor mechanical performance or to apply advanced DoE and statistics to analyze the correlation between the relevant process parameters and the joint performance in FricRiveting.

A 2^k full-factorial DoE was chosen to investigate the impact of rotational speed, friction time, friction pressure, and forging pressure on joint formation and tensile mechanical performance. The joint formation was studied by analyzing the rivet anchoring performance (the response being represented by the mushrooming efficiency, which is the percentual increase in rivet tip diameter after FricRiveting), the rivet penetration depth, and the mechanical energy input. The tensile pull-out force was used as the response for the mechanical performance of the investigated metallic-insert joints. The base materials used were Φ 5 mm titanium grade 3 rivets and 21 mm 30% in weight short-carbon fiber-reinforced polyether etherketone (PEEK-CA30) plates. Changes in rivet geometry were investigated with X-ray tomography before any mechanical testing, to acquire the dimensions of the anchored rivet inside the reinforced polymer, so to support the elucidation of their correlations with the mechanical performance. Therefore, the statistical DoE models can

be used to tailor joint formation and mechanical performance for the joints investigated. Furthermore, a parameter set maximizing the tensile pull-out performance could be determined using an ANOVA to optimize this response. Therefore, a better understanding of these effects may enable joint formation and performance to be tailored according to the requirements of the potential engineering applications.

DOE and Statistical Analysis for Evaluating the Joint Mechanical Performance

The factor levels (−1, +1) of the joining parameters were set as follow: the rotational speed (RS) was varied from 18,000 to 20,000 rpm, the friction pressure (FP) was varied from 0.7 to 0.9 MPa, the forging pressure (FoP) was varied from 0.9 to 1.0 MPa, the friction time (FT) was varied from 1.0 to 1.5 s, while the forging time (FoT) was kept constant at 10 s to ensure sufficient and uniform joint consolidation. The experiments were planned according to a 2^k full-factorial design, including an additional center point. This approach was used to evaluate the effects of the process parameters on the joint formation and mechanical performance under tensile loading. In this way, the input parameters (factors) were the process parameters: RS, FT, FP, and FoP. The output parameters (responses) were the mushrooming efficiency, the rivet penetration depth (H), the mechanical energy input, and the pull-out force ($F_{\text{pull-out}}$).

The DoE 2^k full factorial test matrix is shown in Table 12.1, including the low and high levels, as well as the center point configuration of the process parameters. This full-factorial design contains four factors on two levels, making the number of configurations $N = 2^4 = 16$ with a center point on the intermediate position of all factors (factor level “0”) to generate 17 different joining conditions (Table 12.2). Three replicates were produced for the corner points, and six replicates were produced for the center point to account for the statistical variance.

Table 12.1 Full-factorial design matrix showing the two levels (−1, +1) of the investigated factors and the center point configuration (0) that corresponds to the intermediate position of all factors.

Factor	Level			Unit
	−1	0	+1	
Rotational speed	18,000	19,000	20,000	rpm
Friction time	1	1.25	1.5	s
Friction pressure	0.7	0.8	0.9	MPa
Forging pressure	0.9	0.95	1.0	MPa
Forging time	10			s

Table 12.2 Summary of the test results for all 17 configurations (2^k full-factorial runs and center point with six replicates).

Configuration	Factors process setting parameters			
Run	RS (rpm)	FT (s)	FP (MPa)	FoP (MPa)
1	18,000	1.5	0.7	0.9
2	20,000	1.5	0.7	0.9
3	18,000	1.5	0.9	1.0
4	20,000	1.5	0.9	1.0
5	18,000	1.0	0.7	1.0
6	18,000	1.5	0.9	0.9
7	18,000	1.0	0.9	1.0
8	18,000	1.5	0.7	1.0
9	19,000	1.25	0.8	0.95
10	20,000	1.0	0.9	1.0
11	20,000	1.5	0.7	1.0
12	20,000	1.0	0.7	0.9
13	20,000	1.0	0.7	1.0
14	18,000	1.0	0.7	0.9
15	20,000	1.0	0.9	0.9
16	18,000	1.0	0.9	0.9
17	20,000	1.5	0.9	0.9

The parameter configuration resulted in the selected two-level full-factorial designs, and center points are presented the design matrix in Table 12.2:

Influence of the Joining Process Parameters on the Ultimate Tensile Pull-Out Force

The effects of the four process parameters RS, FT, FP, and FoP on the joint ultimate tensile force were evaluated. Depending on the combination of process parameters, the results varied from 6.3 to 10.7 kN.

Figure 12.1 shows the effect of all four joining process parameters on the joint ultimate tensile force. The higher the RS, FT, and FoP are, the higher the pull-out force will be. The increases in RS, FT, and FoP lead to higher mechanical energy input values, leaving additional heat available for deforming and widening the rivet tip. Therefore, the wider the rivet tip (mushrooming efficiency) is, the larger the volume of polymer above the deformed rivet tip is; therefore, higher forces are required to pull the metallic insert out of the polymeric substrate. From the ANOVA analysis, a nonlinearity of the intermediate level of FP can be observed, which may indicate a nonlinear dependence between the FP and the ultimate tensile force.

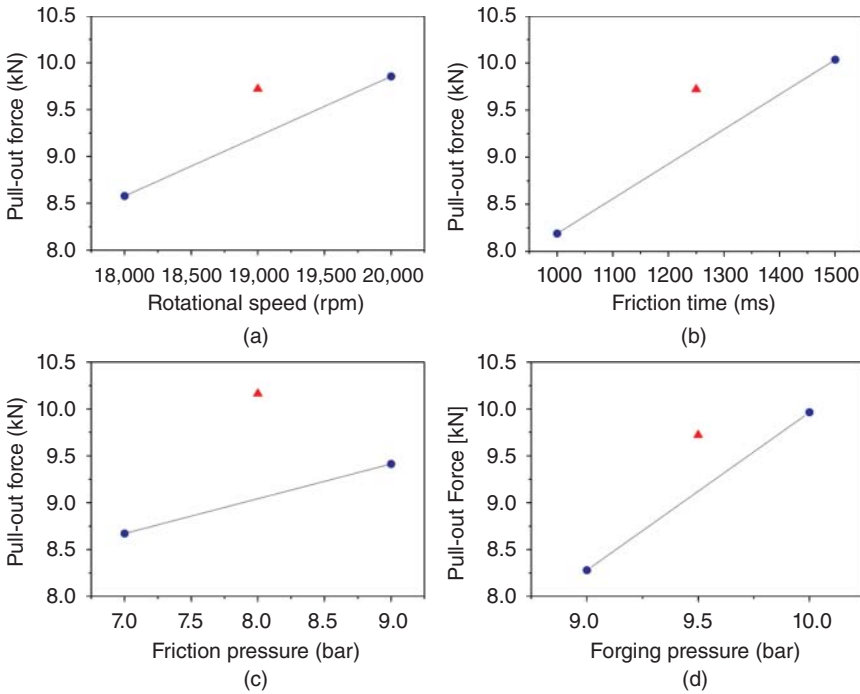


Figure 12.1 Main effects plots showing the relationship between the friction-riveting conditions ((a) RS, (b) FT, (c) FP, and (d) FoP) and the pull-out force.

The ANOVA also confirmed the significant effect exerted by all four process parameters (p -values for RS, FT, FP, and FoP were 0.000) on the ultimate tensile force. Moreover, the influence of two-factor interactions FT-FoP (p -value of 0.001) and RS-FT (p -value of 0.022) is pronounced.

The summary of these main effects plots reveals an optimized process parameter set that might be used to increase the rivet anchoring performance (i.e., to increase mushrooming efficiency). In this optimized parameter set, RS, FT, and FoP must be set at higher levels (RS: 20,000 rpm, FT: 1.5 s, FoP: 1 MPa), while the FP should be set at an intermediate level (FP: 0.8 MPa) because these were the process parameter levels responsible for the highest mushrooming efficiency. Although this parameter set was not part of the factorial runs of the DoE, three joints have been produced and tested to validate the findings of this analysis. The results are reported in a separate manuscript and basically confirmed the expected results [22]. The specimens friction riveted with the optimized process parameter combination resulted in a high mushrooming efficiency of 91.4%. There was only one configuration for the initial study that presented a slightly higher mushrooming efficiency (93.6%): configuration 4 (RS: 20,000 rpm, FT: 1.5 s, FoP: 1 MPa, and FP: 0.9 MPa). Configuration

4 also shows a higher standard deviation (1%) compared to the optimized configuration (0.11%), making the optimized configuration the more robust process parameter combination that still leads to a very high rivet anchoring efficiency.

The analysis of the influence of the joining parameters on the other presented responses (mushrooming efficiency, indentation depth, energy) has been performed on a similar basis and follows the same logic as the discussion of the effects on the joint mechanical performance (the same study shows that the responses mushrooming efficiency and pull-out force are also directly dependent). These findings were reported in [22].

Statistical Model Validation: Evaluation of the Impact of Process Parameters on Joint Properties The acquired data sets were analyzed by using parametric ANOVA. The four responses were analyzed separately. Therefore, the dependent variable was the mushrooming efficiency, the penetration depth, the mechanical energy, or the pull-out force while RS, FT, FP, and FoP were the independent variables. The ANOVA model was modified to include the independent variables and the relevant two-factor interactions. Therefore, the regression analysis of the collected data could be used to predict the impact on the responses due to the significant process parameters and their interactions. The multiple regression equation used is a two-way interaction equation:

$$R = \beta_0 + \beta_1 x_1 + \beta_2 x_2 + \beta_3 x_3 + \beta_4 x_4 + \beta_{12} x_1 x_2 + \beta_{13} x_1 x_3 + \beta_{14} x_1 x_4 + \beta_{23} x_2 x_3 + \beta_{24} x_2 x_4 + \beta_{34} x_3 x_4 \quad (12.6)$$

In this model, R is the response (i.e., mechanical energy input, penetration depth, mushrooming efficiency, and the ultimate tensile force), while the predictor variables include RS (x_1), FT (x_2), FP (x_3), and FoP (x_4). The effects of the different factors and their interactions on the four different responses are shown in the Pareto charts displayed in Figure 12.2. The effects of all the factors and their interactions are standardized, meaning that each effect was divided by its standard error. The length of each bar in the charts is directly related to the absolute value of its associated regression coefficient; the order in which the factors are organized corresponds to the size of the effect, starting with the factor with the highest influence on the evaluated response. The vertical line in each Pareto chart corresponds to a 95% confidence level indicating statistical significance. Therefore, the effect of a factor can be considered significant if its corresponding bar crosses this line in the Pareto chart. The multiple regression equation for each response, including only the significant process parameters and interactions, could be expressed as:

$$E_{\text{mech}} = 8174 - 0.44\text{RS} - 2.94\text{FT} - 889\text{FP} + 0.0506\text{RS} \times \text{FP} + 0.00018\text{FT} \times \text{RS} \quad (12.7)$$

$$T = -16.7 + 0.000861\text{RS} + 0.00483\text{FT} + 0.55\text{FP} \quad (12.8)$$

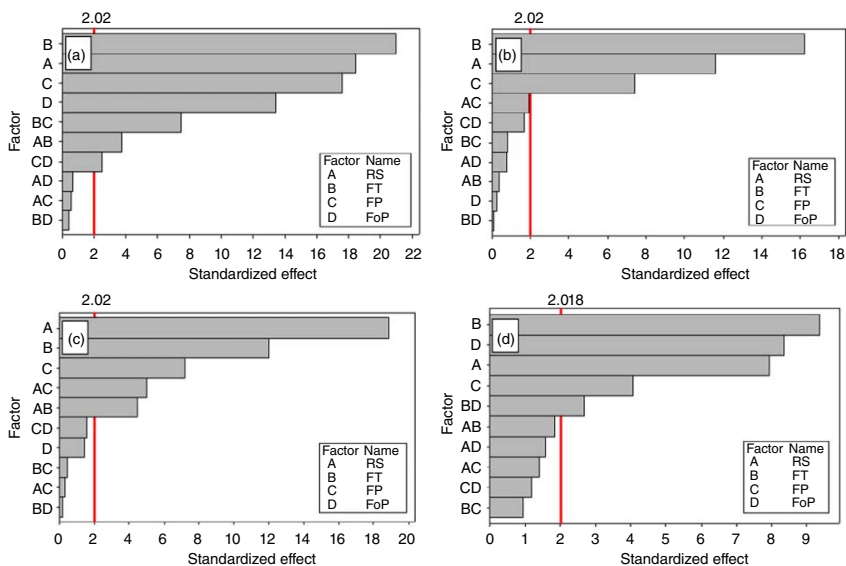


Figure 12.2 Pareto charts of the standardized effects for (a) mushrooming efficiency, (b) indentation depth, (c) mechanical energy, and (d) pull-out force.

$$e_{\text{mush}} = -31.5 - 0.00009\text{RS} - 0.198\text{FT} - 8.86\text{FP} + 12.2\text{FOP} \\ - 0.0134\text{FT} \times \text{FP} + 0.000007\text{FT} \times \text{RS} \quad (12.9)$$

$$F_{\text{pull-out}} = -49.7 + 0.000723\text{RS} + 0.0221\text{FT} + 0.37\text{FP} + 3.98\text{FOP} \\ - 0.00197\text{FT} \times \text{FOP} \quad (12.10)$$

The individual regression equations contain only the factors proven to be significant by using their p -values and the Pareto charts in Figure 12.2. Figure 12.3 compares the predicted data calculated using the equations given earlier and the measured data of all four responses. These scatter plots include a 45° line that demonstrates the perfect linear correlation between the measured and the predicted data; lines representing a 10% deviation, typically adopted in polymer welding, were added here as a complement. A standard deviation of 10% is a common value associated with the mechanical property variations due to differences in production batches of the same plastic part.

