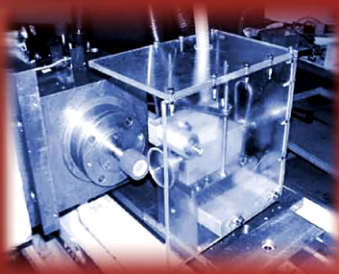




Principles of Modern Grinding Technology



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*To Margaret my wife, I decided this book for her love and support
throughout my work.*

Preface

Principles of Modern Grinding Technology explains in simple terminology the principles that led to rapid improvements in modern grinding technology over recent decades. Removal rates and quality standards have increased a hundred-fold. Very fine tolerances are routine due to improved understanding of the process and factors that need to be controlled. Superb machines have been developed that produce optical quality finishes. This book shows how quality can be improved and costs brought down at the same time as output is increased. Underlying principles, trends, and recent advances are discussed. More advanced analysis is presented in the appendixes at the end of the chapters which would be useful for students and researchers.

The book is aimed at practitioners, engineers, researchers, students, and lecturers. The approach is direct, concise, and authoritative. There are numerous worked examples. Progressing through each major element of a grinding system and then on to machine developments, the reader becomes aware of all aspects of operation and design. Trends are described demonstrating key features required for process improvements. Coverage includes abrasives and super-abrasives, grinding wheel design, dressing technology, machine accuracy and productivity, machine developments, high-speed grinding technology, cost optimisation, ultra-precision grinding technology, machine and process control developments, vibration control and problem solving, centreless grinding, coolants and developments in coolant delivery.

Later chapters analyse grinding mechanisms, and the final chapter includes the latest developments in analysing grinding temperatures. It is shown that under the right conditions, high removal rates can be achieved accompanied by low temperatures. These final chapters are of fundamental interest to researchers, practitioners, and students of the subject. For example, the last chapter presents an authoritative analysis of grinding temperatures including accurate data for both shallow and deep grinding at low and high work speeds. It also includes techniques for measurement of grinding temperatures.

Worked examples are included throughout the book for basic calculations. They give confidence in the scale and magnitudes of practical quantities.

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Salcombe, England
January 2009

Abbreviations

AE	Acoustic emission
ANSI	American National Standards Institution
CBN	Cubic boron nitride
CNC	Computer numerical control
ELID	Electrolytic in-process dressing
FEPA	Federation of European Producers of Abrasives
HEG	High-efficiency grinding
HEDG	High-efficiency deep grinding
ISO	International Standards Organization
PCD	Poly-crystalline diamond
PVD	Physical vapour deposition

Notations for Grinding Parameters

Symbols within a special context are explained in the relevant text.

a_d	Dressing depth of cut
a_e	Effective (real) depth of cut in grinding
a_p	Programmed (set) depth of cut in grinding
b, b_w	Width of grinding wheel contact with work
b_{cu}	Width of uncut chip
b_d	Dressing tool contact width
c	Machine damping
c, c_p	Specific heat capacity
d_c	Control wheel diameter in centreless grinding
d_g	Mean abrasive grain diameter
d_e	Effective grinding wheel diameter
d_s	Actual grinding wheel diameter
d_w	Workpiece diameter
e	Error
e_c, u	Specific cutting/grinding energy (energy per unit volume)
e_{ch}	Specific energy carried in chips
$erf()$	Error function given in math tables
f	Frequency in cycles per second (Hz)
f	Interface friction factor = τ/k
h	Thin film or chip thickness
h_{cu}	Theoretical measure of uncut chip thickness
h_{eq}	Equivalent chip thickness
h, h_f	Convection factor and work-fluid convection factor
h_w	Work height in centreless grinding
k	Shear flow stress
k	Thermal conductivity
k_w, k_g	Thermal conductivity of work material and abrasive grain
l_c	Contact length
l_g	Geometric contact length due to depth of cut
l_f	Contact length due to force and deflection of grinding wheel and workpiece
n	Number of grinding passes
n_d	Number of dressing passes
n_s	Grinding wheel rotational speed
n	Junction growth factor

