

Materials Forming, Machining and Tribology

J. Paulo Davim *Editor*

Measurement in Machining and Tribology



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Measurement in Machining and Tribology

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Preface

Measurement is an essential part of modern science and engineering, but your definition it is not easy. In the classical definition, measurement is “*the determination or estimation of ratios of quantities*”. Also, it is current define measurement as “*the assignment of a number to a characteristic of an object or event, which can be compared with other objects or events*”. The concept of measurement is dependent on the perspective and branch. Therefore, an improvement of the technical capability to use the concept of measurement is very important for the modern industry.

The purpose of this book is to present a collection of chapters exemplifying the aspects of measurement in machining and tribology. Chapter 1 of the book provides the importance of temperature in metal cutting and its proper measurement/modelling. Chapter 2 is dedicated to the role of oxygen in orthogonal machining of metals. Chapter 3 describes geometric accuracy of machine tools. Chapter 4 contains information on machine vision in measurement. Finally, Chap. 5 is dedicated to tribological measurement of electroless nickel coatings.

This research book can be used as a book for final undergraduate engineering course or as a topic on measurement at the postgraduate level. Also, this book can serve as a useful reference for academics, researchers, manufacturing, mechanical, materials, industrial and production engineers, and professionals in industries. The interest of this book is great for many important institutes and universities as well as industries. So, it is hoped this book will inspire others to undertake research in this field of machining and tribology.

Finally, the editor acknowledges Springer for the professional support and all the chapter authors for their availability for this editorial project.

Aveiro, Portugal
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J. Paulo Davim

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J. Paulo Davim received his Ph.D. in Mechanical Engineering in 1997, M.Sc. in Mechanical Engineering (materials and manufacturing processes) in 1991, Mechanical Engineering degree (5 years) in 1986, from the University of Porto (FEUP), the Aggregate title (Full Habilitation) from the University of Coimbra in 2005 and D.Sc. from London Metropolitan University in 2013. He is Eur Ing by FEANI, Brussels, and Senior Chartered Engineer by the Portuguese Institution of Engineers with an MBA and specialist title in Engineering and Industrial Management. Currently, he is Professor at the Department of Mechanical Engineering, University of Aveiro, Portugal. He has more than 30 years of teaching and research experiences in manufacturing, materials, mechanical and industrial engineering, with special emphasis in machining and tribology. He has also interest in management, engineering education and higher education for sustainability. He has guided large numbers of postdoc, Ph.D. and master's students as well as has coordinated and participated in several financed research projects. He has received several scientific awards. He has worked as evaluator of projects for international research agencies as well as examiner of Ph.D. thesis for many universities in different countries. He is the editor in chief of several international journals, guest editor of journals, books editor, book series editor and Scientific Adviser for many international journals and conferences. Presently, he is an editorial board member of 25 international journals and acts as reviewer for more than 80 prestigious Web of Science journals. In addition, he has also published as editor (and co-editor) more than 100 books and as author (and co-author) more than 10 books, 80 chapters and 400 articles in journals and conferences (more than 200 articles in journals indexed in Web of Science core collection/h-index 47+/6500+ citations and SCOPUS/h-index 55+/9000+ citations).

Chapter 1

Importance of Temperature in Metal Cutting and Its Proper Measurement/Modeling



Viktor P. Astakhov and Jose Outeiro

Abstract This chapter reveals the modern objective of metal cutting processes as increased productivity of multiple machining operations. It is discussed that the only feasible option to increase productivity is increasing the cutting speed. The chapter further analyzed the known attempt to increase the cutting speed showing that the cutting temperature is the major constraint in such an endeavor. It is explained the notion of the optimal cutting temperature explaining that its wide acceptance is limited by lack of a physical explanation of its nature. The physical essence of the optimal cutting temperature is revealed. The place where the temperature in metal cutting should be measured is explained. The basic methods of measuring temperatures in metal cutting are discussed. Infrared measuring technology is explained in details including its physical principle, advantages and limitations, and use of very short-wave cameras.

1.1 Introduction

1.1.1 *Modern Objective of Machining*

Reduction of direct manufacturing costs associated with machining operations is a never-ending challenge for manufacturing plants that have this problem more pressing in recent years because two of prime reasons. The first one is the increased use of special alloys with advanced properties and significant tightening of quality requirements for machined parts. The second one is increasing global competition which is changing the environment facing most companies today. Previously

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sheltered from the global market, many manufacturing companies now concern how to hold and even increase profitability against international competitors.

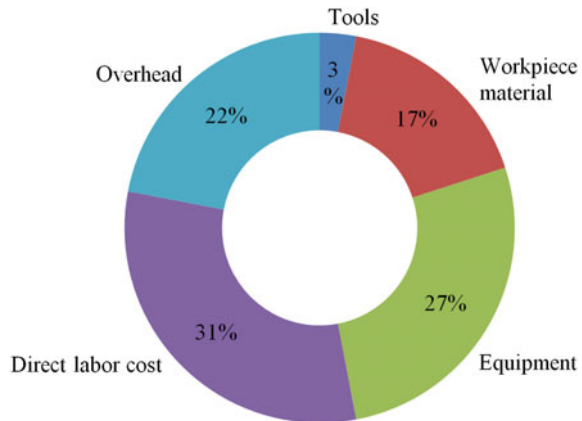
To meet these challenges, many metal-machining manufacturing companies strive to reduce cycle times and costs-per-parts/units through investing heavily in the increased use of high-speed high-efficiently machining operations and thus changing the whole metal-machining culture which was around for more than a hundred years. These changes include the utilization of machines with powerful digitally controlled truly high-speed motor spindles; the application of high-pressure through-tool metal-working fluid (hereafter MWF) supply; the implementation of high-precision hydraulic, shrink fit, and steerable tool holders; the integration of advanced cutting process monitoring; wider use of advanced cutting tool materials, and so on [1].

These changes can be called the fourth “silent” industrial revolution as they happened in rather short period of time [2] currently becoming well known as Industry 4.0 initiative. The implementation of the listed developments led to a stunning result: For the first time in the manufacturing history, the machining operating time became a bottleneck in the part machining cycle time. In shops with stand-alone CNC machines, the machining time is 20% of the operating time, whereas in automotive shops this time reaches 60%. The latter is due to aggressive tool use strategy, elimination of any tool/part inspection in the machine, adjustable pre-setting of the MWF pressure for each individual tool, automated (robotic) workpiece/part loading/unloading, using tools with RFID chips, and so on. In both case, however, the actual machining time is the largest (commonly called as a bottleneck).

Knowing these data, one should realize that the implementation of high-penetration rate tools and well-designed machining operations to reduce machining time has becoming a vital necessity. Unfortunately, some management level authorities of metal-machining companies misinterpreted the essence of the above-presented data and thus their multiple implications. In the authors’ opinion, this is because of management deal with the data presented in the cost format as shown in Fig. 1.1. Even if the direct manufacturing cost is considered, the cutting tool cost hardly exists 6%. These pictures/numbers blind “effective managers” as they look at the data presented as the driving force to send manufacturing operations overseas countries where the labor cost is 5%. Moreover, it implies not to invest in the development of new cutting tools/advanced cutting processes—what to bother with 3% of costs. In the authors’ opinion, this is the major disconnect in modern machining world.

Such a management perception stems from pre-fourth industrial revolution experience when the machining time in a cycle time of manufacturing part was so insignificant due to manual part loading–unloading, part and tool setting on the machine, part gaging in the machine, etc., took most of this time. As a result, a reduction of the machining operating time due to the use of advanced (and thus more expensive) cutting tools and optimization of machining processes was not requested and, therefore, discouraged as manufacturing professionals did not see any benefits of such activities. As a result, a number of “atavisms” are still in full

Fig. 1.1 Manufacturing cost structure in a typical automotive plant



swing even in the most advanced industries. The most profound are strict requirements in the automotive industry for a 10–15% yearly decrease of tool cost and strong drive for increasing tool life. It is not realized that when a modern automotive shop is considered (up to 60% of machining time), even a 50% decrease in the tool cost or a 80% increase in tool life results in 2% of decrease in the manufacturing cost per part.

In the authors' opinion, the only proper way for the modern metal-machining industry to progress is to increase the productivity of machining operations via the implementation of high-penetration rate high-quality tools and well-designed machining operations. A 27%-increased productivity reduces the manufacturing cost per part by 20% as the other costs shown in Fig. 1.1 are fixed cost so their relative impact per part reduces.

The structure of manufacturing cost shown in Fig. 1.1 should not surprise to anyone if he or she analyses the data. An example is presented here to facilitate understanding. Even 20 years ago, operating cost \$0.8/min was the commonly used benchmark for machine tools. Today with the introduction of modern CNC multi-axis high-speed machines with linear drives and excessively ridged spindle units, this cost is almost \$3/min. For 2200 operating hours per year, \$3/min means the operating cost 396,000 per year for just one machine. Even factoring in 80% efficiency for loading/unloading, changing tools, and setup, an increase in productivity by 50% amounts to a potential yearly savings of \$158,400 per CNC machining center per year. As the machining operating time became bottleneck in the part machining cycle time, this can be achieved only through increasing productivity of the cutting tool. In other words, an item that according to Fig. 1.1 accounts for only 3% of manufacturing cost which can affect this cost significantly as productivity is literary determined on *the tool cutting edge*. Therefore, the best, most reliable cutting tools of advanced designs, with the best tool materials and highest possible tool manufacturing quality capable of high-efficient machining should be used in modern metal-working industry regardless of their cost as this cost is still virtually insignificant compared to the gain due to increased productivity.

Another important but routinely ignored aspect is unscheduled downtime. It is up to 10% for stand-alone CNC machine, whereas it reaches 20% in highly automated industries. Nevertheless, it is considered as a kind of unavoidable nuisance. As a result, the unscheduled/uncontrolled downtime due to cutting tool failures is not in the center of interest of researchers and engineers for years because: (1) it was insignificant compared to the scheduled downtime; (2) speed and cutting feed were relatively low so that cutting tool failed by wear, which was periodically controlled; and (3) the costs of tool failure was low as cutting tools were relatively inexpensive and their failures did not bring much damage to the machining system (machine, part fixture, and workpiece). However, times have changed so that the uncontrolled downtime has gradually become significant. Tool failure today is a major cause of unscheduled stoppage in a machining environment and is costly not only in terms of time lost, but also in terms of capital destroyed [3]. Some estimates state that the amount of downtime due to tool breakage on an average machine tool is on the order of 6.8%, while when tool failures are considered, the figure is closer to 20% [4]. Even if the tool does not fail during machining, the use of excessively worn or damaged tools can put extra strain on the machine tool system and cause a loss of quality of the finished part.

1.1.2 Role of the Cutting Speed in Increasing Productivity

As known [4], the productivity of machining operation where the tool or workpiece rotates is determined by the penetration rate commonly known as the feed rate. It is calculated as

$$\text{Penetration rate} = f_m = f \cdot n \quad (1.1)$$

where f_m is the feed rate, mm/min, f is the cutting feed, mm/rev, and n is the rotational speed, rpm. The rotation speed is calculated as

$$n = \frac{1000v}{\pi d_w} \quad (1.2)$$

where v is the cutting speed, m/min, d_w is the workpiece diameter in turning or drill diameter in drilling, mm, $\pi = 3.141$.

It directly follows from Eqs. (1.1) and (1.2) that the penetration rate/productivity can be increased either by increasing the cutting speed, v or by increasing the cutting feed, f or both simultaneously. As discussed earlier [4], the limit of the cutting feed increase is well-set primarily by two factors:

- Quality of matching is surface integrity including surface roughness, circularity, and straightness of machined holes in drilling operations, lobbing in gear hobbling, and so on.

- Tool strength depends on both the properties of the work and the tool materials. In other words, it is set by the allowable normal stress over the tool–chip interface. When the cutting feed, and thus the normal stress over the tool–chip interface exceeds this limit, the whole rake faces cracks. The feed when this occurs is known as the breaking feed.

Therefore, the only feasible option to increase productivity is increasing the cutting speed. Although this was felt intuitively for years, this option was not considered as feasible primarily because high-rpm machines with dynamically rigid high-power spindles were not available along with many other important components of the machining system (e.g., high-precision tool holders, high-pressure MWF supply system) that make such an option of practical interest. As nowadays all these become commercially available, a new look at the high-efficiency speed range should be taken. This range is commonly thought of as located between the cutting speed associated with the minimum cost per part and that associated with maximum parts per unit time. In the authors' opinion, however, the further development of efficient cutting process/operation should be directed towards high end of the high-efficiency range of the cutting speed and then even exceed it as other contributors to a part cycle time as time needed for part loading-unloading, tool change, gaging and so on will be further reduced.

One may argue that to run machining operations at higher cutting speeds, more expensive high-efficient cutting tools of exceptional high quality are needed. However, this added cost is virtually insignificant compared to the gains made due to increased productivity. According to the authors' experience, a common mistake made in industry is multiple attempts to increase the cutting speed with just ordinary tools having ungrounded hopes for success. Specialists and practitioners in the field should realize the physical constraints on the cutting speed increase so that new efficient cutting tools and machining operations can be developed.

1.2 Known Attempts to Increase the Cutting Speed: Tool Life Versus Cutting Speed

1.2.1 Taylor Perception of Heat in Machining

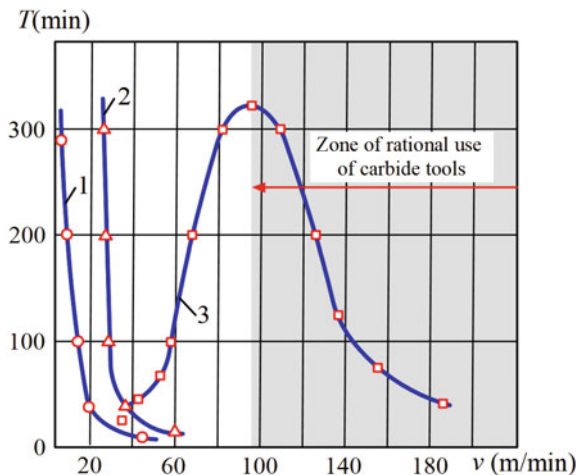
The most relevant correlations between the cutting speed, heat generated, in machining, and tool life have been made by Taylor in his pioneering work “On the art of cutting metals” [5] (paragraphs 175–182 and 183–4). First, he recognized that “*the heat produced by the pressure of the chip has been the chief cause of tool wear.*” Second, he distinguished three classes of tools (tool wear) depending upon the influence of this heat:

- Wear of the first class. According to Taylor, “Wear of the first class—tools in which the heat, produced by the pressure of the chip, has been so light as to have had no softening effect upon the surface of the tool.”
- Wear of the third class. According to Taylor, “Wear of the third class—tools in which the heat has been so great as to soften the lip surface of the tool beneath the chip almost at once after starting the cut.”
- Wear of the second class. According to Taylor, “Wear of the second class—tools in which the heat only slightly softens the surface of the tool during the greater part of the time that it is cutting, while during the latter part of the time heat is the chief cause of wear because, as described in the third class, it greatly softens the lip surface under pressure of the chip.”

Analyzing these obtained results, Taylor concluded that the cutting speeds corresponding to the first class of wear are “*entirely too slow for economy.*” Therefore, “*it is then with the cutting speeds causing wear of the SECOND CLASS*” should be used in every machine shop. “*Cutting speeds of this class are referred to as “ECONOMICAL SPEEDS” or “MOST ECONOMICAL SPEEDS.”*” As can be seen, Taylor was the first who introduced the high-efficiency range of the cutting speed accounting for the tool materials and other cutting conditions used at the end of the nineteenth century.

One should remember that only high-carbon and emerging high-speed steels were available to Taylor for cutting testing. Curves 1 and 2 in Fig. 1.2 represent the dependence of the tool life on the cutting speed for high-carbon and high-speed steels, respectively. As can be seen, tool life decreases with the cutting speed. Because Taylor obtained similar data for a range of similar tool materials, he proposed a simple evidence-based tool life formula universally known as Taylor’s formula

Fig. 1.2 Tool life versus the cutting speed in rough turning (depth of cut—4 mm, cutting feed—0.3 mm/rev) of ANSI 1050 steel (HB 190): (1)—High-carbon (1.2%C) tool steel, (2) T15 high-speed steel, (3) P20 carbide



$$vT^n = C_T \quad (1.3)$$

where T is tool life in minutes, C_T and n are Taylor's constants.

The Taylor's constants are to be determined through series of tool life tests at different speeds under the selected criterion of tool life (commonly it is a certain flank wear, VB_{cr} measured in mm) [6]. The results of the test are the so-called wear curves schematically shown in Fig. 1.3a. Figure 1.3b shows practical determination of Taylor's constants in a $\log(v) - \log(T)$ graph contacted using the data from Fig. 1.3a.

Although Taylor's tool life formula is still in wide use today and is in the very core of many studies on metal cutting, one should remember that it was introduced in 1907 as a generalization of many years of experimental studies conducted in the nineteenth century using work and tool materials and experimental techniques available at that time. Since then, each of these three components underwent dramatic changes. Unfortunately, the validity of the formula has never been verified for these new conditions. Nobody proved so far that the formula is still valid for any other cutting tool materials than carbon tool steels and high-speed steels. A simple test of a single-point tool with sintered carbide as the tool material shows that the wear pattern does not resembles those obtained with high-carbon and high-speed steels as clearly seen in Fig. 1.2. As shown, the zone of rational use of carbide tool is to the right of seemingly highest tool life. In nineteenth century, the cost of machine tools, labor etc., was insignificant so that Taylor recommended the "optimal cutting speed" as corresponding to 1.5 h of tool life (time between tool re-sharpenings as termed by Taylor [5]). As discussed above, the time has changed so does the structure of manufacturing cost as shown Fig. 1.1. As a result, nowadays tool life is measured in minutes as productivity of machining operations is far more important than tool life/tool cost.

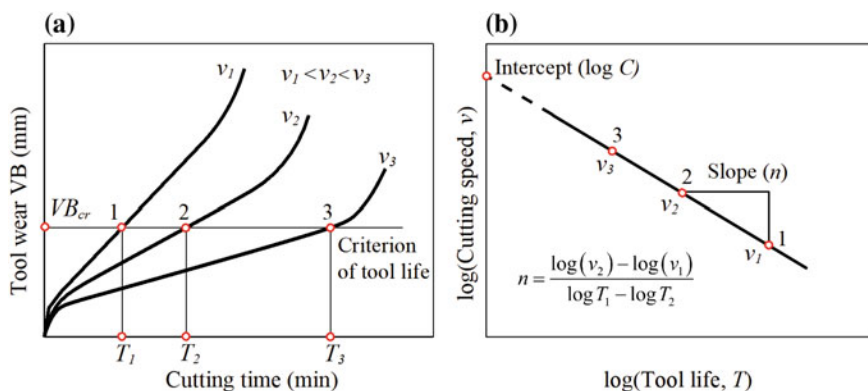


Fig. 1.3 Basic representation of Taylor's formula: **a** wear curves for several cutting speeds (1, 2, and 3) and **b** determination of the Taylor's constants

In authors' opinion, the major problems is that Taylor's formula constitutes the basis of the tool life testing even at the level of national and international standards as ISO 3685:1993 (reviewed and confirmed in 2017) "Tool-life testing with single-point turning tools" and ANSI/ASME "Tool Life Testing with Single-Point Turning Tools" (B94.55M-1985, reaffirmed 2014). The whole notion that tool life decreases with the cutting speed is absolutely wrong hampering the whole idea of high-speed machining, and thus closing the door to increased productivity of metal-machining operations.

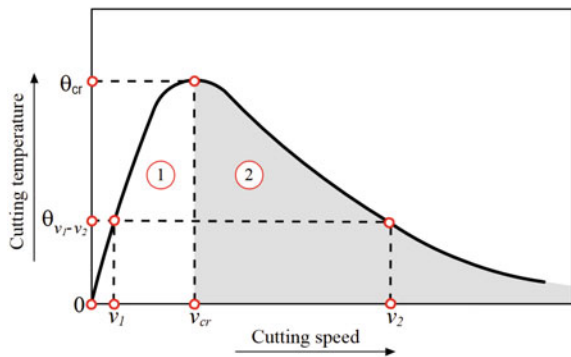
1.2.2 Solomon's High-Speed Machining Perception

As pointed out by King [7], the concept of high-speed machining (HSM) was conceived by Dr. Carl J. Salomon during a series of experiments from 1924 to 1931. A German Patent No. 523,594 was granted in 1931. The whole idea was based on the concept depicted by Fig. 1.4 [7]. As shown, a schematic representation of the "cutting speed–cutting temperature" relationship includes two distinctive regions. In region 1, the cutting temperature θ increases with the cutting speed v , which is consistent with the known experimental results. This holds on until the cutting temperature reaches a certain maximum, termed as the critical cutting temperature θ_{cr} , which is the right boundary of region 1. In region 2, the cutting temperature decreases.

For last almost one hundred years, the notion schematically depicted by Fig. 1.4 was a source of inspiration of HSM proponents. Their simple argument to support the whole idea of HSM is that if a "normal" cutting speed v_1 (from region 1) corresponding to "normal" tool life results in θ_{v_1} temperature, then the same tool life can be achieved at much greater cutting speed v_2 (from region 2) as the cutting temperature at this speed θ_{v_2} is the same as that at cutting speed v_1 , i.e., θ_{v_1} .

Many subsequent researchers tried to achieve the same results with a very limited number of success stories reported in the literature (well summarized by King [7], Erdel [8]). The common excuse is that much of the supporting data were

Fig. 1.4 Schematic representation of the "cutting speed–cutting temperature" according to Solomon's idea



lost during World War II and none of the participants in the research are alive to comment conditions of the Solomon team test details.

In the authors' opinion, the first question to the idea illustrated in Fig. 1.4 is about where exactly and how the discussed temperature was measured. In our today's understanding of metal cutting, there is no such a notion as "the cutting temperature" although it widely used in the literature on the subject. Even a simple finite element analysis fully confirms that temperatures in the workpiece, tool, and chip vary from one point to the next and, moreover, are time-dependent, i.e., constantly vary within each cycle of chip formation [2] as shown by four frames in Fig. 1.5.

The fundamental studies, machining tests, and practical applications of HSM attempted, however, only since the early 1980s when high-speed spindles, and thus, machines became available. The first industry-level HSM was attempted just 10 years ago in the automotive industry where the most advanced high-rpm (up to 25,000) spindles with ceramic bearings and active control were used in the production lines and newly built powertrain plants. However, tool lives of many carbides tools were found to be one-third of those run at conventional cutting speeds for the same machining operations producing the same parts. Because the

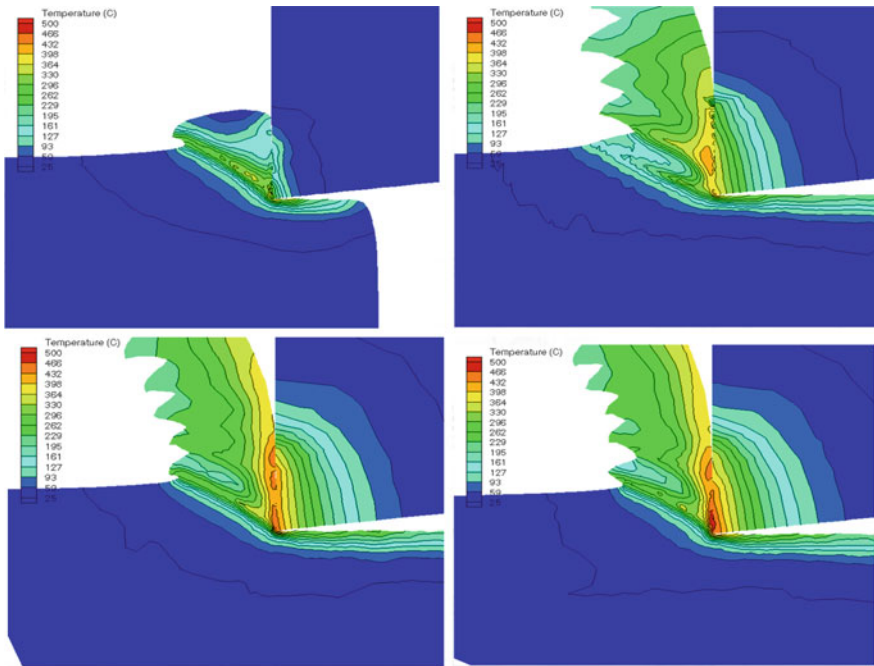


Fig. 1.5 Finite element frames of temperature distribution in the workpiece, tool, and chip. Work material AISI steel 316L; cutting speed $v = 75$ m/min, uncut chip thickness $h_D = 0.2$ mm; cutting tool: material—sintered carbide K10; rake angle $\gamma = 0^\circ$, clearance angle $\alpha = 7^\circ$

direct tooling cost at such plants does not normally exceed 5%, the reduced tool life at significantly decreased cycle time is fully justified by efficiency numbers.

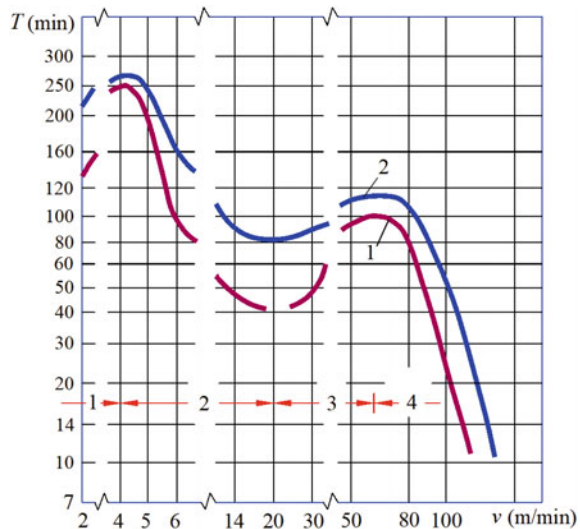
Millions of dollars and years of research have been spent on finding confirmation of Solomon's idea/results, numerous conferences on HSM have being held, and hundreds papers were published. Numerous discussions in the literature took place between supporters and opponents of the whole idea of HSM, e.g., [9]. It seems that nobody tried to provide any kind of physical (mechanical, chemical, etc.) rationale on why such a hypothesis can be possible ever. Instead, the approaches were as follows: Let's increase the cutting speed to maximum of the ability of our machine with the existing tool designs, tool materials and holders, methods of MWF supply and so one and see what happens. In the authors' opinion, such an approach is of no help if one aims to shift the range of high-efficiency cutting speed toward the direction of higher speeds.

1.2.3 Reality for Sintered Carbide Tools

Figure 1.6 shows “tool life—cutting speed” curves obtained with and without MWF. The cutting speed is varied from 2 to 150 m/min. The maximum width of the flank wear land $VB_B \max = 0.6$ mm was used as a tool-life criterion. Curve 1 relates to the test with no MWF, whereas curve 2 relates to the tests with a 8% concentration water-based MWF.

Four distinguishing regions can be observed in this figure. Region 1 corresponds to the turning with very small cutting speeds (2–4 m/min). Within this region, the tool life increases with increasing cutting speed: from 130 to 220 min in dry turning

Fig. 1.6 Tool life versus cutting speed. Workpiece material: high alloy (0.2% C25%Cr20%Ni, 2%Si), tool material: carbide M30 (92% WC8%Co), tool geometry: $\gamma_n = 12^\circ$, $\alpha_n = 12^\circ$, $\kappa_r = 45^\circ$, $\kappa_{r1} = 45^\circ$, tool nose radius $r_n = 1$ mm; cutting regime: $a_p = 2.5$ mm, $f = 0.06$ mm/rev. Curve 1 with no MWF, curve 2–8% concentration water-based MWF



and from 215 to 255 min in turning with MWF. Region 2 (corresponding to the cutting speed range of 4–19 m/min) is characterized by a sharp decrease in tool life with increasing cutting speed; its cause is the transition of the bounded by seizure layer from the stable to unstable condition. The tool wear initially takes place by crumbling-out fine particles and then, with increasing the cutting speed from 10 to 20 m/min, by rough particles of the tool carbide. The deformation of layer being cut takes place non-uniformly, and a chip has parts of different thicknesses along its length [10]. With further increase in cutting speed up to 60–70 m/min (region 3), the tool life, in turning no MWF, increases from 40 to 100 min whereas when MWF is used, tool life increases at a lower rate (from 90 to 118 min). Commonly [10], the increase in the tool life within this speed range is explained by a gradual transition from the adhesion type of tool wear into the diffusion type. Region 4 corresponds to machining at high cutting speed. The decrease in the tool life within this region is explained by high cutting temperatures.

As can be seen in Fig. 1.6, the relation “tool live vs. cutting speed” does not follow Taylor’s formula (see Eq. (1.3) and Fig. 1.3). It is clear that in machining of a high alloy (0.2% C 25% Cr 20% Ni, 2% Si) under the listed conditions, the optimal economically efficient cutting speed is approx. 80 m/min.

Leading cutting tool manufacturers have learned this curve during their multiple tests (although the explanation has never been published). Figure 1.7 shows an example. As can be seen, the recommended optimal machining regime, i.e., the cutting speed, feed, and depth of cut, is indicated for steels (*P*) and for cast irons (*K*).

The presence of a “hump” on “tool life—cutting speed” curves, i.e., the peak of tool life in the region of relatively high (for a given work material) cutting speeds, seems to at least partially supports the above-described Solomon idea particularly if one recalls that Solomon might use a newly invented (approx. 1927) sintered carbide as the tool material, whereas other researchers at the time used high-speed steels. The physical explanation for the “hump” on “tool life—cutting speed” curves cannot be found in any of basic books/texts on metal cutting. A real question is “Do we really need understanding of the above phenomenon, and thus the whole essence of metal cutting?”

The answer is not that simple as one might think. As long as things proceed well, we do not need to explain or to understand the essence. Moreover, explanations and quests for understanding would retard and disturb writing award-winning papers

Fig. 1.7 Example of a box with cutting inserts where the recommended cutting speeds (*P*—for steels, *K*—for cast irons)



and research grant proposals, encouraging people to ask inconvenient questions and question comfortable status quo. However, when something goes wrong or breaks down in a production or research activity, or when a competing industrial or scientific group, company, country etc., achieves better results, so that one is confronted with losing the market or the funding, or even a sector of national economy, then the understanding of the involved processes becomes urgent. It is common, unfortunately, that such urgency becomes evident too late. In fully applicable to metal cutting and tool design research: when machining time was insignificant in the whole time of part manufacturing, this research was not funded; when machining time became bottleneck, no research results and specialists are available for the design-efficient machining operations.

Another vital but routinely ignored aspect of the importance of proper explanation belongs to the plane of human psychology. It can be explained with an example from the world of science. In 1847, decades before the germ theory of infection, physician Ignaz Semmelweis suggested that, by washing their hands before examinations, doctors could save the lives of many maternity ward patients [11]. He published the results of his many-year study showing that hand-washing produced a reduction in maternal mortality to less than 1%, compared to 10–35% in general practice. Surprisingly, his results were totally rejected by medical community as many doctors feel offended by his pure evidence-based finding, and thus a clear suggestion. Needless to say that this suggestion was not followed. The prime reason for that is that Dr. Semmelweis did not offer any scientific rational why his suggestion works regardless of a great body of meticulously carried experimental study data. Only decades later, when the germ theory of infection was widely adopted in medicine, hand-washing became a routine mandatory procedure as doctors understood why they need to do so. This example shows the role of the widely accepted theory (or simply, explanations) for even seemingly obvious experimental data or even the result of observation that everyone can see.

1.3 Optimal Cutting Temperature (OCT) and Speed

1.3.1 *Notion of OCT*

Analyzing a great body of experimental data, Makarow formulated the law [12] which was presented as the first metal cutting law (Makarow's law) by Astakhov [13]:

For given combination of the tool and work materials, there is the cutting temperature, referred to as the optimal cutting temperature θ_{opt} , at which the combination of minimum tool wear rate, minimum stabilized cutting force, and highest quality of the machined surface is achieved. This temperature is invariant to the way it has been achieved (whether the workpiece was cooled, preheated, etc.).

The first metal cutting law, established initially for longitudinal turning of various work materials (example is shown in Fig. 1.8a), was then experimentally proven for various machining operations. Figure 1.8b shows its applicability in twist drilling, Fig. 1.8c, in thread cutting, Fig. 1.8d, in gear hobbing. Considering Fig. 1.8a, one can see that when the cutting feed increases, the surface wear rate, h_s , reduces. However, minimum tool wear rates, when various cutting feed are used, occur at the same OCT, θ_{opt} although the amount of the surface wear rate varies more than 2 times. Even more pronounced effect can be observed in Fig. 1.8b, where the cutting feed has a much stronger influence on tool life, represented by the total length of the tool path, L , to achieve $VB_B = 0.3$ mm. As can be seen, there is a specific combination of the cutting speed and feed at which tool life is at a

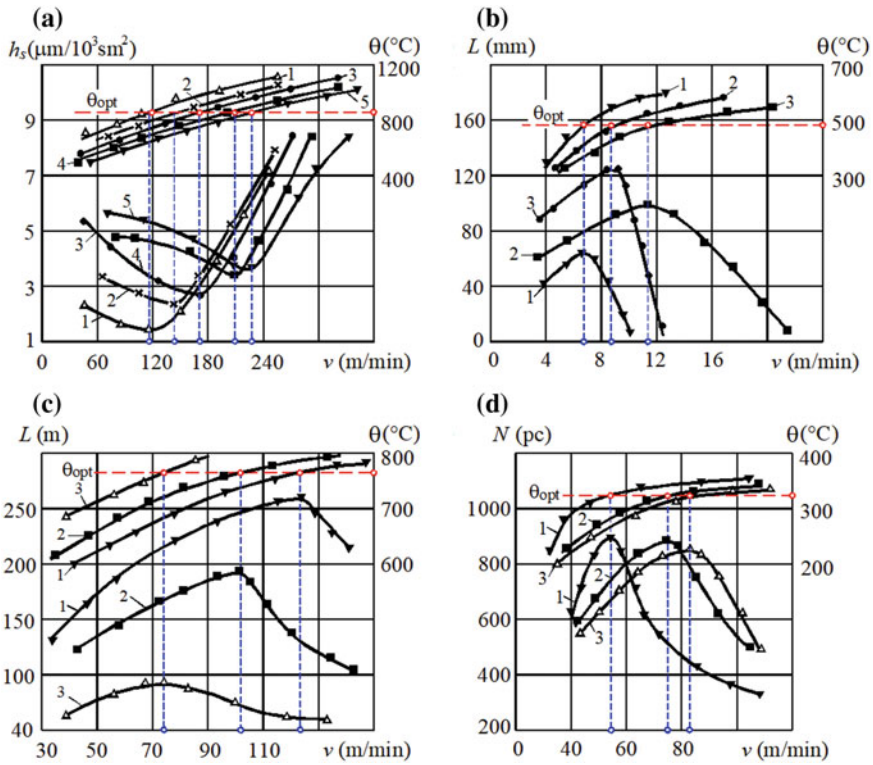


Fig. 1.8 Optimal cutting temperature: **a** turning, tool material—carbide P30, work material—AISI 1045, depth of cut $a_p = 1.5$ mm using different cutting feeds: 1–1.4, 2–0.87, 3–0.61, 4–0.39, and 5–0.21 mm/rev, **b** drilling with twist drills, tool material—HSS T15, work material—316Ti alloy, drill diameter—15 mm, tool life criterion— $VB_B = 0.3$ mm using different feeds: 1–0.28, 2–0.17, and 3–0.10 mm/rev, **c** thread cutting with a single-point cutting tool, tool material—M10, work material—AISI 303, tool life criterion— $VB_B = 0.5$ mm, using different feeds: 1–1.0, 2–1.5, and 3–2.0 mm/rev, **d** gear hobbing, tool material—HSS T15, work material—AISI 4140, module, $m = 2$ mm, number of teeth $z = 61$, $VB_B = 0.7$ mm, using different axial feeds: 1–4.0, 2–2.5, and 3–1.7 mm/rev

maximum. Changing the cutting speed and/or feed on either side reduces tool life while the minimum tool wear rate under a given combination of these parameters corresponds to OCT. The same conclusions can be drawn for the data shown in Fig. 1.8c, d.

1.3.2 *Lack of Explanation*

Although Makarow's findings are of great practical importance, researches, specialists, and metal machining turn a blind eye on these results obtained more than 30 years ago. In the authors' opinion, the situation is the same as it was with the above-described Dr. Semmelweiss' finding. In both situations, no pleasurable explanation was provided. Moreover, the findings by Makarow were in odds with the prevailing notions in metal cutting, e.g., Taylor's formula, according to which the tool life should go down with the cutting speed. Besides, 30 years ago no one seriously considered increasing the machining efficiency through increasing the cutting speed.

As discussed above, the times have changed, so a need is felt to present an explanation on feasibility of improving metal-machining productivity by increasing the cutting speed. Our detailed analysis was carried out to correlate the first metal cutting law with the above-discussed tool life "hump" shown in Fig. 1.6.

Our analysis started with addressing one important concern. The cutting temperature in the Makarow's tests was measured by a tool-work thermocouple, where the tool-workpiece contact is the hot junction of the thermocouple as that the cutting temperature is related to the emf generated across the hot interface between tool and workpiece. This method was thought to be the most successful by Shaw [14] and again was considered the best approach by Stephenson [15]. In spite of its wide use, the basic question of what temperature is actually measured has never been answered. Some researches (e.g., [16]) speculate that it is uncertain whether tool-workpiece thermocouples measure the lowest temperature at the interface or the mean value or that is unlikely the highest temperature over the tool-chip contact area. If, however, one leans the proper calibration procedure of a tool-work thermocouple (e.g., [17, 18]), he or she can conclude that the measured temperature is fairly close to the maximum temperature at the tool-chip interface. If it is so, how this temperature can be correlated with the minimum surface wear rate, h_s , measured on the tool flank face?

Our further analysis of OCT reveals that not only tool wear is at minimum at this temperature, but the lowest (locally) cutting force, the minimum machining residual stress, and the best quality (surface roughness and shape) of the machined surface are achieved. It seems rather difficult to correlate these results to the temperature at the tool-chip interface. In the authors' opinion, however, a clear explanation can be provided if the realistic model of metal cutting process supported by the proper model of work material behavior and relevant experimental observations is used. Moreover, it can be further revealed that the common denominator of many metal

cutting phenomena is temperature. As a result, one of the prime concerns of theoretical, experimental, and numerical studies of metal cutting is to determine temperature distribution in the components of the cutting system (the workpiece, chip, and tool).

1.4 Existing and Realistic Models

1.4.1 Problems with the Existing Models

Experimental studies in metal cutting are expensive and time-consuming. Moreover, their results are valid only for the experimental conditions used and are highly dependent on the accuracy of the calibration of the experimental equipment and apparatus used. In the authors' opinion, they should be carried out only for (1) to determine the work material properties relevant to metal cutting and (2) to validate the developed numerical model. Both experimental procedures should be standardized to obtain relevant results. As the proper physical model is in the core of numerical methods, it is of the utmost importance to use a physically sound model of metal cutting to achieve greater efficiency of machining operations via numerical modeling.

One may argue, however, that after almost 150 years of recorded metal cutting studies (counting from Time [19]) we have such a model that appears in any textbook on metal cutting all over the world [20]. In reality, it is not so. As pointed out by Usui [21], no significant progress has been made, and after many years of study, the theory is still lagging behind the practice. Shaw in his book ([14], p. 152), which summarizes his lifetime experience in the field of metal cutting, came to a discouraging conclusion that *“All this suggests that it is next to impossible to predict metal cutting performance. However, it should not be inferred that detailed study of the cutting process is without value. Each fundamental study that is made and properly interpreted adds to our understanding of the process, and understanding is the next best thing to the ability to predict.”*

Numerical modeling of machining operation using finite element method (FEM) seems to be an attractive alternative to actual testing of cutting tools. This is particularly true because the commercial codes Abaqus, Deform2D, Thirdwave AdvantEdge, and others are readily available in the marketplace. One may wonder why these are not implemented in the numerous computer-aided manufacturing (CAM) commercial packages similar to, for example, MasterCAM software to visualize the material removal sequence, so the kinematic and physical modeling of the machining operation can be carried out in planning of metal cutting operations as it is carried out in metal forming. Until ten years ago, the design of metal-forming tools and processes was mostly based on knowledge gained through experience, and designing of optimum tools often required a protracted and expensive trial-and-error testing. Today, even in the earlier phases, simulations of

the forming process are carried out using FEM analyses. The most important goals of using FEMs are the verification of manufacturability of the sheet metal parts and obtaining vital information on the optimal tool design. As a result, great savings have been achieved due to the introduction of process simulation in metal forming. These savings originate from rapid development of tools and from dramatic shortening of trial-and-error testing. In recent years, tool development and production time have been reduced by about 50% due to the usage of simulations and a further 30% reduction over the next few years appears realistic. The simulation of forming tool has already reached the stage where its results can be fed directly into the press tool digital planning and validation process. Thus, today, starting from the design model and through practically all process steps (as far as the actual design of the press tool), the production of a component can be fully simulated before a first prototype is built [22].

It is clear that FEM simulation in metal cutting is not nearly as readily utilized as it is in metal forming although metal cutting process is often thought of as a metal-deforming process [14, 23]. It appears as a surprise for many specialists because the two most essential inputs to any FEM simulation of metal shaping processes, namely the work materials' behavior model under deformation and friction model to describe contact conditions over the workpiece–tool interfaces, are the same for both forming and metal cutting, whereas practical significance of the results is drastically different. In the authors' opinion, the root cause is in improper definition of metal cutting and thus in the commonly used physical model of metal cutting that is in the foundation of all known FEM packages of metal cutting simulation.

1.4.2 Features of the Realistic Model as Related to Temperature Distribution in the Deformation Zone

1.4.2.1 Fracture and Cyclicity

A schematic evidence-based representation of the basic metal cutting model is shown in Fig. 1.9. As can be seen, a wedge-shaped cutting tool moves with the cutting speed v removing a certain layer of thickness h_D and width w_D from the workpiece. As the rake face of the tool presses the layer being cut, a deformation zone forms in front of the cutting edge A where this layer transforms into the chip. As pointed out by Taylor [5], there is no difference between cutting and compression at the beginning before the partially formed chip forms. After the partially chip is formed, the deformation mode changes. Taylor pointed out: “*After the cut has once been started, and the full thickness of the shaving is being removed, the action of the tool may be described as that of tearing the chip away from the body of the forging....the portion of the chip which has just been turned away, and which is still pressing upon the lip surface of the tool, acting as a lever*

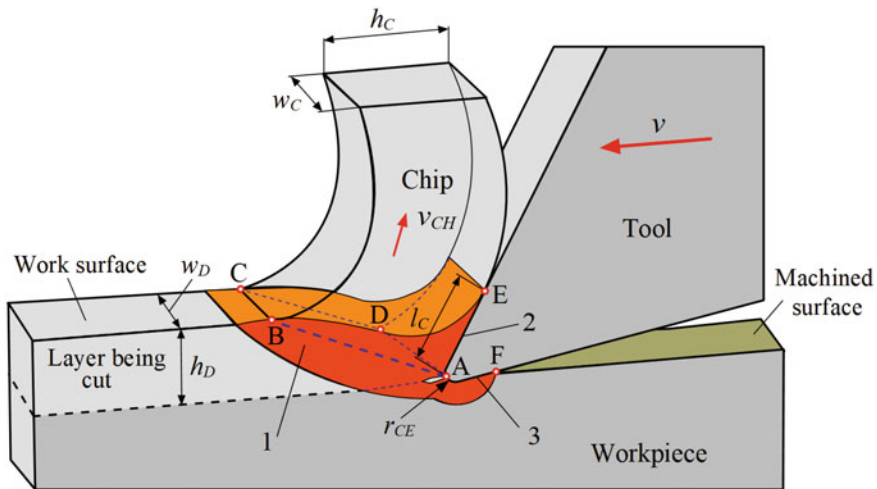


Fig. 1.9 Schematic representation of metal cutting

by which the following portion of the chip is turn away from the main body of the metal.” Moreover, Taylor noticed that the chip thickness h_C “about double the thickness of the original layer,” whereas its the chip width, w_C , is equal to that of the layer being cut, w_D due to friction over the tool rake face in the direction perpendicular to the chip motion. This implies that even the simplest case of cutting, known as orthogonal cutting, includes a 3D state of stress in the deformation zone. Therefore, the prevailing notion about “simple shearing” in deformation zone should be abolished.