The hypothesis test of the regression analysis was performed at an error rate of 5% (i.e., 95% for the confidence level). Figure 12.3 shows the good agreement between the predicted and measured data for all four responses, as fitted by the linear correlation between the measured and predicted data sets. This response proves that the regression equations given in Equations 12.7–12.10 can be used to predict the mushrooming efficiency, the rivet penetration depth, the mechanical energy input, and the ultimate tensile force (pull-out force) as a function of the significant process parameters and their two-factor interactions. The accuracy levels of the predictions for the mechanical energy input, the penetration depth, the mushrooming efficiency, and the pull-out force are reported in Table 12.3. The model equations (Equations 12.7–12.10) present a very good accuracy level, varying from a minimum of 80.7% to 96.1%.

Conclusions of the Case Study

- DoE was used to investigate the impact of four process parameters (RS, FT, FP, and FoP) on the joint formation and performance. The joint formation was studied using the rivet anchoring performance represented by the mushrooming efficiency, the rivet penetration depth, and the mechanical energy input. The analysis of the DoE model can be used to identify optimized process parameter sets for diverse joint requirements. Therefore, for this material combination and the investigated parameter range, joint formation and performance can be tailored according to their specific requirements, depending on the application.
- An optimized parameter set that improves pull-out performance was developed due to the results from ANOVA. For this parameter set, RS, FT, and FoP need to be at their highest level (RS = 20,000 rpm, FT = 1.5 s, FoP = 1 MPa), while the FP must remain at its intermediate level (0.8 MPa).

The regression equations given in (12.7)–(12.10) can be used to predict the mushrooming efficiency, the rivet penetration depth, the mechanical energy

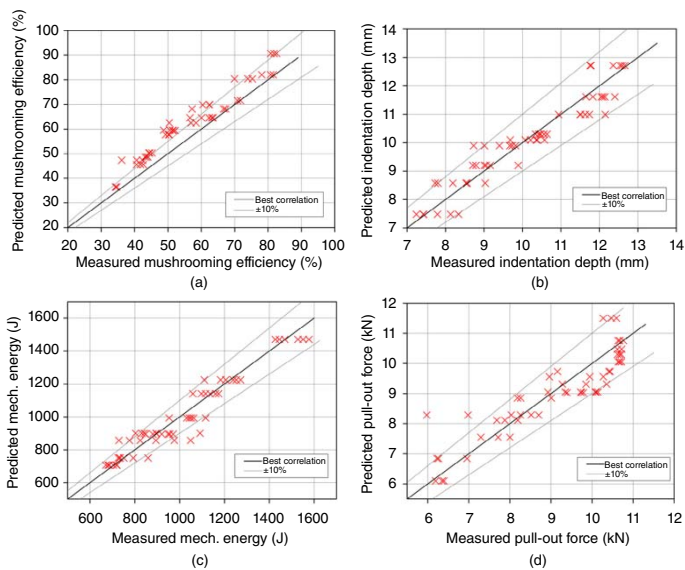


Figure 12.3 Predicted versus measured responses: (a) predicted mushrooming efficiency versus measured mushrooming efficiency; (b) predicted rivet penetration depth versus the measured rivet penetration depth; (c) predicted mech. energy input versus measured mech. energy input; and (d) predicted pull-out force versus measured pull-out force.

Table 12.3 Accuracy levels of the predictions on the mechanical energy input, penetration depth, mushrooming efficiency, and pull-out force by their regression equations.

Responses	E_{mech}	T	e_{mush}	$F_{\text{pull-out}}$
Accuracy level (%)	92.4	90	96.1	80.7

input as a function of the four investigated process parameters (RS, FT, FP, and FoP), and their two-factor interactions. These equations could predict the mechanical energy input with an accuracy of 92.4%, the penetration depth with an accuracy of 90%, the mushrooming efficiency with an accuracy of 96.1%, and the ultimate tensile force reached an accuracy of 80.7%.

12.2.2.2 Case Study 2 – Factorial Design of Experiments in Single-Lap Friction Spot Joints

The next example of factorial DoE in metal–polymer joining process development comes from Friction Spot Joining, a spot joining technique developed at HZG [23, 24] to produce strong overlap joints between metals and composites. More information on Friction Spot Joining can be found in Chapter 3. The following example is partially or fully reproduced with authorization from Goushegir *et al.* [25].

The effects of friction spot joining process parameters on the bonding area and mechanical performance of single-lap joints were investigated using two-level full-factorial DoE and ANOVA. On one hand, the main process parameters with significant influence on the bonding area were joining pressure, tool RS, and joining time. On the other hand, tool RS and joining pressure displayed the highest influence on the lap shear strength of the joints followed by tool plunge depth, whereas the joining time was not statistically significant. The interaction between the RS and joining time was the only interaction with a significant effect on the mechanical performance. Joints with ultimate lap shear force (ULSF) varying between 1698 ± 92 N and 2310 ± 155 N were obtained. It was observed that generally a larger bonding area as a result of higher heat input, leads to an increased mechanical performance of the joints. The generated regression model by the ANOVA was used to identify an optimized set of parameters for increasing the lap shear strength of the joints to 2280 ± 88 N. Furthermore, the process temperature was monitored, which varied in the range of 370–474 °C.

The single-lap shear joint geometry is widely used in the literature by different researchers to study joint mechanical performance of single-lap joints, due to its geometrical simplicity. However, no previous studies have investigated the effect of joining parameters on the mechanical performance and bonding area of single-lap shear friction spot joints for AA2024-T3/carbon-fiber-reinforced

poly(phenylenesulfide)/CF-PPS. The nominal thicknesses of the joining partners were 2 mm for the AA2024/T3 and 2.17 mm for the CF-PPS. The major objective of this study was to investigate the influence of FSpJ process parameters on the bonding area and mechanical performance by statistical analysis. Both a 2^k full-factorial DoE and ANOVA were used to statistically assess the effects of the joining process parameters on the formation of a plastically deformed zone at the composite–metal interface and the lap shear strength of the joints. Statistical models based on regression equations were established for both responses. The developed models were validated by new sets of joining parameters, and a set of joining parameters was achieved to optimize the strength of the single-lap shear joints. Finally, the formation of defects during the joining process and their influence on the mechanical performance were briefly discussed.

Statistical Analysis and Process Optimization for the Bonding Area and Mechanical Performance In this case study, the experiments were proposed according to a 2^k full-factorial DoE with four parameters each at two levels ($N = 2^4 = 16$ runs) and an additional center point, resulting in 17 different runs. The joining parameters (factors) used to evaluate their influence on the selected responses along with their corresponding levels are listed in Table 12.4.

The selected responses (sometimes also called “observations”) analyzed were the normalized plastically deformed area (PDZ) and the ULSF of the single-lap joints obtained from the lap shear testing. The PDZ is the strongest portion of the bonding area in a friction-spot joint and it has been shown to be directly related with the ULSF [26]. The PDZ area of the joints was normalized to the area of the sleeve (63.6 mm^2). Table 12.5 shows all 17 experimental runs given by the 2^4 full-factorial design and the center point. In this work, three replicates for both the corner points and the center point were produced to account for statistical variance.

ANOVA was used for statistical analysis of the results and to elaborate upon the influence of each process parameter and their interactions on the selected responses. A confidence interval of 95% was selected for the analysis. Table 12.6

Table 12.4 FSpJ process parameters (factors) and their respective levels used in the 2^3 -full factorial design of experiments in this work.

Factor (process parameter)	Symbol	Unit	Level 1 (−1)	Level 2 (0)	Level 3 (+1)
Rotational speed	RS	rpm	1900	2400	2900
Plunge depth	PD	mm	0.5	0.65	0.8
Joining time	JT	s	4	6	8
Joining pressure	JP	MPa	0.2	0.25	0.3

Table 12.5 Full-factorial DoE matrix listing the 17 given joining conditions (DoE runs).

Joining condition	Factors (process parameters)			
	RS (rpm)	PD (mm)	JT (s)	JP (MPa)
1	1900	0.5	4	0.2
2	2900	0.5	4	0.2
3	1900	0.5	8	0.2
4	2900	0.5	8	0.2
5	1900	0.5	4	0.3
6	2900	0.5	4	0.3
7	1900	0.5	8	0.3
8	2900	0.5	8	0.3
9	1900	0.8	4	0.2
10	2900	0.8	4	0.2
11	1900	0.8	8	0.2
12	2900	0.8	8	0.2
13	1900	0.8	4	0.3
14	2900	0.8	4	0.3
15	1900	0.8	8	0.3
16	2900	0.8	8	0.3
17	2400	0.65	6	0.25

lists the average and standard deviation results of the responses for all 17 joining conditions.

Influence of the Process Parameters on the Selected Responses

Influence on the PDZ Area The results of the normalized PDZ area listed in Table 12.6 show variations between 2.1 and 4.1 mm². To statistically assess the results, ANOVA with a confidence level of 95% was carried out. The analysis suggested that RS, JT, and JP have significant influence on the PDZ area because the *p*-value for all of these three factors was 0.000. However, the *p*-value for the PD was 0.429, suggesting that the influence is not significant. In addition to the main parameters, the only two-factor interaction with a significant effect on the response was PD-JT (*p*-value of 0.043). All of the other interactions resulted in a *p*-value above 0.05 and hence were not considered to significantly influence the PDZ area. Furthermore, the *p*-value of the curvature was 0.473, which means that, in the selected range, the effect of the parameters on the response can be explained linearly.

Figure 12.4 illustrates the main effect plots of the process parameters on the PDZ area. It is apparent from the figure that RS, JT, and JP significantly

Table 12.6 Summary of the normalized PDZ area and ULSF for all of the 17 joining conditions.

Joining condition	Normalized PDZ area		ULSF (N)	
	Average	STD.	Average (N)	STD.(N)
1	2.1	0.3	1905	125
2	2.8	0.4	1977	108
3	3.2	0.4	1830	96
4	3.5	0.4	1825	27
5	2.9	0.5	1941	115
6	3.7	0.3	2172	193
7	3.7	0.3	1698	92
8	4.1	0.4	2310	155
9	2.5	0.0	1981	107
10	2.9	0.1	2140	138
11	2.7	0.1	1931	113
12	3.8	0.7	2002	22
13	3.5	0.5	2049	292
14	4.1	0.1	2280	88
15	3.3	0.0	2019	225
16	4.1	0.2	2143	168
17	2.8	0.4	2075	139

influence the PDZ area, however, the effect of PD is insignificant. In these plots, a larger slope of the line indicates a stronger effect of the parameter on the response. On the other hand, when the slope of the line is close to zero, such as for PD, the effect is considered statistically insignificant.