p_p	Fluid pumping pressure
r_{cu}	Uncut chip width/chip thickness ratio = b_{cu}/h_{cu}
r_0	Average effective grain contact radius
q	Speed ratio = v_s/v_w
q	Flux value = Heat per unit area in unit time
q_d	Dressing roll speed ratio = v_d/v_s
t	Time
t_d	Dressing time
t_s	Grinding cycle time
t_s	Grain contact time within contact length
t_p	Point/flash contact time of grain and workpiece
t_t	Total cycle time including grinding and dressing
v_d	Dressing roll speed
v_f	Work feed rate
v_{fd}	Dressing feed rate
v_j	Jet velocity
v_s	Wheel speed
v_w	Work speed
x	Deflection
x, y, z	Position coordinates
A	Geometric stability parameter in centreless grinding
A	Wear flat area on grinding wheel as fraction or percentage
A_c	Apparent area of grinding contact zone = $l_c \cdot b$
A_{cu}	Cross-section area of uncut chip
Al_2O_3	Aluminium oxide, alumina
C	Number of active abrasive grains per unit area = cutting edge density
C	C factors giving temperature for particular grinding conditions
C_t	Total cost per part
D	Diameter as in journal diameter
E	Young modulus of elasticity
F_a, F'_a	Axial force and specific value
F_n, F'_n	Normal force and specific value
F_t, F'_t	Tangential force and specific value
G	G ratio
H_p	Fluid pumping power
H_f	Fluid drag power
H_t	Total fluid power
K	Grinding stiffness factor = a_g/a_p
K	Power ratio = H_f/H_p

K	Archard wear constant
K_s	Grinding stiffness = F_n/a_e
K_1	Work plate factor in centreless grinding
K_2	Control wheel factor in centreless grinding
L, B	Grain spacing in grinding direction and in lateral direction
L	Length as in bearing length or work length
L	Peclet number related to thermal diffusivity
N_d	Number of parts per dress
P	Grinding power
P_{NL}	No-load power
P_s, p_p	Supply or pumped pressure
Q, Q'	Removal rate and specific value
Q	Dynamic magnifier of machine deflection
Q	Bearing flow rate
Q_f	Nozzle fluid flow rate
Q_u	Useful fluid flow rate
R_a, R_t, R_z	ISO surface roughness parameters
R_e	Reynolds number
R_L	Contact length ratio = l_c/l_g
R_r	Roughness factor = l_{fr}/l_{fs}
R_w	Fraction of heat going into workpiece
R_{ws}	Work-wheel interface fraction of heat into workpiece
SiC	Silicon carbide
SG	Seeded gel (alumina composite abrasive)—trade name
T, ΔT	Temperature or temperature rise
U_d	Dressing overlap ratio
V	Volume removed
V_{cu}	Chip volume removed
α	Thermal diffusivity = $k/\rho c$
α	Work plate-wheel contact angle in centreless grinding
β	Thermal property
β	Tangent contact angle in centreless grinding
β	Bearing pressure ratio = design value of recess pressure/supply pressure
γ	Work plate angle in centreless grinding
γ	Friction angle = $(\cos^{-1}f)/2$
γ_d	Dressing sharpness ratio = a_d/b_d
ϕ	Grinding contact angle = l_c/d_e radians
Φ	Wheel porosity
Φ	Through-feed angle in centreless grinding
ρ	Density = Mass per unit volume

σ	Direct stress
τ	Time constant of an exponential decay or growth
τ	Shear stress
λ	Grinding system stiffness
μ	Grinding force ratio
ν	Poisson ratio
ω	Frequency (radians per second)
$\omega_n \omega_0$	Natural frequency, resonant frequency (radians per second)
Ω	Work angular speed (radians per second)

Basic Units and Conversion Factors

Length	1 metre = 39.37 inches
Mass	1 kilogram = 2.205 pounds mass
Force	1 newton = 0.2248 pounds
Energy	1 joule = 0.7376 foot pounds
Power	1 watt = 0.7376 foot pounds per second
Density	1 kg/m ³ = 0.06243 pounds mass per cubic foot
Pressure	1 pascal = 0.000145 pounds per square inch 1 bar = 14.5 pounds per square inch
Temperature	1 celsius degree rise = 1.8 fahrenheit degrees rise
Gravitational acceleration in free fall	9.807 m/s ² = 32.175 ft/s ²
Dynamic viscosity	1 N.s/m ² = 0.000145 lbf.s/in ² = 0.000145 reyns

1 Introduction

1.1 The Role of Grinding in Manufacture

Origins of Grinding

The use of abrasives for shaping goes back to more than 2000 years. Abrasive stones were used for sharpening early knives, tools, and weapons. From early times, abrasives have been used to cut and shape rocks and stones for construction of buildings and edifices such as the pyramids. Abrasives were also used for cutting and polishing gems. Abrasives continue to be used in increasingly diverse applications today, and much of modern technology relies on the abrasives industry for its existence. Even in the early days grinding was a finishing process applied to products approaching the most valuable stage in their production.