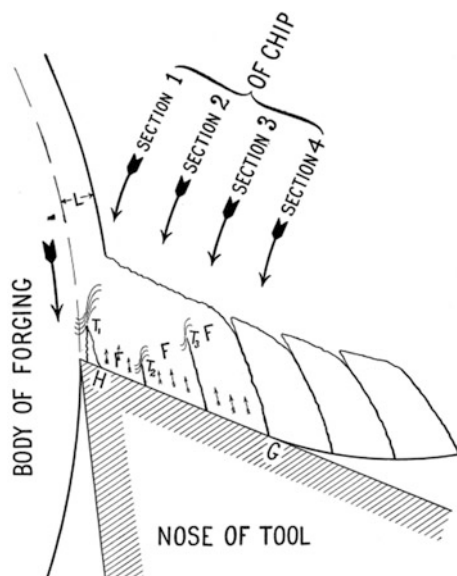
As a result of the complex state of stress in the deformation zone with the prevailing bending stress imposed by partially formed chip cantilever, the chip has limited contact with the rake face (the contact length l_C) as it turns away from the rake face at point E as shown in Fig. 1.9. The whole energy needed to sustain the deformation zone is supplied through the contact length AE through partially formed chip, which moves with velocity v_C relative to the rake face. This velocity is normally less than the cutting speed by the chip compression ratio, ζ (defined as $\zeta = h_C/h_D$), i.e., $v_C = v/\zeta$.

The contact interface over the tool’s flank face (AF in Fig. 1.9) is always formed no matter how sharp is the tool (i.e., how small is the radius of the cutting edge, r_{CE}) due to elastic recovery (a.k.a. springback) of the work material. The greater the strength of the work material and the lower its elasticity modulus, the greater the springback, and thus the longer AF and higher the contact stresses [4]. The sliding velocity over this interface is equal to the cutting speed v .

The major features of the realistic model as related to temperature distribution in the machining zone, tool, and chip are summarized as follows.

Metal cutting is the purposeful fracture of the layer being cut occurring due to complex interactions of the tool, workpiece, and chip [5, 10]. As a result, the

Fig. 1.10 Taylor's representation of the formation and development of cracks in chip formation



fracture properties of the work material obtained under the state of stress, temperatures, and strain rate particular to metal cutting should be considered in the testing and modeling of this process [10, 24]. Fracture is always initiated from a crack formed in front of the cutting edge. It further develops as cracks' propagation, partial diffusion healing, etc., as shown in Fig. 1.10 which depends on the particularities of a given machining operation [5].

Chip formation is a cyclic process [5] frequency of which depends primarily on the work material and cutting speed [10]. Due to cyclicity, the force (energy) required to sustain the process of metal cutting is changing continuously in cyclical manner (Fig. 1.11). As a result, temperatures (the temperature field) in the work-piece, tool, and chip change accordingly as shown by modeling [25, 26] and experimental study using an infrared system [27]. Note that because almost all the deformation is taken place in front of the cutting edge as schematically shown in Fig. 1.9, thermal imaging system measurements were primarily concentrated on this region.

1.4.2.2 Materials Model

In the authors' opinion, the common mistake made in FEM of metal cutting is the use of the improper model of work material behavior bluntly "borrowed" from deforming operations modeling simply because, as discussed above, metal cutting is considered as one of the deforming processes accomplished by plastic deformation. To account for process temperatures, strain rate, and complex state of stress in the practice of modern CAM of deforming processes, the well-known Johnson and

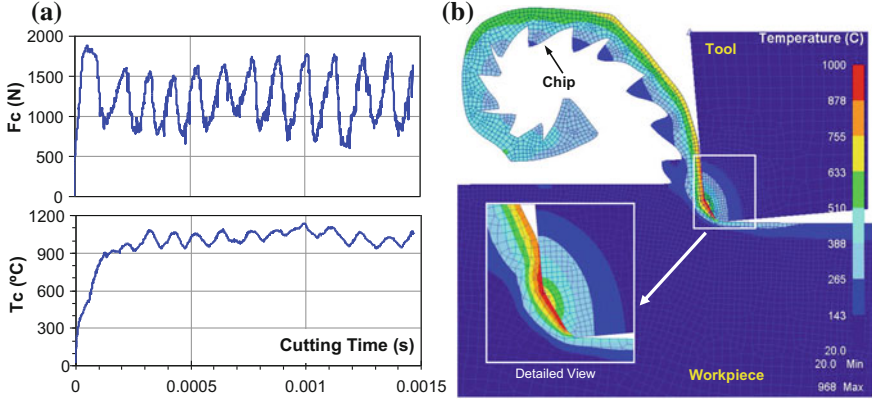


Fig. 1.11 Cyclic variation of the cutting force, F_c , (a), and cutting temperature, T_c , induced by chip formation, during orthogonal cutting simulation of AISI H13 tool steel (b)

Cook model [28] is used. This model, known as the flow curve and *universally used in modeling of forming and machining operations*, has the following full form

$$\sigma_e = \left(A + B \epsilon_{eq}^n \right) \left(1 + C \ln \left(\frac{\dot{\epsilon}_{eq}}{\dot{\epsilon}_{eq}^0} \right) \right) \left[1 - \left(\frac{T - T_0}{T_F - T_0} \right)^m \right] \quad (1.4)$$

where ϵ_{eq}^n is the equivalent plastic strain, $\dot{\epsilon}_{eq}$ is the equivalent plastic strain rate, $\dot{\epsilon}_{eq}^0$ is the reference equivalent plastic strain rate, T is the temperature, T_0 is the room temperature, T_F is the melting temperature, and A , B , C , n and m are coefficients, which depend on the material. These coefficients are determined through mechanical tests.

The major problem with the flow curve represented by Eq. (1.4) is that it allows the material to flow under increasing stress indefinitely with no fracture. Obviously, this is not the case in the reality of metal forming where allowable deformation is limited by necking, tearing, fracture, or wrinkling that define forming limits (i.e., allowable strains). As a result, only a part of the flow curve is actually utilized in forming to limit the strains in the process [29]. To do that, forming limit diagram (FLD) introduced by Keeler and Backofen [30] and Goodwin [31] is used (Fig. 1.12). As can be seen, the safe forming region is rather limited the zones of possible defects. Moreover, fracture boundary, a.k.a. fracture locus is much wider than shown accounting on variations of fracture properties of the work materials and process conditions. Unfortunately, no such a restriction of the deformation limit is set in metal cutting modeling.

When fracture in FEM of metal cutting is considered then the damage curve [2, 24] should be used. Figure 1.13 shows a schematic of the damage curve. The elastic–plastic undamaged path abc is the same as in the flow curve (Eq. 1.4) followed by the departure of the experimental yield surface from the virtual

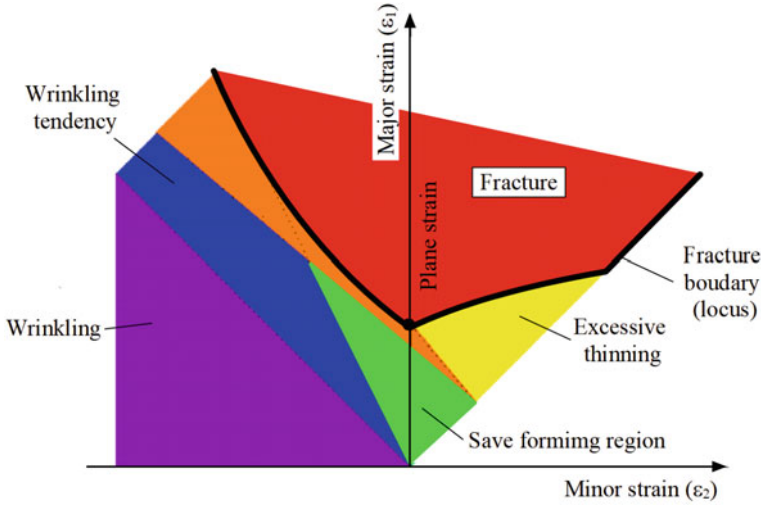
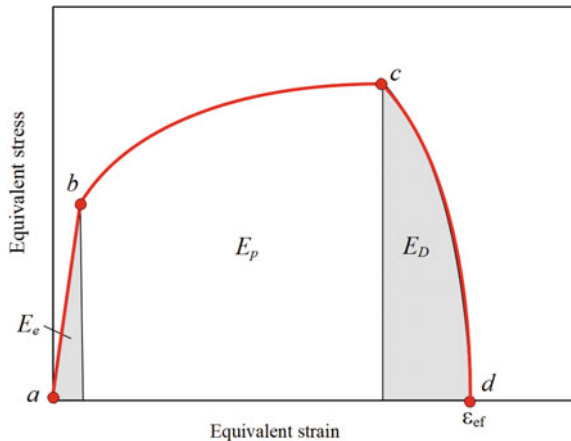


Fig. 1.12 Schematic representation of saving the forming regions in a form limit diagram

undamaged yield surface at point c . Point b can be considered as the hypothetic damage initiation site where the material hardening modulus becomes progressively sensitive to the amount of damage leading to declining of the material loading capacity. This usually occurs in ductile metals when the material loading capacity decreases by 30–70% of its full capacity due to the accumulated damage [32]. The damage curve is approximated as discussed by Abushawashi et al. [24].

Referring to Fig. 1.13, the energy spent in plastic deformation of the unit volume of the work material (i.e., to reach the strain at fracture, ϵ_{ef}) is represented by the area under the damage curve. This energy includes three components: elastic energy, E_e , strain-hardening energy, E_p , and damage energy, E_D . Elastic energy, E_e , is insignificant in cutting of metallic materials.

Fig. 1.13 Damage curve



1.4.2.3 Energy Conversion and Heat Sources

There are three sources of thermal energy generation (conversion from mechanical energy): (1) deformation zone where the layer being cut gradually converts into the chip, (2) tool–chip interface where the chip slides over the tool rake face, and (3) tool–workpiece interface where the machined surface slide over a small area of the tool flank face. These sources are schematically shown in Fig. 1.9. The generated thermal energy flows/distributes in the form of heat creating the temperature fields in the workpiece, tool, and chip. The questions to be answered are about the relative “strength” of these sources and about the way of formation of temperature fields in the tool, chip, and workpiece.

In machining of many ductile materials, source 1 is the strongest accounting on 65–90% of the total thermal energy generated so that 10–35% of thermal energy is generated due to friction over the above-mentioned interfaces. For example, in machining of AISI 1010 steel, approx. 90% of total thermal energy is generated due to plastic deformation; in machining of AISI 1045 steel—76%. Being time-dependent, the temperatures in these fields are also affected by the thermal properties of the materials involved (tool and work) as well as by the machining regime, primarily the cutting speed.

Figure 1.14a shows temperature (average per a chip formation cycle) distribution in dry orthogonal cutting of AISI 1045 steel. As can be seen, although the maximum temperature at the tool–chip interface is 770 °C, the temperature in front of the cutting edge where fracture actually occurs is much lower. An apparent contradiction between the greatest source of heat generation in the deformation zone (source 1 in Fig. 1.9) and relatedly low temperature in this zone is explained earlier [33] by heat advection. This heat advection also explains the well-known result of heat distribution (partition) in metal cutting shown in Fig. 1.14b. The result shown in this figure is a direct contradiction with the second law of thermodynamics that states that heat flows naturally from a region of higher temperature to one of lower temperature. If only thermal conductivity is considered, then the heat generated in the deformation zone and at the tool–chip interface should flow into the cold workpiece, not to the hot chip. However, as the chip moves, it transports heat (the essence of heat advection) so there is no contradiction [33].

1.5 Explaining the Essence of OCT

1.5.1 Physics of OCT

The explanation provided here is based solely on the definition of metal cutting as purposeful fracture of the layer being cut. The energy spent for the fracture of a unit volume of the layer being cut is the energy of its plastic deformation to fracture, so it is defined by the area under the damage curve shown in Fig. 1.13. As can be seen,

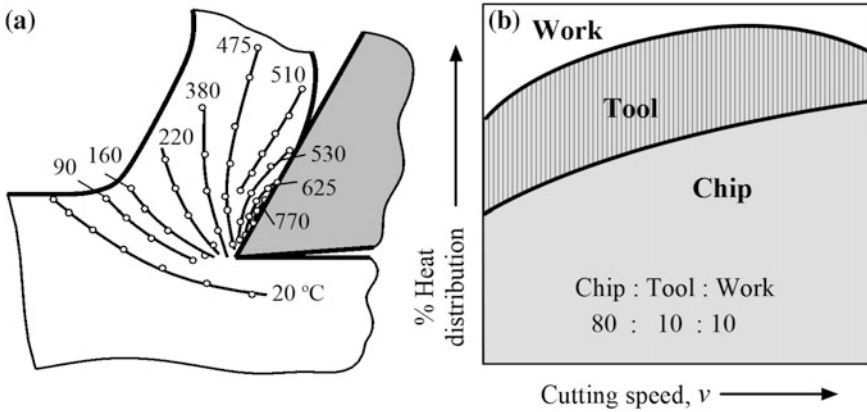


Fig. 1.14 Temperature (average per a chip formation cycle) and heat distribution in machining: **a** isotherms for dry orthogonal cutting of ANSI 1045 steel with a carbide (P10) tool (rake angle 12°) at cutting speed of 100 m/min and uncut chip thickness 0.4 mm, **b** heat distribution

this area is determined by two work materials' characteristics, namely the stress at point c , which is directly correlated with the ultimate strength of the work material, and strain at fracture ε_{ef} , which is directly correlated with engineering transverse $\varepsilon_{f-trans}$ and axial $\varepsilon_{f-axial}$ strains at fracture. As in machining of ductile materials this energy largest by far primarily causing high temperatures at the tool–chip interface [33] and excessive tool wear (primarily plastic lowering of the cutting edge) [13], this energy is to be reduced in order to achieve higher allowable cutting speeds.

Because the area under the damage curve depends on both above-discussed stress and strain, its reduction can be achieved either by reducing the stress at point c (see Fig. 1.13) or strain ε_{ef} or better both simultaneously. Moreover, as a crack in each cycle of chip formation starts to form in front of the cutting edge (schematically shown in Fig. 1.9), these two should be minimized exactly in this region of the layer being cut. As the temperature in this region is higher than room temperature, the relevant stress–strain properties of the work at elevated temperatures should be analyzed.

To understand the discussed phenomena at the level of physical metallurgy, changes in the mechanism of fracture over wide range of temperature should be analyzed as shown in Fig. 1.15. According to Atkins and Mai [34] and Komarovskiy and Astakhov [35], there is a marked increase in the strain at fracture and also in the work of fracture, at about 0.18–0.25 of the melting point (T_m); similar changes occur in other measurements of ductility such as Charpy values (KCU). It explains a number of “strange” results obtained by Zorev in his tests at low cutting speeds [36]. This phenomenon also explains the great size of the zone of plastic deformation observed at low cutting speeds and, as a result, the formation of the built-up edge on the tool rake face.

The increase in ductility over the “transition temperature range” is followed by a gradual drop beyond approximately $0.35 T_m$. It is believed that it happens due to

the continuous fall in the Peierls–Nabarro stress which opposes dislocation movement, coupled with the emergence of cross-slip (as opposed to Frank–Read sources) as a dislocation generator as the temperature is raised [34]. In the authors' opinion, the cause is in dilation–compression reactions as explained in Ref. [35].

At high temperatures, grain boundaries become significant. Below approximately $0.45 T_m$ grain boundaries act principally as barriers inhibiting cleavage and causing dislocation pile-ups. At higher temperatures, the regions of intense deformation, which are contained within the grains at lower temperatures, now shift to the grain boundaries themselves. Voids are nucleated and then cracks develop on the grain boundaries. Shear stresses on the boundaries cause relative sliding of the grains, and voids are reduced in the region of stress concentrations (see Fig. 1.15—position E). Therefore, the region around this temperature can be termed as the ductility (plasticity) valley. Experiments showed [12] that the reduction of plasticity may reach twofold and even more for high alloys. A temperature region in a close proximity to the minimum strain can be called as the region of OCT. *The presence of this valley is the physical cause of the existence of OCT.*

At temperatures above the plasticity valley, recovery and re-crystallization processes set in (recovery relates to a re-distribution of dislocation sources so that dislocation movement is easier, and in re-crystallization, the energy of dislocations generated during prior deformation is used to nucleate and grow new grains, thus effecting annealed structure over a long time). The net effect is increased ductility causing a bump shown in Fig. 1.15—position F.

The phenomenon of OCT can be further explained using the data shown in Fig. 1.16. As can be seen, the ultimate tensile stress gradually reduces with temperature (Fig. 1.16a), whereas the strains at fracture show plasticity valley at temperature particular to the solid structure of the material. As shown, in the region of OCT, the strains at fracture are at local minimum and, moreover, the ultimate tensile stress of the work material, S_u , is significantly reduced compared to room

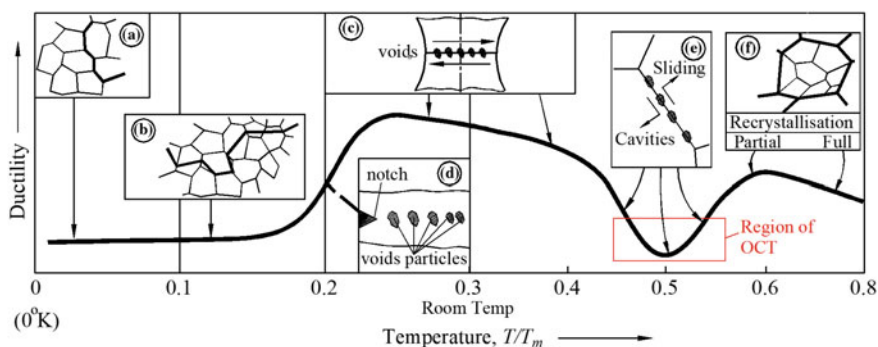
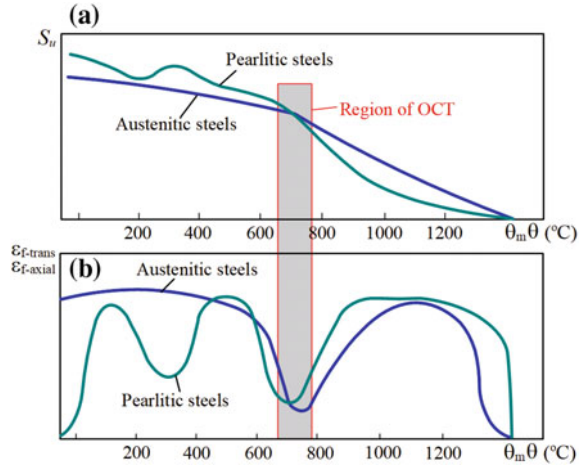


Fig. 1.15 Changes in ductility and typical associated mechanisms of fracture for *bss* materials: **a** at temperatures $<0.3 T_m$: (A) low-temperature intergranular cracks, (B) twinning or slip leading to cleavage, (C) shear fracture at particles, (D) low energy shear at particles; **b** at temperatures $>0.3 T_m$: (C) shear at particles, (D) cavities along grain faces, (E) recrystallisation suppresses cavitation

Fig. 1.16 Changes in **a** the ultimate tensile stress, and **b** engineering transverse $\varepsilon_{f\text{-trans}}$ and axial $\varepsilon_{f\text{-axial}}$ strains at fracture with temperature



temperature. These both lead to the corresponding reduction of the area under the damage curve (Fig. 1.13). As a result, the energy required for removing the layer being cut reduces, and thus, tool life increases. Moreover, the reduction of the strain at fracture reduces the level of strain hardening of machined surface that leads to minimization of the machining residual stresses in this surface when working at OCT.

1.5.2 Explanation of Makarov's Test Results

As discussed above, in the Makarov's tests the temperature was measured by a tool–work thermocouple; i.e., it was related to the emf generated across the hot interface between tool and workpiece. As such, the measured temperature is fairly close to the maximum temperature at the tool–chip interface. Obviously, temperatures determined using this method are higher than those found in front of the cutting edge. As a result, OCTs for the various tested materials are higher than those corresponding to the plasticity valley of the tested materials. Moreover, the differences depend on the thermal conductivity of the both tool and work materials, heat generated on the tool–chip interface as well as on the emf generated at a given temperature that, in turn, is a function of these materials. Therefore, Makarov concluded that OCT depends on both tool and work materials and thus should be established so. As shown above, it is not so as OCT is related only to the deformation properties of the work material.

1.5.3 *Where and When Temperature to Be Measured in Machining Tests*

As discussed above, the maximum temperature in front of the cutting edge occurs in each chip formation cycle just before crack formation. Figure 1.17 shows a FEA frame where this place and crack formations are shown. This temperature should be actually measured and compared to OCT for a given work materials. The sections to follow discuss the existing method of temperature measurement in metal cutting. Once a particular method is chosen based upon particular test situation, a correlation between the temperature measured by this chosen method and that in front of the cutting edge should be established.

1.6 Methods of Measuring Temperature in Cutting

As already mentioned, it is generally recognized that heat generated in metal cutting is extremely important, as it may affect the work material properties and consequently the chip formation process, chip breakability, tool wear and residual stress at the machined surfaces. As a result, since the 1920s, a number of methods have been developed for temperature measurements in cutting, which can fall into the following two broad categories as non-contact and contact methods. Figure 1.18 shows several methods used for temperature measurements in metal cutting, along with the timeline. As mentioned by Davies et al. [37], a number of publications in the field are increasing rapidly and that most methods were first introduced in metal cutting operations having a single cutting edge, with the measurement device affixed to the tool. Some methods such as film thermography have been replaced entirely by more modern solid-state sensors, while other methods, such as the dynamic thermocouple, are still used today.

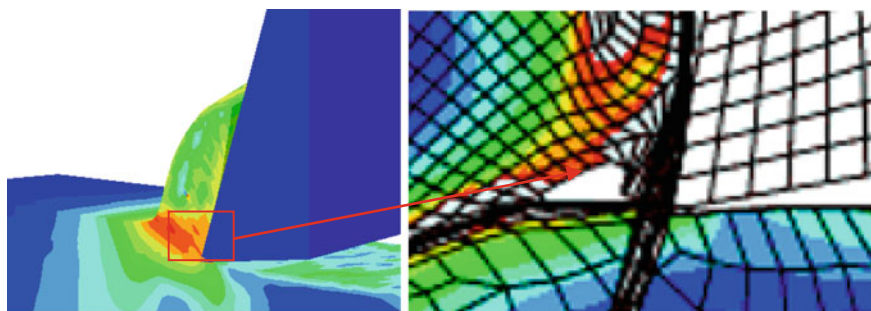


Fig. 1.17 Showing where the temperature should be measured

The next sections are describing three of the most relevant methods used for measuring the temperature in cutting: infrared thermography, thermocouples, and thermophysical processes.

1.6.1 Infrared Thermography

Infrared (IR) thermography, thermal imaging, IR radiometry, IR imaging, and IR condition monitoring are all terms used for a growing field of noninvasive temperature measurements. No matter what particular term is used, thermography has a wide diversity of applications. The technique allows to detect of the IR radiation emitted from a body's surface, thus suitable for determining its temperature. It is increasingly used as an emerging experimental technique in metal cutting for various temperature measurements.

1.6.1.1 Physical Principle

All forms of matter with a temperature over the absolute zero point ($0\text{ K} = -273.15\text{ }^{\circ}\text{C}$) emit radiation in the form of a wave that travel across the electromagnetic spectrum. Figure 1.19 shows the electromagnetic spectrum from the gamma and X-rays to the radio waves. Thermal radiation occurs in the portion of the electromagnetic spectrum from 0.1 to $100\text{ }\mu\text{m}$, which includes a portion of the ultraviolet (UV), all visible, and infrared regions. In most practical applications, it is the energy radiated at infrared range which is used to determine the target's surface temperature. The human eye is only able to perceive the visible light in the range of $0.4\text{--}0.7\text{ }\mu\text{m}$. Therefore, suitable sensors or detectors should be used to detect this energy in the infrared range.

To describe the radiation characteristics of real target surface, it is useful to describe it as an ideal target, called blackbody. A blackbody is a perfect absorber and emitter and serves as a standard against which the radiative properties of actual surfaces may be compared. The radiation power per unit area (E) emitted by a blackbody can be related to its temperature (T) using the Stefan–Boltzmann law, given by the following equation:

$$E = \varepsilon\sigma T^4 \quad (1.6)$$

where ε is the emissivity of the target's surface (equal to 1), and σ is the Stefan-Boltzmann constant, equal to $5.67 \times 10^{-8}\text{ W/m}^2\text{ K}^4$. Figure 1.20 shows the graphical representation of this equation. This figure shows that as blackbody temperature increases, more power is radiated. The radiation wavelength at which peak power occurs becomes progressively shorter as temperature increases. At low temperatures, the bulk of the power is at long wavelengths.

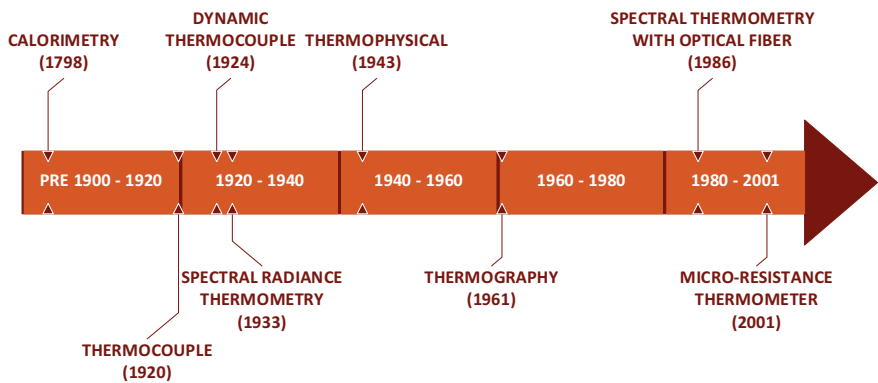


Fig. 1.18 Timeline of temperature measurements methods in metal cutting processes

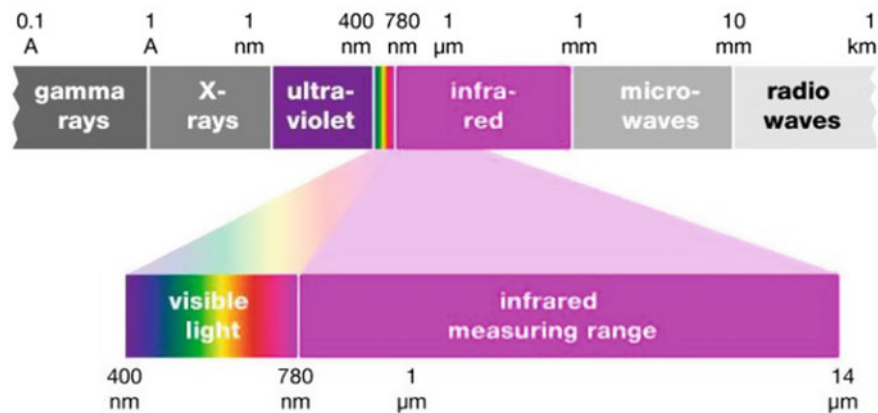


Fig. 1.19 Infrared region of the electromagnetic spectrum

Equation (1.6) can be used to determine the surface temperature of a target by measuring the power emitted from it. However, for a real body the emissivity of its surface is no longer equal to 1.

By definition, emissivity is the measure of the fraction of the radiation emitted by a real body's surface at a given temperature, and that one emitted by of a blackbody at the same temperature. Its value varies from 0 to 1. This "property" of a surface depends on several factors, including wavelength, emission direction, surface temperature, and surface condition (roughness, oxidation, etc.) [38]. Although many emissivity tables for different materials and surface conditions are available in the literature [38–40], none of these tables covers all the possible conditions observed in metal cutting operations. Moreover, during the cutting process tool, workpiece, and chip are made on different materials and they have different surface

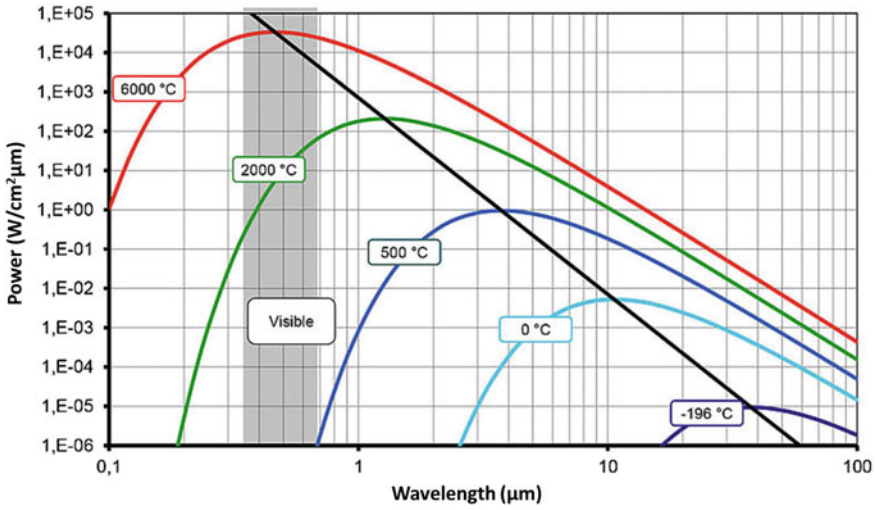


Fig. 1.20 Spectral power emitted by a blackbody for various temperatures

conditions, thus different emissivity values, which makes difficult or even impossible to get the temperature distribution in these three targets in a single thermal image. Complexity and dynamics of the metal cutting process make it even more difficult to determine the correct value of the emissivity factor in every given time instant. Therefore, the incorrect determination of the emissivity in metal cutting gives rise to the one of most common sources of error in temperature measurement using IR radiation [37].

Several literature sources present a mathematical expression for the errors associated with the temperature measurements using IR radiation [39, 41]. The uncertainty in the measured temperature (Δ_{T_m}) can be expressed by the following equation [37]:

$$\Delta_{T_m}^2 = \frac{\lambda T^2}{C} \left(\frac{\Delta_{E_m}^2}{E_m^2} + \frac{\Delta_\varepsilon^2}{\varepsilon^2} \right) \quad (1.7)$$

where λ is the wavelength, T is the temperature, E_m is the measured radiance, ε is the emissivity, Δ_{E_m} is the uncertainty in the measured radiance, and Δ_ε is the uncertainty in the emissivity. This equation shows that errors in the measured temperature increase with the wavelength, square of the temperature, and inverse proportional to the square of the emissivity. Therefore, temperature measurements at the smallest wavelength is preferable, in order to minimize the uncertainty.

1.6.1.2 Infrared Thermographic Systems or Radiation Thermometers

Several types of infrared thermographic systems, also called radiation thermometers, are available. They can be classified in function of several criteria, including the operating spectral range and scanning surface area size. According to the operating spectral range, they can be classified as follows [37]:

- *Total radiation thermometers.* They are used for a wide range of wavelengths, so they can measure temperatures from 200 to 1800 °C with low uncertainty.
- *Spectral band thermometers and imagers.* They are only used for a given (short) wavelength range. They are considered suitable for metal cutting applications.
- *Ratio thermometers.* They are only used for two specific wavelengths, to eliminate the dependence of the surface temperature from the emissivity. They are also known as two-color pyrometers, and they are also considered suitable for metal cutting applications.

According to the operating spectral range, they can be also classified as long wave (LW), short wave (SW), and very short wave (VSW) [38].

Some infrared thermographic systems used to detect the infrared radiation were being applied to metal cutting, like cameras with special films sensitive to infrared radiation [42–44], optical pyrometers [45–48], and infrared cameras [49–51].

The optical pyrometer technique is especially appropriate to determine the average integral temperature of a variable spot area of a few millimeters square [45–47]. Optical pyrometers can be connected to an optical fiber placed into small holes, which allow to measure the temperature within the objects under analysis [48].

Today, infrared cameras are the most suitable equipment to determine the temperature distribution in the deformation zone. Different infrared cameras were applied to metal cutting, from the classical infrared scanning cameras with only one sensor [49] to the most advanced infrared cameras having a detector with an array of sensors, as is the case of the focal plane array (FPA) infrared detector [50, 51]. The last solution allows to evaluate strong temperature gradients correctly, which is very important in metal cutting.

1.6.1.3 Thermal Imaging Using Very Shortwave Cameras

Over the recent years, low-cost camera solutions using charge-coupled device (CCD) and complementary metal oxide semiconductor (CMOS) sensors were being applied to the determination of the temperature distribution on the tool and chip in metal cutting. These cameras were being applied to the determination of the temperature distribution in the tool and chip [47, 52, 53]. They operate in the near-infrared spectrum (wavelength between 400 and 1100 nm), and therefore, they are classified as VSW cameras [38]. Figure 1.21 shows a schematic of a typical measuring setup used in infrared thermography. It includes a CCD camera, objective, high-pass filter, pair of lenses of the convergent type and a graduated rail, where

these listed components were mounted. This assembly is then installed on a machine tool. In addition, a cooling and control units and a computer with dedicated data acquisition hardware and software are connected to the CCD camera. The CCD camera is normally a gray-level digital camera equipped with a special CCD detector, which presents high sensitivity, low noise, and high resolution. This camera is capable to work both in the visible and in near-infrared spectra (up to 1000 nm). The operating parameters of the CCD camera, such as the exposition time of the CCD and the CCD temperature, are set and controlled by the control unit.

To analyze only the infrared radiation emitted by one object, the visible radiation should be eliminated using for example a high-pass filter (or infrared filter). Outeiro, Dias, and Lebrun [27] recommended a filter that eliminates the radiation below 850 nm. Such a filter is mounted directly in the objective of the camera. Therefore, the CCD only received radiation in a wavelength range between 850 and 1000 nm.

Precise temperature measurements in the deformation zone require the size of temperature field analyzed by the camera to be of the order of few millimeters, and this area should be sufficient to cover the zone around the tooltip. The desired area (spatial resolution) for temperature measurements is achieved, using, for example, a pair of magnifying lenses placed between the camera and the machining zone. Because the precise adjustment of the focal distance of the two lenses is needed, the camera is mounted on the same graduated rail as the lenses. To obtain a fixed spatial resolution during temperature measurements, the camera must follow the motion of the objects under analysis, so it should follow the feed motion of the tool. Therefore, the assembly that includes the CCD camera, objective, infrared filter, pair of convergent lenses and a graduated rail must be installed on the carriage that has the feed motion.

To analyze the temperatures in the zone around the tooltip at different angles with respect to the tool reference plane, a special structural support is to be used. Such a support allows changing the orientation of the rail and thus the camera. Normally, temperatures are analyzed in the zone around the tooltip in the direction parallel to the axis of rotation of the workpiece (the axial direction) to the direction normal to this axis (the circumferential direction), covering a range of angles between 0° and 90° . These directions are shown in Fig. 1.22.

The sequences of grayscale images acquired by the camera are stored in the computer. In post-process stage, these images are converted into temperature-scale images (temperature distribution) using appropriated calibration curves.

Examples of measurements are shown in Fig. 1.23 (grayscale images) and Fig. 1.24 (temperature-scale distribution after calibration) for turning of AISI 1045 steel using an uncoated cemented carbide cutting tool with the following cutting geometry: normal rake angle of -4.29° , the normal flank angle of 4.29° , normal wedge angle of 90° ; inclination angle of the cutting edge of -14° , tool cutting edge angle of 72° , nose radius of 0.8 mm, tool cutting edge radius of 0.044 mm [27, 54].

In general, the chip-free surface is very rough (width saw teeth topography) having dark blue color, suggesting a high normal emissivity value. Indeed, according to several emissivity tables found in the literature, the normal emissivity

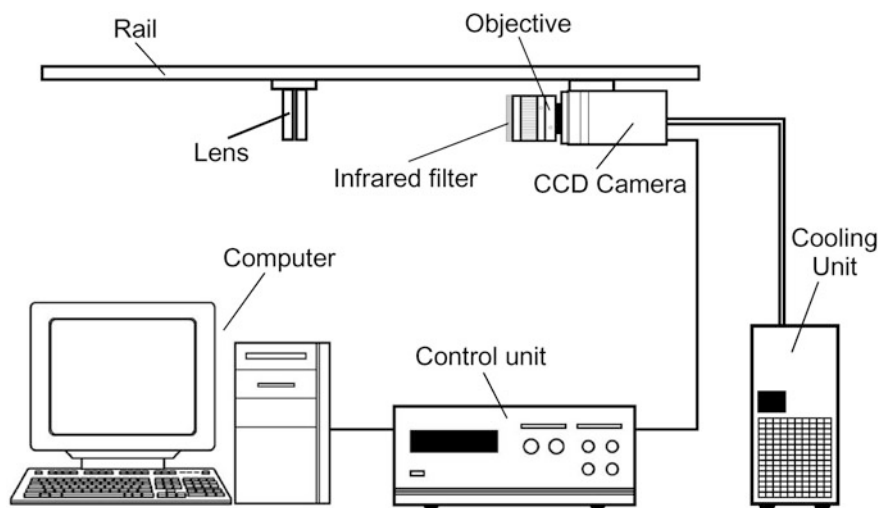


Fig. 1.21 Schematic representation of the thermal imaging equipment

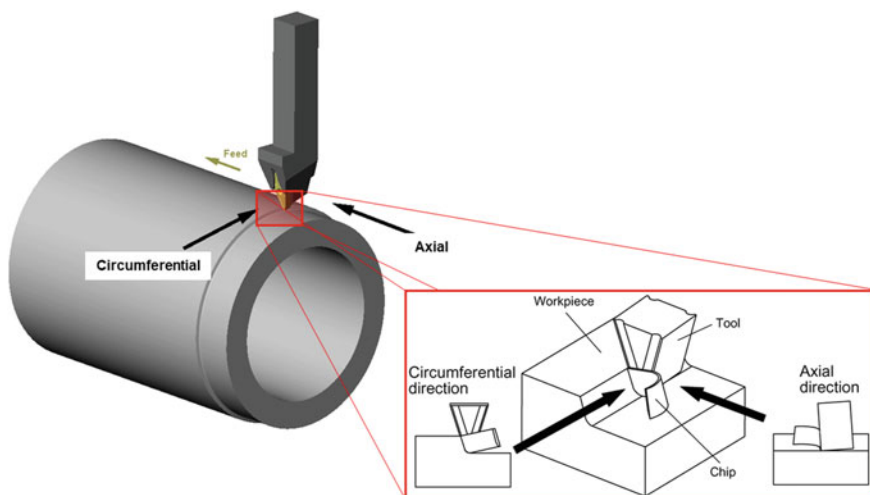


Fig. 1.22 Directions of temperature measurements in turning: direction parallel to the axis of rotation of the workpiece (axial) to the direction normal to this axis (circumferential)

of the chip-free surface of 1045 steel is greater than 0.85 [38–40]. Because the angle between the normal to the chip-free surface and the axis of the camera (the angle of incidence, see Appendix) was less than 40° (about 14°), the emissivity of this surface is a constant equal to the normal emissivity. As a consequence, the temperature measured by the thermal imaging equipment on the chip-free surface should be close to reality.

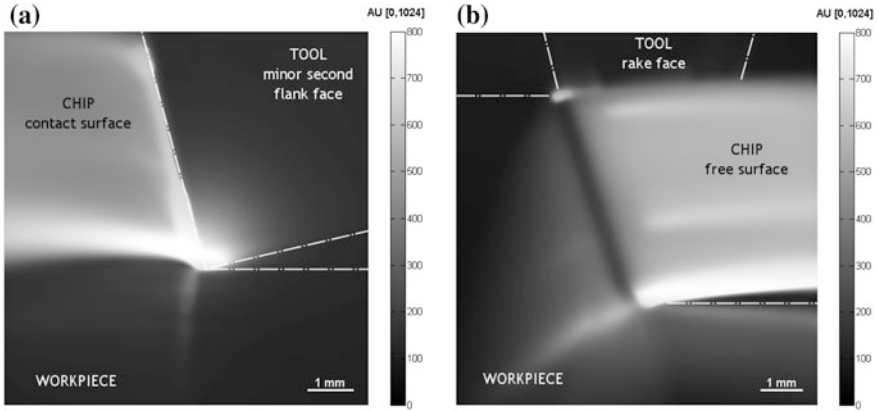


Fig. 1.23 Infrared images obtained with the CCD camera placed in the **a** axial and **b** circumferential directions, showing the tool, chip, and workpiece

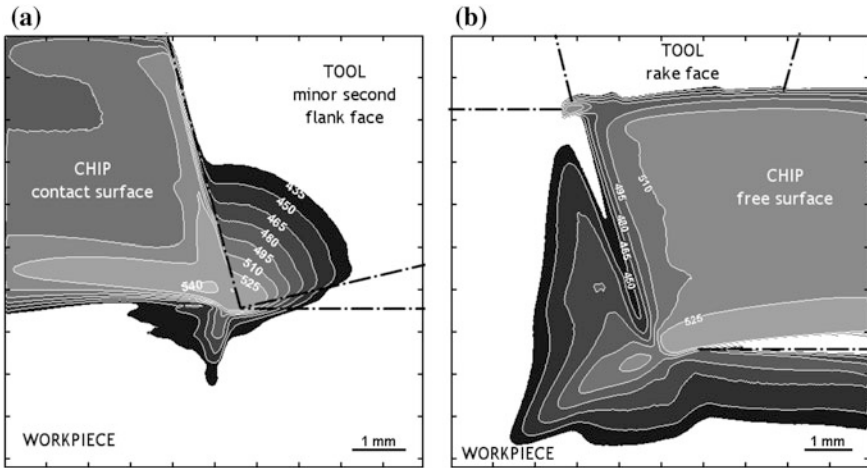


Fig. 1.24 Temperature distribution on the chip and on the tool, obtained with the CCD camera placed in the **a** axial and **b** circumferential directions. Cutting conditions: work material AISI 1045 steel; uncoated tool; $V_c = 125$ m/min; $f = 0.05$ mm/rev; $a_p = 5$ mm

Oppositely to the chip-free surface, the chip contact surface is smooth and shiny, having blue color. Moreover, due to the chip's movement the angle of incidence varies, reaching 90° . This suggests that the emissivity of the chip contact surface should be low and variable. As a consequence, the temperature measured by the thermal imaging equipment on the chip contact surface can be very different from its real temperature and, therefore, must be evaluated carefully.