Heat input during the joining process may be used to explain these results. Heat input is an important parameter in all solid-state joining processes because it governs the temperature in the bonding area and therefore the local and global properties of the joining parts. Equation 12.11 shows the thermal model used for the friction stir spot welding (FSSW) process [27]. Because of the similarities in heat generation between FSpJ and FSSW, this equation can be used to explain the effect of the FSpJ process parameters on heat generation.

$$Q = \sum_{n=1}^N M(n)\omega(n)\Delta t \tag{12.11}$$

Q is the generated heat during the joining process, M is the torque (Nm), ω is the RS (rad/s) of the tool, Δt refers to the joining time, and N is the total

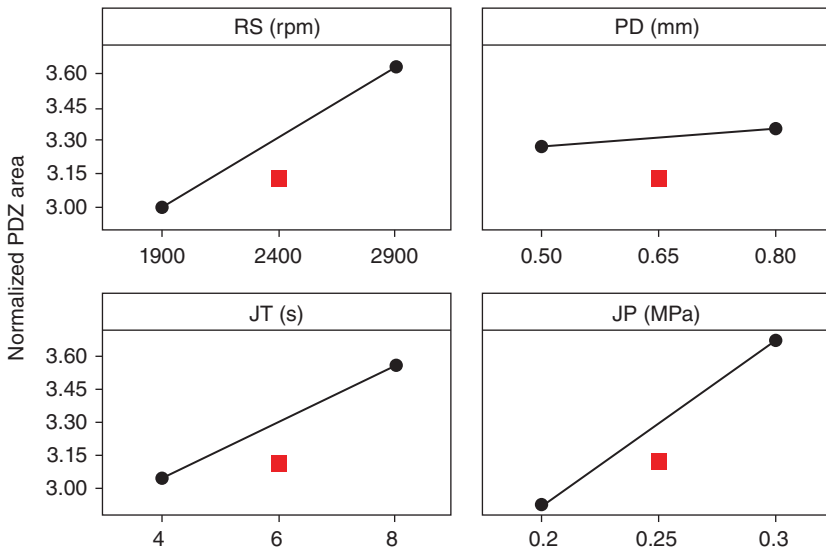


Figure 12.4 Main effect plots illustrating the influence of FSpJ process parameters on the normalized PDZ area.

number of experiments. It can be observed from the equation that RS and JT directly control the heat input, where increases in both of them also increases the heat input of the process. JP is proportional to M , therefore also contributing to create heat by friction. As joining temperature in FSpJ is proportional to Q , the behavior of the joining parameters on PDZ area can be explained [28]. Frictional heat melts a thin layer of the polymer matrix at the interface with the metal. As for most polymers, the melting viscosity of the PPS decreases with the increase in temperature (represented by Q). Therefore, the higher the Q , the larger will be the volume of molten composite matrix and the larger will be the PDZ area.

To prove this assumption, the relationship between the normalized PDZ area and the process temperature was plotted in Figure 12.5. From a rough approximation, PDZ area tends to linearly increase with the process temperature.

From the ANOVA analysis, the only two-factor interaction with a significant influence on the PDZ area was PD-JT, as observed in the interaction plot in Figure 12.6. This result may be explained by the fact that increasing the PD increases the exerted axial force on the aluminum beneath the tool and hence on the molten polymer in the spot area, which will flow at higher rates. When the joining time is short (4 s), the molten PPS does not have enough time to maintain a low enough viscosity, and the outflow rate reduces faster. In this case, a higher PD may facilitate the flow rate by increasing the applied axial force in addition to the exerted force by JP.

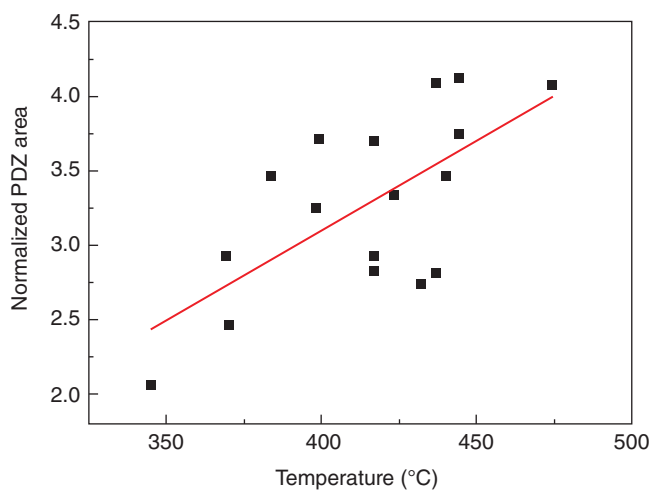


Figure 12.5 Normalized PDZ area–process temperature relationship. Generally, higher temperatures lead to a larger PDZ area.

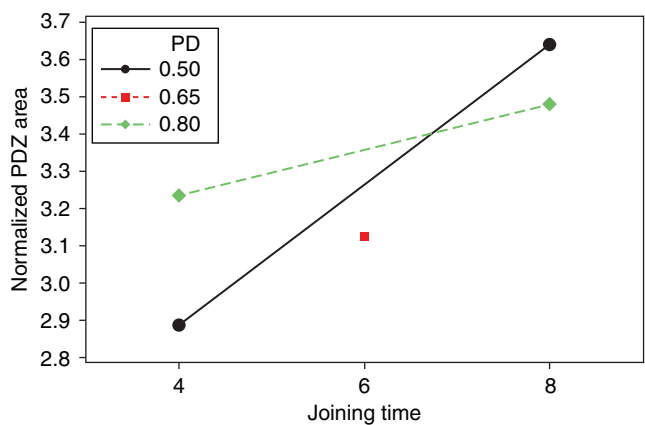


Figure 12.6 Interaction plot illustrating the influence of PD × JT on the normalized PDZ area.

The influence of the PD was reversed when the JT increased to 8 s. At a JT of 8 s, the average PDZ area slightly increases ($\sim 3\%$) by changing the PD from 0.8 to 0.5 mm; however, the means of normalized PDZ area lie in the range of the standard deviation (3.5 ± 0.5 for PD of 0.8 mm and 3.6 ± 0.3 for PD of 0.5 mm). This result is due to the prolonged JT (at 8 s) may keep the viscosity of the molten PPS low for a longer period; therefore, the applied force from the JP is sufficient to spread the molten PPS layer in the overlap area.

Influence on the Ultimate Lap Shear Force (ULSF) The average results of the ULSF (Table 12.6) indicated a variation between 1698 ± 92 N and 2310 ± 155 N. The performed ANOVA showed that RS, JP, and PD have the most significant influence on the ULSF with the p -values of 0.000, 0.013, and 0.031, respectively. However, the p -value of 0.080 for the JT suggested that it does not have statistically significant influence on the ULSF in the selected range of experiments. Furthermore, the interaction of RS–JP with the p -value of 0.026 is the only two-factor interaction with significant influence. In addition, the curvature showed a p -value of 0.675, which suggests that the relationship between the response and the factors is linear.

Figure 12.7 illustrates the main effects of the process parameters on the ULSF response. Increasing the RS, PD, and JP increases the ULSF, whereas increasing the JT reduces the ULSF. The comparison between the influence of process parameters on the responses shows that RS and JP significantly influence both the PDZ area and the ULSF (compare Figures 12.4 and 12.7). Increases in RS and JP lead to an increase in the PDZ area and ULSF, whereas the effect of JT is reversed. In addition, although PD did not show a significant influence on the PDZ area, it has a major effect on the mechanical performance of the joints.

As discussed in the previous section, increasing the RS increases the PDZ area because of the higher heat input and resultant lower molten PPS viscosity. As previously reported [26], the PDZ area is the strongest part of the joint. In the FSpJ, it is desirable to extend this zone. There is a clear trend that a larger

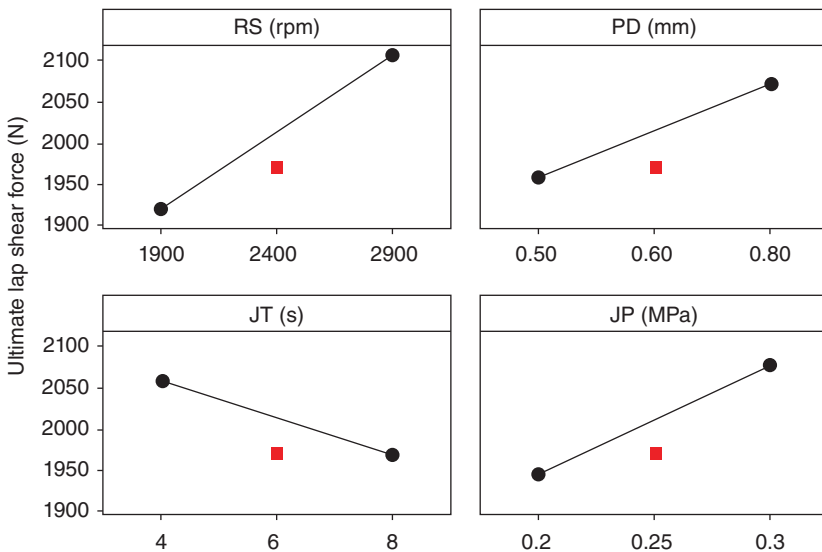


Figure 12.7 Main effect plots illustrating the influence of FSpJ process parameters on the ultimate lap shear force.

PDZ area increases the ULSF of the joints. These results are in accordance with our previous work [26], which showed that increasing the RS of the tool continuously increases the bonding area and hence the lap shear strength of the joints.

The joining pressure is another parameter with a high influence on the lap shear strength of the joints. As previously explained, the exerted axial force by applying sufficient JP facilitates the squeezing flow of the molten PPS, which therefore leads to a larger PDZ. In addition to the larger PDZ, another explanation of the influence of the JP on the ULSF is given in [29]. This result has been explained by the fact that higher JP facilitates the flow of the molten PPS into the pores and crevices on the surface of the aluminum. Improved wetting and pore filling increase the micromechanical interlocking as one of the main bonding mechanisms.

The plunge depth of the tool into the aluminum is also statistically significant when analyzing ULSF as the response. The PD influences the shape of the metallic nub (the geometrical undercut formed in the metal at the interface to the composite, see Chapter 3) and therefore the macromechanical interlocking between the aluminum and the composite. The formation of the metallic nub and its insertion into the composite lead to the increased macromechanical interlocking and strength of the joints, especially under shear loading. Larger metallic nub increases the macromechanical interlocking. Consequently, a higher lap shear strength of the joints will be obtained.

In summary, these results suggest that the highest level of the RS (2900 rpm), PD (0.8 mm), and JP (0.3 MPa) along with the lowest level of JT (4 s) may provide the optimized set of parameters to increase the lap shear strength of the FSp joints. This set of parameters corresponds to run 14 of the selected DoE with the ULSF of 2280 ± 88 N and the normalized PDZ area of 4.1 ± 0.1 mm².

Model Validation The ANOVA provided two models for the normalized PDZ area and ULSF in which these responses were the dependent variables and the joining parameters (RS, PD, JT, JP), and their interactions were the independent variables. Figure 12.8 shows the Pareto charts for the normalized PDZ area and ULSF. The vertical lines in the charts correspond to the 95% confidence interval used to analyze the data sets. All of the parameters and their interactions with bars passing the vertical line are considered statistically significant for the selected response and are included in the ANOVA regression model.