Grinding developed as a metal manufacturing process in the nineteenth century (Woodbury 1959). Grinding played an important role in the development of tools and in the production of steam engines, internal combustion engines, bearings, transmissions, and ultimately jet engines, astronomical instruments, and micro-electronic devices.

What Is Grinding?

Grinding is a term used in modern manufacturing practice to describe machining with high-speed abrasive wheels, pads, and belts. Grinding wheels come in a wide variety of shapes, sizes, and types of abrasive. The reader is introduced to important types of wheels and abrasives in the following chapters.

A Strategic Process

In the second half of the twentieth century, it was recognised that grinding is a *strategic* process for high-technology applications. It was realised, for example, by manufacturers of aero-engines and missile guidance systems that grinding was the key to achieving the necessary quality. This provided the motivation for rapid development in the latter part of the twentieth century. More recently still, grinding has become a strategic process for the production of optical quality surfaces for communications and electronic

devices. Modern technology has also seen a trend towards hard ceramic materials that bring new challenges for economic manufacture.

Quality and Speed

In recent decades, grinding has been transformed both for producing very high quality parts and for fast economic production (Inasaki et al. 1993). This trend is illustrated in Fig. 1.1 where grinding and cutting tools are seen as increasingly competitive both for machining accuracy and for production rate. Owing to modern developments, grinding has a large role in efficient manufacturing industry in terms of both volume and value. For example, in a process known as planar grinding, many flat parts can be ground simultaneously on one worktable. This allows extremely high removal rates to be achieved in addition to high accuracy.

Grinding is used mainly for one or more of the following reasons.

Machining Hard Materials

Abrasive processes are the natural choice for machining very hard materials. It is a general rule that the tool used for machining should be harder than the material being machined. Suitable abrasives to grind hardened steels and aerospace alloys include aluminium oxide, silicon carbide, and cubic boron nitride. A diamond abrasive is used to grind hard ceramics. Hard ceramics are difficult to machine because they are not only very hard but also brittle. Grinding is well suited to coping with the challenges presented by new engineering materials such as silicon nitride.

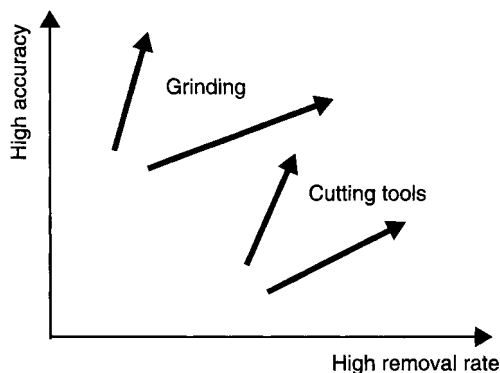


Figure 1.1 Trends in the application of grinding wheels and cutting tools.

Accuracy

Grinding allows high accuracy to be achieved and close tolerances can be held for size, shape, and surface texture. Grinding is used to machine large parts such as machine tool slide-ways where straightness is important and tolerances are usually specified in microns (μm). Grinding is also used to machine small parts including contact lenses, needles, electronic components, silicon wafers, and rolling bearings where all aspects of accuracy are important and tolerances extend from micron to sub-micron and can even approach the nano range. Nano grinding is a process in which accuracies of less than $0.1 \mu\text{m}$ are required. Nano grinding using the Electrolytic In-Process Dressing (ELID) process replaces polishing and achieves vastly improved removal rates for such applications as mirror finish grinding and production of micro tools used in nanotechnology.

Surface Texture

Roughness can be reduced down to mirror finishes and optical quality of flatness. The achievement of this quality depends on the roughness of the abrasive, the quality of the grinding machine, and the removal rates employed.

Surface Quality

Grinding carefully can ensure good quality where other processes may have difficulty meeting specifications. Quality is a term that includes all aspects required for parts to function correctly. Accuracy of dimensions and surface finish are obvious aspects of quality. Another aspect is surface quality. The integrity of the material at the machined surface may not always be obvious but is vitally important in many situations. For example, the surface of a hardened part should not be softened or cracked. It may also be important to avoid tensile residual stresses that reduce strength and shorten service life. All these aspects of quality require careful design and control of the grinding process.