Workpiece machined surface presents high brightness, and the angle of incidence is variable due to its cylindrical shape, being quite close to 90° within the

zone analyzed by the thermal imaging equipment. Analyses of the infrared videos of the machined surface also reveal the existence of high reflection levels. As a result, the emissivity of the machined surface is very low, and therefore, the temperature measured by the thermal imaging equipment on the machined surface can be very different from its real temperature.

Normal emissivities of the coated and uncoated cutting tools were measured by M'Saoubi [55] in function of the wavelength and temperature. For the wavelength range analyzed by the current thermal imaging equipment and considering an average tool temperature of 500 °C, the normal emissivities were 0.85 for the coated cutting tool and 0.70 for the uncoated cutting tool. Due to the inclination of the minor second tool flank face in relation to the axis of the camera, the angle of incidence was 17.5°. As a consequence, the temperature measured by the thermal imaging equipment on the minor second tool flank face should be close to its real temperature.

1.6.1.4 Advantages and Limitations of the Infrared Thermographic Systems

The main advantage of the infrared thermography, compared with the previous techniques, is its capability to operate without contact and, therefore, not interfering with the cutting process. Additionally, the infrared thermography using cameras is the only actual technique that allows to quickly evaluate the full temperature distribution over the objects under analysis, while other techniques needed to perform several temperature measurements. Although a number of obvious advantages, infrared thermography suffers some limitations:

- It can almost always be applied to determine the temperature of the surface of the object under direct observation by the infrared camera. The only exception is the above-described use of fiber optics connected to optical pyrometers;
- Accurate knowledge of surface emissivity is essential for applications in infrared measurements. This parameter depends on several surface conditions, including its geometry, surface roughness, and color.
 - The angle at which a surface is viewed by the camera will affect the emissivity. The more the angle deviates from 90° to the surface, the lower the apparent emissivity becomes, and the greater the possible temperature error if this is not taken into consideration.
 - For highly reflective objects polarization, effect has to be taken into account.
 - The true material emissivity value is continual changing property (dynamic) based upon many material and application factors during the heat cycle. As with temperature, emissivity depends significantly upon the wavelength. In order to make an accurate measurement, all instrument parameters must be known (wavelength and bandwidth). Very few materials and applications have a fixed emissivity.

- The application of this technique is limited to dry cutting conditions;
- When classical infrared scanning cameras are used, its difficulty to evaluate with sufficient accuracy high temperature gradients and high dynamic phenomena which is the case of the metal cutting process.
- High cost of the infrared cameras.

The use of VSW cameras presents some advantages compared to the SW and LW cameras. Due to the fact that VSW camera can operate both in the visible and very low infrared spectra, the following advantages can be pointed out:

- (1) It is a good choice for high temperature measurements (above 400–500 °C), because, when compared with the SW and the LW cameras, they present high sensitivity at these temperatures and the errors due to the wrong selection of the emissivity data have minor consequences in the temperature value deviation (see Eq. 1.7);
- (2) They can measure through usual windows (glass or quartz) where SW cameras need to consider these media selective filters and where LW cameras are blind [38];
- (3) It is possible to use classic objectives and lenses [38];
- (4) As a direct consequence of paragraphs (2) and (3), and due to the fact that the CCD camera is more easy to manufacture than the SW and the LW infrared cameras, they are less expensive [38].
- (5) Another advantage of this equipment stems from the fact that, like the FPA detector, the CCD detector is composed by an array of sensors, which allows to evaluate strong temperature gradients more accurately.

The limitations of the utilization of the VSW cameras are the same of any radiative measurement technique and the considered wavelength interval allows to analyze only higher temperatures, above of 400–500 °C.

1.6.2 Thermocouples

1.6.2.1 Physical Principle

The simplest electrical method of temperature measurement uses the conventional thermocouple [56]. When two dissimilar metals are joined together to form an electrical circuit, as shown in Fig. 1.25a, an electromagnetic field (e.m.f.) will be generated and can be registered by a potentiometer. This e.m.f. is a primary function of the junction temperature. This phenomenon is referred to as the Seebeck effect. If two metals are connected to an external circuit in such a way that a current is drawn, the e.m.f. may be altered slightly owing to the phenomenon called the Peltier effect. Furthermore, if a temperature gradient exists along either or both materials, the junction e.m.f. may undergo an additional slight alternation known as the Thomson effect. Then, there are three e.m.f. present in a thermoelectric circuit:

Table 1.1 Thermal e.m.f. in absolute millivolts for some commonly used metals and alloys in conjunction with platinum

Metal or alloy	Emf (mV)
Cromel (90% Mi, 10% Cr)	+2.4
Iron	+1.8
Molybdenum	+1.2
Tungsten	+0.8
Copper	+0.76
Aluminum	-0.40
Nickel	-1.50
Alumel (1% Si, 2% Al, 0.17% Fe, 2% Mn, 94.83% Ni)	-1.70
Constantan (58% Cu, 40% Ni, 2% Mn)	-3.4
Copel (56.5% Cu, 43.5% Ni)	-3.6

Note Reference junction at 0 °C, terminal junction at 100 °C

the Seebeck e.m.f. caused by the junction of dissimilar metals, the Peltier e.m.f. caused by a current flow in the circuit, and the Thomson e.m.f. which results from a temperature gradient in the connected materials. The Seebeck e.m.f. is of prime concern, as it is dependent on the junction temperature. This dependence is known for any junction formed by most common metals, and therefore, the junction of dissimilar metals can be used to measure temperature if the generated e.m.f. is carefully measured. Table 1.1 shows thermal e.m.f. for some commonly used materials. To obtain a higher output e.m.f., the materials for a thermocouple should be selected so that the first would have the maximum positive e.m.f. and the second would have the maximum negative e.m.f., as selected from Table 1.1.

The main problem arises here when an experimenter attempts to measure the potential, as another thermal e.m.f. will be generated at the junction of the materials, joined to measure temperature. This second e.m.f. will depend on the temperature of connection, and provisions must be made to take into account this additional potential. In this case, two important properties or laws [57] of circuits with conventional thermocouples are useful in applications. First, if a third metal is connected in the circuit as shown in Fig. 1.25b, the net e.m.f. of the circuit is not affected as long as the new connection is at the same temperature as the initial junction ($\theta_1 = \theta_2$). Second, when this is not the case ($\theta_1 \neq \theta_2$), then the circuit develops an e.m.f. of E_n at the first junction and E_p at the second.

The law of intermediate temperatures states that the circuit will develop a resultant e.m.f. $E_r = E_n + E_p$. It may be observed that all thermocouple circuits must involve at least two junctions. If the temperature of the one junction is known, then the temperature of the other junction may be easily calculated using the thermoelectric properties of the metals. The known temperature is referred to as the reference temperature. A common arrangement for establishing the reference temperature is the ice bath as shown in Fig. 1.25c. An equilibrium mixture of ice and saturated-air distilled water at standard atmospheric pressure produces a known

temperature of 0 °C. When the ice–water mixture is kept in a Dewar flask, the reference temperature may be maintained for extended periods of time.

There are three basic types of thermocouples used in metal cutting: tool–work, embedded, and running. The running and tool–work thermocouple techniques are specific to metal cutting studies [58–60], while embedded thermocouples utilized well-developed methodology used in temperature measurements in various applications [61]. These thermocouples are presented in the following sections.

1.6.2.2 Tool–Work (Natural) Thermocouple

The thermocouple is defined by the terminal junction formed by tool and workpiece material, and no third material is added to the circuit. This is one of the first methods for measuring temperature in metal cutting studies [58, 62]. With this method, the average integral temperature at the tool–chip and tool–workpiece interfaces, defined earlier as the cutting temperature, is measured. Because the exact nature of formation of the signal measured by this thermocouple is not known, the result of measurements cannot be compared with the results of analytical studies or measurements obtained using other experimental techniques. Unfortunately, this issue is not well understood, so there are a number of attempts which were made to compare the results obtained using a tool–work thermocouple to those obtained in analytical and numerical studies [63].

Figure 1.26 shows the principle of this method. Because the tool and work materials are normally different, their contact at the tool–chip and tool–workpiece interfaces forms the hot junction of the tool–work thermocouple. The components of this thermocouple are insulated from the machine and fixtures to eliminate noise in the output signal. This output signal is the e.m.f. voltage which is amplified and then is fed to the data acquisition board plugged into a computer for further analysis.

The major problem with tool–work thermocouples is their proper calibration [62]. The objective of the calibration is to obtain a calibration curve (normally, a straight line [15, 58, 63] that correlates the e.m.f. produced by the thermocouple and temperature. The literature offers a wide range of calibration procedures for this thermocouple [13].

Since the calibration is critical to obtaining accurate results, a few severe problems associated with the known calibrations have to be pointed out:

- Circuits used in calibration and in actual cutting are not the same. Commonly, the samples that represent the tool and chip are not even in contact. Even if they brought to a close contact, the actual contact area is not the same as in reality and the contact pressure is well below than that in actual cutting. Besides, the contact between the tool flank and the workpiece taking place in actual cutting is completely ignored and a mercury contact connecting the rotating spindle with the amplifier is not present in the calibration circuit.

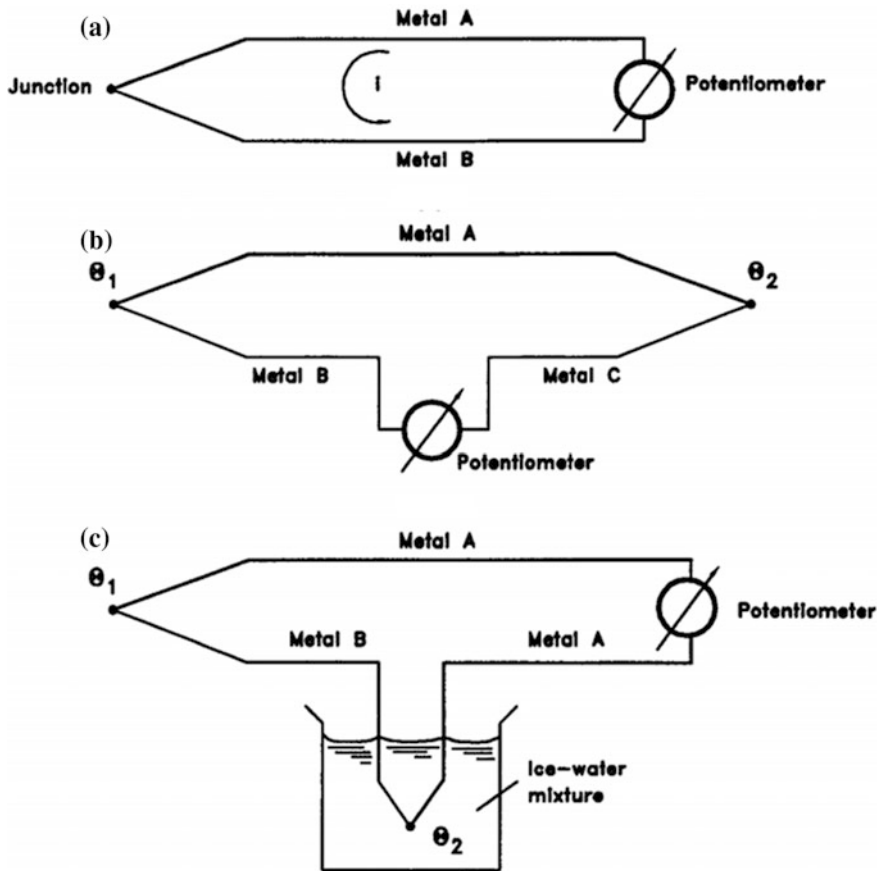


Fig. 1.25 Temperature measurements with conventional thermocouples: **a** junction of two dissimilar metals indicating thermoelectric effect; **b** circuit with a third metal; and **c** conventional method for establishing reference temperature in a thermocouple circuit

- Lead or tin bath is used in calibrations to obtain the uniform maximum calibration temperature. However, the temperature of molten lead is far below than those expecting in cutting. Since the calibration curve for a natural thermocouple may not be linear, it is next to impossible to extrapolate the obtained results; i.e., the obtained calibration curves may contain significant errors. The same problem exists with torch heating.
- Since the specimen representing the tool in calibrations has rather restricted length, it is very difficult to keep the cold junction of a natural thermocouple at constant temperature. As pointed out in [60], it is particularly true when small indexable tool inserts are used. The same problem occurs with the obtaining a sufficient long chip for the workpiece materials of relatively low ductility.

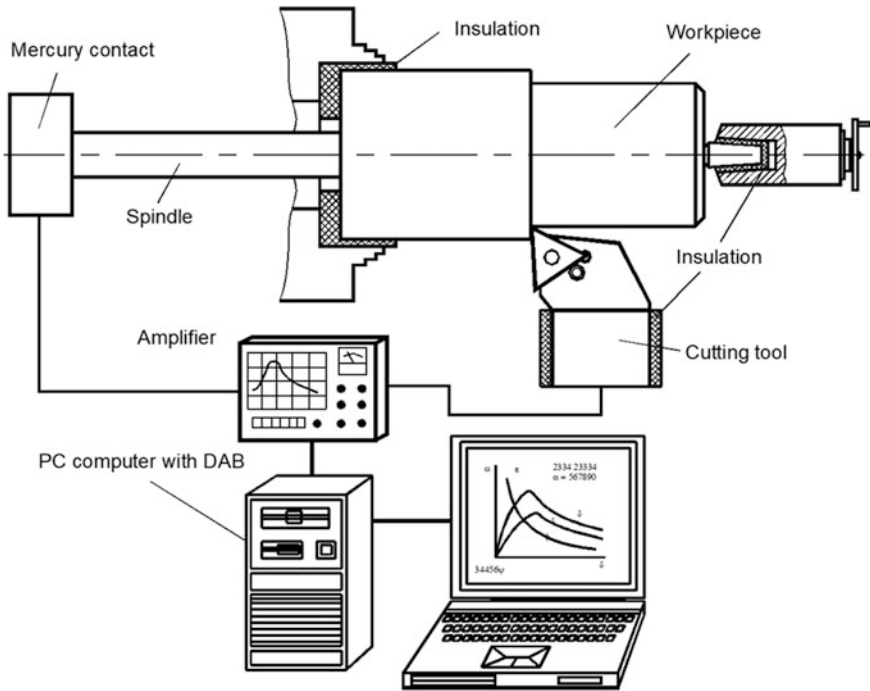


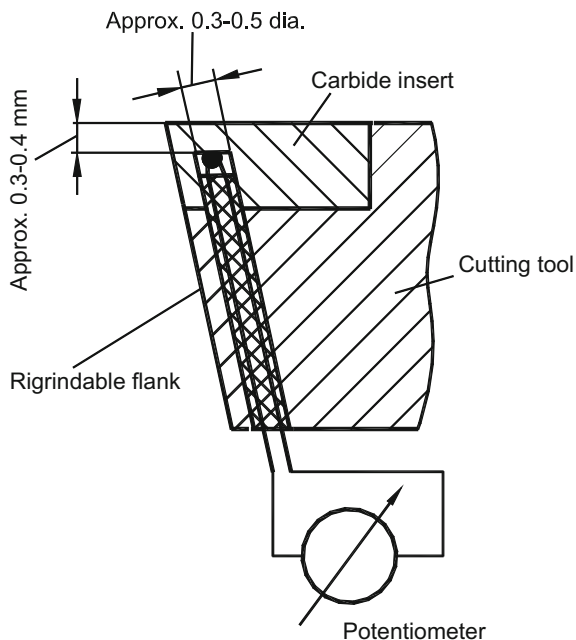
Fig. 1.26 Tool–work thermocouple

1.6.2.3 Embedded Thermocouples

Some of the problems associated with the tool–work thermocouple technique are surpassed if embedded thermocouples (as cromel–alumel or cromel–copel), which are inserted into small holes made in the cutting tool, usually by EDM process [57, 64, 65]. The principle of temperature measurement with an imbedded thermocouple is shown in Fig. 1.27. The diameter of this hole should be as small as possible to reduce disturbances which may have an appreciable effect on the distribution of the thermal energy in the cutting tool and thus the measured temperatures. Experience shows [62] that temperatures can be measured with sufficient accuracy when the terminal thermocouple junction is pressed against the cutting insert (the bottom of the hole) with a force of no less than 50 N. As this is not always possible, it is recommended that the terminal end is welded to the insert using condenser welding when HSS inserts are used. By placing thermocouple holes in different positions of the insert, the temperature field and/or distribution can be determined.

A standard thermocouple and standard calibration procedure can be used with the embedded thermocouple technique in accordance with ASTM Manual on the Use of Thermocouples in Temperature Measurement (ASTM manual Pcn: 28-012093-40 by committee E20 on temperature measurements, 1993), with other international standards (e.g., Thermocouples (IEC-60584), Industrial Platinum

Fig. 1.27 Embedded thermocouple



Resistance Thermometer Sensors (IEC 751), Temperature Measurement Thermocouples (ANSI-MC96.1)). The output of an imbedded thermocouple is in the millivolt range and may be measured by digital voltmeters or data acquisition systems suitable for thermocouple measurements.

The technique of temperature measurements in metal cutting with embedded thermocouples suffers some limitations:

- It cannot be used to measure temperatures at the tool–chip and tool–workpiece interfaces directly as the thermocouple located at a certain distance from the surface. As known [10], a very great temperature gradient (along a normal to the tool contact area toward in-depth of the tool material) exists at the interfaces, so the temperature reading provided by an imbedded thermocouple may not reflect the maximum temperature and its exact location. This drawback becomes more severe when modern low thermal conductivity tool materials are used.
- The exact contact condition at the bottom of the hole affecting temperature measurements is not known.
- The distortion in the temperature distribution is caused by the presence of the holes [66].

Several researchers used this method to determine the temperature in different locations in the tool and workpiece, for several metal cutting operations, including orthogonal cutting, turning, milling, grinding, and drilling. Lazoglu et al. [67] used a newly designed device, named rotary tool temperature (RTT) (Fig. 1.28a), allowing to measure the tool temperature in drilling of Ti6Al4V. This device

receives the electrical signals acquired by thermocouples placed in the drill at different locations closer to the cutting edge and margins (Fig. 1.28b) and transmits them by wireless or by cable RJ45 (at the end of the test) to the acquisition software installed in a computer.

This RTT device consists of connectors with cold junction compensations to acquire signal up to six thermocouples, an internal memory to store the signal as a function of time during machining, an onboard lithium battery, and an internal clock to synchronize all the signals. The RTT device can be mounted on a standard tool holder to measure the temperature in both drilling and milling operations. The system has two parts: the RTT itself assembled and spinning together with the drill and an external module in charge of the data communication to the acquisition computer.

To assure a good contact between the thermocouple and the bottom of the hole, a special attention was paid in inserting the thermocouples in the holes. In this case, the two thermocouples are secured to the drill body using two different high temperature ceramic glues: One is charged with silver particles, as a result improving its thermal conductivity and used at the bottom of the EDM hole to connect the two thermocouple wires with the drill body, and the other one is used to secure the wires to the drill permanently avoiding any signal disruption when the tool is rotating during the machining motion.

Figure 1.29 shows both measured and simulated (see details in [67]) tool temperatures at the specified tool corner and tool lip points. As seen in this figure, both measured and simulated temperature results are matching quite well in trends as well as amplitudes with less than 10% differences at the maximum temperature points, which proves the efficiency the method to measure the temperature in drilling. This figure also shows that for the cutting speed of 10 m/min and feed of 0.1 mm/rev, temperature is about 370 °C at both thermocouple locations. When the cutting speed is increased to 30 m/min and feed of 0.2 mm/rev, tool temperature at the thermocouple points is increased to about 500 °C.

1.6.2.4 Running Thermocouples

Running thermocouples are used when one wants to measure temperatures in the deformation zone, partially formed chip and at the interfaces. The array of running thermocouples to measure temperature distribution in the deformation zone and partially formed chip is shown in Fig. 1.30a. Insulated constantan wires of 0.12 mm diameter are imbedded in holes of different depth in the layer to be removed by the tool. When this layer approaches the deformation zone, it deforms first elastically and then plastically, so the wires are gripped securely in the holes, forming in this way the thermocouples. The cold junction of the thermocouples is secured to the workpiece far enough from the deformation zone to keep this cold junction at room temperature. Arranged in this way, thermocouples produce the e. m.f. while the layer being removed passes the deformation zone, becoming the chip that moves over the tool rake face.

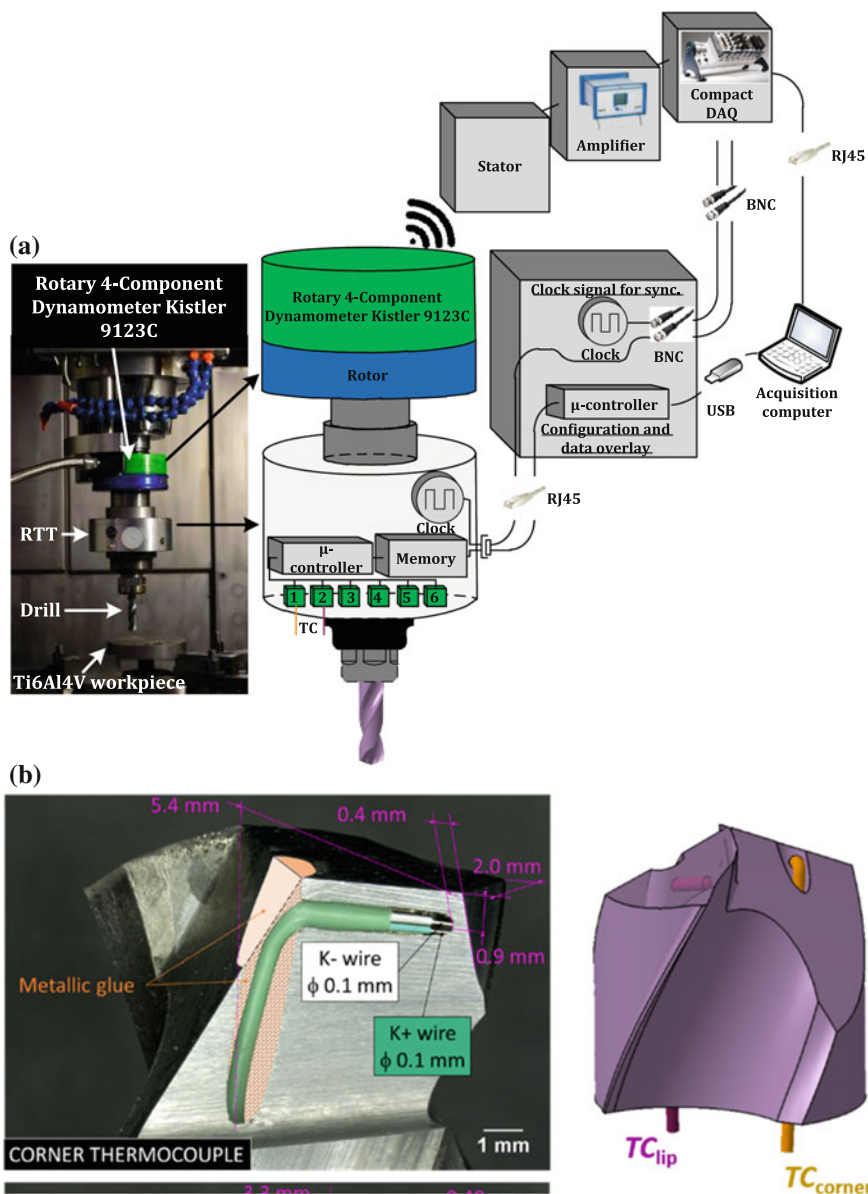


Fig. 1.28 Temperature measurement in drilling using thermocouples implemented in the drill (a) and the RTT device, together with a rotary four-component dynamometer for temperature and force measurement (b)

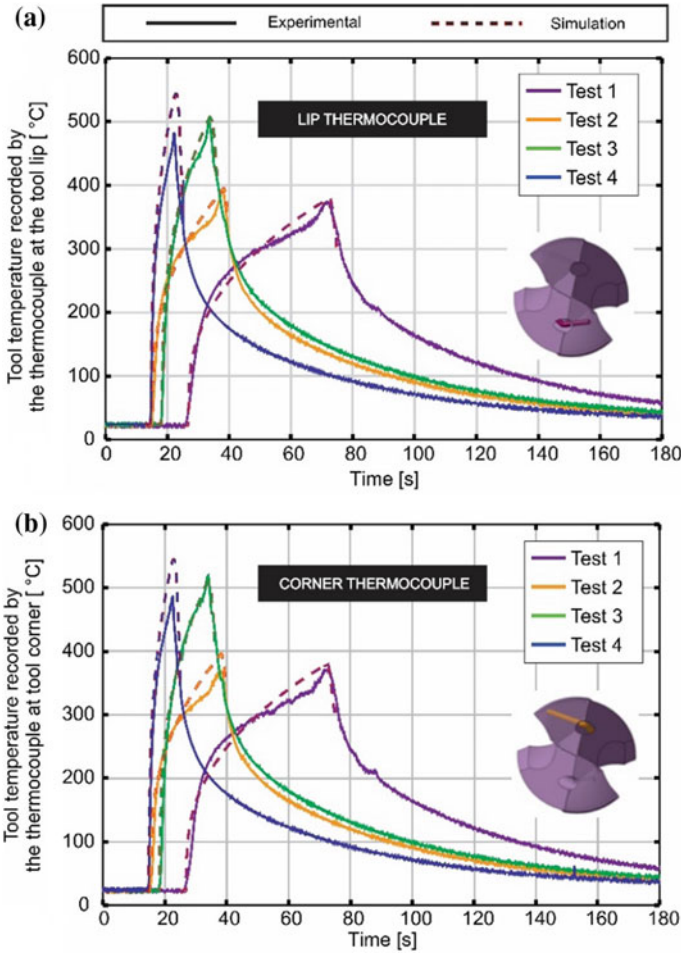


Fig. 1.29 Simulated and measured tool temperatures: **a** for the thermocouple at the lip point, **b** for the thermocouple at the corner point in the drilling of Ti-6Al-4V under different cutting conditions

Using the results of such measurements, the temperature field can then be reconstructed as in an example shown in Fig. 1.30b for AISI 1045 steel work material machined on a shaper with the uncut chip thickness of 2 mm, using P10 carbide tool having rake angle of 10°. Figure 1.30c shows the use of the running thermocouple technique for measuring temperature distribution at the tool–chip and tool–workpiece interfaces. Figure 1.30d shows an example of the obtained result for two work materials: steel AISI 1045 and Ti Grade 1 alloy. Other cutting

parameters were as follows: cutting speed of 120 m/min, feed of 0.26 mm/rev, depth of cut of 3 mm, tool material—M30 carbide, rake angle of 0° , and flank angle of 12° .

Although the technique of temperature measurements in metal cutting with running thermocouples is the only feasible method of obtaining temperature distributions at the interfaces and to determine the location of the points of maximum temperature at these interfaces with respect to the cutting edge, it has some limitations:

- The use of this method requires extensive training of the experimentalist and proper design of a setup. The proper reading can be obtained only by an experienced specialist.
- Each test requires a great deal of preparation time.

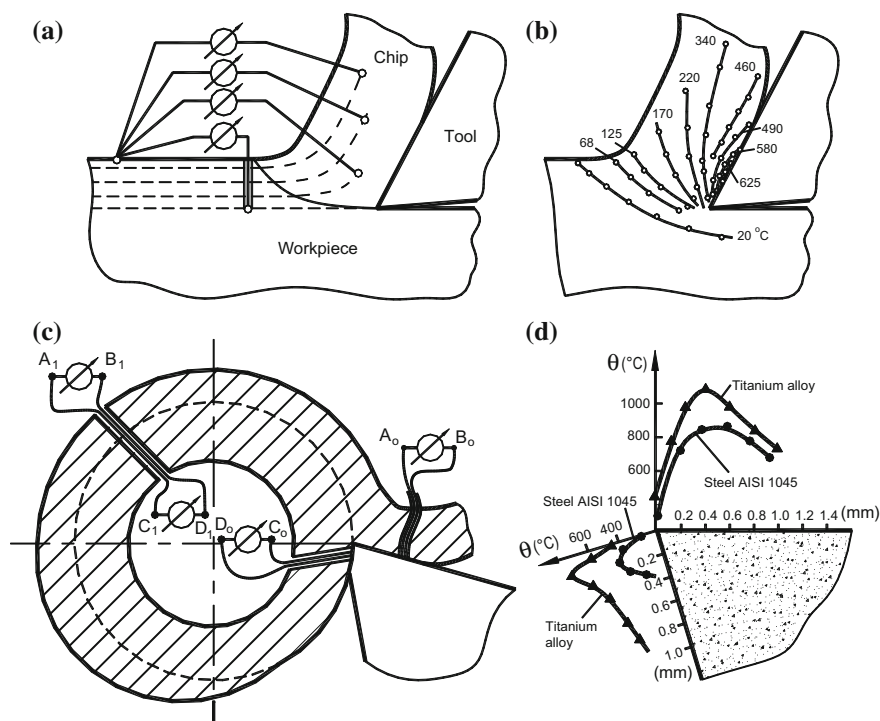


Fig. 1.30 Temperature measurements with running thermocouples: **a** the use for measuring temperatures in the deformation zone and partially formed chip, **b** an example of reconstructed temperature field in the deformation zone and the partially formed chip, **c** assessment of the temperature distribution at the tool-chip and tool-workpiece interfaces, **d** an example of the measured results

1.6.3 Thermophysical Processes

Thermophysical processes techniques were also applied to determine the temperature distribution in the tool and workpiece. These are based on the correlation between temperature and a thermophysical property, such as the change in the microhardness and metallurgical structure of the workpiece or the tool materials in function of the temperature. According to Davies et al. [37], other commercially available techniques can be also used: (i) thermosensitive and thermoindicating paints, (ii) thermochromatic liquid crystals, (iii) thermographic phosphors, (iv) temperature-sensitive crayons and pellets, and (v) pyrometric cones.

Above-mentioned methods are relatively little application to measure the temperature in metal cutting. However, from a historical point of view, a special attention will be given here to the metallurgical-based techniques.

Some researchers [68–70] used the correlation between the microhardness and temperature reached during cutting to determine the temperature distribution in high-speed steel tools, claiming that accuracy of $\pm 25\%$ can be achieved with this technique [70]. However, this technique presents the largest drawbacks that can only be applied to a restricted range of cutting conditions, including tool materials, which causes metallurgical changes in the tool.

Other examples of metallurgical-based techniques are the use of powders [71] or physical vapor deposition (PVD) coatings [72] in a section of split tool or split workpiece and chemical element substitution in the tool [73, 74]. Knowing the melting point of the introduced alloys, it is possible to reconstruct the temperature field by drawing isothermals on the scaled drawing of the tool or workpiece section after cutting and metallographic analysis. Nevertheless, these techniques present some drawbacks as follows:

- The introduced alloy requires a long time to be completely melted.
- In the case of the powders or PVD coatings, the layer of the introduced alloy will affect the heat conduction through the tool and, therefore, the temperature distribution into the tool.
- It is a post-processing technique and, therefore, not suitable for in-process measurement.

In general, the thermophysical process techniques are so wide and varied that sources of uncertainty are large and are best addressed in the context of a particular application [37].

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Chapter 2

The Role of Oxygen in Orthogonal Machining of Metals



Pedro A. R. Rosa, Afonso V. L. Gregorio and J. Paulo Davim

Abstract This chapter presents an experimental investigation of the metal cutting tribology with the purpose of re-examining the role of oxygen in the process mechanics and obtaining a better understanding of the friction coefficient in terms of the major process parameters. Specially designed apparatus and tribological tools have been used to cut pure metals and metallic engineering materials under oxygen-rich surrounding medium. Friction coefficient has been evaluated, and the chip–tool interface has been observed in situ using optical microscopy. It has been shown that pressure-welded junctions occur near the point at which chip detaches from cutting tool, and it has been established that there is an intimate relation between the oxide films formation and the oxygen concentration in the surrounding medium. The chip curl, sticking and sliding zones were seen to change with oxygen concentration. It can be asserted that friction plays an important role in the free plastic flow of metal cutting due to the high friction coefficient values ranging from 0.3 to 1, or even higher. However, the main research contribution of this study is the remark of the friction coefficient as a function of the process parameters, rather than a constant numeric value representing complex phenomena at the contact interface between the chip and the cutting tool.

Keywords Metal cutting • Tribology • Friction coefficient • Oxide film

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2.1 Introduction

Friction exists in all metal cutting technologies and plays a very important role in both process performance and tool life. Due to the demand for higher productivity and lower costs, friction between tool and work material has been established as one of the leading research themes of the last century. Considerable research has been done since then in order to identify how friction influences tool wear and eventual tool failure, cutting forces, heat and temperature, surface integrity of the machined parts, energy consumption, chip curl and the chip formation mechanics [15, 36, 61, 74]. In 1981, Tabor [60] revisited the 200-year progress of fundamental friction research, from the early Coulomb's theories to the late 1980s, concluding that friction mechanics involved three main subjects, namely the real contact area, the strength and nature of the interface, and the shear and rupture of junction bonds at the local contact regions. This critical review concluded that a great progress has been made with regard to our understanding of the phenomena involved in friction, but that many challenging problems remain to be solved [60]. In the following period, there was an increase in tribological research and significant contributions in the field of metal cutting tribology were made by Shaw [51], Ozel [43], Zemzemi et al. [71], among many others. Forty years later, since the critical review of Tabor, one can expect a better understanding of the fundamentals of metal cutting tribology.

Friction is known to have a large number of variables, such as normal load, sliding velocity, interface temperature, material pair, surface roughness, liquids and gases effects at the interface, among many other parameters. Because of this, there are two different views of metal cutting tribology and how chips slide over the rake surface of the tool [49]. The earliest view is based on a more theoretical approach and considers that (i) the tool interface is divided into two different zones, a first zone adjacent to cutting-edge in which chip flow occurs by shear within the metallic material and not by sliding on the contact interface, and a second zone in which the chip slides over the rake surface until it detaches out of contact from the tool. This sticking near cutting-edge approach is inherent in the early theoretical analyses of metal cutting and is implicit in most of the major contributions to the understanding of the process made by Finnie and Shaw [23], Zorev [74], Wallace and Boothroyd [65], Trent [61], Bailey [2], Wright et al. [68] and many others. The other, more practical point of view, considers that (ii) the tool interface is at least divided into two different zones, a first zone contiguous to cutting edge in which chip slides over the rake surface without transfer of metal to the tool even though there is an intimate contact between the underside of the chip and the tool; and a second zone in which the chip sticks to the rake surface near the point where chip detaches from the tool, leaving behind a deposit of material, contributing to an increase in the cutting forces value. This sliding near cutting-edge approach is based on the direct observation of the interface between the chip and the tool [18, 28], but there are still doubts about the mechanism that allows the sliding movement of the chip in that zone [39]. Some researchers concluded that sticking in the second zone is a

consequence of the formation of oxide films in the underside of the newly formed chip, thus increasing adhesion between the chip and the cutting tool [44, 66]. Madhavan et al. [39] obtained similar results with tungsten carbide tools.

Both views of the metal cutting tribology and how the chips slide over the rake surface have serious gaps in determining the strain and stress distribution fields since the incompressible shearing flow must be ensured. As discussed by Wright [69], it is well known that these simplest sticking zone and sliding zone models do not represent the practical flow fields that combine a steady-state material removal regime and the formation of a deposit of material on the rake surface of the tool, as observed experimentally. Even so, despite the divergence between simplified contact interface models, it is significant that Bagchi and Wright [3] have considered the elasto-viscoplastic behaviour of the material flow in order to find a compromise between the different points of view. However, this attempt did not prove sufficient to increase the understanding of the friction mechanism in the contact interface. This is probably the reason why Kuhlmann-Wilsdorf [35], after analysing the work of several researchers, asked the question of the role of contact spots on friction. Others believe that the oxidation process requires high temperature and cannot occur at low cutting velocities.

According to Ernest and Merchant's vision of metal cutting [22], the cutting geometry is completely determined by specifying the rake angle, tool–chip interface friction, material strength and the uncut chip thickness. The primary shear angle, the chip thickness, the chip radius of curvature, and the stresses and velocities within the plastic region can then be calculated. Thus, the cutting geometry should be sufficient to allow predicting the essential information, e.g. chip thickness or chip curl values, according to Lee and Shaffer [36] and Dautzenberg et al. [14]. However, the same cutting geometry under different surrounding medium can provide different values for these process parameters. Oxygen concentration in surrounding medium may have a more significant role in cutting geometry than rake angle, accordingly to Cristino et al. [10]. This achievement seems to suggest that the formation of new metallic surfaces at the tip of the tool plays an important role in the metal cutting process due to the chemical reduction reactions at the interface between the chip and the tool. Similar research was conducted by Li [37].

Chip morphology results from the combination of the strain field, the contact length and the friction at the contact interface, among others. This parameter is usually considered to evaluate the process performance. Chip curl is probably the most evident parameter from the influence of the atmosphere on metal cutting mechanics [38]. In addition, the chip colour can be predicted based on the chemical compositions of oxidation layer on chip surfaces, accordingly to Zhang and Guo [72]. Brinksmeier et al. [6] presented a study on the impact of the chemical effects of metalworking fluids on machining processes, including the chemical interactions, which occur between the intervening surfaces. The affinity of work material with the surrounding environment controls kinematics of formation of oxide films, accordingly to Batra and Kleinman [4] and Zhukovskii et al. [73]. However, it is clear that the existence of a large number of remaining gaps in knowledge has been reported by several researchers [50, 59].

The concern with the oxidation phenomenon is very present in the following fields: (i) tool wear [33, 52]; (ii) tribological and process performance [21, 70]; (iii) difficult to machine materials [42, 55]; (iv) eco-friendly dry machining [56–58]; (v) cooling-lubrication technologies, such as gas assisted minimum quantity [29, 41, 64], active lubricants [26, 31]; (vi) Surface integrity [63]. Usually, the investigations report improvements related to the enhancement of the oxide film formation. However, many of these publications do not assess the value of the friction coefficient. There are few articles reporting chip morphology under different atmospheres. Moreover, there are rare attempts to explain the mechanism by which oxidation phenomenon affects metal cutting.

Ozel [43] investigated the friction calibration for metal cutting simulation and concluded that the frictional conditions in metal cutting should be re-examined in order to select the most appropriate tribology tests and identify the most significant dependencies and interactions between the process variables. A large number of tribology calibration procedures have been suggested in the literature to evaluate the frictional conditions in metalworking processes [49]. They can be generally grouped into three different types: (i) inverse identification based on theoretical procedures, (ii) process tribology tests applying typical metal cutting operations without changing the basic kinematics of the process and (iii) simulative tribological tests using specially design apparatus and procedures to evaluate the friction without losing relevance to real metal cutting applications. One of the most commonly used tribological tests to evaluate friction is the orthogonal cutting test under laboratory-controlled conditions (e.g. null rake angle, extremely sharp tool, polished clearance surface) because it allows the determination of the friction coefficient directly from the experimental measurement of the cutting forces. Yet, the preparation of tribological tools is a significant challenge [17]. The accuracy and reliability of the friction coefficient evaluation in such conditions are generally good, but special conditions must be ensured. The major problem is that contact between tool flank and cut surface promotes an unwanted contribution of the ploughing force [45]. Recently, Smolenicki et al. [54] proposed a new experimental procedure to overcome the above shortcoming.

Pin-on-disc and ring tests are commonly used in metal forming and may be an alternative to the orthogonal tests [13]. Another, ring-on-disc method under low contact pressure is commonly used in machine elements sector. The utilization of these tribological tests has been subject of controversy in the metal cutting community for many years. Some researchers state that neither ring compression nor pin-on-disc testing are adequate to evaluate the friction coefficient in metal cutting since they are not capable of reproducing the conditions of temperature, contact pressure and material flow found in real machining operations [25]. Opponents of the ring compression test claim that the generation of new contacting surfaces results from plastic material flow throughout the entire work material, whereas in metal cutting, the formation of new contacting surfaces and plastic deformation are limited to the tip of the tool and adjacent regions of the shear plane [12]. Moreover, it has been argued that plastic deformation in metal cutting is always combined with sliding of the new surfaces along the rake and relief surfaces of the cutting tools.

Other approach is the modified apparatus of the pin-on-ring method by Hedenqvist and Olsson [27] where a pin rubs on a cylindrical surface in a helical motion. There are other variants presented by Bonnet et al. [5], Iraola et al. [30], Abdelali et al. [1] and Egana et al. [19], among many others. Although none of these procedures can completely avoid oxidized surfaces, the Bonnet et al. [5] design is able to create the necessary contact pressures. Puls et al. [46] presented an experimental set-up, which can be seen as an orthogonal cutting operation on a disc with an extremely negative rake angle. However, seen by Rech et al. [47], only a single rotation of the workpiece is possible before rubbing against the already deformed surface, thus limiting the time span of friction measurements. Similar work was conducted by Cristino et al. [11].

The present chapter is directed towards revising metal cutting tribology in the context of the chemically active and recently formed surfaces at the tool tip, including the likely material hardness effect on the friction coefficient, i.e. by strain hardening as chip flows over the rake surface of the cutting tool. New independent tribological characterization procedures are proposed in order to promote a comprehensive understanding of the phenomenon involved in the contact interface from the transient cutting conditions to the steady-state flow of the chip. To achieve this, the authors propose a new experimental apparatus for metal cutting tests under a laboratory-controlled surrounding media (gas shields). The overall presentation is supported by specially designed orthogonal cutting experiments that were performed on pure metals and engineering metallic materials under laboratory-controlled conditions. WC–Co cutting tools were used as supplied and also under modified conditions to cope with the research goals. This experimental research covers a wide variety of topics such as material flow, friction coefficient assessment, cutting-edge radius effect, adhesion assessment, roughness of polished surfaces, freshly cut metal surfaces, chip curl and influence of inert and active surrounding medium. The results show that the pressure-welded spots on the rake surface initially occur near the point where the chip curls out of contact and progressively extends outward and away from the tip of the tool in combination with an increase in the contact length as cutting progresses. The transition between an oxidized surface and the metallic surface of the material significantly promotes friction at the contact interface. It has been demonstrated that oxygen concentration in the surrounding medium plays a significant role in metal cutting tribology and that the oxidation mechanism is essential for a complete understanding of chip formation mechanics. These outcomes suggest that the oxidation mechanism is also of major importance for theoretical estimates of the cutting forces and energy consumption.

2.2 Materials and Experimental Procedures

This section starts by describing the laboratory apparatus that was developed for assessing the friction coefficient in metal cutting representative conditions. The second part of this section presents the materials preparation and experimental work

plan that was designed to investigate the influence of particular tool–chip contact conditions on the metal cutting tribology.

2.2.1 Experimental Apparatus

Friction at tool–chip interface is commonly a result of several physical–chemical mechanisms interacting to hamper chip flow and to increase energy dissipation over the rake face. These complex mechanisms are very difficult to control, particularly on common machine tools, hence authors decided to develop a special purpose testing apparatus to conduct orthogonal cutting tests under laboratory-controlled conditions. Figure 2.1 presents the set-up that was developed, which is composed of four main groups of components, as depicted in Fig. 2.1a: (i) basic structural parts and transmission system, (ii) control and data acquisition hardware, (iii) gas chamber and a (iv) linear motor. The linear motor previously designed by Silva et al. [53] is essentially an electromagnetic actuator that allows imposing controlled velocity and energy to the kinematics transmission systems. The basic structural parts and transmission systems comprehend a gantry machine structure designed for good static rigidity and both dynamic and thermic stability, and a single linear guide suited to handle heavy loads with good stiffness and high accuracy. The gas chamber shown in Fig. 2.1b was designed to contain the previously mentioned group of components and allow a supply of gas shields (Fig. 2.1c) to control the surrounding medium in which the cutting experiments occur. The influence of the surrounding medium is analysed by conducting cutting tests in the presence of air and by shielding the cutting region with active and inert gases which were supplied from an external source into the cutting region through a nozzle at low pressure and a rate of approximately 5 L/min to avoid turbulent flow.