Note that for the ULSF model, although the joining time is statistically considered an insignificant parameter, it is also included in the model given that JT is one of the main process parameters. Its inclusion slightly increased the accuracy of the model. Equations 12.12 and 12.13 express the regression models for the normalized PDZ area and ULSF, respectively.

$$\begin{aligned} &\text{normalized PDZ area} \\ &= -2.69108 + 0.000630833 \times \text{RS} + 2.89167 \times \text{PD} \\ &\quad + 0.402431 \times \text{JT} + 0.744167 \times \text{JP} - 0.427778 \times \text{PD} \times \text{JT} \end{aligned} \quad (12.12)$$

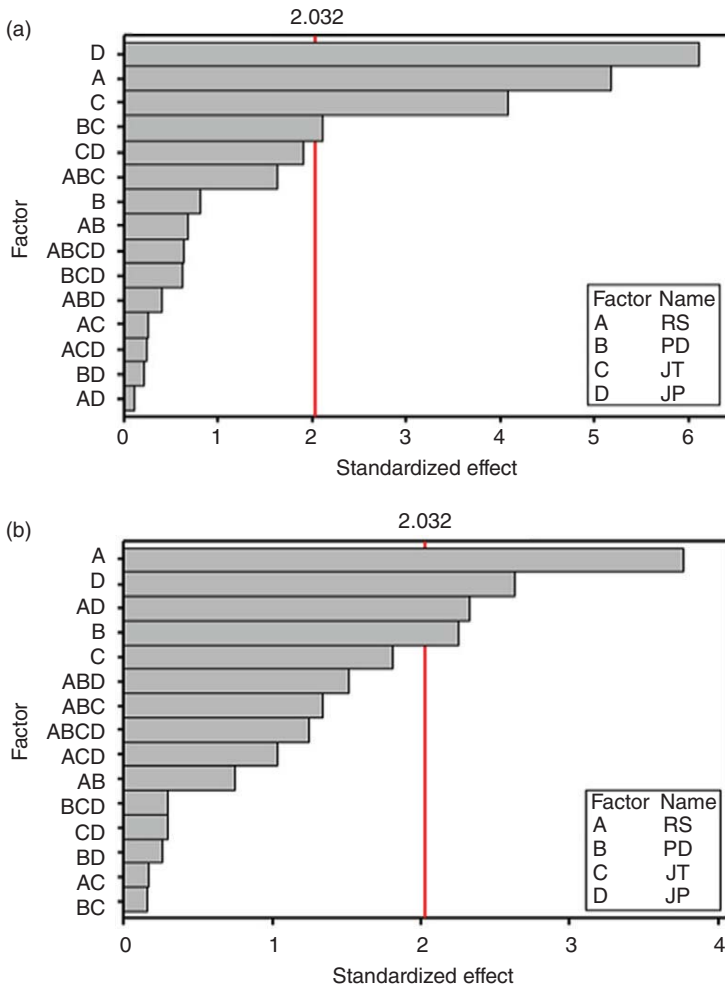


Figure 12.8 Pareto charts of the standardized effects for (a) normalized PDZ area and (b) ULSF.

$$\begin{aligned} \text{ULSF} = & 2843.53 - 0.464686 \times \text{RS} + 182.551 \times \text{PD} - 18.3718 \times \text{JT} \\ & - 627.598 \times \text{JP} + 0.298916 \times \text{RS} \times \text{JP} \end{aligned} \quad (12.13)$$

To validate the regression models from ANOVA, four new joining conditions were designed. The level of each joining parameter was randomly generated within the range of the DoE. Table 12.7 lists the validation joining conditions.

Figure 12.9 presents the comparison between the experimental data for both responses and their respective predicted values for all of the 17 DoE runs (X marks) and the four validation conditions (black circles). The middle 45° solid

Table 12.7 Four sets of joining parameters to validate the regression models.

Joining condition	Process parameters			
	RS (rpm)	PD (mm)	JT (s)	JP (MPa)
V1	2070	0.6	6.6	0.26
V2	2570	0.55	5.2	0.28
V3	2240	0.7	4.6	0.24
V4	2740	0.75	7.2	0.22

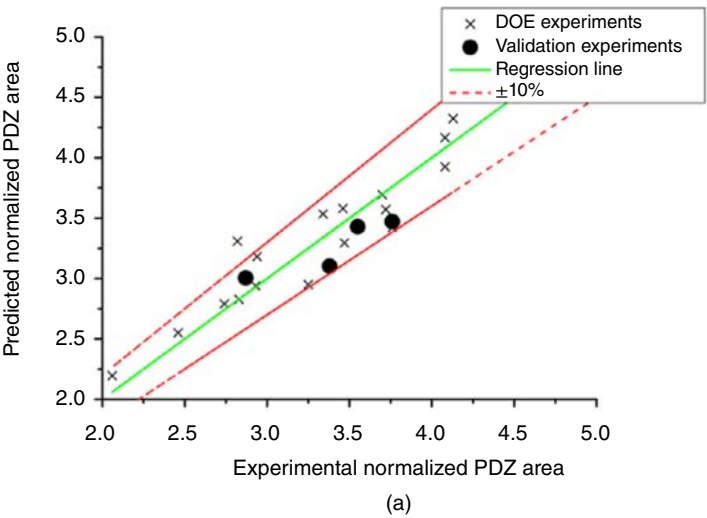


Figure 12.9 Predicted data from regression models versus experimental results for (a) the normalized PDZ area and (b) the ULSF. Cross marks (x) illustrate the DoE joining conditions, whereas black dots refer to the results of the validation joining conditions.

line shows the best linear relationship between the experimental and predicted values, whereas the dashed lines provide $\pm 10\%$ deviation from the perfect line. There is a very good correlation between the experimental data and the predicted values for the PDZ area, as observed from Figure 12.9(a) in which most of the data points lie within $\pm 10\%$ from the best fit. The model could also predict the normalized PDZ area for all four validation conditions within the range of $\pm 10\%$ error. However, in case of the ULSF, there is more deviation present between the experimental and the predicted results, as shown in Figure 12.9(b) in which some of the data are not accurately predicted by the model. Nevertheless, three out of four validation experiments are predicted within $\pm 10\%$ error for ULSF.

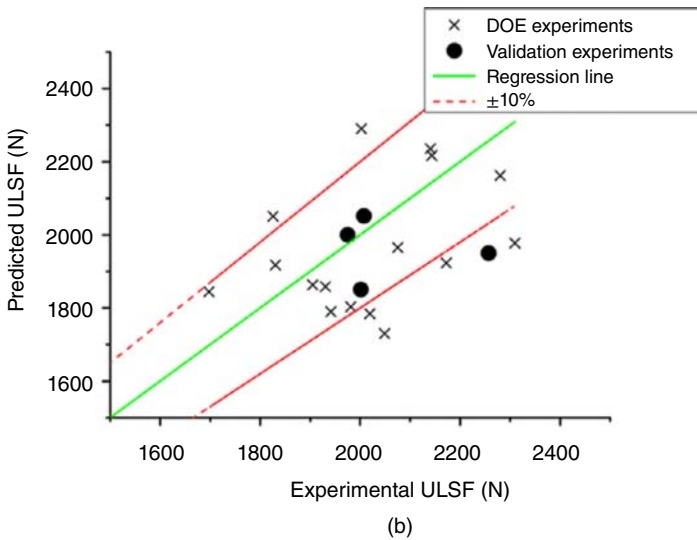


Figure 12.9 (Continued)

The qualitative validation of the data is presented in [25]. The results showed that, for the PDZ area, the R -squared value of the model was 73%, whereas the R -squared value of the ULSF model was 70%. These values suggest that the accuracy of the models to predict the responses in the selected range of the joining parameters is approximately 70%, which is statistically acceptable.

Conclusions of the Case Study The main goal of the current investigation was to statistically assess the influence of FSpJ process parameters on the formation of the PDZ area and the strength of AA2024-T3/CF-PPS single-lap shear joints. Two-level full-factorial DoE and ANOVA were employed for statistical analysis.

Based on the statistical analysis, a set of joining parameters (RS: 2900 rpm, PD: 0.8 mm, JT: 4 s, JP: 0.3 MPa) was achieved, which optimizes the ULSF of the joints. The obtained ULSF with the optimized joining parameters was 2280 ± 88 N. Regression models for both the PDZ area and ULSF were obtained according to ANOVA. The models could predict the responses with an accuracy of approximately 70%, considering a confidence interval of 95%, which is statistically acceptable. Therefore, these findings may be used as a tool to optimize joint performance in FSpJ.

12.3 Final Remarks

This chapter shows the basics of DoE and ANOVA, which have to be considered when optimizing metal–polymer hybrid joining processes. DoE

represents a helpful and well-established tool for optimizing and influencing desirable process responses. Several designs are available, implying various complexities and experimental efforts, meaning also different possibilities for thoroughly understanding the influence of process parameters and their interactions on process responses. Several factorial designs are exemplified in this chapter based on existing case studies for Friction Riveting and Friction Spot Joining, with their analyses of variance, interpretation, and validation of results. Furthermore, literature references are provided for deeper study of the suggested designs, based on the complexities of the processes, the required depth of analysis, and the available resources. The choice of the DoE should be based on the premise of maximum learning effect (i.e., influence of process parameters, correlations between parameters and responses, predictability of results) at the minimum use of resources (i.e., time, material, costs, etc.).

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13

Taguchi Design and Response Surface Methodology for Polymer–Metal Joining

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13.1 Introduction

This chapter presents the basics of the Taguchi design of experiments (DoE) and response surface methodology (RSM), with focus on their application for a profound understanding and optimization of metal–polymer joining technologies. Case studies for Taguchi design and Response Surface Method presented in this chapter are examples from research works within the Advanced Polymer–Metal Hybrid Structures Group at the Helmholtz-Zentrum Geesthacht, Germany, and its international cooperation projects.

13.2 The Taguchi Design of Experiments

As discussed in the previous chapter, factorial DoEs are relatively easy to construct and follow. Nevertheless, depending on the amount of parameters and levels of study, these can be large and impractical, especially due to the increased amount of experimental data to be generated. Instead of testing all possible combination of variables, one can test all pairs of combinations in an efficient way through the use of the Taguchi method of orthogonal arrays [1].

13.2.1 General Description

The design proposed by Taguchi [1] leads to a more condensed set of experiments. At first, we need to select the appropriate orthogonal array, based

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on the number of studied process parameters and their levels. Generally, arrays can be found in the literature, but they can also be derived, drawn manually, or derived from deterministic algorithms [2]. The Taguchi array selector is presented in Table 13.1.

Table 13.1 Taguchi’s DoE array selector.

Level Parameters	2	3	4	5
2	L4	L9	L16	L25
3	L4	L9	L16	L25
4	L8	L9	L16	L25
5	L8	L18	L16	L25
6	L8	L18	L32	L25
7	L8	L18	L32	L50
8	L12	L18	L32	L50
9	L12	L27	L32	L50
10	L12	L27	L32	L50
11	L12	L27		L50
12	L16	L27		L50
13	L16	L27		
14	L16	L36		
15	L16	L36		
16	L32	L36		
17	L32	L36		
18	L32	L36		
19	L32	L36		
20	L32	L36		
21	L32	L36		
22	L32	L36		
23	L32	L36		
24	L32			
25	L32			
26	L32			
27	L32			
28	L32			
29	L32			
30	L32			
31	L32			
32	L32			

Each experiment has a certain number of replicates. For the determination of the effect of each variable (parameter) on the output (response), the signal-to-noise ratio (S/N ratio) needs to be calculated for each experiment. The S/N ratio is a measurement scale used in the communication industry (actual signal measurement and wave-to-sound conversion) adopted in quality engineering. The quality of a measurement is expressed by the ratio of signal and noise [1]. For a measurement system, the input-to-output correlation is studied; the true value of the object is the input, while the result of the measurement is the output. S/N ratios have three elements: sensitivity, slope, and variability. The S/N ratio can be defined with the following equations, according to [1]:

$$S/N_i = 10 \log \frac{\bar{y}_i^2}{s_i^2} \quad (13.1)$$

where y is the value of the output and s is its variance.