Speed of Production

Speed of production depends on the material being machined and the accuracy and quality required. Grinding can be used to combine high removal rate with accuracy; for example, flute-grinding of hardened twist

drills from a solid bar is accomplished in a few seconds. Alternatively, grinding can be employed with moderate removal rates to produce highly accurate parts in large volumes. Examples are bearing rings and rolling elements for bearings. Nano grinding can be considered as a high removal rate process because it replaces much slower processes such as lapping and polishing.

Cost

Alternative processes such as hard turning may be feasible, but often it is grinding that is the least expensive and achieves the quality and speed of production required.

The Value-Added Chain

Grinding is usually done towards the end of product manufacture when the value of the parts is already significant and where mistakes can be expensive. The build-up of costs in product manufacture is illustrated schematically in Fig. 1.2.

As the parts move from one operation to another, such as turning, hardening, tempering, and then grinding, they achieve greater value, and the cost of holding stocks is increased. There are costs of moving parts and protecting them from damage. The increase in cost and lead time with the number of operations is not linear but exponential.

Reducing the Number of Operations

If the number of operations and the lead time can be reduced, it is found that the overall cost of manufacture can be greatly reduced.

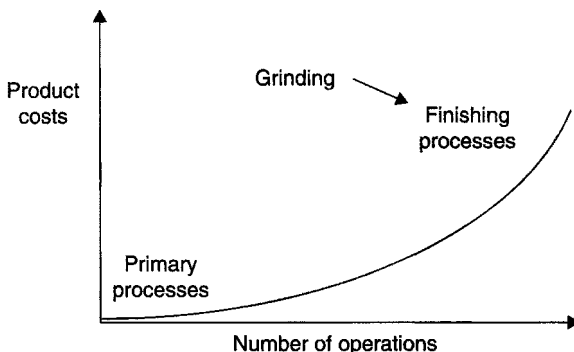


Figure 1.2 The build-up of costs and value addition in product manufacture.

Manufacturers either want to eliminate the grinding process altogether if the required quality can be achieved through an earlier process or else eliminate an earlier process if grinding can achieve the form and accuracy in one operation or even on one machine. Grinding tends to govern the accuracy of the parts produced and is often the key to the required quality. For example, the grinding of the flutes of hardened twist-drills to full form in one operation is very efficient.

1.2 Basic Grinding Processes

Basic Surface and Cylindrical Grinding Processes

Two main classes of grinding are flat surface grinding and cylindrical grinding. Photographs of typical machines appear in Chapters 10 and 11. These two classes of machines provide the four basic processes illustrated in Fig. 1.3. The figure shows peripheral grinding of flat surfaces and cylindrical surfaces and face grinding of non-rotational flat surfaces and rotational flat surfaces. Face grinding of rotational flat surfaces can be carried out on a cylindrical grinding machine and is termed cylindrical face grinding. Basic cylindrical grinding processes include external, internal, and centreless variants.

Internal and External Variants

Figure 1.4 shows examples of internal and centreless grinding processes. Internal grinding of bores is a cylindrical process where a small grinding wheel is mounted on a slender spindle known as the quill and the workpiece is held in a chuck or collet. In the internal centreless process, the workpieces may be held and rotated on a face plate. External centreless grinding is a process where the workpiece is supported at its external surface against a work rest and against a control wheel.

The Range of Processes

In practice, the complete range of grinding processes is very large. It includes profile generating operations and profile copying operations. Profiling processes include grinding of spiral flutes, screw threads, spur gears, and helical gears using methods similar to gear cutting, shaping, planning,