In the present investigation, specially designed orthogonal cutting tests were carried out with the aim of measuring cutting forces, thus providing valuable information about the tribological condition and focusing on friction assessment. For these tests, a set of cutting inserts were prepared, which deviate from commercial stock ones in order to decrease the cutting-edge radius influence and clearance surface contribution to the cutting forces, hence limiting the cutting tool–chip interaction to the rake surface face. The tool design, deeply described further ahead, also eliminates chip-breaker and imposes a null rake angle for direct measurement of the cutting. A tool-holder has been used to fix the cutting insert to the linear ram (Fig. 2.2a), and the cutting specimens were mounted into a support bar by means of a bolted strap clamp (Fig. 2.2b). Double precision micrometres in a cross-table configuration and a linear guideway were used for positioning of the cutting specimens. The positioning device was then fixed on a three-dimensional piezoelectric dynamometer, which in turn was mounted directly on the gantry structure (Fig. 2.1a). The utilization of a linear guideway ensures the correct positioning of the specimens, improves the parallelism of the overall positioning system and allows the applied loads to reach maximum values of 1 kN.

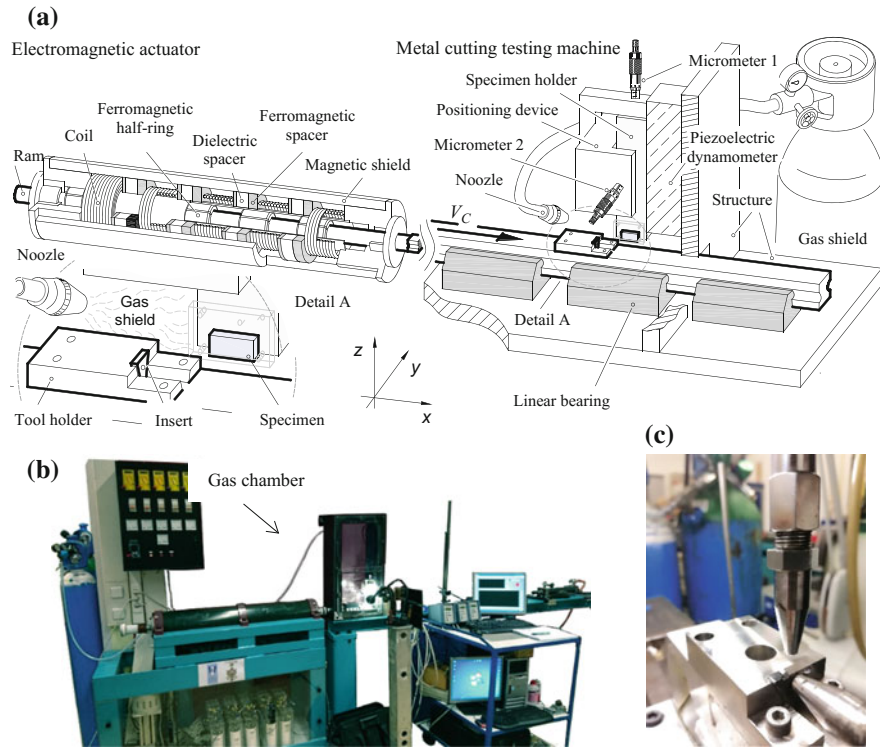


Fig. 2.1 Metal cutting apparatus: **a** schematic representation and nomenclature of the basic structural and kinematics appliances and a detail of the test region showing the cutting tool and the cutting specimen; **b** picture showing the custom-built apparatus (kinematics appliances inside the gas chamber); **c** picture of the forced gas injection system based on nozzles and gas cylinders that were utilized in the experiments under gas shields

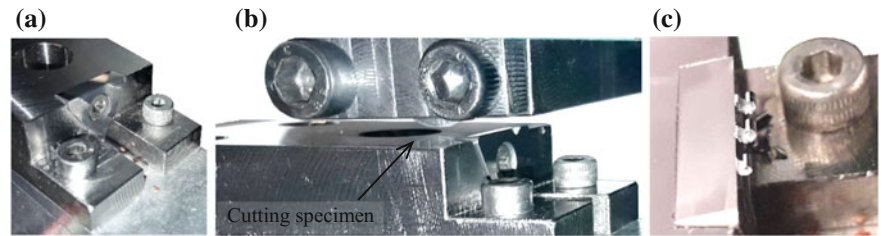


Fig. 2.2 Metal cutting experimentation: **a** picture showing the polished insert and holder; **b** linear stroke of the cutting tool through the test specimen; **c** detail of the cutting-edge region showing welded chips obtained from three different cutting tests

To avoid material deposition from specimens on both rake and clearance surfaces, which could compromise overall results, the inserts were polished and degreased in between tests to regenerate the cutting tool condition—surface roughness and edge sharpness. Since this process is very time consuming, to improve celerity in cutting tests, the cutting-edge was divided into five different regions, one for a first facing operation and therefore to establish the uncut chip thickness and four regions for the cutting tests (Fig. 2.2c). The positioning of the specimens is controlled by the cross-table presented in Fig. 2.1a, a custom-built system of the apparatus that serves the purpose of placing the test specimens on different coordinates along the cutting-edge and also of controlling the uncut chip thickness.

2.2.2 Sensors Design and Calibration Set-up

The control and data acquisition hardware are composed of three functional sub-systems, as shown in Fig. 2.3: (i) cutting forces measuring system that includes a three-dimensional piezoelectric dynamometer connected to a signal amplifier; (ii) displacement measuring system based on one-dimensional inductive displacement transducer specially developed for the present work, which was attached to a function generator and a high-frequency full-wave rectifier; and (iii) multifunction data acquisition board combined with special purpose software for data acquisition and linear motor control. The system has a controllable movement across its entire range and measures forces and displacement with accuracy of 2%, and its resolution allows measuring the dynamic changes in forces or displacement of great amplitude.

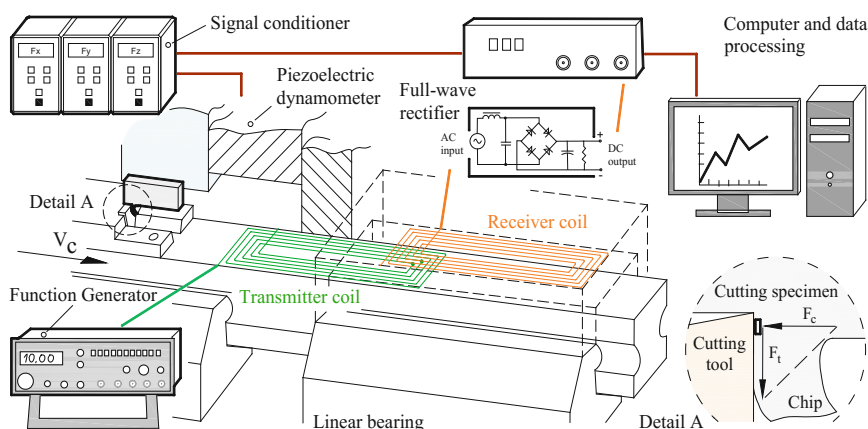


Fig. 2.3 Schematic representation of the data acquisition system showing the three-dimensional piezoelectric dynamometer, the displacement transducer and a detail of the test region

Cutting forces are monitored during the tribological tests, but no importance is usually given to the relative position between tool and material to be cut. In the present research work, the tool's relative position has been considered as a main parameter that can provide valuable contribution to the understanding of the contact mechanics at the chip–tool interface. Monitoring this displacement has its challenges, as it requires high-accuracy sensors able of working at a high speed and harsh acceleration, conditions that demand complex and costly sensors that may not always be commercially available to provide the appropriate technical performance. Therefore, in order to overcome such obstacles, it was developed a displacement sensor, presented in Fig. 2.4, for the orthogonal metal cutting apparatus that makes use of the magnetic field generated by a transmitting coil fed by the passage of a high-frequency current. One coil is installed on the linear guide while the other is installed on a bracket, rigidly fixed to the testing machine structure and, depending on the distance between coils, the coupling between those coils creates a variation of the impedance of the sensor that allows measuring the relative position of the cutting tool. Larger distance between coils means that sensors behave as a loosely coupled system. Such distance can be varied by the axial alignment of the coils while the offset is set constant, being that as the axial alignment increases, considerably tightly coupling can be achieved. Transmitting coil is fed by a function generator, while the receiving coil is connected in series to a full-wave rectifier converting the sinusoidal current wave (AC) into direct current (DC) which is more adequate for the data acquisition system. Finally, for the maximum velocity and acceleration, the sensor's frequency response was adjusted for 1 MHz and the data acquisition system for 200 kHz sampling rate (Fig. 2.3). This proposed technology allows measuring the cutting tool position with high resolution under very high velocities and severe accelerations.

2.2.3 Cutting Tools and Sharpening Technique

The cutting tool was based on a triangular uncoated carbide insert ISO TCMW 16T308 Grade H13A (EAN 10087501) supplied by Sandvik Coromant, with three cutting-edges of 16 mm in length, null rake angle, clearance angle of 7° , flat top

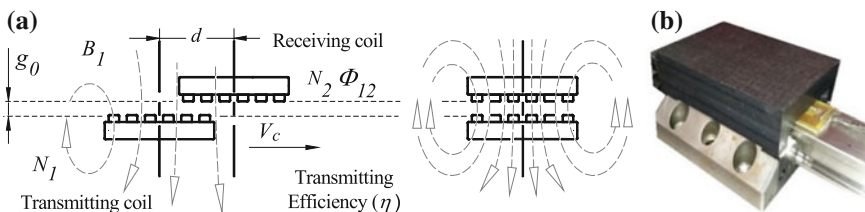


Fig. 2.4 Displacement sensor: **a** basic fundamentals of inductive transducers based on flat spiral coils; **b** picture showing the custom-built displacement sensor

chip-breaker and cutting-edge radius of 25 μm , recommended to process cast iron, non-ferrous and hardened materials (Fig. 2.5a). Despite these recommendations, however, the performance of the tool in terms of structural strength and temperature stability was considered adequate for the materials and operating conditions used during the present research. The selection of a flat top chip-breaker with null rake angle ensures adequate tool geometry for direct measurement of both normal (cutting force) and tangential (thrust force) forces on the tool rake face, whereas the utilization of a specially designed tool-holder based on the screw clamp mounting style, in which the insert does seat properly in the V-shaped pocket sides, ensures excellent rigidity and orientation of the cutting-edge with the cutting direction.

Cutting tools design (edge design and sharpening) for assessing metal cutting tribology starts with a decision on how much taper should be built in and how this affects the cutting-edge strength and the chip formation mechanism. Clearance surface is also of major importance since its texture scratches and rubs the machined surface masking the contribution of the primary rake face on the cutting forces amplitude. To overcome this limitation and minimize the contact on the clearance surface, it is necessary to increase the clearance angle and reduce asperities by surface polishing. Thus, for a tribological chisel, broader clearance angle must be chosen, never less than 7° and not more than 45° , depending on the relative materials strength. The compromise is reached when the clearance angle is slightly lower than that at which the cutting-edge will collapse for more severe conditions. For a generic tribological chisel, to test a wide range of materials from pure soft metals such as tin to high-strength engineering alloys such as Ti6Al4V, the original clearance angle of 7° is an adequate choice. Another important issue has to do with tool sharpening since blunt cutting-edges promote metal forming tribology conditions and tribological contributions to surge from clearance surface instead of rake surface. The thinner the uncut chip thickness, the more relevant is this effect. To address this issue and achieve a truly sharp cutting-edge, two stages of tool preparation have been developed. In stage one, primary-edge faces that come together to form the actual cutting-edge have been ground until reaching a very sharp edge (Fig. 2.5a), while in stage two, geometrically uniform cutting-edge was obtained on a fine diamond hone (Fig. 2.5b).

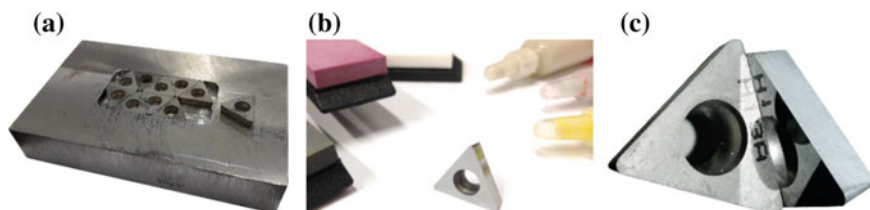


Fig. 2.5 Cutting tools preparation and reconditioning: **a** support block for inserts grinding; **b** honing and lapping apparatus for sharpening and surface polishing; **c** picture showing cutting inserts prepared with different values of surface roughness, stock insert (left) and mirrored insert (right) reflecting the rough one on rake surface

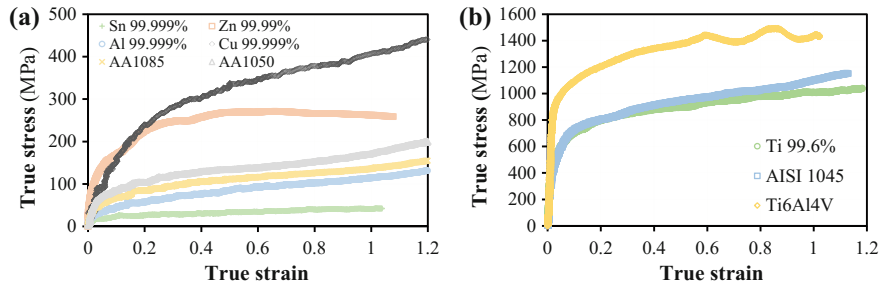


Fig. 2.6 True stress–strain curves for: **a** pure tin, zinc, copper and aluminium and, technically pure alloys AA 1085 and AA 1050; **b** technically pure titanium and engineering alloys AISI 1045 and Ti6Al4V

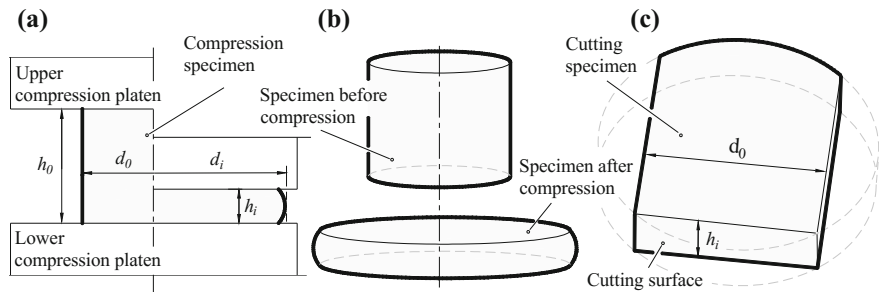


Fig. 2.7 Cutting specimens: **a** schematic representation of the uniaxial compression test; **b** workpiece materials have been compressed up to an effective plastic deformation of 1.25; **c** cutting specimens have been manufactured from the deformed compression specimens

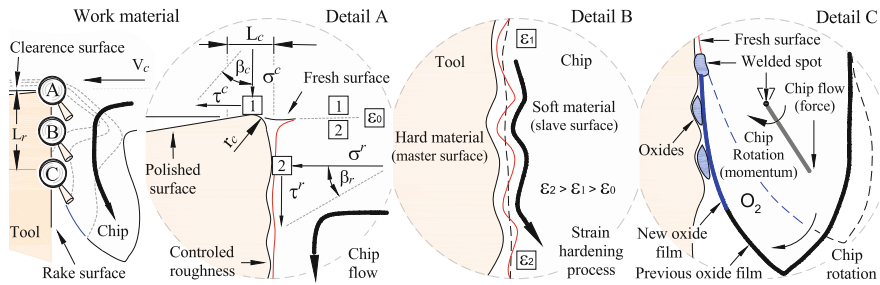


Fig. 2.8 Tribological interaction between tool and work material under orthogonal metal cutting conditions, identifying three main tribological instants that occur during chip formation: (A) influence of rounded cutting-edge on stress components in the separation zone; (B) illustration of the plastic interaction between surface asperities of work material (soft) and rigid asperities of cutting tool (hard) showing material strain hardening process; (C) schematic representation of the oxide films formed between the freshly cut surface and the cutting tool due to chemical reactions with oxygen-rich surrounding medium

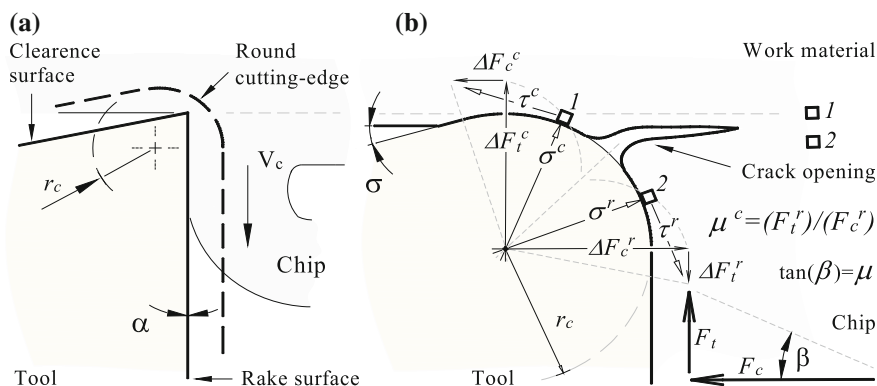


Fig. 2.9 Influence of cutting tool geometry on the friction coefficient assessment; **a** comparison between different round cutting-edges; **b** stock insert with a round cutting-edge design and flank contact

Surface condition of the carbide inserts is another major parameter on metal cutting tribology. As previously mentioned, rough clearance surfaces mislead friction calculations since tool asperities anchor in the machined surface promoting an increase of the cutting forces followed by an apparent friction coefficient reduction. Thus, clearance surface must be polished as much as possible, whereas rake face roughness is a tribological parameter depending on insert manufacturing process and how it becomes worn over time.

Achieving and maintaining a nearly perfect surface presents its challenges. To do so, the inserts were lapped (also presented in Fig. 2.5b) with diamond grits from 5 to 0.5 μm at very low speeds and the rake surface was worked under low-pressure conditions across a lap plate onto which the abrasive was previously applied. To regenerate the surface, adherent boundary layer of chip material that formed on the tool face was removed with sandpaper, since it easily allows removing these metallic layers without scratching the extremely hard surface of the cutting insert and the lapping process was repeated. This procedure assures proper reconditioning of both rake surface and cutting-edge and serves the purpose of obtaining a rake surface with the desired roughness and texture while also guaranteeing the cutting-edge sharpness.

Obtained surfaces had a very polished finish that was not possible to be measured with a contact profilometer. These values were measured along a direction perpendicular to the cutting-edge by means of an Atomic Force Microscope (AFM), having shown values of the average roughness (R_a) in the range 0.006–0.012 μm . As seen in Fig. 2.5c, the polished insert has a mirror-like appearance that highly contrasts with the commercial one. It is worth to notice that this extremely low roughness allowed reducing the directionality of the surface texture and side effects that will inevitably influence the friction coefficient [50].

Surface texture and geometry of the cutting tools were carefully controlled and also the radius of the cutting-edge which greatly affects the minimum value of the

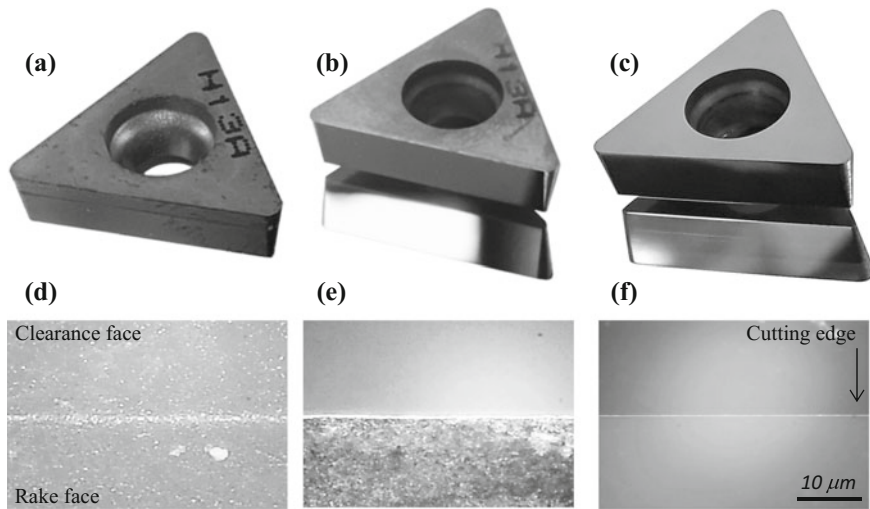


Fig. 2.10 Cutting inserts prepared with different values of surface roughness; **a** stock insert as it was supplied (average surface roughness of $R_a = 0.250 \mu\text{m}$); **b** modified insert by polishing the clearance surface (average surface roughness less than $R_a = 0.01 \mu\text{m}$) while preserving stock rake surface; **c** modified insert that has been completely polished (average surface roughness of $R_a = 0.01 \mu\text{m}$). Microscopic details of the cutting-edge: **d** stock cutting-edge radius of $r_c = 25 \mu\text{m}$; **f** ultra-sharpened cutting tool with edge radius value less than $r_c = 1 \mu\text{m}$; **e** intermediate cutting-edge condition

uncut chip thickness, being that a higher cutting-edge radius insert can only remove layers of higher thicknesses. The custom cutting tools present a value of cutting-edge radius below $1 \mu\text{m}$ (Fig. 2.10), and the ratio of the uncut chip thickness to the cutting-edge radius of the cutting inserts ($1/25$) was small enough to neglect the influence of the ploughing and rubbing mechanism on the cutting forces values.

2.2.4 Materials, Specimens and Mechanical Behaviour

The choice of test materials is crucial to emphasize different phenomena taking place during chip flow and thus to allow a comprehensive analysis about contact mechanics at the tool–chip interface. Metal cutting tribology shall not only focus on friction measurement techniques and lubricants performance, but also on fundamental research since it can be of great relevance for innovation and development in regions where there is lack of knowledge about key parameters that control the manufacturing processes tribology. For mechanical processing, it is worth highlighting the importance of surface roughness and material strength, the influence of strain-rate sensitivity and temperature rise induced by energy dissipation due to plasticity and friction. Furthermore, when fracture occurs, and new metallic

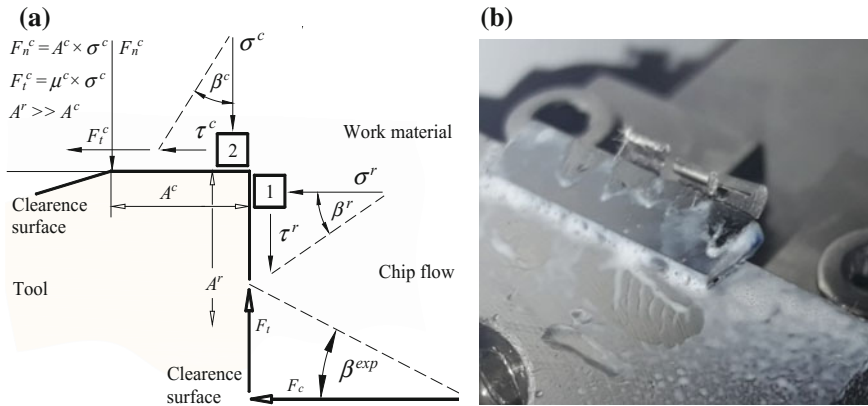


Fig. 2.11 Cutting tool geometry and contact parameters: **a** schematic representation of the microscopic details of the cutting-edge showing the friction stress components; **b** macroscopic observation of a cutting test showing a polished insert and clearance surface lubricated with lithium grease (dry rake surface without lubricant) where chip flow drags lithium grease without scratching or depositing material on the clearance surface

surfaces are generated, it is also important to take into account the chemical stability of the surrounding medium and control the influence of complex phenomenon, such as oxidation of metallic alloys, on the friction coefficient. Unfortunately, the most commonly used engineering materials involve many different ions and alloying elements promoting complex physical and chemical interactions with the cutting medium and making it difficult to understand the contact mechanics. To surpass this shortcoming, pure metals and technically pure metallic materials were chosen since their less complex reaction products under the influence of the active surrounding medium contribute to lower dispersion of the experimental data. The pure metals and technically pure metallic materials also allow a better control of their mechanical proprieties under low cutting speeds and room temperature. Materials selection was conducted for complementary results using pure and low-alloy metals with different physical and chemical properties. However, in practice, most metal cutting applications are based on engineering materials, being these materials subsequently included in the present research.

Specimens were manufactured from engineering materials (AISI 1045, also presented as C45, and Ti6Al4V), unalloyed materials (AA 1050, AA 1085, titanium 99.6% and lead 99.5%) and pure metals (zinc 99.99%, tin 99.999%, aluminium 99.999% and electrolytic copper (99.999%). As previously explained, metal cutting involves severe plastic deformation of the materials that are intended to cut. Due to its importance and acceptance among metal cutting researchers, the authors decided to evaluate the mechanical behaviour of the materials used in the present research. The stress–strain curves were obtained by means of uniaxial compression tests carried out at room temperature and quasi-static conditions on cylindrical specimens with 6 mm in diameter and 7 mm in height. The specimens

were centred between platens lubricated with lithium grease to ensure homogeneous deformation, and compression was performed to predetermined height of 2 mm that allows reaching a final effective strain of 1.25. Additionally, microhardness measurements were performed on the top of the specimens before and after uniaxial compression, showing a uniform distribution of the material hardness. The experimental data resulting from the compression tests is presented on Fig. 2.6 in the form of monotonic stress–strain curves.

2.2.5 *Experimental Work Plan*

The tribological experiments were performed in orthogonal cutting conditions. The cutting-edge was straight and perpendicular to the work material motion, the ratio of the uncut chip thickness (t_0) to the specimen width (w) was small enough (1/80) to guarantee plane strain conditions, and the uncut chip thickness has been defined constant to 25 μm in order to highlight the influence of some operating conditions, such as cutting-edge radius and relief surface roughness. Nevertheless, some complementary cutting tests have been conducted with other values (uncut chip thickness of 0.050, 0.075 and 0.1 mm) to demonstrate the influence of the uncut chip thickness on the friction estimate.

The cutting tools were manufactured from WC–Co to improve the abrasion resistance, impact strength, surface finish and the chemical stability for higher material removal rates. Tool geometry with a rake face angle $\alpha = 0^\circ$, a clearance angle $\sigma = 7^\circ$ and a cutting-edge radius below 1 μm has been kept constant throughout the experiments. Three different conditions of the cutting insert were considered: (i) stock or commercial inserts without any preparation, (ii) modified inserts that have been completely sharpened and ultra-polished and (iii) an intermediate insert condition combining stock rake surface and ultra-polished clearance surface. This third set of inserts has been manufactured for assessing the influence of clearance surface texture on the cutting forces under the same geometry of the first inserts set. Clearance surface has been polished to a smooth surface with an arithmetical mean roughness ($Ra \sim 0.01 \mu\text{m}$) to minimize the contact with the machined surface, whereas rake surface roughness has remained as delivered from stock. The second set of inserts was specially designed to support the comprehensive study of the tribological parameters on friction coefficient of metal cutting. Indeed, orthogonal cutting is not a tribological test and particular conditions must be guaranteed to ensure the reliability of this comprehensive study about metal cutting tribology. Furthermore, the first set of inserts intends to establish a realistic expectation about what physical calibration can be achieved directly from the machine shop. Stock inserts were only of use for one test, while modified inserts have been reconditioned after each test.

The cutting specimens were manufactured from two different conditions: (i) undeformed workpiece material and (ii) previously deformed compression specimens (with an effective strain of 1.25) for analysing the influence of strain

hardening on the cutting forces under the same cutting conditions. These specimens had a disc shape with a diameter of 12 mm and thickness of 2 mm, produced from the above described materials. These cylindrical plates were faced to a rectangular shape with a length of 9 mm, a width of 7 mm and a thickness of 2 mm. The flat surface of the cutting specimen (7×2 mm) was achieved in the testing machine with the intention of ensuring a uniform cut section thickness during the test (please see Fig. 2.7). The specimens were fixed to the transversal adjustable device, and a constant cutting velocity of 500 mm/min of the linear guide was used for all orthogonal tests. Post cutting, microhardness of the machined surface was measured for all specimens using a microhardness tester.

The cutting medium, which plays an important role in defining the nature of the tool–chip contact, was set to determine the oxidation rates (argon, oxygen and room environment). This promotes controlled cutting medium atmosphere of active oxygen (21% or 100%) and inert medium of argon and also favours the observation of the chip flow during the cutting tests through a clear gas medium. The active gas medium filling the detach gap between tool and work material promotes oxidation of the chip during the cutting, favouring the level of permeability. Physical action of the gas flow on the chip detachment has been limited by a gas flowmeter (5 L/min).

2.3 Results and Discussion

This section discusses friction in metal cutting based on simulative and specially designed tribological experiments. The purpose of conducting such research plan is to re-examine the friction coefficient of metal cutting having regard to material separation at the tip of the tool and interaction of the chip with the surrounding medium. The presentation is supported by thoroughly researched quantitative data obtained from laboratory-controlled experimentation, in an attempt to deepen the knowledge about the evolution of friction coefficient in metal cutting. It is structured in five parts referring to the main physical and chemical mechanisms that oppose chip sliding along the rake surface as summarily depicted in Fig. 2.8. Firstly, cutting-edge geometry is comprehensively analysed in order to understand its influence on the ploughing forces and the consequent contribution in the friction evaluation. Secondly, the formation of new non-oxidized metallic surfaces and how this affects friction along the underside of the chip at the tool interface are discussed. The third part provides information about the importance and potential impact of the high-strained layer of the chip underside on the friction coefficient by means of simulative strain hardening tests. The fourth part discusses the friction of different material pairs in controlled surrounding medium affected by the processes of metallic surface oxidation. And, the fifth part discusses the transient evolution of friction to the tribological steady state of metal cutting.

2.3.1 Tool Geometry and Ploughing Force Contribution

Orthogonal metal cutting usually serves as a simplified or simulative test to aid the understanding of relatively complex machining processes. Although not a tribological test, orthogonal cutting can be modified to perform a direct and reliable evaluation of the friction coefficient as previously explained. Figure 2.9 illustrates the influence of clearance surface and round cutting-edge on the unintended contact areas between the workpiece and the cutting tool (here it should be considered as tribological tool) during the experimental test. A round cutting-edge promotes a rotation of the normal vector to tool surface, being this related with the contact pressure. At the same time, just-formed machined surface is moving parallel to the chip flow and so is the tangent vector related to friction. This complex material flow close to the cutting-edge limits a proper evaluation of the friction coefficient on rake surface of the cutting tool, and therefore, one can conclude that a commercial cutting insert will bring misleading data and that specific tribological tools must be used for a proper evaluation of friction. Tribological tools need well-defined contact interface in order to control crack opening and propagation mechanism. Thus, cutting-edge sharpness and clearance surface condition are of crucial importance for metal cutting tribology.

The first set of stock inserts intends to establish a reference about what friction coefficient values can be achieved directly from the machine shop. Figure 2.10a shows the stock inserts with a rough surface and a rounded cutting-edge. These inserts were tested only once in order to guarantee the same test conditions for each successive test. The second set of inserts is shown in Fig. 2.10b, and these inserts were used to demonstrate the clearance surface influence on the friction assessment. In this set, clearance surface has been polished, while cutting-edge, however, was tested only once to preserve a similar rake surface condition during the tribological tests. The last set of inserts, shown in Fig. 2.10c, has been specifically designed to focus on examining the influence of the process parameters on metal cutting tribology. In order to guarantee the reliability of this comprehensive study, surface roughness of the rake surface was kept below Ra 0.01 μm and a sharp cutting-edge with radius (r_c) below 1 μm was used. These inserts were reconditioned after each test.

The influence of tool preparation (tool geometry) on the friction coefficient is summarized in Table 2.1. Cutting tool preparation is followed as described in the previous paragraph, and the workpiece in C45 was cut under room atmosphere and steady-state conditions. Experimental results, shown in Fig. 2.12, suggest that the magnitude of the force depends on the cutting-edge sharpness and clearance surface roughness. Thrust and cutting forces both decrease with tool sharpness, surface polishing and tool lubrication. However, the direction of the force that is mainly related with the friction coefficient at the rake surface changes very little with the clearance surface lubrication. It therefore follows that small changes on the tool sharpness and surface polishing may have an apparent adverse impact on friction coefficient unlike what was to be expected (Fig. 2.11).

Table 2.1 Tool preparation influence on the friction coefficient of AISI 1045 (C45) obtained by orthogonal cutting tests under room atmosphere and steady-state conditions

Insert preparation condition	F_c	F_t	μ_{st}	$\pm\Delta\mu$
Stock insert (strained)	515	308	0.600	-0.030
Stock insert (annealed)	355	188	0.535	-0.040
Polished clearance (annealed)	334	177	0.53	-0.065
Polished insert (annealed)	312	193	0.620	-0.030
Polished insert and lubricated clearance (annealed)	231	129	0.560	-0.010

Round cutting-edges and rough clearance surfaces results in additional load contribution on the cutting tool due to ploughing and rubbing action, as discussed by Chien [9] and Masuko [40]. This indentation effect contributes with two switched vector components to cutting forces. Frictional shear stress (τ^c) and contact pressure (σ^c) increase the experimental measurement of the cutting (F_c) and thrust (F_t) forces, respectively. Thus, the evaluation on the contact pressure (σ^r) and frictional shear stress (τ^r) over the rake surface is not a direct calculation based on the experimental measurement of the contact area and cutting forces, even for a null rake angle and a very sharp cutting-edge. Equation (2.1) presents a conceptual model in order to help a better understanding of the tool geometrical influence on the experimental assessment of the friction coefficient. Measurements accuracy increase as the contact interface area (A^c) between flank and cut surface decreases, and friction angle on the clearance surface (β^c) reduces as much as possible its unwanted contribution. Contact interface area (A^c) is merely controlled via the clearance angle, while friction is minimized by smoothing and lubricating the clearance surface. Inert lubricant is essential to eliminate non-controlled chemical reactions on the new and chemically active surface generated during cutting. These contributions affect the friction angle estimates on the rake surface (β^r), to the extent that it becomes difficult to correlate influence between the operative parameters and the friction value estimates (Fig. 2.11a). Thus, sharp cutting-edges and adequate clearance angle allow reducing the contact area to a minimum. Lubricated clearance surfaces allow minimizing unwanted frictional contribution from the tool flank, resorting to an inert lubricant to avoid any kind of chemical reaction between the new machined surface, tool and surrounding medium (Fig. 2.11b).

The results presented in Table 2.1 show that proper tool preparation leads to an improved cutting stability due to an enhanced cutting-edge performance. These results are in agreement with those presented in Fig. 2.13 for three different materials (Ti6Al4V, AISI 1045 and Ti) that have been cut under different room ambient conditions. However, a higher friction coefficient for a polished insert or a lower friction for a different metallurgical condition of the work material may be questionable, so a careful reading of these results is still required. For this reason, the geometrical interaction between the cutting tool and work material does not seem to be sufficient for a full explanation of the metal cutting tribology, not even with the conceptual model help provided in Eq. (2.1). Furthermore, adhesion

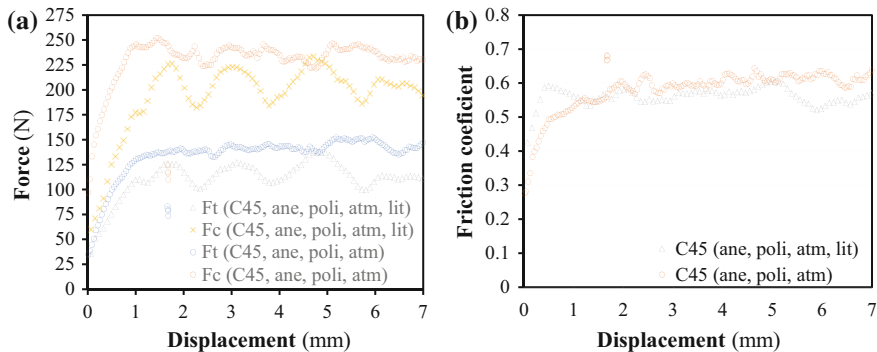


Fig. 2.12 Tribological cutting tests: **a** evolution of the cutting and thrust forces and **b** evolution of the friction coefficient as a function of the tool displacement for AISI 1045 under room ambient conditions with (i) dry and (ii) lubricated clearance surface. Testing conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished clearance, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

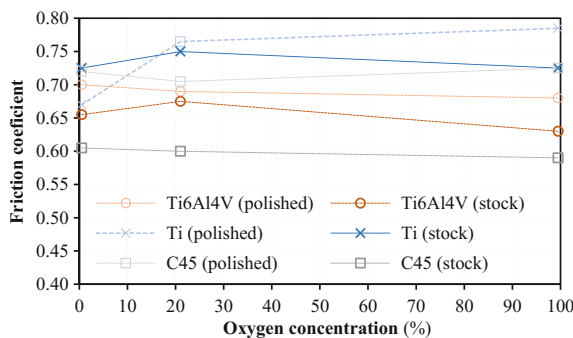


Fig. 2.13 Influence of the tool preparation on the friction coefficient as a function of the oxygen concentration in the surrounding medium. Three test materials (Ti6Al4V, AISI 1045 and Ti) have been cut using two different inserts conditions; (i) stock insert as provided by the manufacturer and (ii) perfectly polished insert with an average surface roughness less than $0.01 \mu\text{m}$. Testing conditions: WC–Co tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

contribution is determined by rake surface preparation since a polished surface may not necessarily lead to a decrease in friction coefficient, being even more conducive to another surface phenomenon.

$$\mu^r \approx \frac{F_T}{F_C} = \frac{F_\tau^r + F_\sigma^c}{F_\sigma^r + F_\tau^c} = \frac{F_\tau^r + A^c \cdot \sigma^c}{F_\sigma^r + \mu^c \cdot \sigma^c} \quad (2.1)$$

2.3.2 Friction of Non-oxidized Metallic Material

As the cutting tool contacts the workpiece, some compression occurs, and the metal begins flowing along the rake surface similar to the plastic indentation process (Fig. 2.14a). This asymmetric indenter deviates material flow along only one face of the wedge (the rake face) concerning just plastic flow and friction. As more stress is applied to the workpiece by the cutting action, metal pushed from the uncut chip thickness region piles up, causing the contact length between the work material and the rake surface to progressively increase (Fig. 2.14b). This piled-up metal adjacent to the rake surface is under compression and is forced away from the workpiece.

While the contact pressure on the rake surface is interrelated with the material flow stress, the frictional stress is mainly determined by the surfaces characteristics. The characterization of tribological surfaces is, however, a major challenge that makes the understanding of friction mechanics difficult, since: (i) tribological surfaces often exhibit a non-stationary and microscale nature, (ii) changes in apparent contact surface occur during the process, (iii) changes in topography that occur between the interacting surfaces significantly reduce real area of contact, and the real (local) contact pressure accordingly becomes very high, (iv) non-homogeneous bulk material with hard particles results in surfaces of a very abrasive nature, and (v) thin films and coatings promote a friction performance different from bulk material.

In the first stage of orthogonal cutting, the work material surface presents a thin film of metallic oxide, perhaps with some contamination due to handling and ambient debris accumulation, and usually presents a textured surface. Thus, it would not be surprising that textured surfaces improved the lubricant distribution

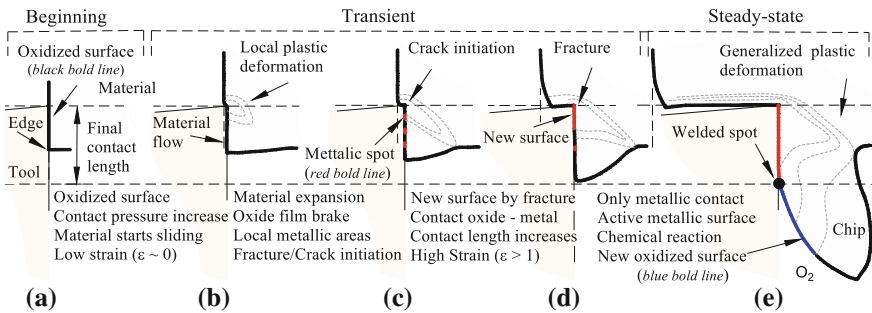


Fig. 2.14 Schematic illustration of the transition from beginning to steady-state cutting; **a** initial contact of the cutting tool on the workpiece's oxidized surface; **b** contact pressure increases and material starts sliding over rake surface; **c** chip begins to form and material expands over contact interface exposing small metallic areas; **d** transition from the initial oxidized surface to the just-formed metallic surface by fracture at the tip of the tool, competing to change friction; **e** contribution of the welded spots and generalized plastic deformation to the friction mechanism allowing to reach steady-state condition at the contact interface

(also soft contaminants), promoting a hydrodynamic lift and consequently friction coefficient decrease [16]. Additionally, as deformation proceeds, natural thin oxide film expands, breaks and fragments, exposing several small non-oxidized metallic areas with changes in hardness and adhesion values (Fig. 2.14c). This non-stationary surface brings up a relatively complex tribological interface, but even so, similar to what can be found in metal forming.

As the stress applied to work material by the cutting action increases, the metal compresses until it reaches a point where rupture occurs and the chip separates from the uncut portion (Fig. 2.14d). This crack favours the material separation required for the tool to move in the cutting direction at a lower energy than for just further indentation, even though plastic flow and friction continue to occur simultaneously. The putative chip formed by the crack at the tip of the tool has not necessarily attained at this point its steady shape yet. Contact area still continues to grow on the rake surface until chip flow departure from the tool because the chip acquires more curl as it passes through the deformation zone. However, the curvature of the chip at first departure from contact is at its least but then it increases to a larger value (radius increases) suiting steady-state material flow conditions (Fig. 2.14e). The increase of the transient contact length is probably due to an energy compromise between the plastic work and the amount of frictional work, which is inversely proportional to the radius of curvature of the chip. This is a well-known fact that chips always begin curled to progressively end only slightly curved due to a transient frictional stress. As chip moves adjacent to the rake surface and under an extreme contact pressure, oxide films particles, lubricants and other contaminants are swept out from the contact interface. That is, typically oxidized metallic surfaces of metal forming are progressively replaced by the newly formed and real metallic surfaces of metal cutting. That is why pin-on-disc, double cup extrusion or ring test are unable to provide a realistic friction coefficient for the metal cutting processes.