Hereby,

$$\bar{y}_i = \frac{1}{N} \sum_{u=1}^N y_{i,u} \quad (13.2)$$

and

$$s_i^2 = \frac{1}{N-1} \sum_{u=1}^N (y_{i,u} - \bar{y}_i)^2 \quad (13.3)$$

with i – the number of the experiment, u – the number of the replicate (trial), and N – the number of replicates (trials) per each experiment.

In order to maximize one response (output), the S/N ratio should be defined as follows:

$$S/N_i = -10 \log \left[\frac{1}{N} \sum_{u_i} \frac{1}{y_u^2} \right] \quad (13.4)$$

After calculating the S/N ratio for each experiment, the average S/N ratio for each factor and level can be established. The range R for every parameter is calculated ($\max S/N - \min S/N$), meaning that, the larger R for a parameter, the higher its effect on the process. Using S/N ratios enables the maximization of the robustness of a process simply by selecting the levels of control factors that have the largest S/N ratio. Linearity, one of the components of the S/N , is important for simplifying adjustments in process design as well as for calibration of measurement systems. When the input/output relationship is not linear, the deviations are evaluated as the error after decomposing the variations; therefore, the S/N ratio becomes smaller [1, 2].

13.2.2 Analysis of Variance

Once the design is established and the experimental data gathered, all the experimental data can be statistically analyzed for quality improvement. In the examples (case studies) shown in this chapter, the MINITAB software has been used for this purpose. ANOVA was selected for the statistical analysis. ANOVA compares the mean results of several groups of parameters and measures the variances of the recorded data. Variances are separated into systematic variances (due to experimental process effects) and unsystematic variances (due to errors and malfunctions). The ratio between these two variances is the so-called *F* ratio/value. There are one-way ANOVA (a single independent variable and more levels or groups) and factorial ANOVA (two-way, three-way for multiple combinations with two or more variables, etc.) [2].

13.3 Example of Taguchi Design of Experiments in Joining of Metal to Composite Structures

13.3.1 Case Study 1 – Taguchi L9 (3^4) DoE in Double-Lap Friction Spot Joints

The selected example of Taguchi DoEs is a case study on Friction Spot Joining (FSpJ), published by Esteves *et al.* [3]. The following section reproduces fully or partially the mentioned research work with publishing authorization.

In this study, the individual effect of the joining process parameters on the microstructure and mechanical strength of hybrid AA6181-T4/CF-PPS double-lap joints was investigated using Taguchi method and ANOVA. Rolled AA6181/T4 sheets of 1.0 and 1.5 mm of nominal thicknesses were used in this work, to be joined with 2.17 mm (nominal thicknesses) CF-PPS laminates.

As already discussed in the previous sections, the four main controllable process parameters of the FSpJ technique are: tool rotational speed (RS), joining time (JT), sleeve plunge depth (PD), and joining force (JF). These process parameters directly influence the bonding mechanisms and consequently the microstructure and mechanical performance of the joints. Briefly, RS and JT control the heat input and the amount of generated polymer molten layer as well as its viscosity. PD is responsible for the shape and depth of the metallic nub, while JF ensures intimate contact between the plates and controls the flow of the polymer molten layer. For further information on the FSpJ process, please refer to Chapter 3.

The DoE approach designed by Taguchi *et al.* [1] is a powerful statistical method that enables optimizing the performance of a product, process, design, and system with a significant reduction in experiments, time, and costs [4]. This method has already been successfully used in several studies on the optimization of welding processes. When combined with the use of ANOVA,

Taguchi models can be used to determine the relative importance of each joining process parameter to joint properties [4].

The Taguchi DoEs method and ANOVA were used to investigate the individual influence of each FSpJ process parameter (RS, PD, JT, and JF) on the microstructure and lap shear resistance of aluminum AA6181-T4/CF-PPS double-lap joints. RS, followed by JT and PD, presented the highest influence on lap shear strength of double-lap joints, while the JF had the lowest influence. Analysis of variance indicated that, the larger the JT (larger heat inputs), PD (bigger macromechanical interlocking), and JF, the higher the joint strength was. An upper limit for the RS was identified, where further increases resulted in smaller joint strengths (due to tool slippage decreasing heat generation) related to smaller bonding areas. Increase of JF led to better spreading (squeeze flow) of the molten polymer at the interface, resulting in a better crevice and pore filling at the metal surface; as a result, adhesion forces and joint mechanical strength were improved.

13.3.1.1 Process Optimization

The joining parameters (factors) and their levels are listed in Table 13.2. The studied response was the ultimate lap shear force (ULSF) of the joints. Taguchi's L9-orthogonal matrix was selected for this purpose. It consists of four columns and nine rows, which resulted in nine experiments. This DoE – developed to decrease the number of experiments of a 3^4 full-factorial model with 81 runs [1] – has been used in welding to evaluate joint strength [5, 6]. The range of joining parameters (levels), as the input for Taguchi experiments, was selected by preliminary microstructural analysis to evaluate joint formation (i.e., size of bonded area, presence of thermal volumetric flaws in the consolidated polymer, cracks in the aluminum partner), using

Table 13.2 Experimental mean values and standard deviations of ULSF and S/N ratios for AA6181-T4/CF-PPS joints as a function of the FSpJ parameters.

Condition	RS (rpm)	PD (mm)	JT (s)	JF (kN)	ULSF (N)	S/N ratio
1	1200	0.75	2	6.8	2107 ± 470	65.85
2	1200	1.0	4	7.5	2480 ± 286	67.75
3	1200	1.15	6	8.3	3523 ± 527	70.69
4	1400	0.75	4	8.3	3241 ± 662	69.68
5	1400	1.0	6	6.8	3460 ± 381	70.65
6	1400	1.15	2	7.5	3254 ± 485	69.97
7	1600	0.75	6	7.5	3153 ± 282	69.88
8	1600	1.0	2	8.3	3112 ± 523	69.47
9	1600	1.15	4	6.8	3248 ± 722	69.63

the one-factor-at-a-time (OFAT) approach corresponding to the variation of a single parameter at a time. The statistical evaluation criterion chosen was “the-larger-the-better” for the S/N ratio, aiming at the determination of the maximal value of the response (ULSF). The S/N ratio for the larger-the-better analysis (S/N_{ltb}) was calculated using Equation 13.5 [1]:

$$S/N_{\text{ltb}} = -10 \log \left(\frac{1}{n} \sum_{i=1}^n \frac{1}{y_i^2} \right) \quad (13.5)$$

where n is the number of tests and y is the value of ULSF of the i -th test.

13.3.1.2 Influence of the FSpJ Process Parameters on Joint Mechanical Performance by Taguchi Design of Experiments

Table 13.2 lists the Taguchi orthogonal array for the FSpJ parameter combinations along with the experimental mean values (responses) and the standard deviations of the ULSF and the S/N ratios for the AA6181-T4/CF-PPS double-lap joints. The S/N ratio indicates how much the quality characteristic ULSF deviates from the desired value. The higher the S/N ratio, the more reliable is the investigated response [1].

The joints exhibited shear forces ranging from 2107 ± 470 N (Run 1) to 3523 ± 527 N (Run 3). The joints show displacement-at-peak load values from 0.7 ± 0.06 mm (Run 1) to 0.9 ± 0.09 mm (Run 3), indicating a brittle fracture. Runs 1 and 2 showed the lowest values for ULSF, probably due to the lower heat input, as it will be discussed within this section. All remaining FSpJ runs (3–9) led to similar ULSF values, taking into account the overall mean ULSF value (3064 ± 466 N) and the standard deviations of each joint condition tested.

Run 3 showed the highest S/N ratio as a result of the selected combination of process parameters leading to high heat input and large PD. Based on the similarities of FSpJ and other friction-based welding processes concerning the heat generation, Equation 13.6 [7] was used to better understand the effect of the FSpJ process parameters on double-lap joint mechanical behavior (see case study 2 in Chapter 12 for the detailed description).

$$Q = \sum_{n=1}^N M(n) \omega(n) \Delta t \quad (13.6)$$

One can conclude from Equation 13.6 that RS and JT are important variables for the heat generation. The larger these parameters, the larger the heat input. Higher heat input induces the formation of large bonded joining areas, as visually confirmed by a large amount of PPS molten layer at the interface. This can be seen in the fracture surface of Run 3 in Figure 13.1(a) (indicated by white arrows) in comparison to Run 1 (Figure 13.1(b)). Furthermore, pronounced nub formation was observed in Run 3 due to the combination of large heat input (RS: 1200 rpm; JT: 6 s) and PD (1.15 mm) (black arrows, Figure 13.1(c)),

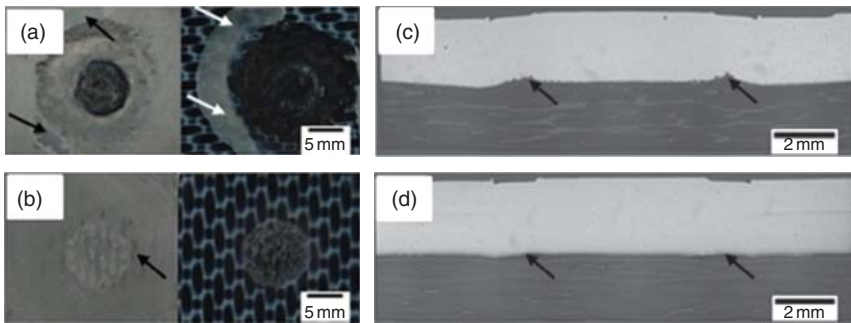


Figure 13.1 Fracture surfaces of the joints after lap shear testing: (a) Run 3; (b) Run 1. Microstructure of the joint cross section: (c) Run 3; (d) Run 1.

while for Run 1, nub formation was almost inexistent due to combined lower heat input (RS: 1200 rpm; JT: 2 s) and smaller PD of 0.75 mm (black arrows, Figure 13.1(d)).

The fractures of Runs 1 and 3 occurred predominantly by adhesive failure (at the interface between aluminum and consolidated polymeric layer) and cohesive failure (within the thickness of the consolidated polymeric layer in the top composite plies [8]), respectively. In the cohesive failure, the PPS consolidated layer remains partially attached to the aluminum and the composite plates. Run 1 shows the lowest S/N ratio in the experimental setup, due to its low thermal contribution as a result of the selected combination of process parameters (combination of minimum values of RS and JT); it induced the formation of a smaller PPS molten layer and adhesion forces at the interface. Therefore, a smaller bonded area was formed, reducing the mechanical strength. Moreover, condition 1 was performed with the lowest level of PD (0.75 mm), reducing the formation/size of the nub and the additional macromechanical interlocking, as indicated by the black arrows in Figure 13.1(b) and (d).

The selected L9-orthogonal array in this Taguchi DoE allows for analyses on the individual effect of each FSpJ process parameter on joint strength. This is a limitation for this DoE in comparison to its counterpart 3^4 full-factorial design, as some important second- and upper-order interactions are not taken into account. The mean values of the S/N ratio for each process parameter at levels 1 (minimum), 2 (mean), and 3 (maximum) were calculated for ULSF from the S/N ratio values listed in Table 13.3. The maximum difference between the mean S/N ratio values in the three tested levels (delta max) was calculated for each joining parameter. This difference indicates how sensitive is the response of ULSF to the variation of a given joining parameter, allowing ranking of the individual importance for each joining parameter [2].

Figure 13.2 is a more complete graphical representation of Table 13.3 illustrating the plots of the main effects for S/N ratio. The dashed line represents

Table 13.3 Main effects of the *S/N* ratio for the current case study.