The transient beginning to the steady-state tribological conditions can be further analysed in Fig. 2.15. As seen, the thrust force (F_t) increases as a linear function of the cutting force (F_c); however, a simple graphic analysis allows the identification of three different slopes corresponding to very well-defined regions: (i) a leftmost region ($F_c < 100$ N for Ti and 45 N for Zn), where the friction angle is nearly constant ($\beta_{\text{transient}} = 28^\circ$ for Ti and 14° for Zn); (ii) a rightmost region where the cutting force reaches a maximum force related to steady-state cutting conditions ($F_c > 180$ N for Ti and 60 N for Zn), from which the friction angle remains nearly constant ($\beta_{\text{steady}} = 56^\circ$ for Ti and 33° for Zn), even when significant variation of the cutting force is present (point-cloud); and (iii) a region in between where the friction angle value progressively grows from the smallest to the largest measured values, indicating intimate contact between the chip and the tool in that region [74]. There is also a slight decrease on the evolution of the thrust force F_t against the cutting force F_c at the begging of the steady-state regime of the orthogonal metal cutting test (Fig. 2.15b, A for Ti) but this subject will not be further addressed here due to its small relevance for the overall discussion since friction coefficient remains unchanged.

The main results derived from the graphical analysis of Fig. 2.15a can be summarized in two points: (i) the transient condition of the middle region seems consistent with the progressive replacement of the oxide film by real metallic material in the contact interface; and (ii) the friction angle is lower at the initial non-steady-state conditions and higher at the steady-state regime, which came to a maximum of 56° for Ti (33° for Zn), far higher than the commonly accepted theoretical limits of the metal forming processes.

The transient tribological conditions may also be observed in Fig. 2.15b. The friction coefficient increases sharply at the beginning of the test (A in Fig. 2.15b) and remains almost constant throughout subsequent steady-state cutting conditions (B in Fig. 2.15b). In the leftmost region, it is worth noticing that experimental data trend does not pass through the origin and the positive friction coefficient interception is the adhesion measurement for the material pair (WC–Ti and WC–Zn). After that, friction coefficient seems to grow asymptotically and reaches a maximum value ($\mu_{\text{steady}} = 0.85$ for Ti and 0.5 for Zn) for a tool displacement much higher than the uncut chip thickness. This seems to illustrate that, although chip formation is well established, it requires additional chip sliding over the rake face to sweep up various forms of contamination (lubricant, oxides debris, etc.). Consequently, oxides seem to promote low friction and reduced metal–metal contact [60]. Nevertheless, these very higher displacement values do not seem consistent with just oxides removal mechanism and serious questions remain to be asked about neglected phenomenological contributions in metal cutting tribology.

The discussion in the previous paragraph may be extended to the other materials of the present investigation. Figure 2.16 sketches some of the typical behaviours of these materials. However, it should be noted that the evolution of the friction curves can vary with other tribological parameters, as explained in the following sections. In general, it is observed that materials with higher mechanical strength and brittle behaviour (Ti64, Ti and C45) rapidly reach the steady state. The more ductile

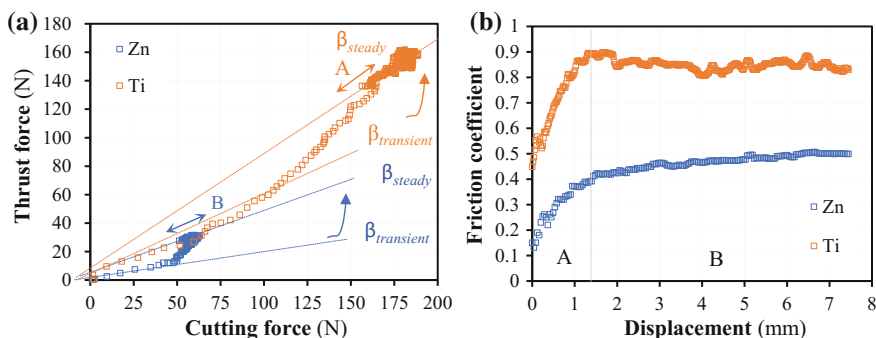


Fig. 2.15 Transient tribological conditions for titanium (Ti) and zinc (Zn): **a** typical evolution of the forces during an orthogonal cutting test; **b** evolution of friction coefficient as a function of the tool displacement. Testing conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished clearance/surfaces, room ambient, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $V_c = 500 \text{ mm/min}$

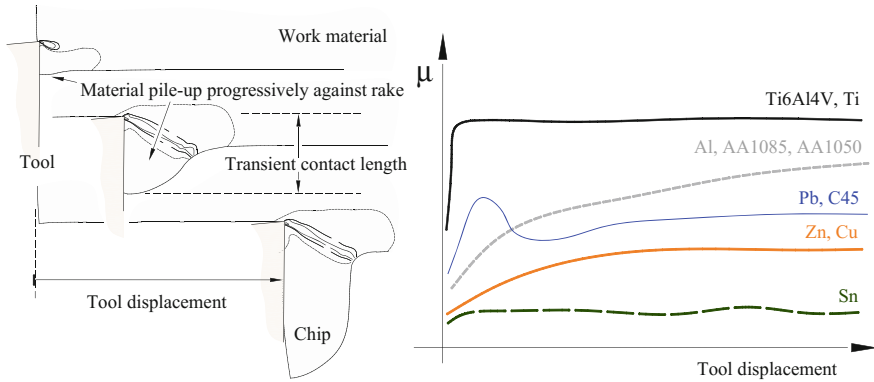


Fig. 2.16 Illustration of the progressive material pile-up process and typical evolution of the friction coefficient curves of the test materials

materials require the tool to further advance the material to achieve this condition (Al, Cu, Zn, Pb, AA 1085 and AA 1050). This seems to indicate, considering similar cutting conditions, that the higher hardening coefficient typical of ductile materials promotes the accumulation of more material and extended the length of contact before reaching steady-state cutting conditions. However, it is also possible to observe two other cases of interest: (i) those where a given material has difficulty in reaching steady conditions, tending to increase the coefficient of friction progressively, and (ii) where the material reaches a maximum value followed by steady-state regime with a lower friction coefficient. This seems to indicate the existence of another mechanism that tends to facilitate or hinder stationary tribological conditions.

2.3.3 Strain Hardening Contribution to Friction

The physical contact between surfaces is normally localized to a limited number of small contact points that transfer the load between contacting surfaces in motion. This real area of contact is extremely small when compared with the apparent one, and effective contact pressure accordingly becomes very high. Effective strain may also be much higher than in macroscopic compression or tensile tests since the stress state has a large hydrostatic component, promoting deformation and suppressing the nucleation and growth of cracks. Microscopic asperities' interactions involve essentially two main contact geometries: (i) top-to-top asperities (adhesion), where the hardest one (tool) acts like a forging tool moving perpendicular to the contact surface until it reaches an equilibrium contact pressure. In this case, a constant shear stress results from the atomic forces attraction at the interface of the specific material pair; (ii) side-to-side asperities (interlocking), in which soft

asperities are deformed by relative sliding action. In this interlocking mechanism, friction results from the maximum opposition of soft material asperities to the relative sliding. Thus, it is expected that friction increases with the work material strength (linearly related with material hardness) and the surface roughness.

Annealed work materials exhibit a regular and nearly defect-free pattern in the bulk lattice. As the bulk material is work hardened (throughout the primary shear plane), it becomes increasingly saturated with new dislocation storage and its arrangement in dislocation cells, and more dislocations are prevented from originating. Additional hardness is attained at contact interface by shear deformation within asperities to accommodate relative sliding of the work material.

Figure 2.17 illustrates the material strain hardening process from the annealed material within the uncut chip thickness to the very strain hardened layer attained when chip starts to detach from cutting tool. As bulk material gets close to the broad and diffuse primary shear zone, the narrow bounding strips of pure shear will progressively increase the work hardness, towards the cutting tool. There, material in the vicinity of the cutting-edge is subjected to a severe plastic deformation under significant compression and the just-formed surface is pushed over the rake surface. Therefore, as chip slides over the rake surface, hard asperities of tool can anchor and plough the soft surface of the chip promoting additional plastic deformation and progressive increase of chip surface hardness will be observed. Yet, material hardening is very sensitive to strain, strain rate and temperature that will encourage the shear plane to broaden into a larger plastic shearing zone promoting complex hardening and dynamic recrystallization mechanisms on both chip and machined surface.

Below follows an analysis of the material strain hardening and its relevance on the value of the friction coefficient during chip flow. Figure 2.18 shows experimental evidences that the friction coefficient is generally promoted by material hardness. Simulative hardness experiences were carried out in AISI 1045 for two different hardness conditions: (i) work material supplied with 185 HV and

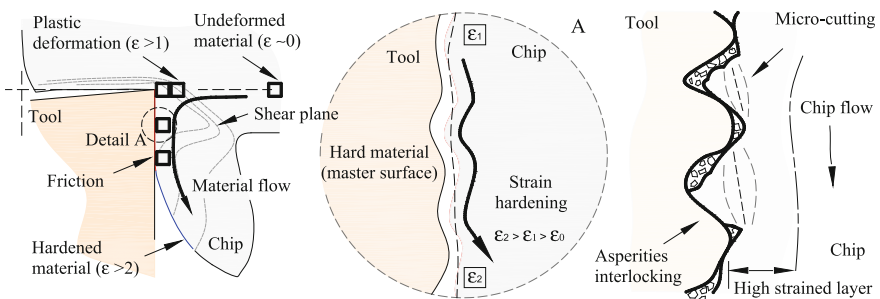


Fig. 2.17 Schematic illustration of the material strain hardening process from the annealed material within the uncut chip thickness to the very strained layer in the underside of the chip showing the interaction between surface asperities of the material (soft) and rake surface of the tool (hard)

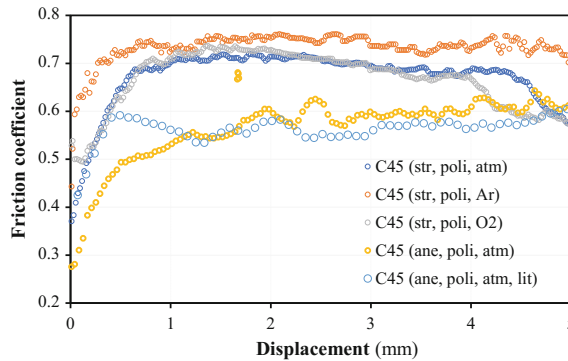


Fig. 2.18 Evolution of the friction coefficient of AISI 1045 as a function of the cutting tool displacement for two different hardness conditions (“ane” as supplied with 185 HV and “str” strained with 269 HV). Cutting tests have been carried out under different tribological conditions (surrounding medium: argon, oxygen, room ambient and atmosphere with a lubricated clearance surface with lithium grease). Other testing conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

(ii) strained material by uniaxial compression to 1.25, increasing material hardness to 269 HV. Two different friction coefficient values have been found for the stock condition ($\mu = 0.55$) and the hardened material ($\mu = 0.72$). These tests have been verified under different metal cutting conditions (surrounding medium) with similar conclusions.

The above discussion was extended to the easy-to-anneal materials of the present investigation. Figure 2.19 shows the correlation between the friction values of annealed condition and strained condition. AISI 1045 was included for reference. Two different results can be observed: (i) material hardness presents significant influence on friction coefficient or (ii) no influence has been found and friction remains almost constant (points on the symmetry line, red dashed line). Engineering materials (AISI 1045, AA 1085 and AA 1050) seem to exhibit a significant sensitivity to material hardness, while friction coefficient value of pure metals remains almost constant. These tests have been verified under different metal cutting conditions with similar conclusions. These results are not per se an evident conclusion, but this may indicate that adhesion is the dominant contribution for the very soft pure material, while mechanical asperities interaction (penetration of the hard asperities on the soft material) depends most on the work material strength. These competing contributions make it difficult to understand the influence of the hardening mechanism on the coefficient of friction. This conclusion is in agreement with the low correlation that is generally observed between the numerical modelling and the experimentation of the ring test used to quantify friction in metal forming. Some solutions have been published showing the way in which hard asperities deform the surface of a softer metal in the presence of interfacial adhesion and these provide a very promising beginning for interfacial plasticity solution. However, since they do

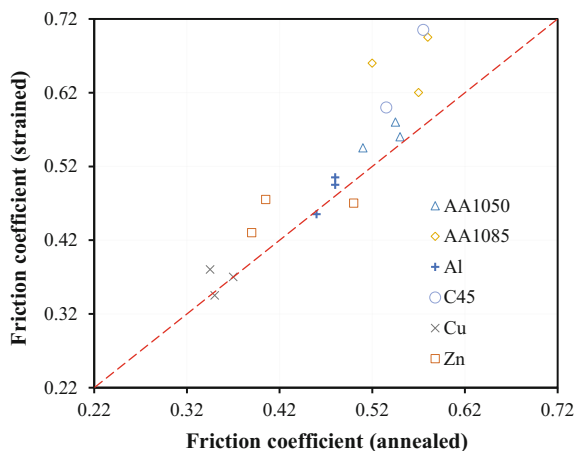


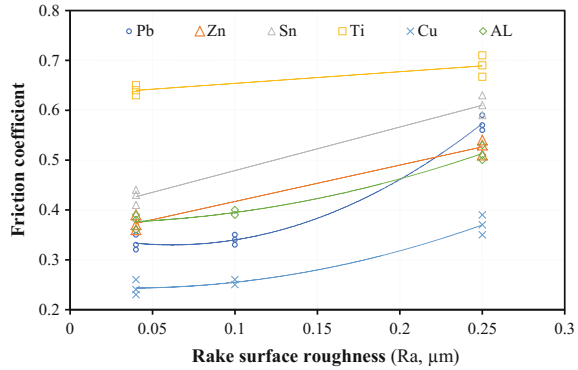
Fig. 2.19 Comparison of friction coefficient values obtained from annealed and strain hardened conditions for different materials (Al, Cu, Zn, AA 1085, AA 1050 and AISI 1045). Cutting tests have been carried out under different tribological conditions (surrounding medium: argon, oxygen and room ambient). Other test conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

not take into account the work hardening produced by the process itself, the numerical results are of limited applicability [7].

The surface roughness of the cutting tool and its ability to maintain the superficial microgeometry during the mechanical interaction of asperities is a major parameter in metal cutting tribology. In what follows, it can be assumed that tool asperities were found to be sufficiently rigid and remained unchanged during all metal cutting due to high elastic modulus of the WC–Co in the range 600–700 GPa (when compared with 90–150 GPa of titanium, copper and related alloys, 60–80 GPa of aluminium and related alloys, or 200–220 GPa of steel alloys). In the same vein, the WC–Co has an extreme yield stress of more than 4 GPa (when compared with 500–900 MPa of titanium, 300–500 MPa of copper and related alloys, 50–500 MPa of aluminium and related alloys, or 300–700 MPa of steel alloys). Therefore, as tool roughness increases, more the hard asperities of rake surface can anchor and plough the soft surface of the chip promoting material hardness and a progressive increase of friction coefficient at the tool–chip contact interface.

Figure 2.20 shows the evolution of the friction coefficient acting in the contact interface as a function of surface roughness of the rake surface. As seen, surface roughness has a significant influence on friction coefficient. In some cases, an increase of up to 80% higher relatively to the polished rake surface results (from R_a 0.05 to $0.25 \mu\text{m}$) was observed. Figure 2.20 presents fitting lines to help readers identifying friction evolution for different materials, yet intermediate experimental measurements would be desirable for better describing the friction-roughness curve signature.

Fig. 2.20 Evolution of the friction coefficient as a function of average roughness of rake surface for different materials (Al, Cu, Zn, Sn, Ti and Pb). Testing conditions: WC-Co tool, $R_a = 0.01 \mu\text{m}$ polished clearance, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$, argon surrounding medium



In general, it is possible to infer from Fig. 2.20 that friction coefficient trends do not pass through the origin, having a positive friction intercept. This intercept is known experimentally and is usually explained away in terms of rubbing on the clearance face of the tool, but this unwanted geometric contribution can be minimized using sharp-angled tools and well-polished surfaces (both clearance and rake surfaces). Even in this condition, a positive interception can be found; therefore, experimental measurements of friction can be taken as an unequivocal indicator of adhesion ($\mu \sim \mu_{\text{adhesion}}$). As surface roughness of the cutting tool increases, a more pronounced interaction between the hard asperities of rake surface and the soft freshly formed underside of the chip is noted.

The deep ploughing and the extra material resistance (asperities strength) to sliding, caused by material hardening, raise the friction force and, therefore, the friction coefficient for rougher tools is larger than for smoother tools (Fig. 2.20). However, the hardening mechanism only becomes effective if the tips of the asperities are able to indent the work material. Titanium seems to be less sensitive to this mechanic possibly due to its higher mechanical resistance to penetration. The influence of roughness on the friction coefficient seems to be more effective on pure metals than on high-strength materials (e.g. engineering materials).

Above paragraphs illustrated the friction coefficient as a function of the material hardness, which in turn can be promoted by surface roughness of the cutting tool as chip slides over the rake surface and where intense plastic shear occurs on the chip surface. Thus, the choice of a friction constitutive model is crucial because of the need to ensure the transient conditions as the chip slides over rake surface and a progressive increase of friction coefficient occurs. This seems to be in accordance with what was observed by other researches in experiments with photoelastic tools that showed that the shear stress was constant over much of the region close to the cutting-edge, where the normal stress was very high but variable, indicating that Coulomb's law of sliding friction is not appropriate [8, 32, 62].

In friction, models should be clear, simple and enforceable. Nonetheless, they cannot ignore the contribution of plastic deformation by eliminating the influence of strain hardening, strain-rate sensitivity and temperature rise induced by energy

dissipation due to plasticity and friction itself. Thus, a friction model for metal cutting can be based on Coulomb's friction law, but a plastic deformation-dependent coefficient (e.g. based on the instantaneous material hardness, $HV_{\text{chip}}(\varepsilon, \dot{\varepsilon}, T)$) must be considered, as proposed in Eqs. (2.2) and (2.3). This new coefficient may depend on the stress-strain field distribution obtained from the numerical simulations. The friction force at the element node could continue to be calculated on the basis of a variable friction coefficient and contact pressure. Otherwise, a contact algorithm implementation for the numerical analysis of metal cutting would be quite complex. Yet, an absolute reference is needed and could be defined based on the friction coefficient of the annealed material (μ_{annealed}). The results obtained for AISI 1045 and Ti6Al4V allowed obtaining the following model parameters $K_{\text{C45}} = 0.0022$ and $K_{\text{Ti64}} = 0.0004$, respectively. Friction coefficient presents a slight evolution for AISI 1045, but remains almost constant for Ti6Al4V, similar to what has been observed for Ti.

$$F_{\text{Friction}} = \mu(HV_{\text{chip}}) \cdot F_{\text{Normal}} \quad (2.2)$$

$$\mu(HV_{\text{chip}}) = \mu_{\text{annealed}} + K \cdot (HV_{\text{annealed}} - HV_{\text{chip}}(\varepsilon, \dot{\varepsilon}, T)) \quad (2.3)$$

2.3.4 Oxidation Effects on the Friction Coefficient

The formation of non-oxidized metallic surfaces close to the cutting-edge and friction along the underside of the chip at the tool interface were discussed in previous sections. Yet, it is not clear how and where the oxidation process of the chemical active surface occurs, neither how the formation of new oxides can affect friction. This chemical active surface, after detaching from rake surface, is exposed to oxidizing environment of the room ambient or the metal cutting fluid.

Metal cutting fluids are used due a variety of reasons such as (i) lubricating the contact interface for reducing friction and increasing tool life, (ii) cooling the workpiece inhibiting metallurgical changes, thermal deformation and enabling part handling, (iii) improving surface finish and corrosion protection of the machined surface, and (iv) flushing chips away from the cutting zone. Despite the fact that there is generally consensus in the metal cutting community on this issue, it may not be obvious on how cutting fluids influences friction. As previously mentioned, material flowing over the rake surface under extreme contact pressure and temperature promotes an effective removal of various forms of contamination (lubricant, oxides debris, etc.). Not only it is effective to remove contaminants, but also prevents new contamination since just-formed chip moves from inner edge to the point where chip detaches from the tool. Thus, it will become virtually impossible to lubricate the contact interface between chip and tool. In fact, several important key questions are still lacking a proper answer: What is the role of cutting fluid in the tribology of metal cutting? Is the choice of the lubricant important? Is its action

only physical or is there a chemical component as well? How does this affect the friction coefficient in the contact interface between the chip and the cutting tool?

The formation of new oxides at the detach point where chip leaves the rake surface is a complex chemical process involving the work material, cutting tool and surrounding medium. This section has no intention to analyse this complex chemical process but rather its consequence on the friction coefficient value. Unfortunately, commonly used engineering materials and cutting tools are unable to provide a clear result spectrum, due to combined reactions between the allowing elements and the surrounding medium. Surface topography will also enhance the oxides formation to spread into a large zone out of the real contact area. Pure metals and very polished acute tools were then chosen for this analysis due to the need of ensuring slim zones of chemical interaction between the involved materials, and reducing the variety of the reaction products (oxides types and other products).

Restricted varieties of reaction products may be expected when a pure metal and a gas shield are employed; thus, friction sensitivity to gas surrounding is also expected to show a clear evolution. Experimental tests were conducted changing oxygen concentration in the surrounding medium, where 0% means an inert gas shield of argon (Ar) and 100% means an active gas shield of oxygen (O_2), both from certified gas cylinders (purity level higher than 99.99% in volume). Yet, for 21% concentration of oxygen, tests were conducted at room ambient (RA) under uncontrolled environment with approximately 78% nitrogen, 0.9% argon, 0.04% carbon dioxide and small amounts of other gases. Air also contains a variable amount of water vapour, on average around 1% at sea level. This last condition, RA, is often used in metal cutting experimentation.

As chip detaches from rake surface, the freshly and chemically active cut surface of the chip is thus exposed to the surrounding environment. Gaseous surrounding medium can be of two types: (i) active or (ii) inert gas shield. In the case of inert gas shields, such as those involving argon (Ar) or nitrogen (N), oxidation of the chip takes place only after material removal process and friction is not disturbed. However, when the removal of the material occurs in the presence of oxygen, it results in the formation of oxide films at the underside of the chip. This chemical reaction occurs under pressure, yet less significant than that near the cutting-edge, promoting formation of cold pressure-welded spots in the tip of asperities (Fig. 2.21). Both Fig. 2.21b, c show evidences of pressure-welded spots over the tool rake surface whose position density increases with the oxygen concentration. Thus, these punctual oxide films are smeared over the rake surface of the cutting tool having two commonly combined contributions to the friction mechanism: (i) chip and tool joined together by several welded junctions and (ii) oxide-welded spots left over the rake surface that promote an artificial surface roughness of the cutting tool. Oxide formation and deposition occurs not only over the rake surface, but they can be stacked on top of each other and the thickness of this artificial deposit must be taken into consideration due to significant impact on the friction in the contact interface between chip and tool.

Experimental tests have been carried out to check the influence of oxygen concentration (0, 21 and 100% in volume) on the friction coefficient. Figure 2.22a

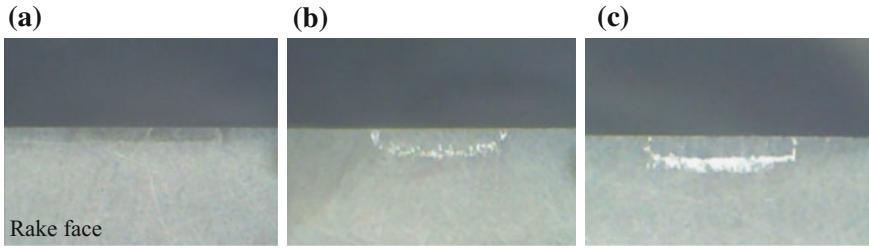


Fig. 2.21 Cutting under inert and active gas shields showing the pressure-welded spots onto the rake surface. Photographs show the rake surface after cutting Sn under three different surrounding medium conditions: **a** argon; **b** room ambient; **c** oxygen

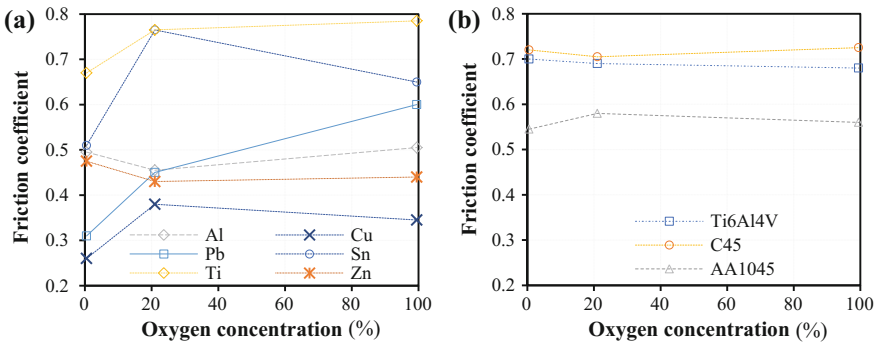


Fig. 2.22 Friction coefficient as a function of oxygen concentration in surrounding medium from for several materials: **a** pure metals (Al, Cu, Pb, Sn, Ti and Zn); **b** engineering materials (Ti6Al4V, AISI 1045 and AA 1050). Testing conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

shows that surrounding medium has some effect on the friction for several pure metals. These measurements have been done during metal cutting steady state indicating that the chemical changes in the exposed metallic surface persist during chip flow and may affect friction coefficient. In the leftmost region of the graphics, the basic source of friction is adhesion because surface roughness is very small, and the inert characteristics of argon avoid the formation of surface films, allowing the chip to slide smoothly along the rake surface. These reference values (adhesion of the material pair) help to assess the influence of oxygen concentration by comparison with the rightmost region of the graphic. Similar findings for engineering materials are presented in Fig. 2.22b. Yet, pure metals seem to be more sensitive to oxygen concentration than the engineering metallic materials. These results are in agreement with Gane et al. [24].

Figure 2.22a shows two different friction signatures: (i) friction coefficient increases and (ii) friction coefficient decreases with the oxygen concentration, depending on the test material. This can be attributed to chemical reactions of the

oxygen with the pure metal to form oxide films on the underside surface of the chip. In fact, chip formation mechanics is very sensitive to small changes in the tribological condition due to its unconfined plastic deformation nature. As discussed above, in the previous section, the contact length and chip curl increase progressively from the transient beginning of cutting to the steady-state regime of chip flow. Thus, steady-state conditions can only be attained when an energy compromise between the plastic work and the amount of frictional work is reached. As friction grows, it is necessary to maintain a stable equilibrium by redefining the cutting geometry. Frictional work is directly proportional to the contact length and, therefore, needs to be compensated by the plastic work associated with the plastic curling of the chip. In the event of both contact length and chip curling radius continuously increase with friction coefficient, spreading oxides over the entire the rake surface, equilibrium state will not be reached, and chips continue to pile up over the rake surface to failure.

The chemical affinity between the metal element and the oxygen controls the direction of influence of the oxygen concentration on the coefficient of friction. For example, in the case of Cu, the oxygen concentration in the cutting fluid promotes an increase of the friction coefficient, which seems to indicate that the cutting of copper alloys using, e.g., soluble oils (typically 95% of water and 5% of oil) rich in oxygen, seems inappropriate. The use of straight oils may be preferable.

As seen in Fig. 2.23, a dry cutting of Sn under an inert gas atmosphere allows chip to curl naturally whereas the exposure to an oxygen-rich atmosphere causes the chip curl radius to become significantly larger (Fig. 2.23b). As seen, dry cutting of Sn under an inert gas medium may allow a friction coefficient reduction down to 30% lower than measured in the presence of room ambient (air) or under an oxygen-rich atmosphere. Similar trends for Ti and Cu were observed. The reaction products formed between chip and tool at the detach point act as pressure-welded spots (cold welding) opposing relative sliding movement, independently of the contact pressure. As a consequence, these junctions formed by localized pressure welding at the asperities should be sheared to allow the tool to move. Separation along a fracture path through the soft work material becomes more favourable from an energetic point of view. Thus, well visible oxides films that are almost impossible to remove with a dull object form on the rake face and promote an artificial roughness over the polished rake surface. The interlocking between these oxide deposits and the underside chip asperities gives rise to higher values of friction limiting the chip to curl away from the rake surface face, thereby increasing its overall contact length and spreading oxide deposits throughout a larger area in the case of cutting under an oxygen-rich atmosphere.

In fact, the exposure to 100% oxygen atmosphere causes the Sn chips to flow parallel over the rake surface. This secondary rotation effect that opposes natural curling of the chip is presented in Fig. 2.23b. The total friction force to the relative sliding movement between the chip and the cutting tool is a result not only of the traditional metal forming tribology but has also of an additional contribution from the shear force to break the weld spots and to surpass pure interlocking with the artificial oxide asperities.

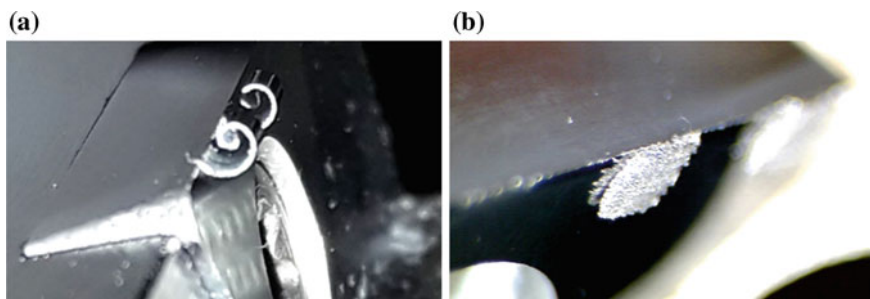


Fig. 2.23 Cutting under inert and active gas shields showing its influence on chip curl. Photographs show the chip curl for the Sn under different gas shield conditions: **a** inert gas shield of argon; **b** active gas shield of oxygen. Test conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished surfaces, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c = 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

Despite the tribological mechanics set out in the previous section, there are other materials where friction increases in the absence of oxygen, such as Zn and Al. It can be speculated that the formation of some reaction products can have an action similar to that of solid lubricants allowing reducing the coefficient of friction. For this, the mechanical strength of the reaction products should be lower than the base material and have lower affinity with the tool. This physical–chemical characteristic should be stable under the typical contact pressure and temperature of metal cutting processes. There are similar successful applications of this type of lubrication in dry machining processes. For example, low carbon ferrous alloys tend to adhere to the tool cutting-edge and rake surface, causing built-up edge (BUE) formation, component surface damage, vibrations and chattering, high cutting forces, excessive tool damage, increased friction at the rake face and further temperature build-up [48]. To prevent this, steel may be alloyed with lead to reduce friction, such as in Ck45Pb [34]. This lubricating effect can be leveraged if the reaction products at the contact interface between the chip and the tool allow reducing the real contact area.

Engineering materials are alloyed with various elements to improve physical properties and to produce special properties such as corrosion or heat resistance. The interrelationships between alloying composition, phase stability, microstructure, mechanical properties, among others, could have an undesirable influence over friction. Experimental tests have been carried out to check the influence of the allowing elements percentage (Al 99.999 wt%, AA 1085 with 99.85 wt% and AA 1050 with 99.5 wt%) on the friction coefficient. Results, presented in Fig. 2.24, show that pure aluminium presents a minimum of friction from all the tested conditions (different material hardness and different gas shield combinations). Strained condition presents an average increase in friction of about 6%, yet AA 1085 have presented a consistent but anomalous growth of about 17%. Friction coefficient seems to be more sensitive than the flow stress (please see Fig. 2.6a) to changes in the alloying elements of aluminium, even for small quantities.

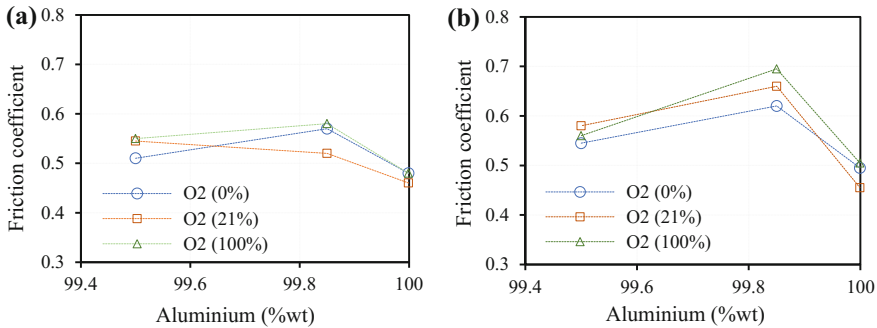


Fig. 2.24 Friction coefficient as a function of aluminium allowing (Al 99.999 wt%, AA 1085 with 99.85 wt% and AA 1050 with 99.5 wt%) under different oxygen concentration in surrounding medium: **a** annealed condition and **b** strained condition to 1.25 effective strain. Test conditions: WC-Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$

Titanium can be alloyed with various elements to alter its mechanical properties, such as strength, formability, fracture toughness and fatigue. One of the most commonly used titanium alloys in metal cutting applications is the Ti6Al4V, which is mainly a combination of titanium, aluminium and vanadium. The influence of above elements is well documented in the literature showing that solid-state phase transformations are main factors determining the functional characteristics of this titanium alloy. However, the effect of alloying elements on the tribological properties is still not well understood, yet it is of great importance for improving products performance and materials processing. Friction comparative tests between Ti6Al4V, Ti and Al have been made under different oxygen concentrations (Fig. 2.25). The pure metals present divergent results, with Al showing lowest friction values and Ti showing extremely high values. In line with the previous results, the friction coefficient for Al tends to increase while for Ti tends to decrease with oxygen concentration. The titanium alloy Ti6Al4V presents an intermediate friction coefficient closest to that of Titanium with neglected influence of oxygen concentration, in agreement with the values measured by Smolenicki et al. [54]. These preliminary findings seem to show that increasing additions of Al to the titanium alloys improve its resistance to oxidation. The effects of the alloying elements to friction in the contact interface between chip and tool has still not been well understood.

The extremely high friction coefficient led the present research to compare orthogonal tribological tests with additional tests using a plate-on-tool apparatus (Fig. 2.26a). Friction coefficient values observed in the plate-on-tool are significantly lower (50%) than those measured in orthogonal cutting tests. This divergence arises since friction coefficient was measured after oxidation of the plate (changes in the surface composition) that persists during wear of the material, even in an inert surrounding medium. Moreover, sliding alone would not be enough to sweep up the

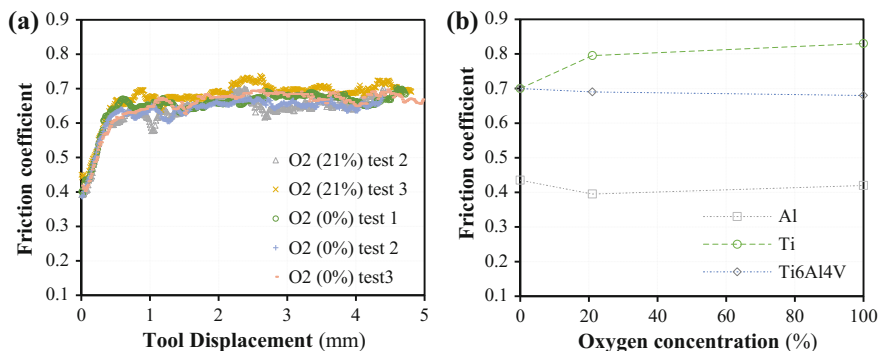


Fig. 2.25 Friction coefficient of Ti6Al4V under different oxygen concentration in surrounding medium: **a** evolution of the friction coefficient as a function of cutting tool displacement; **b** comparative orthogonal tests between Ti6Al4V and pure metals (Ti and Al), measurements taken at steady-state conditions. Test conditions: WC–Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c < 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$, previously strained specimens to 1.25

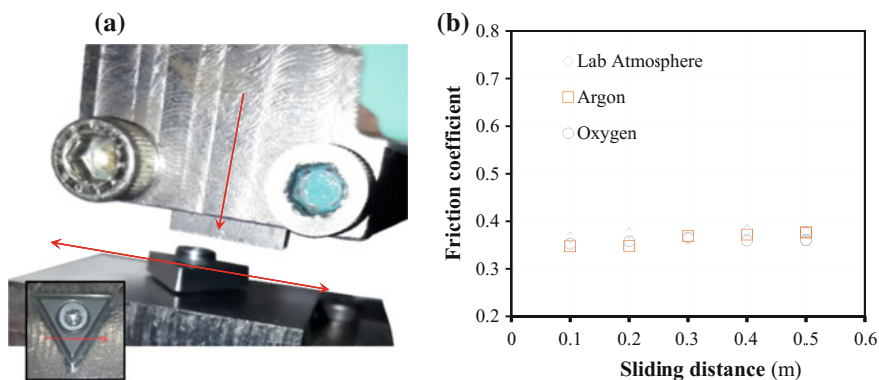


Fig. 2.26 Plate-on-tool tests to assess friction coefficient of Ti6Al4V: **a** experimental apparatus showing how normal load is applied over the tool surface (estimate contact pressure of 200 MPa) while relative sliding is attained by oscillatory movements using the linear actuator (red arrow depicts testing zone over the cutting insert); **b** friction coefficient as a function of relative sliding distance between the Ti6Al4V plate and the cutting tool under different tribological conditions (surrounding medium: argon, oxygen and room ambient). Test conditions: WC–Co, stock condition tool, $V_c = 500 \text{ mm/min}$ (sliding velocity), specimens previously strained to 1.25

various forms of contamination (lubricant, oxides debris, etc.), opposing to what a newly formed surface does in metal cutting. However, a detailed analysis of the Fig. 2.25a shows that transient friction coefficient passes through the origin with positive friction intercept about 0.35. This transient value occurs at specific tribological conditions similar to metal forming, as explained above. Friction coefficient of Ti6Al4V presents a similar value when it is assessed with the pin-on-plate

tribometer (Fig. 2.26b). Indeed, pin-on-disc tribometers do not allow replicating real metal cutting conditions at steady-state regime.

2.3.5 Transient to Steady-State Friction

The friction coefficient of a material pair under metal cutting conditions is different from that under metal forming conditions, requiring more time until achieving steady-state conditions. Plastic deformation and fracture mechanics play an important role for setting the tribological conditions, as well as surface oxidation. However, oxide films and pressure-welded spots mechanism do not occur at a specific moment, and they also require a transition to steady-state regime. Figure 2.27 presents a schematic spread of the pressure-welded spots onto the rake surface showing the formation of a thick oxide films promoting an artificial surface roughness. Photograph showing evidence of the oxide films on the rake face of the cutting tool when cutting in the presence of an oxygen gas shield can be seen in Fig. 2.21, as observed by other [3, 20, 67]. This process results in an overlapped deposition of welded spots and formation of a kind of artificial chip-breaker.

Assessing the coefficient of friction obtained at the begging cutting against that acquired in steady-state regime allows to conclude that tribological conditions, performed in dry friction conditions, with adequate control of surface morphology and under a controlled gas shield, present a transient regime after chip detachment

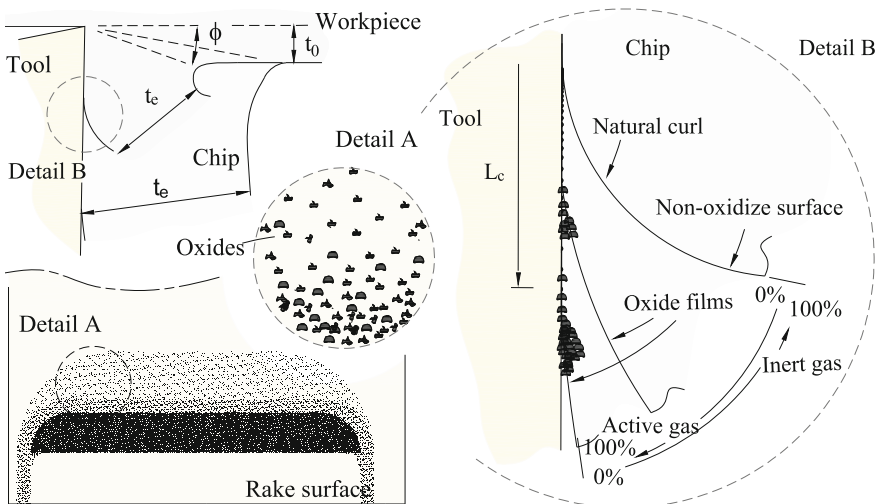
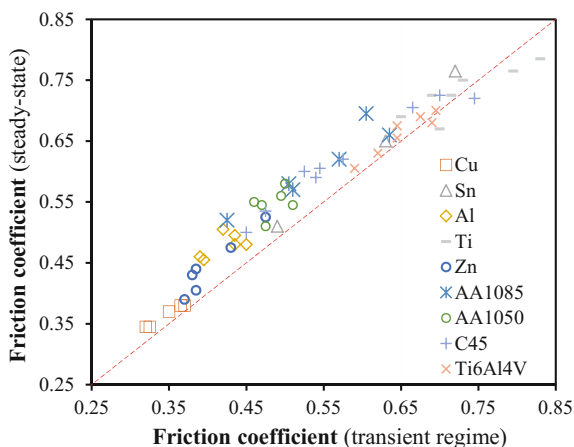


Fig. 2.27 Schematic representation of the oxide films that are formed on the freshly cut surface of the chip and are pressure welded onto the rake surface of the cutting tool in the presence of oxygen. Detail (A) shows the dispersion of welded spots over rake surface and (B) its accumulation as a function of the oxygen concentration

Fig. 2.28 Correlation between the friction coefficient values obtained at transient and that obtained at steady-state conditions, for all test materials under different surrounding medium (argon, oxygen and room ambient). Test conditions: WC-Co tool, $R_a = 0.01 \mu\text{m}$ polished tool, $\alpha = 0^\circ$, $\sigma = 7^\circ$, $r_c = 1 \mu\text{m}$, $t_0 = 0.025 \text{ mm}$, $w = 2 \text{ mm}$, $V_c = 500 \text{ mm/min}$



from rake surface until chip curling stabilize. Figure 2.28 shows that friction coefficient presents a transient regime for all test materials since friction at steady-state conditions crosses the symmetry line (red dashed line). Typical overall increase range is between 5 and 15%. These results have been verified under different metal cutting conditions with similar conclusions. This is because the underside surface of the chip continues to be subjected to compression causing a progressive pile-up mechanism and contact length to further increase. This oxide film deposition progressively forms an artificial chip-breaker until a steady-state regime is attained. In some cases, debris may also be produced from microcutting in the asperity level (tips of the asperities) forming a metallic layer covered by adsorbed contaminants. Yet, when steady-state regime cannot be reached, the exaggerated increase of the built-up chip-breaker eventually breaks off and is removed with the chip.

2.4 Conclusions

This study provides an experimental investigation of metal cutting tribology in the context of the formation of new metallic surfaces at the tip of the tool and examines three main questions in relation to (i) non-oxidized metallic surfaces sliding over the cutting tool, (ii) strain hardening during material flow and (iii) complex chemical oxidation at the point where chip detaches from rake surface; and how all these phenomena will affect friction coefficient. This experimental investigation of individual shifts in cutting parameters examines the question whether there is a significant, observable shift in the friction coefficient under oxygen-rich surrounding medium, without the undesired contribution of cutting tool flank in the thrust force. This involved the development of a specially designed apparatus to cover the specific needs of the present research. The focus is on revealing the

relation between the chip formation mechanism and the oxygen concentration on the transient evolution of the friction coefficient.

Experiments were made to determine the influence of oxidation mechanism on friction by performing orthogonal metal cutting tests under active and inert gas shields. The experimental results show that cutting in the presence of oxygen leads to significant variations in the friction coefficient and cutting geometry, which reveals that the surface films formed on a freshly cut surface have a major influence on the metal cutting tribology. The friction coefficient may increase or reduce due to oxide contribution depending on the work material and gas shield combination. The proposed concept for explaining this influence is based on the formation of new chemical active surfaces and its interaction with the rake surface to allow a better understanding of the metal cutting tribology. The inclusion of the pressure-welded spot helps clarify why cutting in the presence of different oxygen concentrations influences the process performance. The experimental observations show the friction coefficient as a function of the process parameters, varying as the chip slides on the rake surface, not as a constant value. Friction plays a much more important role in the free plastic flow of metal cutting where friction coefficient is higher (from 0.3 to 1, even higher) than in metal forming processes (typically ranging from 0.1 to 0.4).

The overall results deviate from classical analysis of metal forming tribology to focus on subjects specifically related to metal cutting fundamentals that arise from the interaction between cutting fluid, tool roughness and chemical active surfaces. The evaluation of friction in metal forming can be performed on a plate-on-tool test (pin-on-disc variant); however, when compared with metal cutting tribological tests, the friction coefficient values are completely different. The main reason for the low correlation between the tribological techniques seems to be related to different characteristics of the contact surface, mainly by the presence of oxide films in the contact interface.

Further work should be concentrated on establishing a theoretical basis for new tribological constitutive models due to the non-oxidized metal surfaces and the oxidation mechanism contributions to the chip forming mechanism. In fact, it is well known that traditional finite element programs for numerical simulation of forming and cutting processes do not provide friction models that consider the oxidation phenomenon. Furthermore, higher friction values reduce numerical stability in the numerical solution of differential equations. Sometimes, the friction value is truncated to allow a convergent numerical solution and general numerical stability for metal cutting simulation. It is expected that the incorporation of the oxidation mechanism in the numerical models and the development of new tribological techniques for the macroscopic calibration of the friction coefficient of truly metallic surfaces and the shear stress of the pressure-welded spots should allow to model metal cutting more accurately.

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Chapter 3

Geometric Accuracy of Machine Tools



Michal Holub

Abstract Geometric accuracy belongs to one of the main parameters of the machine tool. It serves not only for the purpose of machine delivery to the customer or for demonstration of proper service intervention but also for assessment of improvements compared to the current state. The information on geometric accuracy of the machine is also used as a feedback to the machine development. Therefore, increasing the geometric accuracy is a feature of the machine that needs to be constantly improved. This chapter presents geometric accuracy tests, instrumentation and examples of selected procedures to increase the geometric accuracy of CNC machine tools.

3.1 Types of Accuracy of Machine Tool

With the accuracy of machine tools, everyone can imagine several partial properties of the machine. Accuracy will be different from the constructor's and the metrologist's point of view.

From the point of view of metrology, accuracy describes how close the measurement result is to the true value of the quantity [1]. In the field of machine tools, we can talk about several types of accuracy, while determining accuracy is only qualitative (low, middle, high). This is geometric, working and production accuracy. Each of these accuracies has its justification.

Geometric accuracy of the machine tool is related to the quality of the construction, the production of individual components and the assembly of the machine tool.

Working accuracy is based on the machine quality assessment with respect to test modes in the machine operating mode (machining under defined conditions).

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Production accuracy in relation to the entire production process as early as at the customer and to a long-term stability of geometric and working accuracy.

Each machine tool delivered by the manufacturer, whether new or overhauled, is subjected to testing of the properties. The tests are carried out according to prescribed standards and procedures [2].

For a machine tool, it can be stated that the machine has low, middle or high accuracy. This assessment is usually based on the values measured on the workpieces. For example, for workpieces with a tolerance of more than 0.04 mm, it cannot be said that the machine has a low, middle or high accuracy. Table 3.1 gives an overview of the tolerances achieved on the workpiece by machine size. For small- and medium-sized machines, this means that the workpiece tolerance of 0.04 mm corresponds to the middle accuracy. With a large CNC machine, it is a high accuracy. Therefore, it is necessary to distinguish between the machine size class we refer to, but also the respective time period. The evolution of machine accuracy was predicted by Taniguchi as early as in 1983 [3]. For milling, the value of 5 μm was predicted for the year 2000 and the value of 1 μm for grinding technology.

Table 3.1 shows the status as of 2016 and represents mainly the values for milling technology.

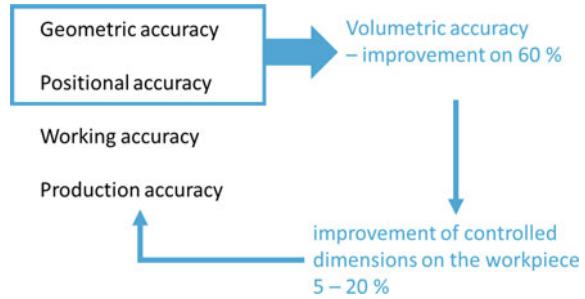
For the end-user of the machine, the parameter of production accuracy has a high reporting value. This is the parameter that has a direct link to the workpiece or the production spectrum of workpieces.

When handing a new machine to the customer, an important parameter is the working accuracy of the machine assessed on the particular workpiece, most often on the customer's workpiece. The working mode (machining) takes place under defined conditions, including:

Table 3.1 Degree of production accuracy by machine size [4]

	Travel in the longest axis (mm)	Weight of workpiece (kg)	Tolerance achieved on the workpiece (mm)	Degree of production accuracy
Small CNC machine tool	<999	<999	>0.05	Low accuracy
			0.01	Middle accuracy
			0.001	High accuracy
Medium-sized machine tool	1000–3999	1000–9999	>0.09	Low accuracy
			0.04	Middle accuracy
			0.006	High accuracy
Heavy machine tool	>4000	>10,000	>0.3	Low accuracy
			0.1	Middle accuracy
			0.04	High accuracy

Fig. 3.1 Effect of volumetric accuracy on production accuracy



- environment,
- cutting process,
- workpiece fixing tools,
- defined tools.

Geometric accuracy is used to assess the future functionality of the machine. Geometric accuracy can be used to simply verify the correctness of the production of the individual parts, their assembly and the final adjustment of the machine.

Figure 3.1 shows a system approach to increasing the production accuracy by means of geometric accuracy. An increase in production accuracy by 5–20% (relative to the workpiece tolerances) can be achieved by increasing the volumetric accuracy. In this case, 60% improvement in the machine volumetric accuracy was achieved. The size of the above-mentioned scatter of production accuracy is, in particular, due to the dimensional and geometric tolerance of the workpiece and to the size of the workspace where the machining process takes place.

3.1.1 Geometric Accuracy

Geometric accuracy is one of the parameters of machine tool that takes into account the quality of the machine production and assembly in the unloaded condition (no machining forces are considered). Under the geometric accuracy, we can imagine [5]:

- deviations of the shape and position of fixing surfaces for workpieces and tools,
- the relative position between the workpiece and the tool when defining the mutual adjustment of the individual parts of the machine which can be changed during the axis feed or when adjusting their positions.

Geometric accuracy, therefore, describes the geometric structure of the machine tool, based on which it is possible to evaluate the properties of the functional parts further influencing the working and production accuracy of the machine tool. Geometric accuracy tests are described in ISO 230 which states that the tests are carried out on the unloaded machine or under finishing conditions of machining.

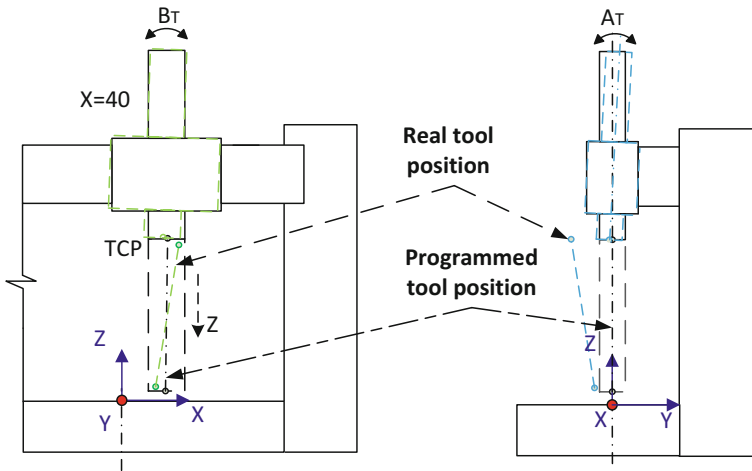


Fig. 3.2 Evaluation of ram travel error [6]

The foundations for these tests were provided as early as in 1927 by Professor Georg Schlesinger [Prüfbuch für Werkzeugmaschinen: die Arbeitsgenauigkeit der Werkzeugmaschinen]. These are the tests performed by conventional gauges, including control rulers, control bars, dial gauges, collimators, spirit levels. The procedures proposed by Professor Schlessinger have been used so far.

Figure 3.2 shows a schematic view of the ram travel of a vertical lathe. This test is performed with the laser tracker and the aim of this test is to obtain the information on the direction and magnitude of the TCP deviation relative to the workpiece fixed on the table top. Using self-aligning tracking devices is an effective solution, both in the field of machine tools and workpiece inspection.

Due to the uncertainty of laser trackers measurement, it is currently the field of large machine tools and large workpieces.

3.1.2 Positional Accuracy

This parameter describes the accuracy and repeatability of the positioning travel of linear and rotary numerically controlled axes. “Determination of accuracy and repeatability of positioning numerically controlled axes” is described in ISO 230-2/6 (ISO 230-2 Test code for machine tools—Determination of accuracy and repeatability of positioning numerically controlled axes; ISO 230-6 Test code for machine tools—Determination of positioning accuracy on body and face diagonals), but the directive VDI/DGQ 3441 is also often used. The procedure for measuring and evaluating the positioning test is described in detail in the publications and relevant standards [2, 7, 8].

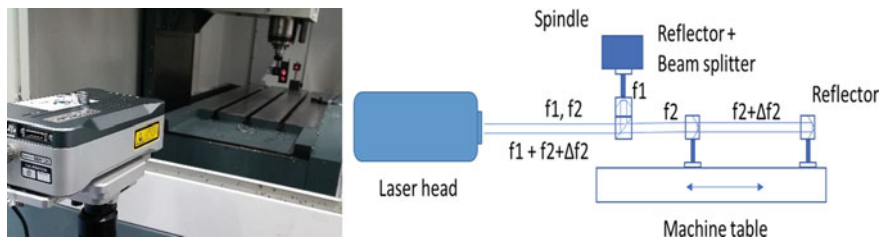


Fig. 3.3 Application of laser interferometer—left, principle of laser interferometer—right

Figure 3.3 shows a sample and scheme of measurement using the RENISHAW laser interferometer XL80. A subsequent evaluation according to ISO 230-2 of the linear axis with the three-axis machining centre is shown in Fig. 3.13. The test parameters considered include:

- bidirectional positioning error of axis *A*,
- bidirectional positioning repeatability of axis *R*,
- mean reversal error of axis *B*,
- bidirectional systematic positioning error of axis *E*,
- mean bidirectional positioning error of axis *M*.

This test is standard for all linear and rotary controlled axes.

3.1.3 Volumetric Accuracy

Advanced and highly progressive methods include the assessment of volumetric accuracy and its subsequent compensation. The purpose of these advanced compensations is to minimize the deviation in the tool centre point (TCP) at any point of the measured machine space [9]. TCP volumetric deviation is defined as the sum of partial deviations in individual axes [10].

$$ve = \frac{\sum_i^n \sqrt{(x_{nom} - x_{meas})^2 + (y_{nom} - y_{meas})^2 + (z_{nom} - z_{meas})^2}}{n} \quad (3.1)$$

The volumetric error is described in Fig. 3.4. This is the difference between the TCP programmed position and orientation and the real TCP position and orientation. As can be seen, the deviation results from the imperfection of the mechanism in each axis and the mutual position of the two axes. Based on sufficient knowledge of individual errors, the resulting TCP error can be corrected.

For a three-axis machine, it is possible to describe 21 geometric errors of machine. This number is based on the number of degrees of freedom (DOF) of the axis in the space.

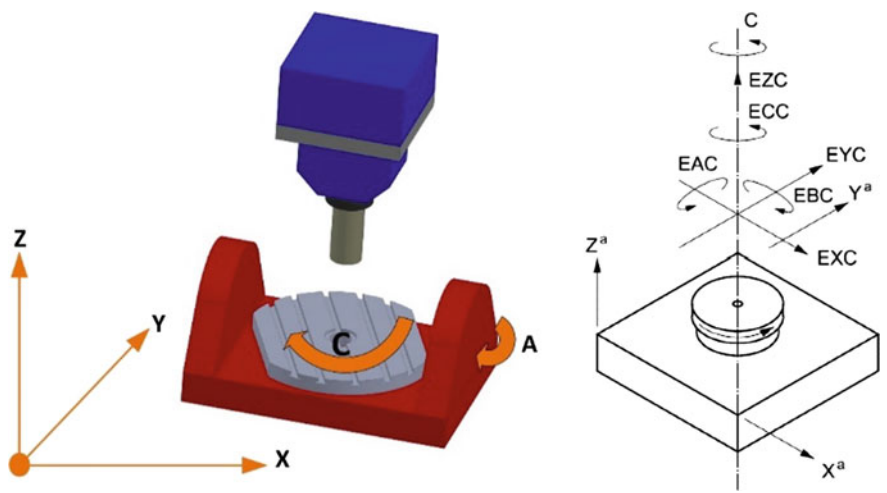


Fig. 3.6 Kinematic pair—Cradle [13], geometric C-axis errors [14]

Table 3.2 Sum of errors for five-axis machine tools

	Errors of axes	Positional errors	Total
Three linear axes	18	3	
Two rotary axes	12	10	
Total	30	13	43

- radial displacement (EXC, EYC, EYA, EZA),
 - inclination errors (EAC, EBC, EBA, ECA),
 - squareness errors (AOC, BOC, COC, AOA, BOA, COA),
 - positioning errors (XOC, YOC, YOA, ZOA).
- In total, errors can be assigned according to Table 3.2; these are errors assigned to linear axes, rotary axes, and also errors of axes and positioning errors of axes. Overall, there are 43 errors for five-axis kinematics.

3.1.4
Quasi-static Errors

Quasi-static errors are defined as errors in the relative position of the TCP and the workpiece; the errors vary slowly in time. These errors are directly related to the structure of the machine tool and can be broken down into geometric, kinematic and thermal errors. In the publication [15], quasi-static errors also include dynamic errors that arise very slowly in terms of the relative position of TCP and workpiece.

Geometric errors are affected by the production of individual components and constitute the machine structure as described in the introduction to this chapter. Kinematic errors are those depending on the direction of travel, the machine compliance, and include the thermal dilatations of the individual parts. According to Prof. Rameshe, a quasi-static error proportion is 60–70% of the total working accuracy of the machine [16]. This is mainly related to three-axis machining centres. Prof. Ibaraki has further expanded this estimate in the tests conducted at five-axis machining centres, claiming that the proportion of quasi-static errors is even higher, up to 80% [17].

3.1.5 Working Accuracy

It is the property of the machine tool that expresses the quality and productivity of the workpiece manufacture. Working accuracy is expressed by the production of a test workpiece or series of test workpieces.

The working accuracy of the machine is affected by the accuracy of the relative tool path:

- geometric accuracy of the machine,
- accuracy of tool positioning relative to the workpiece (positioning accuracy),
- machine resistance to elastic deformations (caused by cutting forces, workpiece weight, etc.),
- machine resistance to thermal dilatations (“temperature stability”),
- by selecting the cutting conditions, etc.

A total sum of the factors affecting the accuracy of the machine tool is shown in Fig. 3.7. The resulting error in the Cartesian coordinate system is shown according to Eq. (3.1) as a spatial error between the programmed and the real TCP position (Fig. 3.4).

3.1.6 Production Accuracy

Production accuracy describes the accuracy of the production process evaluated on the workpiece. Production accuracy is affected by geometric accuracy, positioning accuracy, working accuracy, errors caused by machine operators (poorly adjusted tool, poorly fixed workpiece) and by changes in environmental conditions. Direct information on the production accuracy is given by the fluctuations in the dimensions of the test workpieces during the production process.

The publication [19] presents three main factors affecting the machine tool and the production process that cause fluctuations in workpiece dimensions:

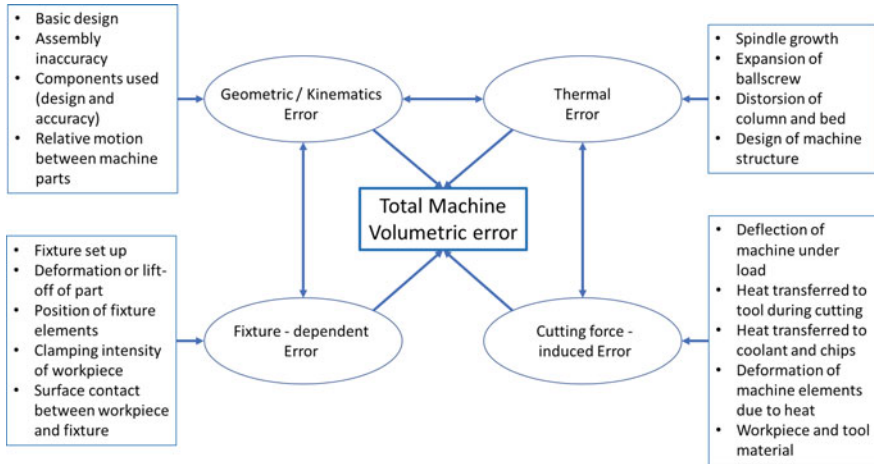


Fig. 3.7 Overview of the error budget in a machine tool and the factors affecting it [18]

- thermal effects,
- static stiffness of machine—tool—workpiece system,
- dynamic compliance of machine—tool—workpiece system.

The following effects can be more closely related to these three basic effects [19]:

- production technology 15%,
- working accuracy of the machine 25%,
- measurement 15%,
- environment 20%,
- machined part 5%,
- machine operator 20%.

3.2 Complex Assessment of the Geometric Accuracy of the Machine Tool

A diversity of the production process also requires a diverse approach to production machines both in their preparation and maintenance. The approach to the machine will primarily vary from the requirements placed on the workpiece or the workpiece series. For production with low or middle accuracy, it is sufficient to carry out machine maintenance as recommended by the manufacturer. In the case of high-precision production, which reaches the limits of machine accuracy (see Table 3.1), it is possible that both conforming and non-conforming products can be produced. It is then necessary to take an appropriate action. As a rule, it is a

machine that has been in production for a long time, and the first step concerns the machine set-up. The aim is to achieve such parameters that only the conforming products are manufactured by the machine. This intervention should ideally ensure a long-term production accuracy according to the requirements on the product. However, in the production halls, the surrounding conditions can very often change, which significantly contributes to the dimensional and shape accuracy of the product. In such cases, where the machine accuracy limit is reached, the machine undergoes a regular set-up before each piece being machined. As a rule, only those set-ups are proposed that directly affect the critical dimension of the workpiece. For example, one-dimensional tolerance can be used, in which case only one axis setting is carried out by testing the accuracy and repeatability of positioning. Such an approach is, of course, economically and time-consuming, and it is only approached for such orders where there is a high added value of production. Otherwise, this approach leads to high economic losses in terms of the order.

Complex and regular maintenance is meaningful for production machines where it is a key machine in production that cannot be shut down from day to day but careful planning of downtime is needed. These are machines that are irreplaceable in the production process, and in the event of an unexpected failure, production would have to go to cooperation. In the event of a contract delay, the manufacturer is in serious danger of being penalized.

Another type of production, where a regular inspection is planned, is for products with high added value. Here, in the case of production of a non-conforming dimension (part), there would be high financial losses.

Complex assessment of the machine geometric accuracy can also be represented by the cycle illustrated in Fig. 3.8. This cycle involves the steps of delivering the machine to the customer where the set-up operations of the machine are performed according to the delivery documentation.

Step 1

Within the mechanical setting of the machine (Fig. 3.8—Step 1), the following devices are used:

- measuring prisms,
- granite cubes,
- dial gauges,
- control bars,
- spirit levels,
- etc.

Step 2

In the second step, there is a series of machine axes set-ups where the following errors are inspected and subsequently compensated by software:

- positioning,
- linearity,
- angular errors,
- squareness.

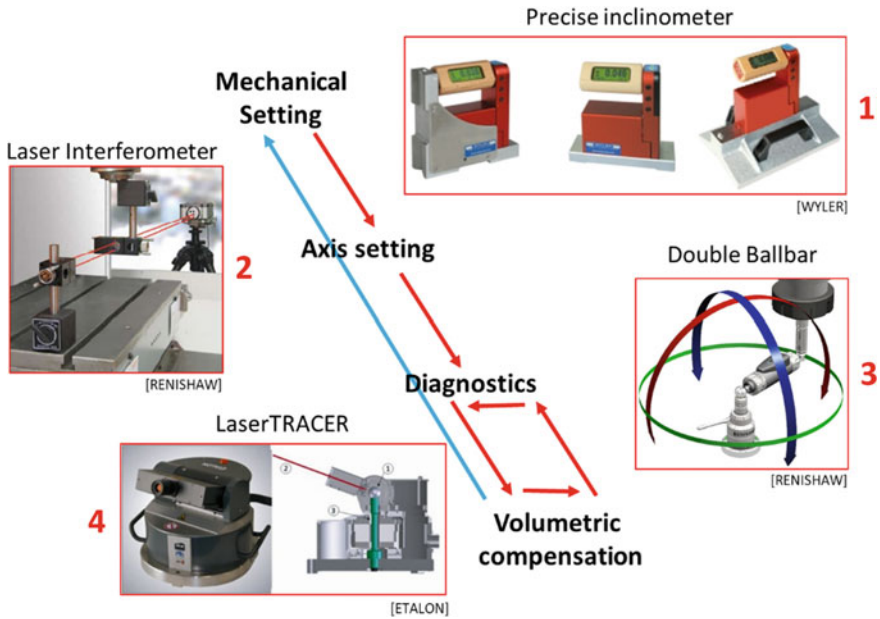


Fig. 3.8 Scheme of complex approach to setting of a machine tool

For these operations, the following equipment is most often used: Fig. 3.8—Step 2:

- laser interferometers,
- inclinometers,
- collimators.

Step 3

This step should be followed by machine diagnostics (Fig. 3.8—Step 3), which describes the condition of the machine in the commissioning phase. Due to this condition, it is subsequently possible to identify the causes of errors in the production of non-conforming parts due to the change in machine geometry. For this purpose, for example, tests based on the principle of circular interpolation and associated expert software are used. Based on the measured data, errors are then evaluated:

- squareness,
- linearity,
- backlash,
- reverse peaks,
- circularity,
- scale errors,
- etc.

For these operations, the following devices are used:

- Ballbar QC20-w (RENISHAW),
- KGM (HEIDENHAIN),
- ROTARY ANALYZER (IBS Precision Engineering).

Step 4

If machine parameters are below tolerances than standard, the so-called advanced methods are used. These methods typically require much more sophisticated measuring devices and highly qualified equipment handling. One of these methods is volumetric compensation (Fig. 3.8—Step 4).

For these advanced measurements, the following devices are used:

- LaserTRACER (ETALON-AG),
- Laser tracker (LEICA, API),
- Multi-beam laser interferometer XD5 (API),
- Multi-beam laser interferometer XM60 (RENISHAW).

The publication [20] presents the examples of measuring methods and examples of measuring techniques for increasing the accuracy of machine tools. As is evident from Fig. 3.9, the authors divided the compensations of geometric errors into two

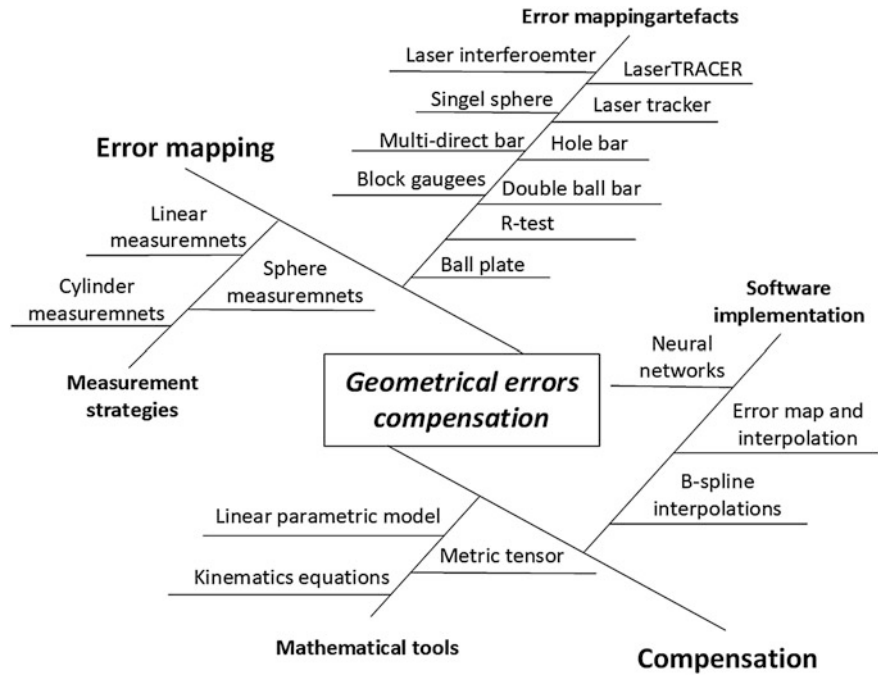


Fig. 3.9 State-of-the-art methods and techniques for increasing the accuracy of machine tools [20]

branches. The first branch is dedicated to error mapping, which is further broken down into equipment and measurement strategy. The second branch is focused on compensations and is further divided into software and mathematical tools. When dealing with the task of deployment of compensations on machine tools, it is necessary to approach this task according to the scheme described above in order to achieve the best results of the final compensation.

3.2.1 Measurement Strategy

However, using the most accurate measurement equipment does not guarantee the acquisition of the most accurate data on the machine behaviour; it is only an assumption. In order to achieve this, we also need to prepare a measurement strategy for the particular machine and the environment in which the machine is located. From the point of view of the error evaluation, we can divide the measurement methods, and therefore also the chosen strategy, into direct and indirect measurements. Direct methods determine the individual error parameters by always travelling in one axis. According to the metrological reference, it is possible to divide the methods into those checking the calibrated artefacts (granite rulers, block gauges), laser methods (laser interferometer) and gravitational methods (digital inclinometer). Indirect methods, on the other hand, track the resulting tool/workpiece path in multiple axes, and the individual error parameters are computed by mathematical modelling. Converting a real path into the model brings about a simplification that can cause misinterpretation of error components, but it allows significant reductions in needed measurements with the same information gain as well as a simultaneous acquisition of multiple error parameters. It was precisely the demand for data collection that prevented the direct methods from being used in measuring the volumetric accuracy of machine tools and its subsequent compensation [21].

Today's modern technologies cannot do without their own software for data processing. In general, new technologies bring about a higher accuracy, shorter preparation time, measurement and evaluation. Thanks to the software, it is possible to obtain more information from the data. Digitizing the methods and collaborating with the software allows for a unified measurement and evaluation process and thus the objectivity and comparability of the results. In some cases, increasing the functionality of measuring systems also raises the need for training of the operator. In addition, modern technologies are associated with higher costs of calibration, insurance or transport, which needs to be considered in advance [21].

In conclusion to this chapter, it is advisable to consider the economy of regular maintenance. As mentioned above, it is necessary to approach each type of production individually and to calculate the possible risk of manufacturing a non-conforming piece. On the model example, the basic price of a workpiece will be calculated. In this case, it will be a Pelton turbine machined at the five-axis machining centre DMGMORI DMU 210 P. The Pelton turbine has the final dimensions—diameter of 1024 mm $\times$ height of 340 mm and mass of 1050 kg and

is made of X3CrNiMo13-4 material. Machining time is approximately 312 h. For a clear idea, a basic calculation, which includes the machine, material and tool rate (author's estimate for 2018), was made:

- machine hourly rate, $MT = 100$ EUR/h,
- workpiece material, $M = 14$ EUR/kg,
- mass, $m = 2500$ kg,
- tools, $T = 18$ EUR/h,
- machining time, $t = 312$ h.

The workpiece price (WP) is then given by the sum of the individual items:

$$WP = MT \cdot t + M \cdot m + T \cdot t = 100 \cdot 312 + 14 \cdot 2500 + 18 \cdot 312 = 72,000 \text{ EUR} \quad (3.2)$$

The estimated workpiece price is € 72,000. It is with these types of production that we cannot afford to manufacture a non-conforming part. Non-conformity may not only be in geometry but also in surface quality and material defects. All these defects of the intended product can be prevented by means of preventative measures. In terms of geometrical accuracy, it is, for example, a regular inspection, e.g. with the ballbar diagnostic system. After fine-tuning on the machine, this time-consuming test based on the circular interpolation principle can be performed within 20 min. Such a period of time is already acceptable for many users. If these tests are carried out on a regular and long-term basis, the so-called knowledge base is created. On such knowledge, machine downtimes can be very efficiently planned, along with more complex interventions on the machine (volumetric compensation of machine, ball screw replacement, machine alignment, etc.).

3.3 A Case Study for Assessment of Geometrical Accuracy of MCV 754 QUIC Machine Tool

The case study is carried out on the three-axis vertical machining centre MCV 754QUICK (Fig. 3.10). The coordinate system of demonstrator corresponds to the kinematic chain W (Workpiece)- X - Y - Z - T (Tool) with machine workspace (WS) and reduced measured space (MS). Reduction of workspace WS is necessary due to the deployment of the measuring instrument LaserTRACER that, because of its dimensional and handling parameters, does not allow measurements of the entire workspace WS.

The convention of geometrical deviations is described in ISO 841. For the interpretation of the results and the possibility of implementing corrections in a machine tool, a uniform coordinate system must be kept. This coordinate system is based on three fundamental translational travels X , Y , Z ; with multi-axis machines, more translational travels, such as q , w , u , can be added. The traditional concept of



Fig. 3.10 Vertical machining centre MCV 754QUICK, KOVOSVIT MAS

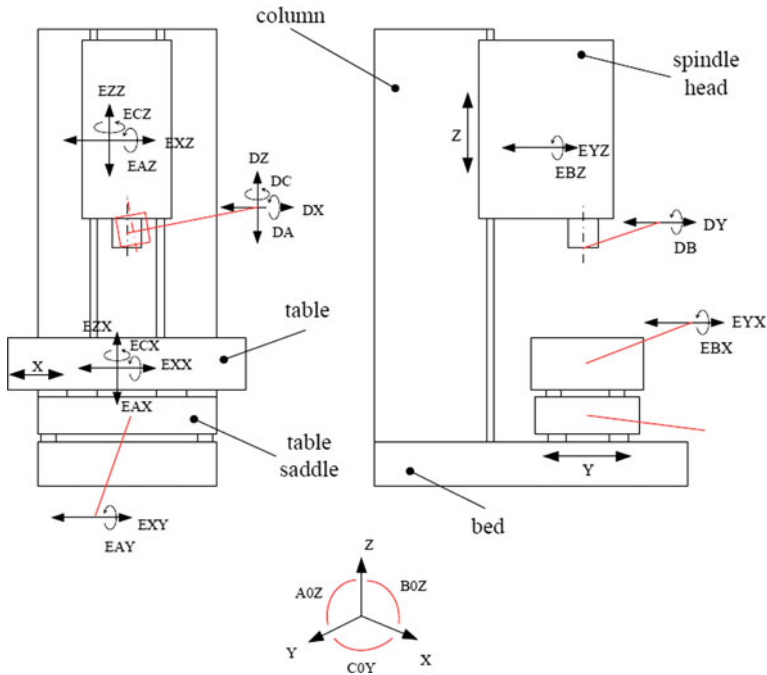


Fig. 3.11 Scheme of three-axis vertical milling machine MCV [22]

the five-axis machine is based on three translational and two rotary axes (e.g. A, C). The A-axis rotates around the X-axis, B-axis around Y, and C-axis around Z-axis.

Each translational and rotary axis can be assigned a deviation corresponding to six degrees of freedom. For example, with the X-axis, according to the convention, deviations are labelled as EXX, EYX, EZX, EAX, EBX, ECX. In addition,

it is necessary to consider a deviation of squareness between two axes—e.g. between X and Y —COY (Fig. 3.11).

Other errors that can be found in the resulting machine accuracy are the errors caused by the spindle shift and rotation (Fig. 3.11). Errors occur depending on the construction compliance and loading and also on the basis of thermal dilatations. Based on these shifts, we can obtain a spindle shift in X -axis (DX), Y -axis (DY) and Z -axis (DZ), a spindle tilt in XZ (DXZ) and YZ (DYZ) and spindle rotation in C (DC). The last DC rotation error can be excluded in the horizontal milling machine. This is a rotation error in the direction of the tool rotation. As spindle errors occur only under loading, they are no longer considered in geometric models of machine tools.

3.3.1 Digital Inclinometer

The first step is the mechanical setting of the machine, which is ensured by either the manufacturer or the supplier. For small machines, including the MCV 754QUICK, the machine is set up so that it is delivered to the intended destination in its entirety. Mechanical setting is then carried out, whereby the machine is gradually inclined on the basis of the measured values by adjusting the machine “levelling feet” until the values prescribed by the manufacturer are reached. In this example, WYLER’s BlueSYSTEM SIGMA digital inclinometers were used (Fig. 3.12). These digital inclinometers are highly effective in terms of their wireless connection to the software provided. This greatly reduces the measurement time and the possibility of transferring the errors from the operator to the processing of the results.

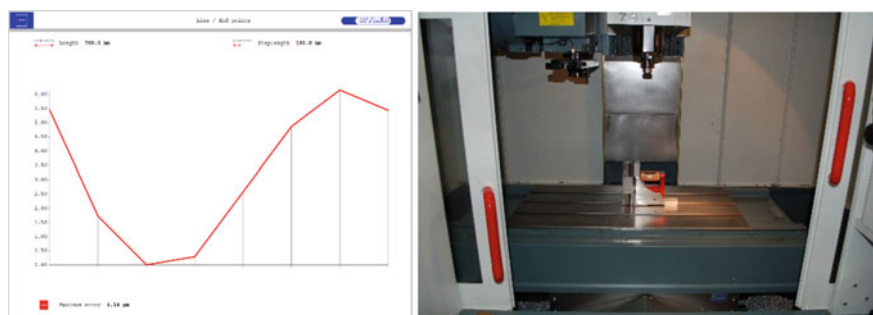


Fig. 3.12 Measurement with BlueSYSTEM SIGMA inclinometer

3.3.2 Laser Interferometer

The next step is the accuracy and repeatability test of machine positioning travel according to ISO 230-2 using the RENISHAW laser interferometer XL80. The aim of this test is to inspect and subsequently compensate each controlled CNC axis. This type of inspection is a necessary part of the acceptance of a new CNC machine tool, but it is also part of regular machine maintenance.

With the machine MCV754 QUICK, these are X, Y and Z axes that have been measured and evaluated according to ISO 230-2. The test setting was selected to evaluate the bilateral accuracy of the axes positioning. Here, an example of the setting is for the X-axis, which has a 750 mm axis start. The X-axis was divided into 11 measuring points at intervals of 75 mm. As can be seen from the graphs in Fig. 3.13, the original accuracy of travel to the position (A) of the X-axis was 23.5 μm , positioning repeatability (R) was 1.0 μm , and the systematic positioning error (E) was 23.0 μm . After performing the axis compensation, the value A equalled to 1.6 μm of positioning repeatability (R) 1.5 μm and the system positioning error (E) equalled to 1.2 μm .

For a closer examination of calibration and verification values, it is necessary to take into account the uncertainty of the measurement of the laser device, which is approximately 0.5 μm for the length of 750 mm. Thus, the measured values can be presented in the following format:

- calibration value A $23.5 \pm 0.5 \mu\text{m}$,
- verification value A $1.6 \pm 0.5 \mu\text{m}$.

Over the results, in particular of the verification measurement, further discussion can be conducted on the magnitude of the measured error, which may lie in the range of 1.1–2.1 μm . Despite the “big” error from the measurement uncertainty in relation to the measured value, it can be definitely stated that the verification value is highly satisfactory.

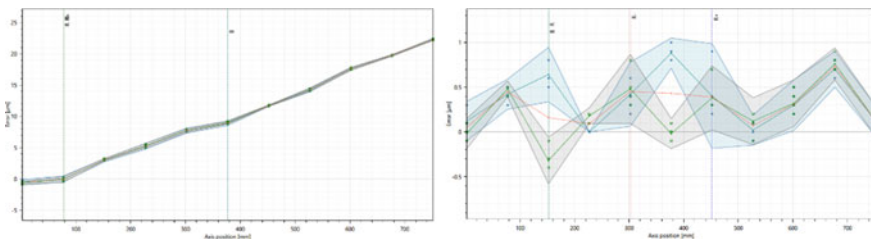


Fig. 3.13 Calibration and verification measurement with laser interferometer

3.3.3 Ballbar

The inspected parameters for machine tools include also circularity. This machine parameter represents the overall machine setting and displays both geometric and dynamic errors. For long-term tests that are not standard, the temperature behaviour of the machine can also be evaluated. This is particularly understood as heating of machine parts, such as ball screws. The circular interpolation test is described by ISO 230-4 and is part of the standard machine testing process. The test is usually conducted in three planes: X-Y, Y-Z, Z-X. Figure 3.14 shows the placement of the ballbar in the X-Y plane. The measured circularity was $10.7\text{ }\mu\text{m}$. A comparison of calibration and verification measurements is described in detail in Fig. 3.19. The test was performed on a 150 mm radius, a feed rate of 1000 mm/min and an ambient temperature of $21.6\text{ }^{\circ}\text{C} \pm 0.5\text{ }^{\circ}\text{C}$ [23].

3.3.4 Spindle Error Analyser

To inspect the run-out of spindles, inspection arbours and control bars are standard. If we need to know the spindle behaviour in more detail, there is a spindle error analyser from Lion Precision company and similar devices designed for spindle diagnostics.

It is a highly sophisticated measuring device that can evaluate several properties of the spindle based on the performed measurement. These are, in particular, radial run-outs (DX, DY), axial run-outs (DZ), tilt (DA, DB) shown in Fig. 3.15. Last but not least, thermal drift and displacement dependencies in relation to the speed (Fig. 3.16). From the results, it is possible to describe the state of the spindle,

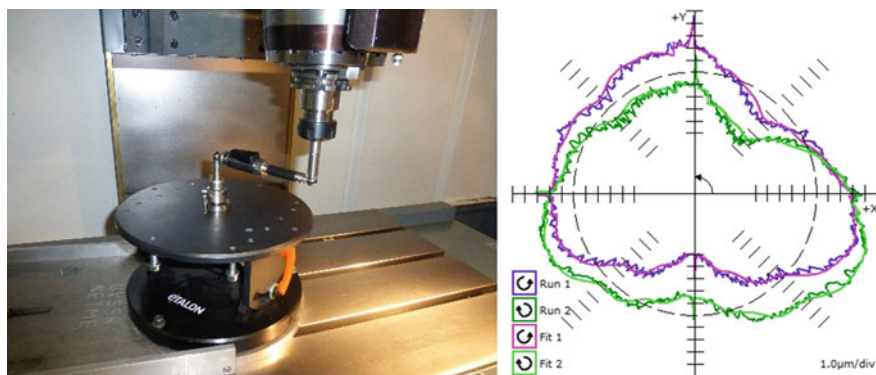


Fig. 3.14 Measurement of circular interpolation on the MCV machine in the XY plane—left, graphical interpretation of the test result—right

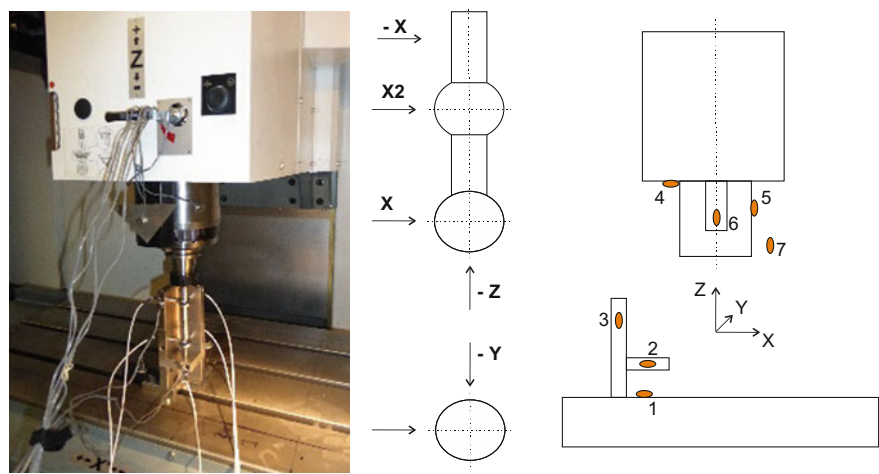


Fig. 3.15 Spindle run-out measurement on the machine MCV—left, labelling of measurement planes and arrangement of temperature sensors—right

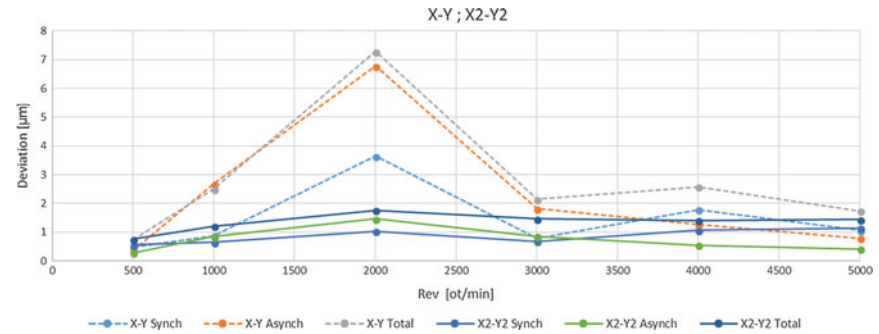


Fig. 3.16 Results of displacement measurements in the X-Y and X2-Y2 planes, depending on machine speed

but also to prepare compensation tables, on the basis of which the errors of thermal drift or the displacement caused by the speed change are corrected.

In this step, spindle tests were performed on MCV machine to describe its behaviour in different working modes.