Level	RS	PD	JT	JP
1	68.1	68.47	68.43	68.71
2	70.1	69.29	69.02	69.2
3	69.66	70.09	70.41	69.94
Delta max	2	1.62	1.98	1.24
Rank	1	3	2	4

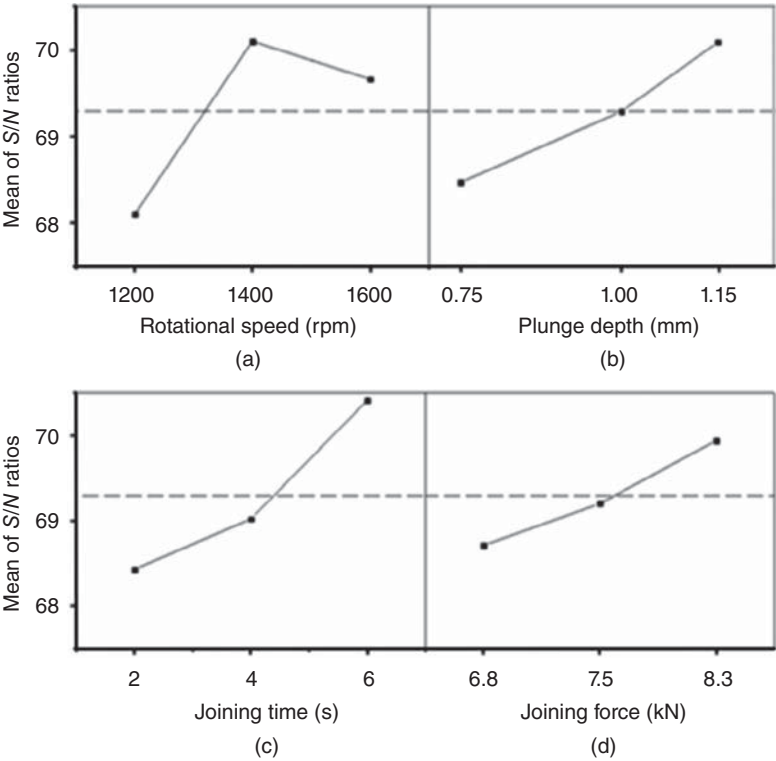


Figure 13.2 Main effects plots for *S/N* ratio for each FSpJ process parameter: (a) rotational speed, (b) plunge depth, (c) joining time, and (d) joining force.

the total mean value for *S/N* ratio, which was calculated as 69.3 db (dash line in Figure 13.2(a–d)).

According to Table 13.3 and Figure 13.2, the RS has the greatest effect on the ultimate lap shear strength of the joints, followed by JT, PD, and JF (as explained in the previous sections, the larger the curve slopes, the bigger the influence of a joining parameter). The effect of the most important process

parameters – rotational speed (RS) and joining time (JT) – on the mechanical strength of the joints shows that the heat input (see Equation 13.5) has a major influence on the joining mechanisms, due to the fact that it is responsible for the level of melting of the PPS matrix at the interface. In addition to this effect, the PD promotes the formation of macromechanical interlocking of the sheets through plastic deformation of the metal and formation of the nub. Therefore, the control of the heat input and PD is crucial in obtaining strong double-lap friction spot joints.

ANOVA was carried out to define the relative importance of each FSpJ process parameter to the ULSF of the joints. The contribution percentage ($P\%$) was calculated by the ratio of the sum of squares (SS) of the selected factor and total sum of squares. The error was not considered by the statistical model in this case, since the degree of freedom of the error is zero. The mean square (MS) is calculated by the SS divided by the number of degrees of freedom associated with the respective process parameter [1]. The results showed in this table are in agreement with the results obtained in Table 13.2 in which the RS (34.77%) is the most important process parameter influencing the ULSF, followed by JT (32.37%), PD (20.70%), and JF (12.15%). The individual influences of the FSpJ process parameters on the joint strength and microstructure were individually analyzed and discussed in more detail in the following sections.

Rotational Speed (RS) and Joining Time (JT) The ULSF – represented by S/N ratio – increases with the RS from 1200 to 1400 rpm. This can be attributed to the increase in the heat input, which results in larger bonded areas at the composite–metal interface, and therefore higher mechanical performance. Such behavior was already reported in Chapter 3 for FSpJ of AA2024/CF-PPS. We showed that bonded area increases continuously by increasing the RS of the tool due to higher heat input, therefore leading to higher joint mechanical performance. However, in contrast to the previous observation, the lap shear strength of the double joints in this study decreased at RSs higher than 1400 rpm. This behavior may be explained by the well-known tool slip phenomenon, usually observed during the welding of aluminum by friction stir spot welding (FSSW) [9, 10]. The increase in temperature, when further elevating RSs, leads either to the reduction of the plasticized aluminum viscosity and/or local melting [10] (the latter was not detected in this study). When RS is changed from 1400 to 1800 rpm, slippage between the tool and the surrounding aluminum may take place. This decreases the torque and reduces the frictional heat generation, leading to smaller bonding areas and decreasing the joint strength (Figure 13.3).

According to the ANOVA results, JT is the second parameter with the highest influence on the mechanical performance of the joints. For the same reasons discussed for the RS, increase of the JT within the analyzed interval (2–6 s) increases the bonding area and the lap shear strength (Figure 13.4).

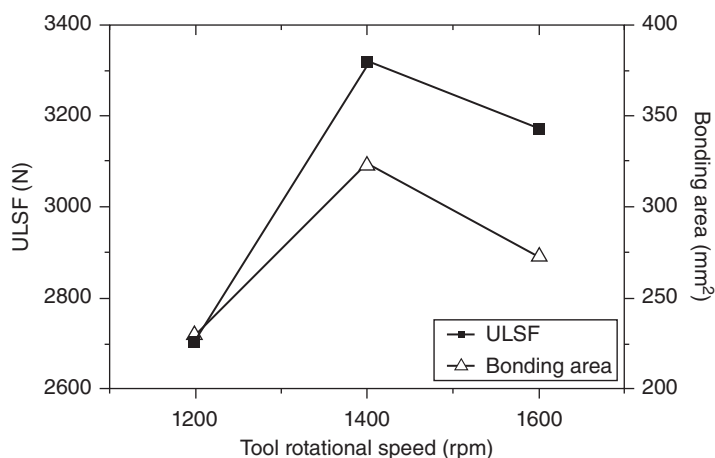


Figure 13.3 Influence of the tool rotational speed (RS) on the ULSF and bonding area.

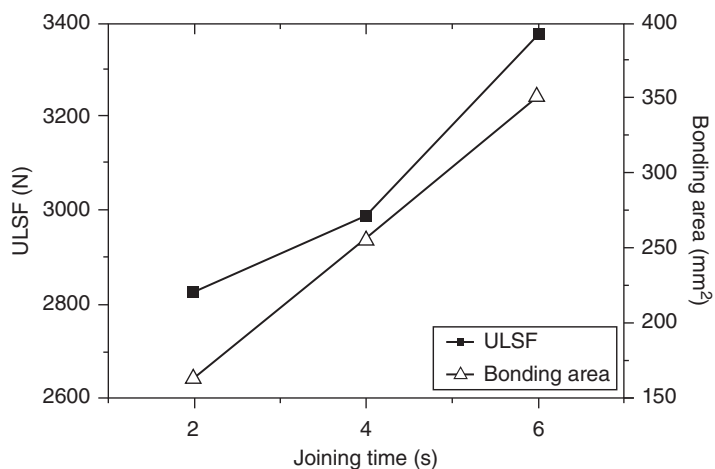


Figure 13.4 Influence of the joining time (JT) on the ULSF and bonding area.

Plunge Depth (PD) As previously discussed in Figure 13.1, lap shear strength of friction spot double-lap joints is a combination of bonding area size and macromechanical interlocking related to the metallic nub. The increase in the PD within the selected range augments the ULSF (Figure 13.5), given the larger indentation of the aluminum into the composite surface (metallic nub) and macromechanical interlocking.

The mechanisms dictating the formation of the metallic nub are still not fully understood; however, current theory states that size and shape will be directly

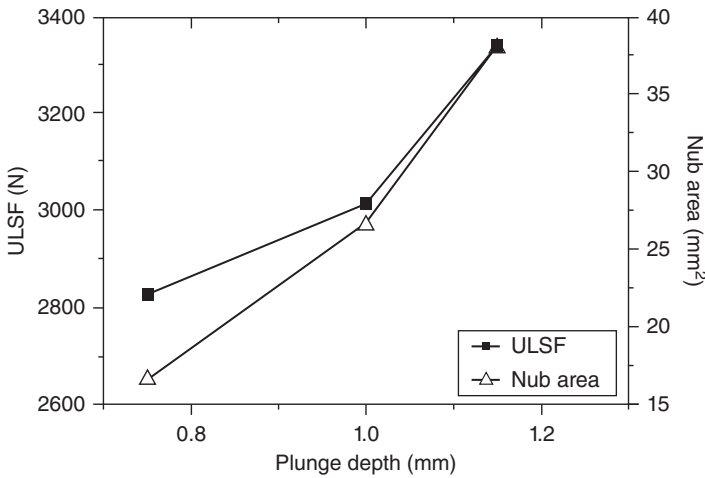


Figure 13.5 Influence of the plunge depth (PD) on the ULSF and nub area.

influenced not only by PD, but also by the local metal formability. Therefore, different heat input levels may change the final geometry of the metallic nub, at same PDs. For example, Runs 1 and 7 of the Taguchi orthogonal array L9 have the same level of PD (0.75 mm). Nevertheless, Run 7 was joined with higher heat input (RS: 1600 rpm and JT: 6 s) compared to Run 1 (RS: 1200 rpm and JT: 2 s), increasing the aluminum formability and bonding area, resulting in higher mechanical strength.

Joining Force (JF) For the selected materials and studied range of process parameters, double-lap shear strength showed the smallest sensitivity to the variation of the JF. Bonding micromechanisms at the interface of metal–composite were recently reported in [8]. Hydrostatic forces created as a result of the applied JF mainly control the flow of molten polymer and asperities/crevice filling at the aluminum surface. Higher JFs are expected to increase spreading of the molten polymer layer onto the crevices and pores of the metal surface improving wettability (pore filling) at the interface. Therefore, micromechanical interlocking efficiency may increase supporting better mechanical performance. On the other hand, excessive JF can decrease molten layer thickness by pushing off molten polymer outside the bonding area (squeezing flow). Very thin consolidated polymer layers at the metal–composite interface will decrease adhesive forces at the interface, leading to lower strengths. In this work, the selected range of JF obtained through the OFAT analysis was optimized to allow joint formation. In this way, significant differences in the mechanical performance of the samples were not observed. Further studies within larger ranges of JFs are required to further understand this behavior.

13.3.1.3 Conclusions of the Case Study

The individual effect of the FSpJ process parameters on the microstructure and mechanical strength of aluminum AA6181-T4/carbon-fiber-reinforced poly(phenylene sulfide) laminate (CF-PPS) double-lap joints was investigated using DoE and ANOVA.

The use of a Taguchi L9 (3^4) DoEs and analysis of variance allowed the determination of the individual influence of the main joining parameters on joint lap shear resistance. Taguchi analysis indicated that the tool RS is the parameter with the largest influence on the lap shear strength of the joints (34.77%), followed by the JT (32.37%), PD (20.70%), and JF (12.15%). It was demonstrated that the combined interaction of RS and JT increases heat generation, changing joint microstructure and the lap shear strength. The level of heat input is responsible for the amount of PPS molten layer at the joint interface increasing bonding area. Moreover, PD plays an important role in the macromechanical interlocking mechanisms at the metal–composite interface controlling the formation of the metallic nub. The higher the PD, the more pronounced will be the metallic nub (Figure 13.1). Therefore, larger heat inputs will be present as a result of elevated JTs and optimized RSs (there is an upper limit for the former, where increases in the latter will induce tool slippage, decreasing heat generation) as well as PD will lead to stronger joints. However, the PD has a limitation since too large PDs can cause the rupture of the aluminum plates within the spot joint area.

The selection of the Taguchi DoE in substitution to a full-factorial DoE has shown to be an interesting tool to an initial understanding of microstructure and mechanical performance of friction spot double-lap joints. The current results showed a fairly good correlation with the full-factorial results discussed in Chapter 12; this indicates that, for the stand-alone influence of the individual factors (joining parameters), the lower resolution associated with the L9-Taguchi design was not disadvantageous. In truth, this shows that for a preliminary understanding of new joining materials or techniques, Taguchi may be a good alternative to reduce material consumption, man power, and experimental time in comparison to the full-factorial approach. However, the latter is a more elaborate DoE and may provide important valuable information regarding second- or higher-order factor effects on a certain response. Therefore, the final decision on the selection of the best DoE should be made based on the amount of joining factors to be evaluated, the type of responses (joining properties), and the available resources.