3.3.5 LaserTRACER

The ETALON LaserTRACER measuring equipment is one of the first devices designed exclusively to measure and compensate volumetric errors of coordinate

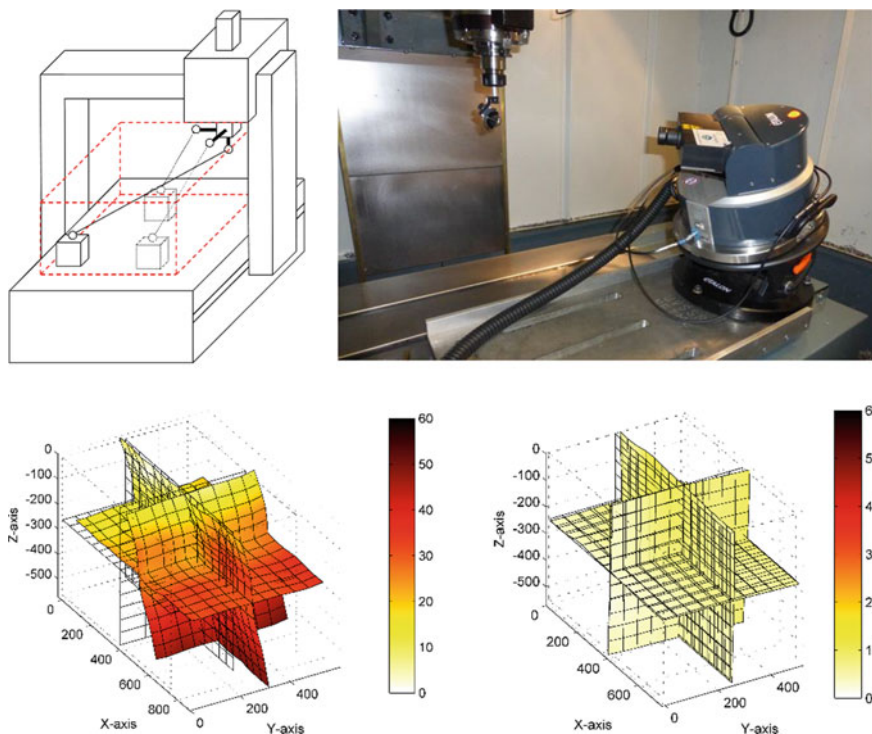


Fig. 3.17 Principle of LaserTRACER [24], application on MCV milling machine, volumetric accuracy of calibration and verification [23]

measuring machines (CMM) and machine tools (MT). The suitability of LTC deployment for CMM is given by its measurement uncertainty $U_{(95)} = 0.2 \mu\text{m} + 0.3 \mu\text{m/m}$, with the resolution of $0.001 \mu\text{m}$ and measuring range of $0.2\text{--}20 \text{ m}$. Low measurement uncertainty is given by the measurement method itself based on the principle of multilateration [24, 25]. In the case presented below, this is a sequential multilateration. The principle is that LTC must be shifted to more measuring positions and, from each LTC position, it must measure the working space of the machine (Fig. 3.17). This results in an increase in the time necessary to measure the machine, and the time required to set up and rearrange the machine is mostly up to three times longer than the time necessary for LaserTRACER measurements.

A full rigid body (FRB) model was chosen to design a measurement strategy for the MCV three-axis milling machine; this model allows a description of 21 machine geometric errors (Figs. 3.5, 3.11). As can be seen from Fig. 3.17, an improvement of the volumetric error achieved in the compensated machine space was from the original 51.1 to $20.0 \mu\text{m}$, which is an improvement of approximately 60%. As can be seen from the following graph (Fig. 3.18), not all parameters have been improved. Deterioration was observed, for example, in the case of EAX, EBZ, ECZ

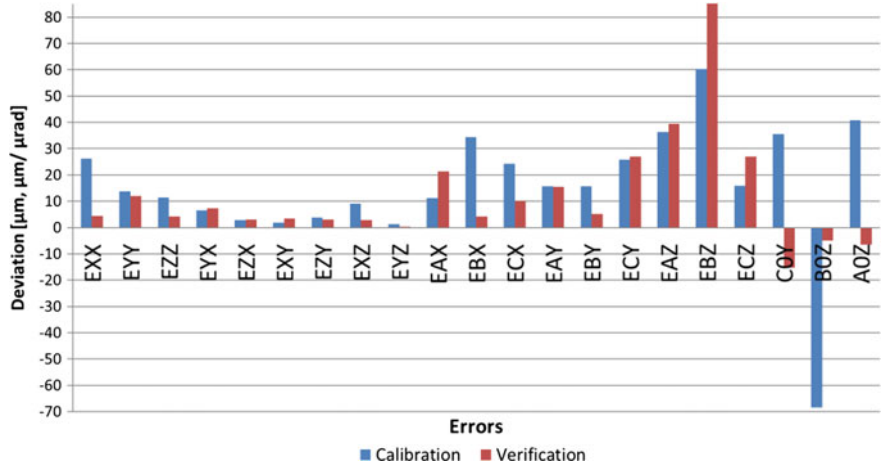


Fig. 3.18 Comparison of 21 geometric errors [23]

errors caused by an inappropriately chosen measurement strategy against these angular errors. The measurement uncertainty is also reflected in the resulting uncertainty. With the measurement time, if the working space is not air-conditioned, the change in temperature occurs and thus the associated dilatations resulting in the change of machine geometry. Therefore, it is very desirable to perform all measurements in the shortest possible time. In the publications [26, 27], three possible types of LTC control were presented, namely time lag, trigger and on-the-fly control. The difference between them is related to the time needed to measure the machine. For the above MVC machine, comparative measurements were performed to highlight time savings. The time required to measure the entire machine (without shifting the device to other positions) was different for:

- time measurement with time lag of 3 s 99 min,
- trigger with feed rate of 3000 mm/min 54 min.

This represents a shorter forced downtime for machine tool users. With such a small machine, it is a 60 (time)/20 (trigger) min saving, but for large machines with travels of 18,000 mm × 5000 mm × 2000 mm, it is already a 7 h saving. Another great advantage of shorter measuring time is significantly less influence on the machine geometry caused by changes in ambient conditions.

3.3.5.1 Ballbar

The ballbar QC20-w from RENISHAW company was again used for verification. The measurement was carried out under the same conditions as the calibration measurement. The results show a significant improvement in both the resulting geometric shape and the magnitude of deviations (Fig. 3.19). The circularity value

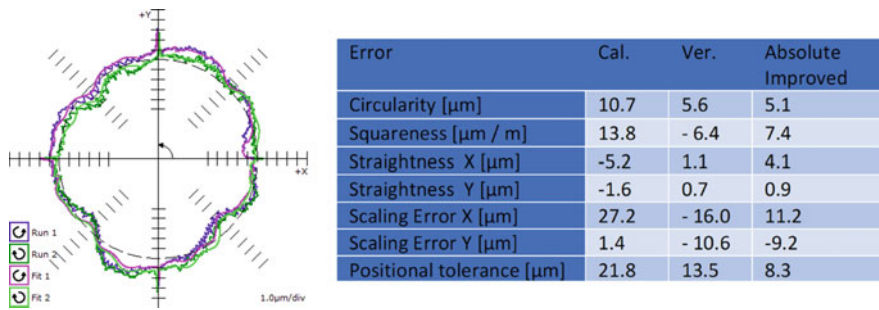


Fig. 3.19 Test of circular interpolation, ballbar, comparison [23]

for the verification measurement was 5.6 μm, with an improvement of 47%. Other geometric errors were improved as follows: squareness by 53%, X-axis straightness by 78%, Y-axis straightness by 56% and position tolerance by 38%.

From the above results, it is evident that the geometry of the machine thus modified is further positively reflected in the dimensional and shape tolerances of the workpiece. It is difficult to predict to what extent the geometric accuracy of the machine is reflected in the tolerances of the workpieces. In the introduction to this chapter, it was said that further errors occur in the dimension and shape of the workpiece due to the adjustment of the cutting process. The performed study [28] found that the improvement was in the range of 5–20%. These values were obtained within the serial production tests on the TM 2000 TAJMAC-ZPS machine.

The geometric accuracy of machine tools belongs among and will be one of the main properties of a machine tool. This is suggested due to the number of solved research activities of machine tool manufacturers themselves in this field and the deployment of new applications from the manufacturers of control systems focused on the geometric accuracy (volumetric accuracy). It can be further assumed that the development of Internet of things, big data, smart factory and artificial intelligence will further develop the area of geometric, working and production accuracy of machine tools.

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Chapter 4

Machine Vision in Measurement



B. Suresh Kumar, V. Vijayan and J. Paulo Davim

Abstract The demand in manufacturing industries productivity with high product quality is important. Nowadays, NC, CNC, and automated machine shops are playing vital role for higher productivity. Similarly, the quality inspection of the product also needed higher productivity. For the reason, there are lots of inspection methods such as direct and indirect measurement techniques which are used in measurement of products. In that machine vision is one of the newer techniques, which is used to measure the products with the aid of CCD camera and image processing techniques such as image acquisition, denoising with filters, comparison of real image and actual image, mapping of image, and image processing algorithm. In this chapter, the two important measurement techniques were discussed: firstly tool wear measurement and secondly surface finish measurement. Finally, this chapter proposes the machine vision technique that is best suitable to measure the tool wear and surface finish in automated manufacturing industries.

4.1 Introduction

Vision-based measurement has higher attention in industrial field due to its capability of faster measurement combining with computer hardware, camera, and sensors [1]. Nowadays, inspection of dimensional accuracy, geometrical features, and surface finish simultaneously tool condition monitoring are significant factors in machining area. If any deviations with these features and the parts are fails during assembly and design aspects. For the reason, the incorporation of newer measurement techniques such as optical measurement is playing vital role. In that the

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machine vision system is a newer trend in industrial instrumentation because of its smartness and simplicity [2]. The vision-based manufacturing is important for better product quality and minimizes the production time. This approach is also called as automated inspection system [3]. The complexity of measurement in circular parts, keyways, flats with partial arcs is most significantly required advancement in instrument techniques such as automated measurement with computer hardware and localized sensors [4].

A lot of researchers are concentrated on the machine vision inspection for identifying tool wear [5], tool condition monitoring [6], surface finish [7], discontinuous perimeter [4], broken inserts [8], automated visual inspection [9], crack length measurement [10], contouring error detection [11], automated wear characterization [12], nose radius wear [13], surface roughness evaluation [14], in cycle monitoring of tool nose wear and surface roughness [15], non-contact roughness measurement [16], measurement loss in machine vision metrology [17], milling cutter measurement [18], automated flank wear measurement [19], chatter detection [20], micro-milling tool wear inspection [21], machining setup verification [22], size measurement [23], machining setup modeling [24], and online tool wear measurement [25].

In this chapter, we discussed the details of machine vision metrology utilized for measurement in machining. The study focuses on how the surface roughness, tool wear, and dimensional accuracies are measured with machine vision system. The generalized camera specification is utilized for machine vision system as shown in Table 4.1.

Table 4.1 Some CCD camera details used for machine vision metrology

S. No.	CCD camera model	Resolution (pixels)	Lens focus (mm)	Measurement
1	Daheng DH-HV3103UC	2048×1536	50	Tool wear
2	JHSM500Bf-E, 1/2.5" CMOS	2592×1944		Size measurement
3	JAI CV-A1	1296×1024	55	Tool wear
4	JAI CV-A1	1392×1040	50	Tool wear
5	GMHR32514MCN		25	Surface roughness
6	Basler scout	1392×1040		Surface roughness
7	Mitutoyo QI-B3017B	1280×960		Tool wear

4.2 Basic Machine Vision System

The basic machine vision system is demonstrated in Fig. 4.1. The machine vision system consists of following procedure:

- i. Image capturing
- ii. Image acquisition
- iii. Image processing
- iv. Feature extraction
- v. Pattern classification.

4.2.1 Image Capturing

The basic machine vision system starts with image capturing with CCD camera after light emitting from the source. The illuminated image is converted into digital image by the use of image sensors.

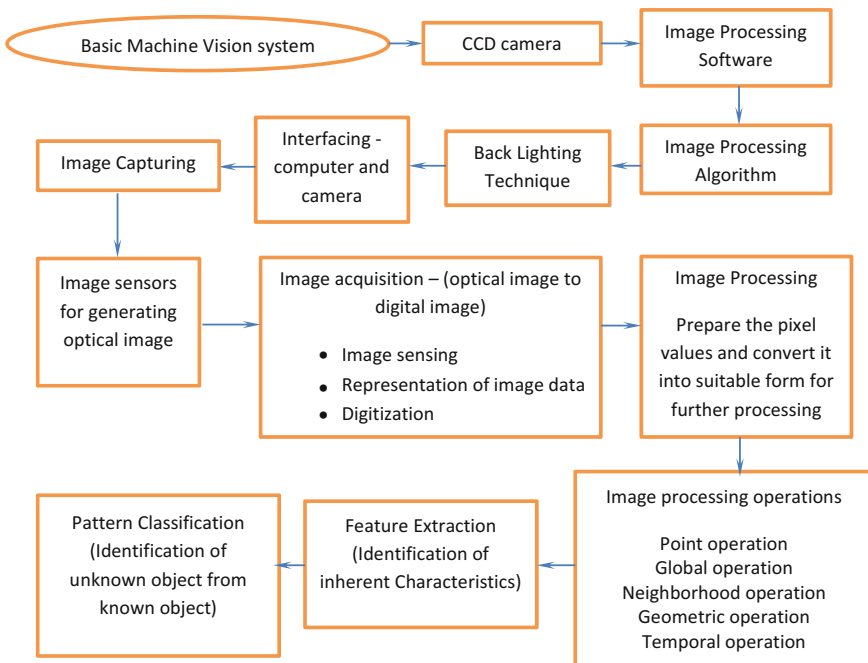


Fig. 4.1 Basic operations of machine vision system

4.2.2 Image Acquisition

Image acquisition is the process of converting optical image to digital image. This conversion process follows three different steps such as image sensing, representation of image data, and digitization.

4.2.3 Image Processing

Image processing is one of the preprocesses used to prepare the pixel values, and it converts it into suitable form for further processing. It involves five different operators like point operation, global operation, neighborhood operation, geometric operation, and temporal operation.

4.2.4 Feature Extraction

Feature extraction means identification of inherent characteristics of the object or image.

4.2.5 Pattern Classification

Pattern classification is the final step on machine vision; in this process, it identifies unknown object from known group of the objects.

4.3 Tool Wear Measurement with Machine Vision

The quality of machined parts is affected by various reasons such as cutting speed, depth of cut, feed rate and work piece materials, tool material, cutting tool geometry, coolant used, and condition of machine tool [13]. Similarly, the tool wear also has significant role in deciding the quality of the product. Generally, the tool wear is classified into two major types: (i) flank wear and (ii) crater wear. So the tool wear measurement in machining is important one. Here the tool wear measurement carried out two different methods called direct method and indirect method [26]:

Indirect method [27–29]:

- Acoustic emission signals
- Dynamometer

- Vibration
- Ultrasonic vibration
- Power consumption
- Cutting force
- Spindle torque.

Direct method:

- Machine vision system
- Optical microscope
- Tactile sensor.

The indirect measuring methods do not produce accurate value on tool wear [21]. The machine vision is the direct method which measures the wear in the form of flank wear and it can be measured from edge detection technique to identify the width change in cutting plane.

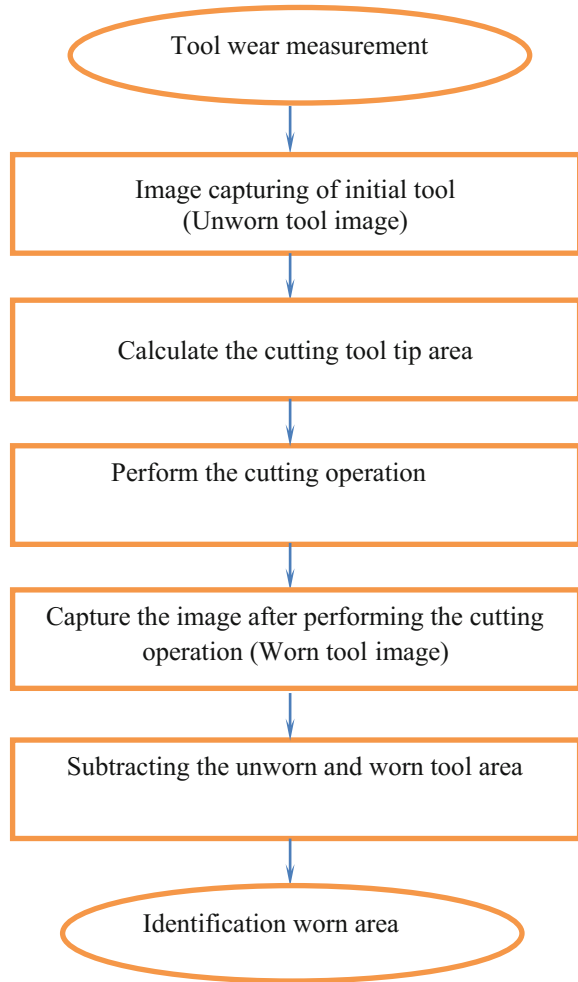
4.3.1 Tool Wear Measurement Procedure

Figure 4.2 shows the tool wear measurement procedure. The images of tool tip before and after machining are carried out with CCD camera. The image captured with CCD camera is transferred to computer with frame buffer as an interfacing element. The edge detection technique is used to identify the sharp edges to differentiate the object and background. After that the tool tip area calculation carried out before (unworn) and after (worn) machining. Finally, the subtraction process carried out with unworn and worn tool tip with careful alignment of images.

The tools are cleaned from chips and coolant particles with the aid of air nozzles for getting clear image. Three things are important to capture the image for tool wear measurement: (i) tool position, (ii) angle, and (iii) intensity of light for getting clear contrast between object and background on image. The worn area is easily identified with the aid of visual inspection on images. In worn area, the grayscale level and values are higher than unworn area. From that we can infer the worn area from tool image [25].

Chen [5] use the blob analysis for tool status monitoring from group of pixels of grayscale image. Blob means the group of pixels. The blob analysis follows the feature and analyses that identify features. The author considered two features in their work: (1) area and perimeter of group of pixels and (2) compactness. Area of group of pixels is calculated from the number of pixels covered within the area of tool wear region. Similarly, the perimeter is defined as the total length of the pixel in tool wear region. Compactness is the ratio between area of the blob and width of the blob. Based on blob analysis, the three statuses are identified through machine vision. These statuses are

Fig. 4.2 Tool wear measurement procedure through machine vision



- (i) Higher order status—using of fresh cutting edge
- (ii) Steady status region—because of smoothening of micro-roughness of the tool
- (iii) Increased tool status rate—increased contact area of tool and workpiece, decrease in micro-roughness of the tool.

Machine vision system produces remarkable similarity with area, perimeter, and compactness in all the three phases. The machine vision system has good agreement on estimated wear rate versus actual wear rate. Chethan et al. [6] considered root mean square deviation method with region of interesting for tool wear measurement. In this method, the main parameters are gray value of digital image, average of gray value, and number of pixels in region of interesting in focus image. The tool

wear measurement with machine vision follows the two main algorithms: (1) correct the angle by rotation image—for getting clarity on flank wear and (2) suppress the noise—for getting accurate wear variables. The tool wear measurement can start from *identification of tool center, adjusting the orientation of images* for getting two center point of images, *denoising by edge preserving filtering* because of micro-metal particles produce the speckles. The denoising is carried out through linear and nonlinear filters [26]. The projection algorithm is used to correct the accurate angle in ROI region extraction and fast calibrate from pixel to micron scale. Finally, the white speckles are removed from images by comparing the region sizes. From these above-mentioned procedures, the premature of tool failure can be avoided through machine vision.

Similarly, the nose radius is also calculated by the use of machine vision. Liu et al. [13] investigated the nose radius wear through machine vision. Nose radius measurement started from image capturing, enhancing the contrast, crop image and binarize the images. After that identification of tool boundary points, intersection points and tool radius center from images with polar radius coordinates.

4.4 Surface Finish Measurement

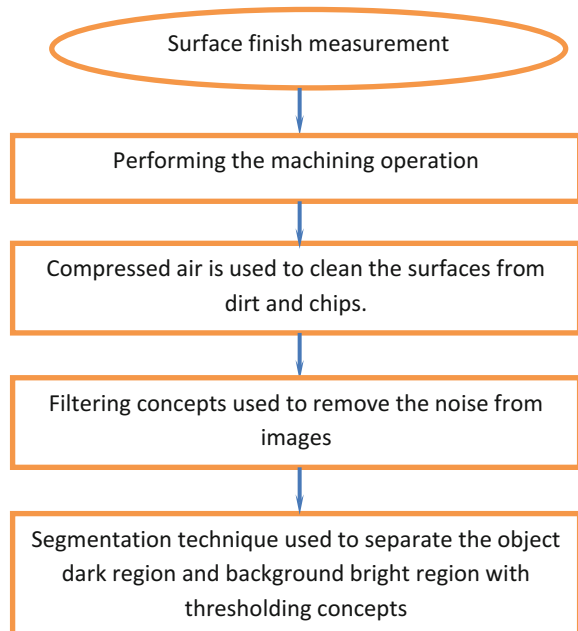
The surface texture of the machined surface is one of the important aspects in machine design. The poor surface finish affects the functional performance of the machined components. The direct contact instruments such as profilometer, scratch card, stylus are used to measure the surface roughness in manufacturing industries. This direct contact method also makes some wear on high-accuracy machined surfaces. So, non-contact-type measuring instruments such as optical devices and machine vision system are used to measure the surface roughness values. In that machine vision system is playing vital role for online monitoring system of surface texture. Similarly, the main advantage in this method is we can control the parameters during the machining operation with the aid of intelligent system incorporation. So the awareness about machine vision system in measurement is important one. Figure 4.3 represents the procedure of surface finish measurement with machine vision system.

The requirements of machine vision system for measuring surface finish are [14] as follows:

- Proper lighting and optics
- Image processing algorithm
- High computer configuration (speed, storage, and capacity).

Similarly, the machine vision has some difficulty for application due to the images may be affected by

Fig. 4.3 Surface roughness measurement procedure through machine vision



- Illumination
- Ambiance
- Reflectivity.

4.4.1 Surface Finish Measurement Procedure

The surface finish measurement on machined components procedure is carried out as follows:

- Image capturing
- Filtering the image
- Surface roughness measurement.

4.4.2 Image Capturing

The light reflected from machined surface is captured with the aid of CCD camera. The nature of surface and surface roughness is evaluated with this image.

4.4.3 Filtering the Image

Filtering the noise from image is conducted by three steps as follows:

- (i) Application of low-pass filter to original image for getting low-pass-filtered image.
- (ii) Low-pass-filtered image is subtracted from original image for obtaining the surface roughness image.
- (iii) The filtered surface roughness image is quantified by gray-level average.

Generally, these procedures are followed as per the 2D standard ISO 11562-1996 (2D profile filtering). For higher computer efficiency, the Fourier transform algorithm is used in image filtering.

4.4.4 Surface Roughness Measurement

The quantified and binarized images are studied in terms of matrix form based on light intensity [15].

The measurement algorithm as follows:

- White area intensity denoted by 1
- Black area intensity denoted by 0.

The surface roughness measurement is based on change of intensity value from 1 to 0. The algorithm for measuring the surface profile is as follows:

- (i) The algorithm initiated by scanning the first pixel of the first column in the image matrix.
- (ii) If 0 obtained, stop the scan and follow at the second row.
- (iii) If it is not 0, scan the second pixel in the same row.
- (iv) This scanning procedure continues to find 0 pixels in the first row.
- (v) Scan the first 0 value pixel of the second row.
- (vi) Repeat this procedure for all rows.

Finally, we can get the surface profile from this measurement algorithm by the conversion of pixel roughness value to micrometer. Similarly, [20] investigated the surface defects in machined surface through (i) image normalization for getting uniform response from group of images, and (ii) image division is used find the surface profile through processing blocks. The block size consists the combination of one hill and one valley of surface profile, (iii) gradient orientation, and (iv) mapping of surface defects.

4.4.4.1 Other Measurements

The location and position of workpiece and the orientation of the tool and workpiece are the important factors in machined parts quality. In that machine vision setup can be utilized for jig and workpiece setup on machine tool table. This can be processed by comparison of real setup image and virtual made by modeling. This method avoids the complicated equipment needed to assembly, and it increases the safe productivity. The roundness of components is also measured by using machine vision system with the hybridization of intelligent optimization technique. This integrated setup can be incorporated to other measuring instruments such as coordinate measuring machine [4]. Finally, machine vision measurement system has great impact on irregular shapes which are not obeying the parametric mathematical equations such as Bezier, B -spline and cubic spline shapes.

4.5 Conclusion

The machine vision system requires high-resolution camera and back lightening for achieving better tool wear measurement. In tool wear measurement, the three parameters—area, perimeter, and compactness—are important in higher order status, steady state, and increased tool status rate during machining. The sudden tool failure can be reduced with this system from any abnormalities available with wear land; the cutting feed rate can be reduced with the automated machine vision system. The smart machine vision system can be developed through add intelligence in previously available automated machine vision system, from that we can improve the reliability, safety, and quality of the products. The future research in tool wear measurement is to be concentrated about wear and product quality degradation with the sense of dimensional integrity and surface finish.

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Chapter 5

Tribological Measurement of Electroless Nickel Coatings



Prasanta Sahoo and Arkadeb Mukhopadhyay

Abstract The tribological characteristics of electroless nickel coatings have been in focus since its inception. With the passage of time, electroless nickel coatings have proven to be a promising candidate as tribo-coatings. Electroless nickel coatings have now evolved into a large family of functional coatings due to the flexibility of the process. Depending upon the need and cater into the rising industrial demands, different varieties of electroless nickel coatings have been deposited and evaluated. In fact, simulation of test conditions and test environments as per industrial demands has led to the investigation of the coatings on a variety of test setups. Different tribological measurement techniques employed to evaluate the coatings and the response of the coatings to the same are summarized. The present chapter gives a comprehensive overview of tribological measurements and evaluation methods related to surface roughness and friction/wear characteristics of electroless nickel coatings.

5.1 Electroless Nickel Coatings—Classifications, Applications, and Emergence as a Tribo-Coating

Tribology is derived from the Greek word ‘Tribos’ which deals with the science of interacting surfaces in relative motion and friction, wear and lubrication. Though the subject is relatively new, practices related to tribological principles dates back to older than recorded history [1]. It is essential to conceive tribological aspects during the design stage of a mechanical component since it reduces the requirement of maintenance. The reliability of interacting surfaces is also improved leading to substantial economic benefits [2]. Various tribological principles related to the measurement of friction/wear, lubrication, surface engineering, bearings, etc., have been discussed elaborately by Sahoo [1] and Hirani [2].

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Surface engineering is a part of tribology which aims to increase wear resistance and modify frictional behavior of surfaces [3] without modifying bulk properties of the material. Therefore, it is necessary that components to be used in tribological contact applications be made from a bulk material (based on strength, ease of manufacture, availability, cost, etc.) and a surface material that would improve friction and wear characteristics [3]. This may be achieved by surface treatment (induction hardening, flame hardening, laser hardening, electron beam hardening, carburizing, cyaniding, nitriding, carbonitriding, chromizing, aluminizing, ion implantation, etc.) or application of surface coatings. Again surface coatings may be deposited by electrodeposition, electroless plating, hard facing, vapor deposition (physical or chemical), etc., and each method have their own advantages and disadvantages.

Among the aforesaid coating deposition techniques, electroless nickel (EN) coatings have gained immense attention from researchers and their applicability has increased by leaps and bounds [4]. This is due to several factors such as (a) use of electricity is avoided; (b) uniform deposition throughout the surface which means intricate components may be coated with ease; (c) high hardness; (d) high wear/corrosion resistance; (e) low coefficient of friction (COF); and (f) good surface finish. An EN bath consists of a source of nickel ions, a reducing agent, buffering agent, stabilizers, surfactants, and complexants [5]. The reducing agent undergoes oxidation and supplies current, i.e., electrons. Cation of the metal to be deposited is reduced by receiving electrons from a catalytically active substrate. The reducing agent delivers electrons to this surface. Autocatalytic deposition takes place once the initial layer of coating is deposited. Finite bath life is a disadvantage of EN deposition. A schematic diagram of EN bath is shown in Fig. 5.1.

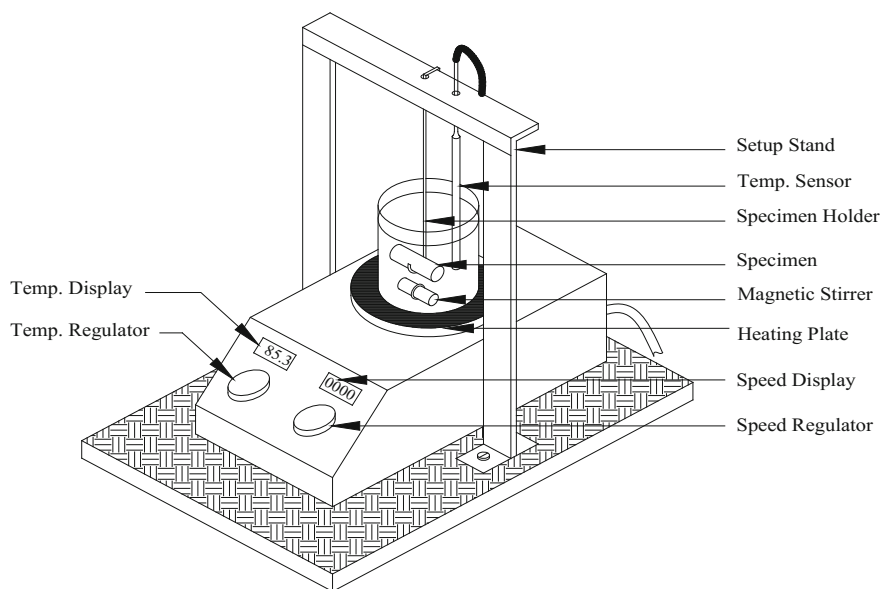


Fig. 5.1 Schematic diagram of EN deposition

EN coatings may be broadly classified as pure Ni, alloy (binary, ternary, quaternary, or poly-alloy) and composite (micro or nano). Pure Ni coating has been discontinued due to the hazards associated with its reducing agent, i.e., hydrazine [5]. Binary or poly-alloy EN coatings are obtained from sodium hypophosphite- or borohydride-based baths leading to the formation of Ni–P or Ni–B [6] coatings. Ni–B coatings may be also obtained when the reducing agent used is dimethylamine borane (DMAB) or *N*-diethylamine borane (DEAB). EN composite coatings are obtained by the co-deposition of micro/nanoparticles such as Al_2O_3 , TiO_2 , diamond, SiC, Si_3N_4 , B_4C , (h) BN, WC, carbon nanotubes (CNTs), Ag, polytetrafluoroethylene (PTFE), MoS_2 [7]. Hard particles improve the hardness and wear resistance of EN coatings while soft particles impart anti-friction characteristics. Therefore, the choice of composite coatings depends on the application desired.

In case of Ni–P coatings, coating characteristics depend on the P content. Based on P content, Ni–P is classified as low-P, mid-P, and high-P coatings [8]. Similarly, Ni–B coatings are classified as low-B (0.5–4% by wt), mid-B (5–6% by wt), and high-B (7–9% by wt) coatings [9]. The P and B content in EN coatings control the crystallinity of the deposits as well as its tribological characteristics. Low-P or Low-B coatings are nano-crystalline, mid-P and mid-B coatings are a mixture of amorphous and nano-crystalline while high-P or high-B coatings are completely amorphous [4, 5]. Heat treatment results in complete crystallization of EN coatings with precipitation of phosphides and borides for Ni–P and Ni–B coatings, respectively. X-ray diffraction patterns of as-deposited and heat-treated EN coatings are shown in Figs. 5.2 and 5.3, respectively. Precipitation of hard phases leads to improvement in tribological behavior of the coatings. Furthermore, EN coatings have a nodular surface morphology which leads to low COF. Typical surface morphology observed in a scanning electron microscope (SEM) of as-deposited Ni–P and Ni–B coating is shown in Fig. 5.4.

Due to excellent surface finish and tribological characteristics, EN coatings find wide usage in industries. They include aerospace hardware, cylinder liners, automotive parts, chemical and textiles industry, turbine blades working in extreme

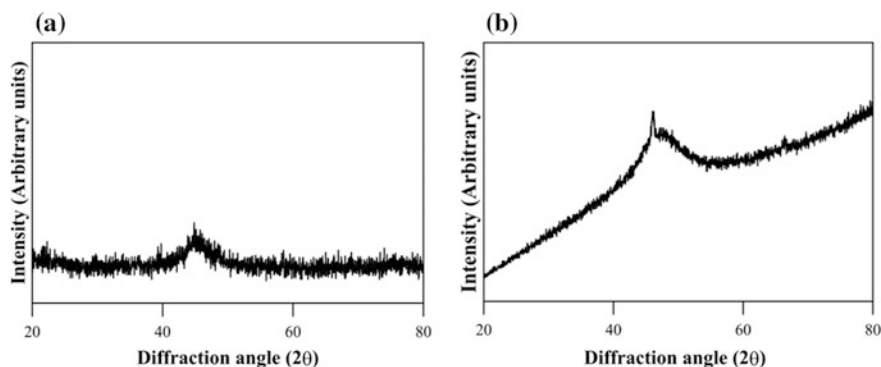


Fig. 5.2 XRD of as-deposited, **a** Ni–P and **b** Ni–B coatings

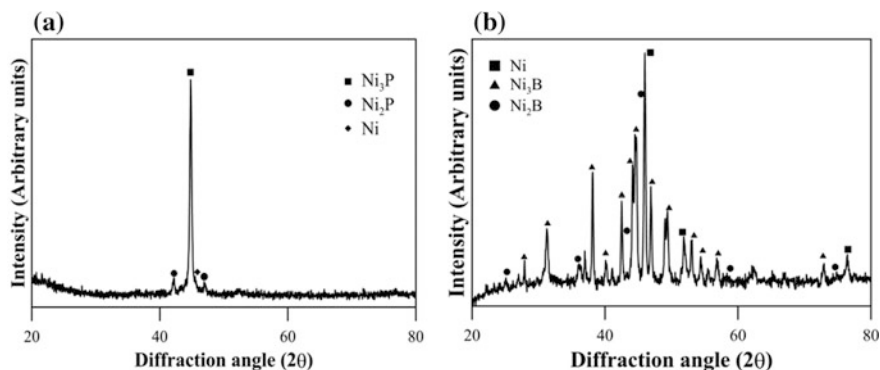


Fig. 5.3 XRD of heat-treated, **a** Ni-P and **b** Ni-B coatings

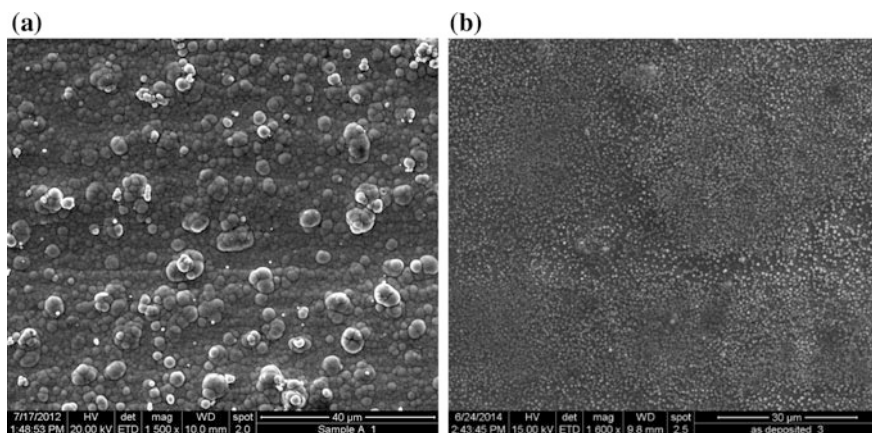


Fig. 5.4 SEM micrograph of as-deposited, **a** Ni-P and **b** Ni-B coating

conditions, fluid power components, printed circuit boards, etc [6]. Applications of EN coatings in petrochemical industry include rod pumps, couplings, mud pumps, extruder and blenders, fire tubes, tanks and vessels [6]. Electroless Ni-B coatings have the capability of protecting small arm weapons from wear used in dusty regions [10]. Ni-P coatings are also used in chlor-alkali plants for protecting steel compressors from wear and corrosion [11]. EN coatings have widespread usage in automotive industry for prevention of wear and corrosive wear of piston rings and cylinder liners when bio-oil is used.

The increasing demand from industries led to the evolution of EN coatings into a huge family with superior surface and tribological characteristics. The last few decades have witnessed a tremendous surge in the measurement and investigation of tribological characteristics of EN deposits to cater into the demanding industrial needs. Such investigations have been carried out in different test configurations to

simulate sliding wear, erosive wear, abrasive wear, erosive–corrosive wear, etc. Therefore, there is a need to study and mark the significant developments in tribological measurements of EN coatings particularly the different approaches and methods adopted. The present chapter summarizes the different approaches to tribological measurement of EN coatings related to its surface roughness, friction/wear characteristics, simulation of different wear modes/environments, and coating response to such test conditions. Finally, research trends and future directions are also suggested in this chapter.

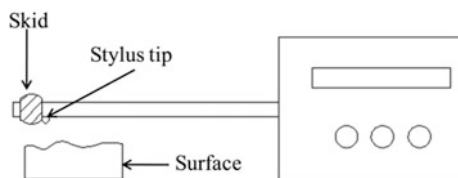
5.2 Measurement of Surface Roughness of Electroless Nickel Coatings

EN coatings generally follow the surface profile of the substrate on which it is deposited. Below a critical substrate roughness, the smoothing effect caused by filling of valleys between asperities is avoided in the electroless method in comparison with electroplating. EN coatings are commercially important for finishing steel, aluminum, copper, and other materials [4] due to their smooth appearance and uniform deposition capabilities. But, proper bath control is necessary to achieve desired surface finish of the electroless deposits. Surface roughness of EN coatings has been addressed by researchers by the methods as discussed subsequently.

5.2.1 Surface Profilometer

A stylus-type profilometer is by far the most widely used method for measuring surface roughness of EN coatings. In this method, a fine and lightly loaded stylus is moved across the coated specimen for a fixed traversing length at a constant speed. A diamond-tipped stylus with finite tip radius is generally used. The displacement of the stylus tip corresponding to the surface under examination gives a measure of the surface roughness. A schematic diagram of a stylus-type profilometer is shown in Fig. 5.5. The roughness parameters mostly used by researchers to characterize an EN coated substrate is centre line average (R_a) roughness. Other parameters include root mean square roughness (R_q), mean line peak spacing (R_{sm}), skewness (R_{sk}), and kurtosis (R_{ku}).

Fig. 5.5 Schematic diagram of a profilometer



The EN bath constituents have a significant effect on the coating roughness. A systematic investigation using design of experiments (DOE) and statistical analysis revealed that the concentration of reducing agent (sodium hypophosphite) and its interaction with the concentration of nickel source (nickel chloride + nickel sulfate) had a significant effect in controlling above-mentioned roughness parameters for Ni–P coatings [12]. The concentration of reducing agent (sodium borohydride) had the highest contribution to controlling roughness of electroless Ni–B coatings [13]. The addition of surfactants improved the R_a of electroless Ni–P coatings deposited on Mg alloys [14].

Apart from the parameters mentioned above, the surface of EN coatings was also addressed by another parameter namely fractal dimension. Fractal characteristics are independent of the resolution of the instrument used or length scales. Sahoo [15] observed that the fractal dimension was strongly influenced by the source of nickel in the electroless bath for Ni–P alloys. In case of Ni–B coatings, bath temperature was found to be the most influential parameter in controlling fractal dimension [16]. The fractal dimension of Ni–P [15] as well as Ni–B [16] coatings was obtained by processing data of the surface profile captured using a stylus-type profilometer.

As mentioned previously, the substrate has a significant effect on the coating roughness. During the initial stages of deposition, the nodules of nickel are formed along the lays of the polished surface [17]. Surface roughness measurement revealed that R_a of Ni–P coatings deposited on CFRP decreased with an increase in roughness of the substrates due to ‘nodule coalescence’ [18]. The roughness of EN coatings also evolves with the plating duration. Vitry et al. [19] observed the evolution of roughness of electroless Ni–B coatings with plating duration using a stylus-type profilometer. The R_a of Ni–B coatings increased slightly during the first stages of deposition since isolated nodules were formed initially. As a continuous layer was formed, R_a decreased (after 4 min of deposition). At the end of 10 min of deposition, R_a was close to the final level. While at the end of plating process (60 min), roughness was similar to that of substrate.

5.2.2 Atomic Force Microscopy

The atomic force microscope (AFM) generates high-resolution 3D images of surfaces by combining the principles of surface tunneling microscopy with stylus-type profiler. A detailed discussion on the working of AFM may be found in the work done by Sahoo [1]. In comparison with AFM, the mechanical stylus-type profiler is more widely used due to its simplicity and cost-effectivity. However, investigation on an atomic scale is possible using AFM.

High-resolution AFM images of binary and ternary variants of EN coatings revealed that the coatings have different roughness values due to different growth patterns [20]. AFM images of Ni–P coatings deposited on mild steel coupons (11 nm roughness) indicated that the roughness was around 12 nm which was quite close to the substrate. Co-deposition of Cu led to an increase in roughness to 32 nm

while the inclusion of W led to a roughness of 60 nm [20]. Therefore, the inclusion of a third element had a significant effect on the roughness of the coatings.

Yu et al. [21] showed that AFM images of electroless Ni–P–Cu coatings could be characterized by multi-fractal spectra which gave more detailed information than the simple fractal. The AFM images of Ni–P–Cu coatings were processed to obtain the parameters of multi-fractal spectrum which gave information about the growth process of electroless Ni–P–Cu alloys. The nodules were seen to grow in both horizontal and vertical directions. Non-uniformity of height distribution could be also observed in Ni–P–Cu coatings with increasing deposition time [21]. In a recent study, AFM was also used to characterize the surface of electroless Ni–B coatings before and after impregnation of PTFE [22]. PTFE-impregnated Ni–B coatings were observed to have lower roughness in comparison with the binary alloy. The high-resolution AFM images revealed that such difference in roughness was due to the ability of PTFE to fill the valleys between the nodulated structures leading to a smoother appearance. Therefore, clearly a deeper insight may be obtained through the use of AFM in characterizing surface of EN coatings.

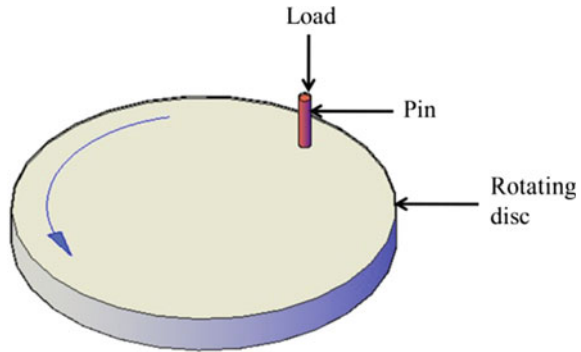
5.3 Measurement of Friction and Wear of Electroless Nickel Coatings

Owing to the high hardness of EN coatings, they find usage in tribological contact applications for reduction of friction and wear. The cauliflower-like surface morphology makes EN coatings inherently lubricious resulting in a low COF. Therefore, friction and wear behavior of EN coatings along with the governing wear mechanisms have been of great interest to researchers. To investigate friction and wear characteristics of EN coatings, different methods and tribological test configurations have been demonstrated in research works which are discussed subsequently.

5.3.1 *Pin-on-Disc*

This is the most widely used tribological test configuration that has been used for investigation of sliding wear behavior and mechanism of EN coatings. A schematic diagram of a pin-on-disc arrangement has been presented in Fig. 5.6. In this method, a vertical cylindrical pin of appropriate dimensions presses against a rotating disc. Either the disc serves as a counterface material while the pins are coated or vice versa. Load is applied onto the pin via a loading lever by placing dead weights on a loading pan or it may be applied by spring, hydraulic, or pneumatic loading [3]. Sliding speed and sliding distance are other parameters that are also varied along with the applied normal load. In general, frictional force is

Fig. 5.6 Schematic diagram of a pin-on-disc type arrangement



measured using a load cell, while wear is indicated by a linear variable differential transformer (LVDT). But a deeper insight into the wear behavior may be obtained from wear rate of the coatings which is given as follows:

$$w_s = \frac{m}{d} \quad (5.1)$$

$$w_s = \frac{m}{L \times d} \quad (5.2)$$

$$w_s = \frac{V}{L \times d} \quad (5.3)$$

where m is mass loss (mg or gm), V is the volume loss of coating (μm^3 or mm^3), L is the applied normal load (N), and d is the sliding distance (m). In several research works, wear has been also indicated in terms of mass loss of the coatings.