13.4 Response Surface Methodology

13.4.1 Introduction

The RSM is nowadays one of the most used techniques where optimization of a process is concerned, due to its simplicity and statistical theory [11]. The name

derives from its clear illustrative graphical representation, where an outcome response is depicted as a surface dependency of the given independent input factors [12, 13].

RSM is based on a set of mathematical and statistical techniques that aim to establish a functional correlation between the response (output variable) and the parameters (input variables), affecting such response of the process in question. The response of a given process can be defined as qualitative or quantitative, depending on the studied process. The optimization or further understanding of the process is achieved by examining the “surface” generated by a three-dimensional representation of the response function, which models the response–factors relationship. This response function is represented by a polynomial model expressed as [12]:

$$y = f'(x)\beta + \varepsilon \quad (13.7)$$

where β denotes an unknown vector of p coefficients and ε represents a deviation (a random error) resulting from factors not taken into account by the vector function (x). Considering that ε is assumed to be equal to zero, then

$$\eta(x) = \beta f'(x) \quad (13.8)$$

with $\eta(x)$ being the mean response of y .

The DoE aiming for the fitting of response surfaces is characterized by the following attributes:

- The mapping of a response surface over a pre-established region of interest, process-parameter-wise.
- Evaluation of the parameters' significant influence on the responses.
- In order to optimize a given response, parameter values can be obtained from the fitted model.
- Multi-response simultaneous optimization is possible, given balanced settings.

The most widely assumed forms of Equation 13.7 are the first-degree regression model:

$$= \beta_0 + \sum_{i=1}^k \beta_i x_i + \varepsilon \quad (13.9)$$

and the second-degree regression model:

$$y = \beta_0 + \sum_{i=1}^k \beta_i x_i + \sum_{i=1}^k \beta_{ii} x_i^2 + \sum_{i < j}^k \beta_{ij} x_i x_j + \varepsilon \quad (13.10)$$

where β represents the regression coefficient of the fitted model, β_k denotes the main effects, β_{kk} denotes the quadratic main effects and $\beta_{k-l,k}$ denotes the two-factor interactions, which define the vector.

The $n \times k$ matrix represents the design matrix (D):

$$D = \begin{bmatrix} x_{11} & x_{12} & \cdots & x_{1k} \\ x_{21} & x_{22} & \cdots & x_{2k} \\ \vdots & \vdots & \vdots & \vdots \\ x_{n1} & x_{n2} & \cdots & x_{nk} \end{bmatrix} \quad (13.11)$$

where $\mathbf{x}_u = (x_{u1}, x_{u2}, \dots, x_{uk})'$ is the vector of the u -th design setting of $x_1, x_2, \dots, x_k$, each row representing one of the design points. With this, Equation 13.10 can be rewritten representing the u -th response value as,

$$y_u = f'(\mathbf{x}_u)\boldsymbol{\beta} + \varepsilon_u, \quad u = 1, 2, \dots, n \quad (13.12)$$

or

$$\mathbf{y} = \mathbf{X}\boldsymbol{\beta} + \boldsymbol{\varepsilon} \quad (13.13)$$

where $\mathbf{X}$ is a $n \times p$ matrix with the rows representing the values of $f'(\mathbf{x}_u)$.

As assumed before, $\boldsymbol{\varepsilon}$ having a zero mean and a variance–covariance $\sigma^2 \mathbf{I}_n$ matrix, allows the calculation of the ordinary least-squares estimator of $\boldsymbol{\beta}$, as follows:

$$\hat{\boldsymbol{\beta}} = (\mathbf{X}'\mathbf{X})^{-1}\mathbf{X}'\mathbf{y} \quad (13.14)$$

The variance–covariance matrix of $\hat{\boldsymbol{\beta}}$ becomes

$$\text{Var}(\hat{\boldsymbol{\beta}}) = \sigma^2(\mathbf{X}'\mathbf{X})^{-1} \quad (13.15)$$

Substituting $\boldsymbol{\beta}$ for $\hat{\boldsymbol{\beta}}$, it becomes the prediction equation:

$$\hat{y}(\mathbf{x}) = f'(\mathbf{x}_u)\hat{\boldsymbol{\beta}} \quad (13.16)$$

The variance of $\hat{y}(\mathbf{x})$, takes the form

$$\text{Var}[\hat{y}(\mathbf{x})] = \sigma^2 f'(\mathbf{x})(\mathbf{X}'\mathbf{X})^{-1}f(\mathbf{x}) \quad (13.17)$$

When the experimenter does not possess an initial parameter-level window – that is, the experimental region of interest – one must conduct a series of preliminary experiments to determine the range of joining parameters' values or levels. This leads to an optimized response or to the region where the optimum point is thought to be located. Two strategies commonly used are the OFAT approach previously described and the steepest ascent approach (SAA).

The SAA consists of performing a small-scale experiment, commonly a two-level factorial or fractional factorial, with parameter levels on a region thought to contain the optimal response condition. Then the response data obtained is fitted into a first-order model. From this model, the direction of the steepest ascent path is found, and based on the response planar surface and contours plot, it is verifiable that the direction is perpendicular to the contours. By moving the experiment along this line in incremental steps, the vicinity of the optimal response will be found when the experiments conducted

in sequential steps do not yield an increase of the response or even denote a decrease (an indication of an inflexion point). When the region of interest is defined around this area, a higher-order model can be used to evaluate the curvature of the response surface [12, 13].

Once the region of interest is defined, a second-order model can be applied, containing

$$p = \frac{(k+1)(k+2)}{2} \quad (13.18)$$

parameters, where p is the minimum required number of design points.

The most common RSM DoEs are the three-level full factorial, the Box–Behnken, and the Central Composite. Although much information regarding parameter interactions can be extracted from the full factorial as discussed in the previous sections, these are more time- and resource-consuming DoE options. In the following section, the Central Composite Design (CCD) will be reviewed [12, 13].

13.4.2 The Central Composite Design

13.4.2.1 General Description

CCD was introduced by Box–Wilson [13]. It is considered an efficient second-order DoE composed of three parts, a fractional, an axial, and the center points portions, enounced below in the same order:

- A two-level full (2^k) or a resolution V fractional (2^{k-f}) factorial design with coded factors' levels, $-1, +1$.
- An axial arrangement of $2k$ points, where at each point, only one factor has a coded value, α , different from its mid-level (0), which denotes the axial distance to the center of the design.
- A n_c number of replication runs performed at the center point.

Coded levels are obtained by:

$$\text{coded level} = \frac{\text{real value} - \text{mean value}}{\text{half of factors' range}} \quad (13.19)$$

Several properties of the design are dependent on the axial value α and the n_c , hence, these must be chosen in accordance to experimenters' objectives. The value of α varies from 1 to $\sqrt{k}$.

The number of runs (or design points) necessary for a CCD is

$$N = 2^k + 2k + n_c \quad (13.20)$$

having $(k+1)(k+2)/2$ model parameters to be estimated. One of the properties to be taken into consideration is the rotatability of the design. A design is considered rotatable if the variance of a predicted response value is independent of the direction. For example, if two points are located at the same

Table 13.4 Determination of α in CCD.

k	F	N	α
2	4	$8 + n_c$	1.414
3	8	$14 + n_c$	1.682
4	16	$24 + n_c$	2
5	32	$42 + n_c$	2.378
$5 (\frac{1}{2} \text{ rep})$	16	$26 + n_c$	2

Source: Box and Wilson 1951 [13].
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Wiley & Sons.

distance from the center of the design, yet at opposing directions, both will have the same prediction variance. They are thus insensitive to a rotation of the coordinated axes. Table 13.4 illustrates the values of α that confer rotatability to the design regardless of the number of center points, n_c [14], with

$$\alpha = \sqrt[4]{F} \tag{13.21}$$

where F represents the total amount of experimental runs performed on the factorial part of the design.

The choice of n_c , will allow rotatable design to have uniform precision or orthogonality properties. A design is considered orthogonal if the $X'X$ is a diagonal matrix. The uniform precision property is verified when $\text{Var}[\hat{y}(x)]$ has the same value at the origin of the design and at a distance of one from it. Khuri and Cornell [15] provide values for n_c in order to obtain either of these properties to a rotatable design.

13.4.3 The Box–Behnken Design

This DoE was introduced by Box and Behnken in 1960 [16]. It has its basis on the theories for balanced incomplete block designs [17], whereby fitting of a second-order response surface into a three-level design is performed. The Box–Behnken design (BBD) is widely applied in the industry due to the fact that they are economical designs. The configuration of BBD is relatively similar to the CCD, but it does not entail extreme or corner design points, requiring also fewer runs, that is, three-level fractional designs. They are also useful when it is intended to avoid extreme factor–level combinations. Figure 13.6 illustrates graphically the spatial distribution of the coded design points for BBD. One may observe that BBD is composed of three full-factorial design blocks, each having two factors with two levels and a third factor fixed at zero, the mid-range value. The design pointers are disposed on the surface of a sphere centered at the origin of the coordinate system [16].

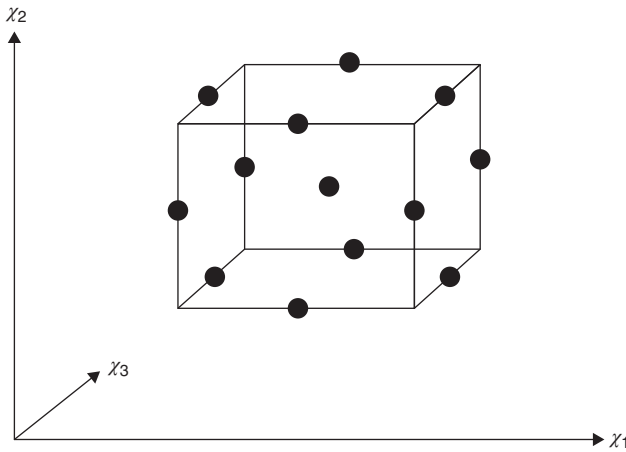


Figure 13.6 Graphical illustration of the spatial distribution of BBD design points
(Source: Box and Behnken 1960 [16]. Reproduced with permission of John Wiley & Sons).

13.4.3.1 General Description

The BBD can be described as considering part of the factorial combinations of a three-level full-factorial design. Each factor is coded in three settings, -1 , 0 , $+1$. This design not always has rotatability properties, depending on the number of factors considered. In addition, the BBD should not be used when the experimenter intends to consider predicting responses for pointers located in the corners of the design cube; this is due to the fact that this design does not take into account design points where all the factors are simultaneously at their extreme levels. Table 13.5 shows a BBD design matrix example for a process with three parameters and three levels (as shown in Figure 13.6 graphically) [12, 16].

Considering a design with three variables, three blocks would be applied, where each two of the variables would have -1 , $+1$ values and the third kept constant at 0 on a second-order two-level factorial design, resulting in different factor combinations on each of the blocks. For BBD designs with different numbers of factors, the authors recommend the consulting the textbooks by Box and Draper [11, 18].

13.4.4 Case Study 2 – Central Composite Design in Friction Riveting

The example presented in this section aims to demonstrate in a simplified manner how to make use of the RSM, through a CCD applied to Friction Riveting.

This study aims to evaluate the joint formation of the Friction Riveting process for the AA2024-T351/PEI material combination, across a wide range of parameters. The base material specimens used were AA2024-T351

Table 13.5 BBD design matrix for three parameters with three levels.