Experiments conducted on a pin-on-disc configuration tribo-tester revealed that electroless Ni–P coatings in as-deposited condition exhibit higher COF and lower wear resistance compared to heat-treated ones [23]. This was attributed primarily to the high ductility and lower microhardness in as-deposited condition which resulted in an increase in contact area of the tribological pair. Similar results were also reported for Ni–B coatings by Krishnaveni et al. [24]. Furthermore, sliding wear tests on pin-on-disc setup have also revealed that EN ternary alloy coatings exhibit higher wear resistance compared to binary Ni–P or Ni–B coatings [25, 26]. The poly-alloy coatings have higher hardness and toughness due to solid solution strengthening of nickel matrix. The composite/nano-composite variants of EN coatings have also shown a low wear rate compared to the alloy variants on a pin-on-disc machine [27, 28] due to dispersion strengthening.

Since different tribological pairs are susceptible to the presence of liquid/solid lubricants, chemical/corrosive environments, gases, and so on, it is necessary to simulate such enclosed environmental condition. Tribological pairs work under oil-lubricated condition in automotive components while in marine components, they work under corrosive environment. In hot metal-working industries, the tool

and die are subjected to sliding action at elevated temperatures. Suitable modification to the pin-on-disc setup has enabled the investigation of friction and wear characteristics of EN conditions in such simulated environments. Sliding wear study of electroless Ni–P coating in NaCl, NaOH, and H₂SO₄ media (considered to have corrosive effect) on a pin-on-disc machine showed that the coatings had lower wear rate in NaCl or NaOH media compared to H₂SO₄ [29]. To simulate sliding wear under lubricated condition on a pin-on-disc setup, the lubricant was directed at the interface of the pin and disc via a pump [30]. Therefore, such research works reflect the suitability of EN coatings in automotive/marine industries.

In some recent developments, the high-temperature tribological characteristics of EN coatings have been reported and experiments were carried out on a pin-on-disc type setup. Such experiments may be carried out in a test chamber at high temperature or appropriately heating the pin, disc, or both to the desired temperature. At a high temperature of 550 °C, Ni–P coatings in as-deposited condition exhibited wear rate ~ 0.0009 mg/m while at room temperature, a wear rate ~ 0.016 mg/m was observed by Masoumi et al. [31]. In the same work, COF reported at room temperature was ~ 1.99 which decreased to 0.42 at 550 °C. This improvement in tribological behavior of Ni–P coatings at high temperature in comparison with room temperature condition was attributed to the toughening of the coating matrix due to in situ heat treatment effect, microstructural changes, and formation of oxide glazes. Such phenomenon also governs friction and wear characteristics of Ni–B coatings and its variants at high temperature as observed by Mukhopadhyay et al. [26].

5.3.2 *Ball-on-Disc*

This method is similar to the pin-on-disc setup where the cylindrical pin is replaced by a spherical ball. In general, the spherical ball is made of hard material such as Al₂O₃, TiO₂, WC and acts as the counterface material. The ball presses against the rotating disc which is coated. Test parameters that may be varied are normal load, sliding speed, sliding distance, track diameter, etc. Wear tracks are formed on the coated disc. A profilometer is used to measure width and depth of wear track which in turn gives volume loss. Further wear rate may be evaluated using Eq. (5.3) from the volume loss. Wear rate may be also calculated from mass loss of the coatings using Eqs. (5.1) and (5.2). Frictional force/COF is obtained using a load cell. A schematic diagram of ball-on-disc arrangement is shown in Fig. 5.7.

Li et al. [32] investigated the wear rate and COF of electroless Ni–P coatings deposited on AZ91D alloy in as-deposited condition and after tempering on a ball-on-disc setup. A steel ball of diameter 3 mm and hardness 62 HRC was used as the counterface ball. Wear rate of the coatings was calculated both by mass loss method and cross section method. The wear rate was calculated as $W_R = V_{\text{loss}}/d$ where V_{loss} is the volume loss and d is the sliding distance during the test period. A schematic diagram of the cross section method is shown in Fig. 5.8. Area of wear track and V_{loss} was calculated from the following equations [32]:

Fig. 5.7 Ball-on-disc type arrangement

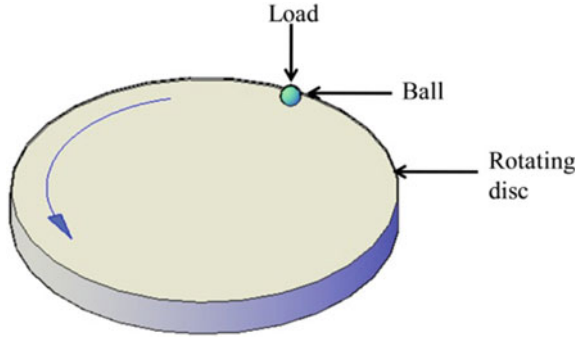
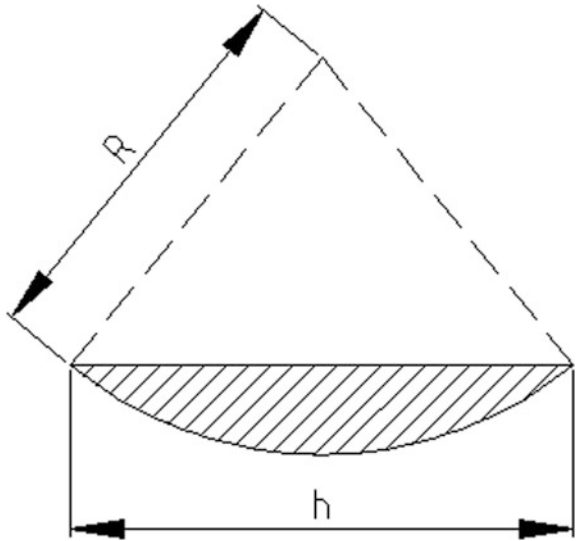


Fig. 5.8 Schematic of the cross section method [32]



$$A = R^2 \arcsin[h/(2R)] - \frac{h}{4} \sqrt{4R^2 - h^2} \quad (5.4)$$

$$V_{\text{loss}} = 2\pi r \left(R^2 \arcsin[h/(2R)] - \frac{h}{4} \sqrt{4R^2 - h^2} \right) \quad (5.5)$$

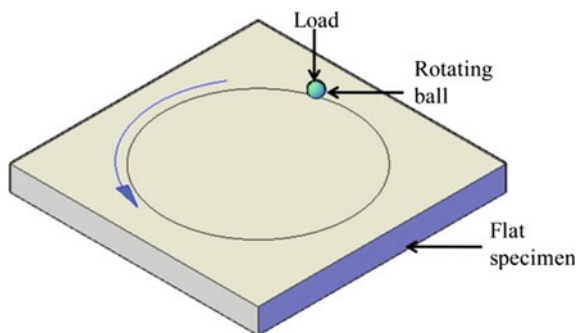
where R is the radius of steel ball, r is the radius of sliding track while h is the width of track measured from optical micrographs of worn surfaces. Experimental results indicated that wear rate obtained by mass loss method as well as using Eq. (5.5) agreed well with each other. Furthermore, tempered (400 °C for 1 h) Ni–P coatings exhibited a wear rate of $\sim 3.056 \times 10^{-4} \text{ mm}^3/\text{m}$ and COF $\sim 0.13\text{--}0.177$ at a load of 1.5 N which was significantly lower compared to the as-deposited (COF = 0.10–0.351 and wear rate = $9.038 \times 10^{-3} \text{ mm}^3/\text{m}$) ones.

The tribological behavior of electroless Ni-B coatings deposited on pure magnesium and AZ91D alloy was investigated by Correa et al. [33] on a ball-on-disc type tribometer with alumina ball (6 mm diameter and 21 GPa hardness) as the counterface disc. Tribological test parameters used were 6 N load, 0.014 m/s sliding velocity and 53 m sliding distance. Wear rate was measured using Eq. (5.2) where mass loss was evaluated in kg. The wear rate of Ni-B coatings on Mg and AZ91D alloy observed was $7.34 \times 10^{-11} \pm 1.82 \times 10^{-11}$ kg/(N m) and $7.34 \times 10^{-11} \pm 1.63 \times 10^{-11}$ kg/(N m), respectively. The bare Mg and AZ91D alloy exhibit wear rate of $5.17 \times 10^{-9} \pm 1.60 \times 10^{-9}$ kg/(N m) and $5.53 \times 10^{-9} \pm 2.57 \times 10^{-10}$ kg/(N m), respectively. Therefore, a significant improvement in wear behavior of commercial purity Mg and AZ91D alloy was obtained by the deposition of Ni-B coatings. In fact, Ni-B coatings reportedly improved the tribological behavior of AISI 316L austenitic stainless steel which is used in several industries [34]. Here, the tribological behavior was investigated on a ball-on-disc type tribometer with alumina ball (10 mm diameter and ~ 19.6 GPa hardness) as the counterface. Wear rate was evaluated using Eq. (5.3). The bare substrate and Ni-B coatings showed wear rate of 8.2×10^{-4} mm³/(N m) and 1.5×10^{-5} mm³/(N m), respectively. COF of 316L steel and Ni-B coatings was 0.85 ± 0.05 and 0.20 ± 0.10 , respectively. Such excellent tribological behavior of Ni-B coatings was attributed to its high hardness, nodular surface morphology, and columnar growths. This reduces actual contact area of the tribo-pairs resulting in low wear rate and COF. High-temperature tribological behavior of EN composite coatings such as Ni-P-MoS₂ [35] and Ni-P-SiC [36] with self lubricious properties was also investigated on a ball-on-disc tribometer. Excellent anti-wear and anti-friction characteristics were observed due to the formation of oxide glaze.

5.3.3 *Ball-on-Flat*

In this method, a flat specimen is held stationary against a rotating counterface ball. Applied normal load, rotational/sliding speed, and track diameter are the test parameters that may be varied. Tribological characteristics such as wear rate and COF may be measured accordingly using Eqs. (5.1)–(5.3) and appropriate sensors. A schematic diagram of a ball-on-flat tribo-pair is shown in Fig. 5.9. COF of electroless Ni-P and Ni-P-PTFE coatings was investigated on a ball-on-flat tribometer by Zhang et al. [37]. Flat specimens made of steel acted as substrates for Ni-P and Ni-P-PTFE deposition while GCr15 steel ball (5 mm diameter and a hardness of HRC 62) acted as the rotating counterface ball. At a rotation speed, rotation diameter and load of 45 rpm, 15 mm, and 5 N, respectively, Ni-P coatings indicated an increase in COF with sliding distance while in case of Ni-P-PTFE coatings, it remained almost stable at ~ 0.15 . Due to the non-stick, non-gelling and self-lubricating capabilities, Ni-P-PTFE coatings exhibited superior anti-friction characteristics compared to binary Ni-P alloy.

Fig. 5.9 Schematic of ball-on-flat configuration tribo-pair



5.3.4 Ring-on-Block

In the ring-on-block test method, coated blocks are held stationary against a rotating ring as shown in Fig. 5.10. COF is obtained from the frictional force which is recorded online during the test via torque as measured by a strain gauge or a load cell. Wear is indicated either in terms of mass loss or calculated from Eqs. (5.1) to (5.3). Chen et al. [38] investigated the friction and wear characteristics of EN alloy and composite coatings by this method under oil-lubricated condition. The coatings under consideration were Ni-P, Ni-P-SiC, Ni-P-graphite, and Ni-P-CNTs. Tests were carried out at 700 N and at a rotating speed of 800 rpm for 6 h. The lowest mass loss and COF were exhibited by Ni-P-CNT coatings compared to the other variants. It was also concluded that overall, the composite coatings showed excellent wear resistance and low COF and may be recommended for the development of modern automobile industry and advanced space technique.

5.3.5 Ring-on-Ring, Ring-on-Disc, and Disc-on-Disc

In the ring-on-ring test configuration, coated rings are held stationary against a rotating counterface ring or in some cases both may be given rotational motion. Test parameters that may be varied are applied normal load, rotational speed, or sliding duration. Friction and wear are measured accordingly. A schematic diagram of the test method is shown in Fig. 5.11. Electroless Ni-P coating with improved microhardness and wear resistance was investigated by this method [39]. In the same study, one ring was made of GCr15 with a hardness of 64 HRC (counterface) while Ni-P coating was deposited on the other. The speed of GCr15 ring and the coated ring was 200 and 180 rpm, respectively, while the applied load was 490 N. Wear test was carried out for a period of 2 h and evaluated in terms of mass loss (mg). Ni-P coatings with varying P content were deposited and results indicated that coatings with P content of 7.97 at wt% had the highest wear resistance.

Fig. 5.10 Schematic of ring-on-block configuration test

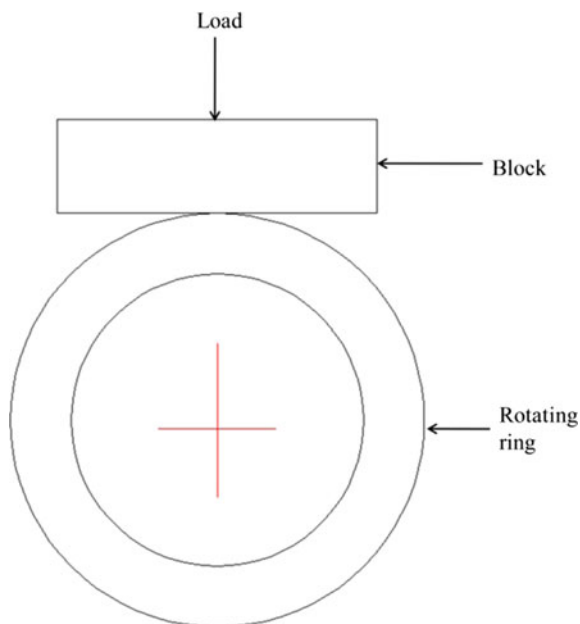
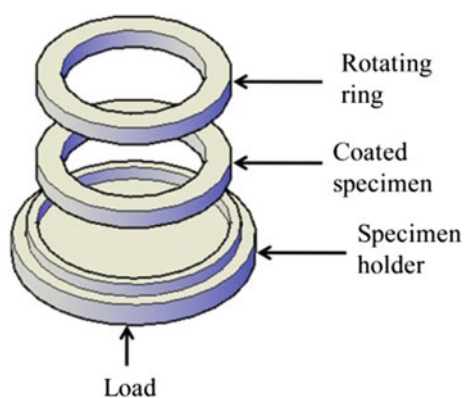


Fig. 5.11 Schematic of ring-on-ring configuration test



The ring-on-disc or disc-on-disc arrangement is fairly similar to ring-on-ring configuration. A rotating counterface ring slides against a stationary EN coated disc. Electroless Ni-P-Gr, Ni-P-SiC, and Ni-P-Gr-SiC coatings were evaluated on a ring-on-disc type setup where 45# carbon steel (40–45 HRC hardness) acted as the counterface [40]. Tests were carried out at 50 rpm rotational speed for 20 min and at a load of 150 N. COF was indicated continuously (as a function of revolutions) while normalized wear rate was calculated from the volume loss (converted from mass loss) of the coatings per unit sliding distance. Results indicated that Ni-P-Gr exhibit the lowest COF among the three while Ni-P-SiC had highest wear

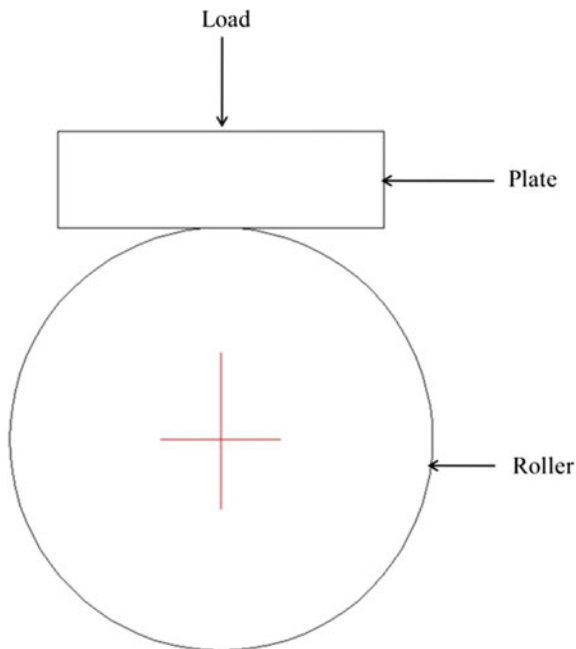
resistance. A combination of both anti-friction and anti-wear characteristics was achieved in Ni–P–Gr–SiC coatings. Formation and replenishment of Gr mechanically mixed layer (MML) to the tribo-surface ensured low COF while SiC particles mixed with Gr acted as load-bearing sites preventing easy shearing of the MML.

In the disc-on-disc method, a stationary coated disc slides against a rotating counterface disc. Electroless Ni–P–diamond composite coatings were evaluated on a disc-on-disc type of arrangement where a counterface disc of high strength tool steel (800 HVN) was used [41]. Tests were carried out under dry condition at 5 N load, 250 rpm rotational speed, and 30 min duration. Diamond particles of three different sizes 3–6, 6–12, and 20–40 μm were incorporated into Ni–P coatings with varying phosphorus content (6–7, 8–9, and 9–10% P). Annealing temperatures of 230, 350, and 400 $^{\circ}\text{C}$ for 2 h were also considered. But the wear resistance was evaluated from a different approach where integrated intensities of diffraction lines from the composites were considered. A measure of retention of the composites was obtained from the ratio of integrated intensity of diamond to that of nickel. This ratio was obtained at regular intervals of time. Results indicated a higher wear resistance of the coatings with finer diamond particles and higher P content.

5.3.6 Plate-on-Roller/Block-on-Roller

The plate-on-roller arrangement is depicted schematically in Fig. 5.12. Coated EN block/plate specimens are held stationary against a hard rotating counterface roller. Applied normal load, rotational speed of the roller and test duration are controlled suitably. Wear may be indicated in terms of ‘wear depth’ (measured using LVDT) or simply from mass loss or using Eqs. (5.1)–(5.3). COF may be measured from the frictional force using a load cell. Optimization of tribological behavior (wear depth and COF) of EN coatings and its variants has been reported extensively on a plate-on-roller arrangement. Sahoo [42, 43] formulated optimum coating bath parameters of electroless Ni–P coatings based on low wear depth and COF of the coatings on a plate-on-roller arrangement tribo-tester. The design of experiments (DOE) and statistical analysis of variance (ANOVA) indicated that wear depth of Ni–P coatings was influenced significantly by annealing temperature and bath temperature [42]. Similarly, it was revealed that nickel source concentration in the bath had a strong influence on COF [43]. Das and Sahoo [44] reported optimum bath composition of Ni–B coatings for minimum wear depth and COF using Taguchi’s DOE technique and grey relational analysis (GRA) on a plate-on-roller tribo-pair with a steel roller coated with titanium nitride (hardness of 85 HRC) as counterface. ANOVA results revealed that concentration of reducing agent (sodium borohydride) had the highest contribution in controlling the friction and wear behavior of the coatings. In case of Ni–P as well as Ni–B coatings normal load, rotational speed of roller and time was kept fixed at 25 N, 60 rpm, and 300 s. Similar studies have been also reported for Ni–P–W and Ni–P–Cu coatings in the literature [45].

Fig. 5.12 Schematic of plate-on-roller arrangement



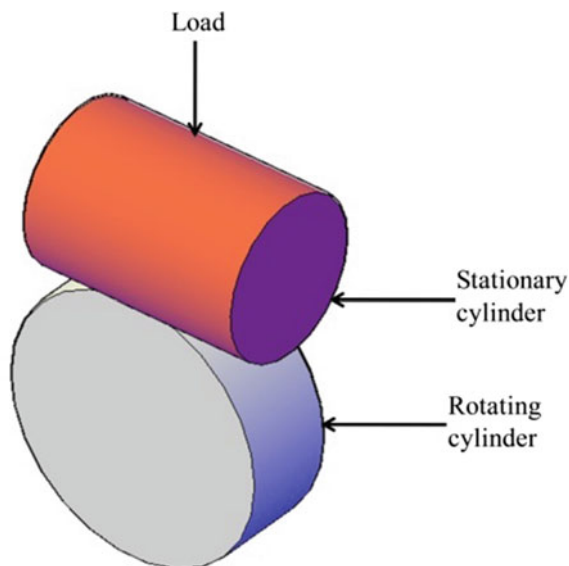
5.3.7 Crossed Cylinder

In this method, the tribo-pairs under consideration are two cylinders arranged in a manner as shown in Fig. 5.13. Tribological behavior of Ni-P and Ni-P-PTFE coatings deposited on AISI M2 steel was evaluated on a crossed cylinder rig under dry condition [46]. The counterface material was AISI 52100 hard steel (hardness of 730 HV₃₀) which was held stationary against a rotating EN coated cylinder (159 rpm). Applied normal load and sliding duration was varied in the range of 2–35 N and 4 min to 30 h, respectively. An improvement in wear resistance of Ni-P coatings under dry sliding condition could be achieved by the inclusion of PTFE in the EN matrix [46]. A similar arrangement was also used to evaluate the wear behavior and COF of Ni-P and Ni-P-PTFE coatings under the influence of bio-lubricants [47]. Wear rate was calculated using Eq. (5.3). From experimental results, it was concluded that Ni-P-PTFE coatings may prove to be advantageous under boundary lubrication situation due to its intrinsic low COF.

5.3.8 Four-Ball Arrangement

This arrangement is useful with respect to tribological behavior of bearings and wear preventive characteristics of lubricants. In this method, generally a rotating

Fig. 5.13 Schematic of crossed cylinder arrangement



ball presses against three stationary balls as shown in Fig. 5.14. A four-ball tester was used to evaluate the potential of electroless Ni-P-Si₃N₄ for reduction of wear of ferrous materials as bearings working under water-lubricated condition [48]. AISI 52100 ball bearings and races were coated with 4- μ m-thick Ni-P-2.9Si₃N₄. Tribological tests were carried out on a four-ball tester to simulate the performance of angular contact bearings. The test procedure was also modified to simulate actual water-lubricated condition. All the balls were submerged in demineralised water of pH 10 ± 0.5 . The upper ball was rotated at 500 rpm which made an angular contact of 40.8° with the lower balls. The three lower balls were free to rotate in an angular contact race. A load of 50 kg/mm² was applied and a running time of 18 h was set. The test parameters correspond to ISO 76 and 281 where at 90% reliability, the basic life rated is 1 million load cycle at the rated load. Test life of the upper ball was 6 h since a single load cycle of lower balls corresponds to three load cycles for the upper ball. Under such specified conditions, it was concluded that 4- μ m-thick Ni-P-2.9Si₃N₄ coating may be used for 9 years for tribological contact under water at pH 10 [48].

5.3.9 Inclined Plane Rig and Ramp Apparatus

An inclined plane rig is the simplest and most fundamental arrangement for measurement of friction coefficient and is shown in Fig. 5.15. A specimen block rests on a flat plane whose inclination with the horizontal is gradually increased. When the inclination θ with the horizontal is such that the block just starts sliding, then

Fig. 5.14 Schematic diagram of a four-ball configuration setup

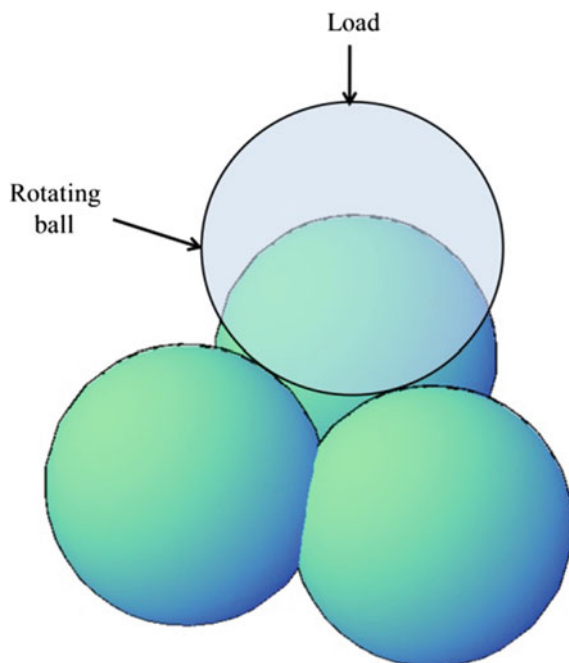
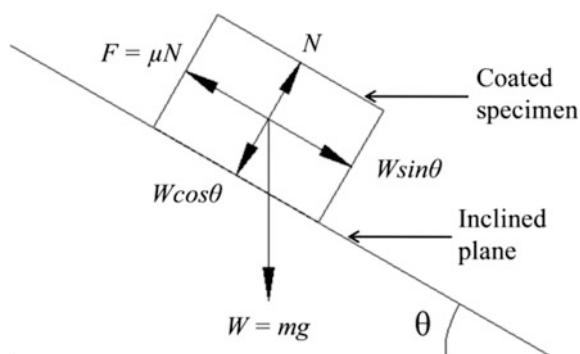


Fig. 5.15 Schematic of inclined plane test arrangement



coefficient of static friction is obtained as $\mu_s = \tan \theta$. But friction during continuous sliding is difficult to monitor using this method [1, 3].

A modification of the inclined plane rig led to the ramp apparatus which was used to report the kinetic friction coefficient of Ni-P coatings in as-deposited condition and post-heat treatment [49]. In the ramp apparatus, Ni-P coated blocks slid down a U-shaped ramp. A heat-sensitive ticker tape was attached to the coated specimen sliding down the ramp. The ticker tape also passed through a ticker device which was capable of producing sparks at a rate of 60 per second. As the specimen slid down, it pulled the ticker tape along with it through the ticker device.

This energized ticker device indented the tape with burn marks. The distance between two marks was representative of the distance travelled by the specimen in $1/60$ s. Thus, velocity and acceleration of the coated specimen could be calculated. Finally, the kinetic COF was obtained from the following relation [49]:

$$\mu = \tan \theta - \frac{a}{g \cos \theta} \quad (5.6)$$

where a is the acceleration of the coated specimen sliding down the block while g is the acceleration due to gravity and θ is the angle of ramp. Experiments were carried out both under dry and lubricated condition (3-in-1TM multipurpose oil and Leco[®] silicone mold release lubricant). Results were indicative of the fact that Ni–P coatings were capable of reducing the kinetic COF of their substrates irrespective of their heat treatment condition. Furthermore, it was also concluded that the effect of using lubricants on the COF was not significant enough. Coating a steel substrate with Ni–P may lead to better friction behavior than lubrication [49].

5.3.10 Reciprocating Rig

The reciprocating rig employs a reciprocating counterface material over a flat surface or vice versa. The counterface material may be in the form of a cylindrical pin or a spherical ball. The flat surface may be a rectangular plate or circular disc. Schematic diagram of a reciprocating test arrangement is shown in Fig. 5.16. Applied normal load, test duration, reciprocating frequency, and stroke length of the counterface are specified. Wear of the flat surface may be evaluated from its mass loss or volume loss or Eqs. (5.1)–(5.3).

Friction and wear characteristics of Ni–P coatings with 9.4 and 14.4 wt% P were evaluated on a reciprocating arrangement tribo-tester [50]. The reciprocating frequency was 40 min^{-1} , and a force of 98 N was applied. The upper stationary specimen was a cylindrical pin of GCr15 bearing steel (counterface) while reciprocating motion was given to Ni–P coated AISI 1045 rectangular block. Results

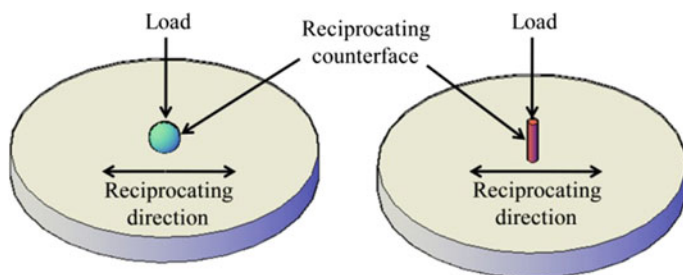


Fig. 5.16 Schematic diagram of a reciprocating rig

indicated that mass loss of the coatings increased with an increase in reciprocating numbers. Furthermore, wear mass loss decreased on heat treatment at 300 °C and lowest mass loss was exhibited by the coatings heat treated at 600 °C for 1 h. A major change in COF (0.2 at the beginning of test and 0.6 at the end of test) could not be observed in reciprocating motion in as-deposited or heat-treated condition for Ni–P coatings with 9.4 and 14.4 wt% P. The predominating wear mechanism was adhesion and scuffing.

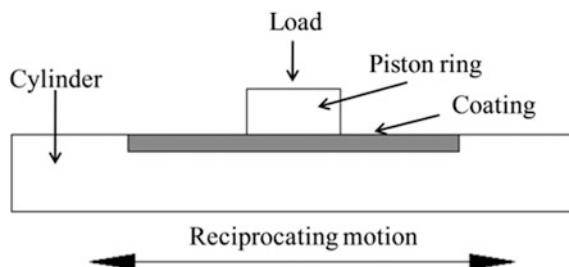
A reciprocating ball-on-disc wear testing machine was used to evaluate the wear resistance and COF of Ni–P nanometer diamond coatings [51]. Experiments were carried out under dry sliding condition at 20 N load, 10 Hz frequency, 1 mm amplitude, and 20 min test duration. The counterface upper ball used was GCr15 steel (hardness: 62HRC) with a diameter of 10 mm. Wear resistance was indicated in terms of wear volume loss (measured using a profilometer). Ni–P nanometer diamond coatings exhibited excellent wear resistance and low COF compared to Ni–P coatings. The tribological behavior improved further on heat treatment at 500 °C for 2 h in vacuum. The wear volume loss and COF of Ni–P nanometer diamond coating heat treated at 500 °C were $3.9 \times 10^{-4} \text{ mm}^3$ and 0.39, respectively, which was significantly lower than binary Ni–P alloy (wear volume: $10.2 \times 10^{-4} \text{ mm}^3$ and COF: 0.58).

In a recent study by Chang et al. [52], the tribological behavior of Ni–P and Ni–P/SiC coatings was evaluated on a reciprocating motion ball-on-disc sliding pair. The upper specimen was a ZrO₂ ball with 6.25 mm diameter. Tests were carried out under dry condition at 10 N load, 1 mm stroke, and 32 mm/s mean sliding velocity over a distance of 57.6 m. Wear resistance was evaluated in terms of volume loss of the coatings. In this case, results indicated that Ni–P binary alloy has superior wear resistance and anti-friction characteristics in as-deposited condition in comparison with Ni–P–SiC composites, whereas on heat treatment at 400 °C for 1 h (in Ar gas environment), the composites showed superior wear resistance and anti-friction characteristics compared to Ni–P alloy.

5.3.11 *Piston Ring–Cylinder Liner Arrangement*

This is particularly suited for the evaluation of EN coatings under lubricated condition for automotive applications. Due to excellent corrosion resistance, wear resistance and low COF, EN coatings and its variants have wide applicability in the automotive sector where tribological contact occurs under lubrication. The piston ring–cylinder liner tribometer is well suited for simulation of such experimental condition. A schematic diagram of the arrangement is shown in Fig. 5.17. In this setup, the piston remains fixed and the piston ring slides/reciprocates on its surface. Either the cylinder/liner material is coated or the piston rings. Sliding occurs under lubrication. Applied normal load, sliding speed, sliding distance, or sliding time are chosen accordingly. Friction and wear characteristics of Ni–P and Ni–P–Cu coatings were evaluated under bio-oil-lubricated condition on this arrangement [53].

Fig. 5.17 Schematic diagram of piston ring–cylinder liner arrangement



Cylinder specimens made of cast iron were coated, whereas piston rings were used as-received (without coating). Electroless Ni–P coatings showed tremendous potential under bio-oil-lubricated condition, wherein the wear loss of the cylinders could be reduced by almost 90%. This was attributed to the fact that Ni–P coatings had the ability to protect the cylinder from corrosiveness of bio-oil. Moreover, Ni–P–Cu coatings were observed to have higher wear resistance and lower COF in comparison with Ni–P. The coatings contributed to lower wear of the piston rings too. The evolution of COF with sliding velocity for both the coatings agreed well with the classical Stribeck curve where a transition from boundary lubrication (BL) to mixed lubrication (ML) to hydrodynamic lubrication (HL) took place with an increase in sliding speed. Enhanced tribological behavior of Ni–P–Cu compared to Ni–P coating was attributed to the formation of a tribo-film of CuO. In a recent study [54], it was reported that combined MoS₂ and GO in Ni–P coatings had a synergistic effect of reducing corrosive wear under bio-oil-lubricated condition. This has accelerated its potential use in IC engines where bio-oil is used. The tribological test setup used was a piston ring–cylinder liner arrangement for simulation of realistic situation [54].

5.3.12 Abrasion/Micro-abrasion and Erosive Wear Test

In a two-body abrasion test, the counterface material is a uniformly bonded abrasive paper/cloth. Abrasive wear resistance of electroless Ni–P and Ni–P–Al₂O₃ coatings was observed by employing a counterface wheel of 50 mm on which SiC (10-μm grit) paper was mounted [55]. Applied normal load was varied between 2 and 24.5 N. As-deposited and heat-treated coatings (400 °C for 1 h) were made to slide to and fro against the counterface at 40 cycles/min for 300 cycles. Worn dimensions 33 mm long and 10 mm wide were produced on the coatings. Anti-wear characteristics were observed from the mass loss of the coatings. Results indicated that as the applied normal load increased, wear mass loss increased for Ni–P as well as Ni–P–Al₂O₃ coatings. But the wear resistance of the composite coating was higher than the binary alloy in both as-deposited and heat-treated condition. The worn surfaces of heat-treated Ni–P coating appeared shiny due to the polishing action of

the SiC grit. Whereas in case of heat-treated Ni-P-Al₂O₃ coatings, the worn surface is characterized by plowing of the particles out of the EN matrix by SiC counter-surface. Incorporation of WC in the EN matrix was also seen to improve the abrasive wear resistance compared to the binary Ni-P alloy against a SiC abrasive wheel [56].

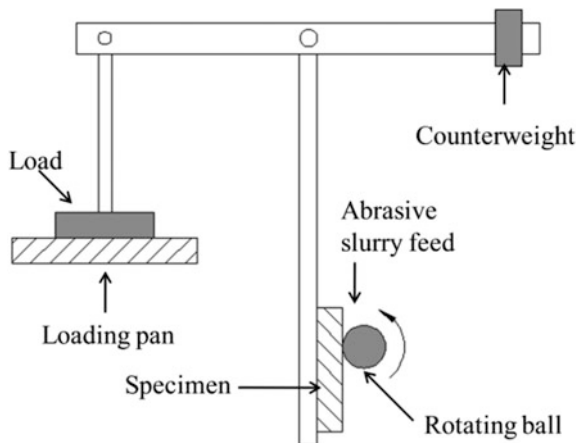
The micro-abrasion test is best suited for thin coatings, and it is possible to differentiate between abrasive wear of substrate and coating even though the coating is worn through its thickness [57]. A schematic diagram of a micro-abrasion test setup is shown in Fig. 5.18. The coated specimen is held stationary against a rotating ball in the presence of abrasive slurry. Wear scar produced during the test by the ball is assumed to reproduce its shape. On measuring the scar dimension, the wear coefficient of coating (k_c) and substrate (k_s) may be calculated as follows [57]:

$$\frac{SL}{b^4} = \left(\frac{k_s - k_c}{k_s k_c} \right) \left(\frac{\pi t}{4b^2} - \frac{\pi R t^2}{b^4} \right) + \left(\frac{1}{k_s} \right) \left(\frac{\pi}{64R} \right) \quad (5.7)$$

where S is the sliding distance of the ball, L is the normal load, b is the outer diameter of crater, R is the radius of ball, and t is coating thickness. Now if a linear plot is obtained for (SL/b^4) versus $(\pi t/4b^2) - (\pi R t^2/b^4)$, the slope and intercept gives the value of k_c and k_s , respectively. Lower wear coefficients indicate higher wear resistance.

Resistance to abrasive wear of Ni-P coating (deposited on aluminum alloy LM24) was observed under the influence of SiC (3 μ m) slurry [58]. The ball was made of AISI 440-C steel of 25.4 mm diameter while the load and speed was 0.5 N and 0.1 m/s, respectively. The number of ball revolutions was varied between 400 and 2000. With an increase in sliding distance, the wear volume increased for bare substrate as well as for as-deposited and heat-treated (330 $^{\circ}$ C for 1 h) Ni-P coating. Though, the wear volume of heat-treated coating was the lowest. Wear coefficient of EN coatings was observed to be lower than LM24 alloy indicating higher

Fig. 5.18 Schematic diagram of micro-abrasion setup [58]



abrasive wear resistance of the coatings. It was also observed that lapping of the substrates had a negative impact on the abrasive wear resistance. Abrasive wear resistance of LM24 alloy could be further improved by the deposition of Ni–P–SiC composite coatings [59].

The testing methods for erosive wear resistance include [1, 3]:

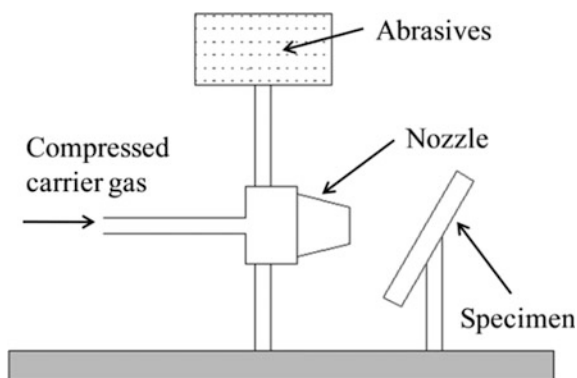
- Jet impingement method;
- Re-circulating loop test;
- Centrifugal accelerator;
- Whirling arm rig.

Details and schematic diagrams of these test procedure may be observed in the work done by Sahoo [1, 3]. Among the aforesaid methods, the jet impingement technique was reported for Ni–P coating (105 μm thick) deposited on pipeline steel [60]. EN coatings were deposited on pipeline steel to improve their resistance to solid particle erosion since this phenomenon reduces the working life of oil and gas pipelines significantly. In the jet impingement method, generally particles are accelerated through a nozzle along with a fluid stream. This accelerated stream strikes the target which is held at some distance from the nozzle at a fixed angle [1, 3]. To evaluate the erosive wear resistance of Ni–P coatings, alumina particles were accelerated through a nozzle of diameter 2.3 mm [60]. The coated specimen was kept at a distance of 3 mm from the nozzle at different angles (30° – 90°). The particle velocity was 36 m/s and dry compressed gas was the carrier. Test duration was varied between 5 and 600 s. A schematic diagram of the test arrangement is shown in Fig. 5.19. The normalized erosion rate was calculated as [60]:

$$E = \frac{\text{Erosion rate (mg/s)}}{\text{Particle flow rate (mg/s)}} \quad (5.8)$$

From experimental results, it was observed that mass loss of Ni–P coatings increased with an increase in impact angle [60]. In fact on increasing the impact angle beyond 30° , the wear mass loss of coatings was higher than pipeline steel. At

Fig. 5.19 Schematic diagram of erosive wear test by jet impingement [60]



90° impact angle, the weight loss of coatings as well as steel increased with an increase in test duration. This was attributed to the brittle nature of the deposited coatings. Hence, it was concluded that further work is required to improve erosive wear resistance of Ni–P coatings by increasing its toughness.

Another form of erosion test is the cavitation erosion. Cavitation occurs due to the collapse of numerous vapor bubbles due to localized pressure changes when fluid is flowing at high velocity. When bubbles collapse in a localized high-pressure region, shock waves are emitted that cause fatigue failure and material loss. To improve cavitation erosion behavior, the deposition of Ni–P coatings was considered as a surface modification technique [61]. An ultrasonic vibration rig with distilled water and 3.5% NaCl test solution was considered. Horn frequency and amplitude was 20 kHz and 25 μm , respectively. Electroless Ni–P coatings were deposited on AISI 1045 carbon steel substrates and heat treated at 400 °C for 1 h to increase hardness. Cavitation erosion of bare substrate and Ni–P coatings in as-deposited and heat-treated condition was measured in terms of mass loss (mg) after every 15 min for total duration of 120 min. In both distilled water as well as 3.5% NaCl, heat-treated Ni–P coatings had higher resistance to cavitation erosion. Furthermore, in both environments, the mass loss of coatings as well as bare substrate increased with an increase in test duration. But the resistance to cavitation erosion in distilled water for heat-treated Ni–P coatings was higher in comparison with NaCl. This was overcome by the inclusion of nano-SiC in Ni–P coatings which could reduce occurrence of pitting in the binary Ni–P alloy [62] in both distilled water as well as NaCl.

5.3.13 Corrosion Erosion Test

This method aims to check the synergistic effects of erosion and corrosion on the mass loss or rate of mass loss of the coatings. EN coatings are rotated in a slurry pot containing corrosive media as well as abrasives to which a three-electrode standard electrochemical cell is attached. This electrochemical cell monitors the corrosion parameters during the erosive wear of the coatings. Erosion corrosion resistance of Ni–P coatings could be improved by at least one order by the incorporation of reduced graphene oxide in the matrix [63].

5.3.14 Scratch Test

The response of EN coatings to scratch test is a measure of its adhesion. Generally, a diamond tip stylus traverses over the coating surface at some advance rate and loading rate. Failure of the coatings is then predicted by observing the scratch marks under a microscope or employing acoustic emissions. Response of electroless Ni–P and Ni–P–W coating to scratch test was reported by Tsai et al. [64]. In as-deposited

condition, both Ni–P and Ni–P–W coatings underwent failure by chipping and adhesive mode. In post-heat treatment, the length of cracks observed for Ni–P–W coatings was shorter than Ni–P. Thus, the properties of Ni–P could be improved by inclusion of W. Annealing at 600 °C in Ar environment improved the scratch hardness of Ni–P coating by 75% [65]. On the other hand, scratch test response of Ni–B coatings in as-deposited and heat-treated (400 °C) condition revealed an overall metallic and ductile behavior throughout the test [66]. No spalling or delamination of the 25- μ m-thick Ni–B coatings could be observed for a load rate of 100 N/min (final load of 150 N) and advance rate of 6.75 mm/min. Hence, it was concluded that Ni–B coatings have good adhesion to the substrate.

5.4 Summary and Future Research Directions

The tribological behavior of EN alloy and composite coatings has been widely investigated which suffice the demanding needs of industries. Such investigations have been carried out under different tribological contact conditions and environments. From the various approaches to measurement of tribological characteristics, it is seen that EN coatings have enhanced sliding wear resistance under dry condition as well as lubricated condition. In fact, the coatings are also beneficial for tribological contact under bio-oil-lubricated condition due to their high corrosion resistance. EN alloy as well as composite coatings may also prove to be beneficial for friction and wear reduction of mechanical components at high temperatures. This happens due to the formation of protective oxide glazes and microstructural changes resulting from in situ heat treatment. However, there is scope for improvement of high temperature tribological characteristics. Furthermore, simulation of actual sliding wear condition during hot metal-working processes and assessing the viability of EN alloy/composite coatings require further investigations. There is also wide scope for exploring the erosive wear characteristics of EN coatings. This may be done by tailoring the bath components and improving toughness of the coatings. Recent trends also indicate that the development of EN nano-composites is gaining importance due to their higher friction and wear reduction capabilities.

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