X_1	X_2	X_3
−1	−1	0
1	−1	0
−1	1	0
1	1	0
−1	0	−1
1	0	−1
−1	0	1
1	0	1
0	−1	−1
0	1	−1
0	−1	1
0	1	1
0	0	0
0	0	0
0	0	0
0	0	0

cylindrical rivets, with 5 mm of nominal diameter and 60 of nominal length, and plates of PEI amorphous thermoplastic with 13.4 mm of nominal thickness. Several measurements were performed on the joint’s rivet overall geometry as a basis of comparison and correlation with the process parameters. The evaluated responses allowed for calculation of a volumetric ratio (VR) for each joining condition. This VR has been demonstrated by Blaga *et al.* [19] to be an indicator of the global mechanical performance/anchoring efficiency of friction-riveted hybrid joints. The VR is determined with the following equation,

$$VR = \frac{(H - B)(W^2 - D^2)}{W^2 * H} \tag{13.22}$$

where H is the rivet penetration depth into the polymeric component, B is the height of the deformed rivet tip, D is the rivet diameter, and W , is the width of also the deformed rivet tip anchored in the polymer. In this case study, the parameters considered were: RS; friction time (FT); friction force (FF); Forging Time (FoT); and Forging Force (FoF). In order to reduce the amount of experiments, a CCD with a 2^{5-1} fractional factorial component was selected, with a total of 16 runs. A total of 10 experiments were performed as center points and for the axial points of the design, an α of two was established. Using X-ray

Table 13.6 CCD design-coded levels' table.

Factors	Coded levels				
	−2	−1	0	1	2
Rotational speed (rpm)	17,000	18,000	19,000	20,000	21,000
Friction time (s)	1.4	1.6	1.8	2	2.2
Friction force (N)	1,500	2,000	2,500	3,000	3,500
Forging time (s)	0.5	1	1.5	2	2.5
Forging force (N)	3,300	3,900	4,500	5,100	5,700

tomography, the anchoring deformed rivet tip's shape was assessed before any mechanical testing had been performed on the joints. The process parameters coded levels used for this work are shown in Table 13.6. The complete design matrix is presented in Table 13.7.

The correlation between process parameters and the studied response, VR, was evaluated. The VR was found to vary from 0.14 to 0.48. The results were assessed by means of a backward stepwise elimination method, which determined the statistical significance of the terms of the response surface regression model for the VR. In this procedure, the least significant factors can be removed step-by-step and the model analysis of variance recalculated. Considering that statistical significance involves a p -value inferior to 0.05 within the ANOVA (in this case a confidence level of 95%), the main effects plots for the process parameters that respected this criterion are presented in Figure 13.7.

From these plots, it can be seen that there is a relatively linear increase of the VR value when individually increasing the RS, FT, and FoF. The main effect of the FF was found to be influenced by higher-order factors, displaying a pronounced curvature on the plot. It was also with this parameter that the VR varied the most. The p -values for the RS, FT, FF, and FoF were 0.012 for the first and 0.000 for the remainder. The FoT was found not be statistically significant.

The polynomial regression equation for VR prediction of this model is:

$$\begin{aligned} \text{VR} = & 0.343 + 0.018 * \text{RS} + 0.043 * \text{FT} + 0.060 * \text{FF} \\ & - 0.030 * \text{FoF} - 0.031 * \text{FF}^2 - 0.017 * \text{FT} * \text{FF} \end{aligned} \quad (13.23)$$

The values predicted by the statistical model are plotted against the actual values of VR in Figure 13.8. In this validation plot, the dotted line depicts the one-to-one correlation between the axes. The solid gray lines defined the upper and lower model prediction limits for a single observation.

The model has an explanatory power (adjusted R -squared) of 83.2%, a standard error (S) of 0.03, and a predicted R -squared of 73.9%. From the plot, one can observe that all the predicted VR values fall within the prediction intervals and relatively close to the 45° line.

Table 13.7 CCD design matrix for the case study.

	Run	Parameters				
		RS (rpm)	FT (s)	FF (N)	FoT (s)	FoF (N)
Factorial points	1	18,000	1.6	2,000	1	5,100
	2	20,000	1.6	2,000	1	3,900
	3	18,000	2	2,000	1	3,900
	4	20,000	2	2,000	1	5,100
	5	18,000	1.6	2,000	2	3,900
	6	20,000	1.6	2,000	2	5,100
	7	18,000	2	2,000	2	5,100
	8	20,000	2	2,000	2	3,900
	9	18,000	1.6	3,000	1	3,900
	10	20,000	1.6	3,000	1	5,100
	11	18,000	2	3,000	1	5,100
	12	20,000	2	3,000	1	3,900
	13	18,000	1.6	3,000	2	5,100
	14	20,000	1.6	3,000	2	3,900
	15	18,000	2	3,000	2	3,900
	16	20,000	2	3,000	2	5,100
Center points	17	19,000	1.8	2,500	1.5	4,500
	18	19,000	1.8	2,500	1.5	4,500
	19	19,000	1.8	2,500	1.5	4,500
	20	19,000	1.8	2,500	1.5	4,500
	21	19,000	1.8	2,500	1.5	4,500
	22	19,000	1.8	2,500	1.5	4,500
	23	19,000	1.8	2,500	1.5	4,500
	24	19,000	1.8	2,500	1.5	4,500
	25	19,000	1.8	2,500	1.5	4,500
	26	19,000	1.8	2,500	1.5	4,500
Axial points	27	17,000	1.8	2,500	1.5	4,500
	28	21,000	1.8	2,500	1.5	4,500
	29	19,000	1.4	2,500	1.5	4,500
	30	19,000	2.2	2,500	1.5	4,500
	31	19,000	1.8	2,500	0.5	4,500
	32	19,000	1.8	2,500	2.5	4,500
	33	19,000	1.8	1,500	1.5	4,500
	34	19,000	1.8	3,500	1.5	4,500
	35	19,000	1.8	2,500	1.5	3,300
	36	19,000	1.8	2,500	1.5	5,700

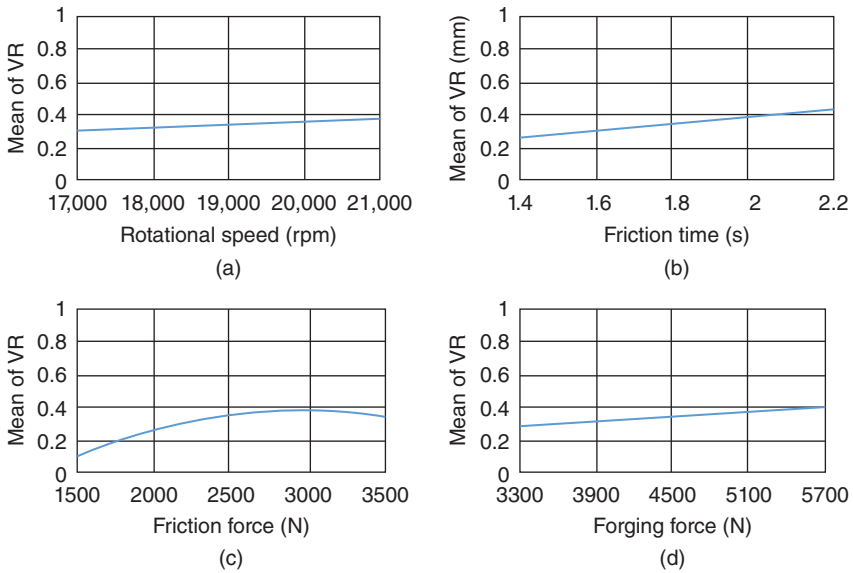
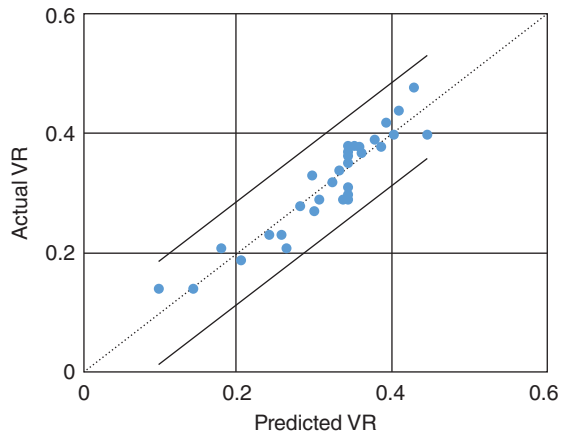


Figure 13.7 Main effects plots for the mean of VR for all the process parameters: (a) rotational speed, (b) friction time, (c) friction force, and (d) forging force.

Figure 13.8 Statistical model validation plot for VR.



Recalling that the interaction between FT and FF was found to be statistically significant, an analysis of the response surface for these two parameters is justifiable. The respective surface and contour plots are shown in Figure 13.9. The contour plot is basically the horizontal or vertical projection of the response surface for the studied response, in this case VR. One useful direct application of the use of the contour plot would be, that is, to determine the optimal area of maximum response for the two parameters. Contour plots

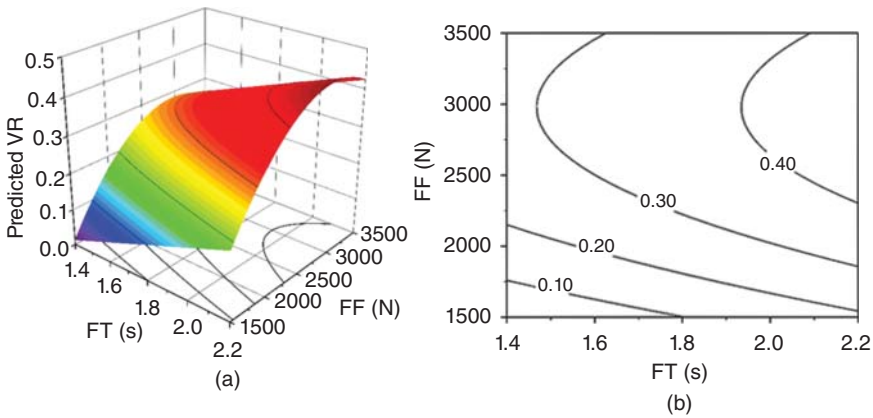


Figure 13.9 Friction time (FT) and friction force (FF) interaction plots with volumetric ratio, VR. (a) Response surface plot and (b) contour plot.

are helpful for in-depth visualization of the response surface, in which cuts at a desired investigated level of a parameter (axis) are performed while the influence of another parameter can be studied.

It is clear that the quadratic effect of the FF is present along the entire range of the FT parameter. Increasing the time leads to an increase of the VR value, an effect that is more pronounced at lower levels of FF. The maximum contribution of this interaction to the increase of VR can only be achieved with FT values close to the upper limit of our parameter range with FF within the upper half of its range. The in-depth analysis of all interactions and other responses in Friction Riveting via CCD will be the subject of a separate publication.

13.4.4.1 Conclusions of the Case Study

The influence of the Friction Riveting parameters on joint formation was assessed by means of a CCD of experiments. The evaluation of the VR response, presented in this case study, yielded a polynomial regression equation capable of predicting this response given the process parameters, for the ranges and materials studied. Furthermore, it was possible to evaluate how the two-way interaction between FT and FF also influences the VR.

13.5 Other Surface Designs

According to Montgomery [4], Myers and Montgomery [12], and Khuri and Cornell [15], several DoEs can be applied to the study, development, or optimization of an engineering process. A full-factorial design with three levels, 3^k , or a fractional one, 3^{k-f} , is commonly suggested where curvature is

present on the response surface. These include the previously presented, such as the CCD and BBD, saturated or near-saturated cuboidal designs, hybrid designs, small composite designs, and mixture experiment designs, whose description can be found in [4, 12, 20]. Before choosing one of these designs, one must take into account the final goal for doing so. The final objective of the experimenter could also set the need for using a sequence of two or more designs aiming for a more precise approximation to the real response surface.

13.6 Final Remarks

The Taguchi method and the RSM are exemplified in this chapter based on existing case studies for Friction Riveting and FSpJ, preceded by a brief explanation of their fundamental theories and principles. As for the previous chapter on factorial DoEs, the authors recommend several literature references for guidance related to different complexities of Taguchi designs and Response Surfaces. As seen from the presented case studies, these methods are effective for both process optimization (technical approach) and determination of process–structure–properties correlations (materials science and engineering approach).

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