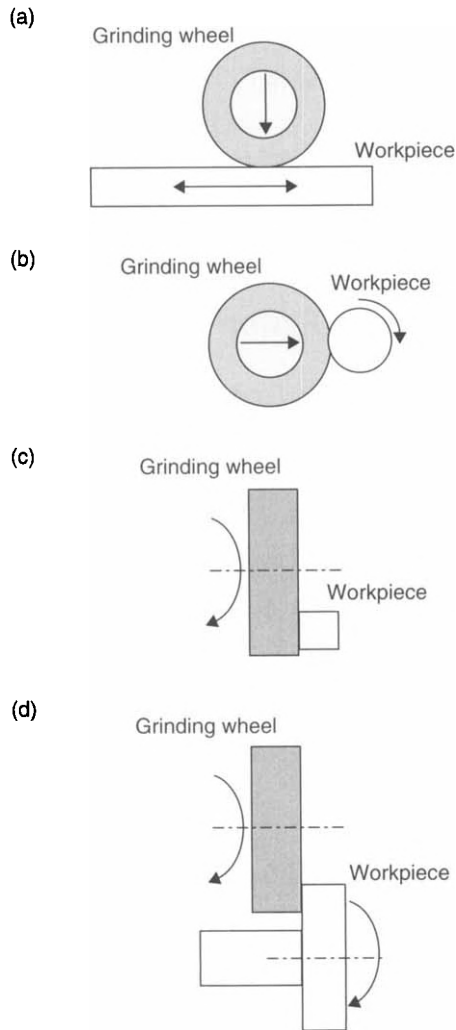


Figure 1.3 Four basic grinding processes: (a) peripheral surface grinding, (b) peripheral cylindrical grinding, (c) face surface grinding, and (d) face cylindrical grinding.

or hobbing with cutting tools. There are other processes suitable for grinding cam plates, rotary cams, and ball joints.

Examples of a variety of these processes are illustrated in Marinescu et al. (2004, 2006). Other useful books for reference are Andrew et al. (1985), CIRP (2004), King and Hahn (1986), Malkin (1989), and Tawakoli (1990).

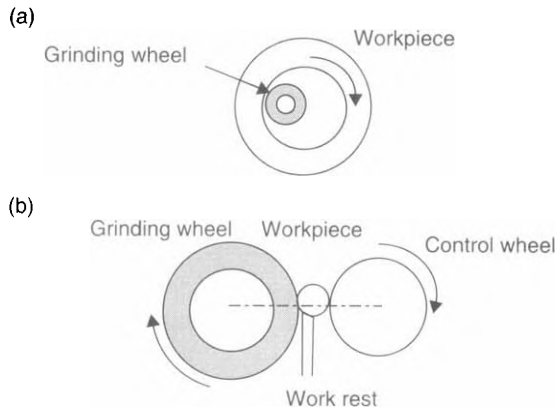


Figure 1.4 Grinding processes: (a) Internal grinding and (b) centreless grinding.

1.3 Specification of the Grinding System Elements

Basic Elements

Figure 1.5 illustrates the basic elements of a grinding system that the engineer has to coordinate. Grinding is most productive when all the elements of the system have been selected to work well together. Some of the elements to be considered are the grinding machine, the grinding wheel, the workpiece, the grinding fluid, the atmosphere, and the grinding swarf. Another is the wheel dressing tool.

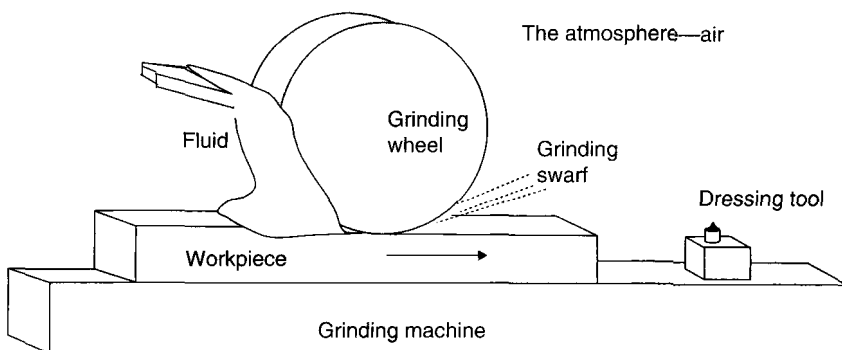


Figure 1.5 Elements of a basic grinding system.

System Elements

The system elements consist of inputs, disturbances, productive outputs, and non-productive outputs (Czichos 1978). The elements of a grinding system are illustrated in Fig. 1.6.

Element Characteristics

A system specification includes the following details:

- Workpiece material: Shape, hardness, stiffness, thermal, and chemical properties.
- Grinding machine: Type, control system, accuracy, stiffness, temperature stability, vibrations.
- Kinematics: The geometry and motions governing the engagement between the grinding wheel and the workpiece. Speeds and feeds of the workpiece and the wheel.
- Grinding wheel: Abrasive, grain size, bond, structure, hardness, speed, stiffness, thermal, and chemical properties.
- Dressing conditions: Type of tool, speeds and feeds, cooling, lubrication, and maintenance.
- Grinding fluid: Flow rate, velocity, pressure, physical, chemical, and thermal properties.

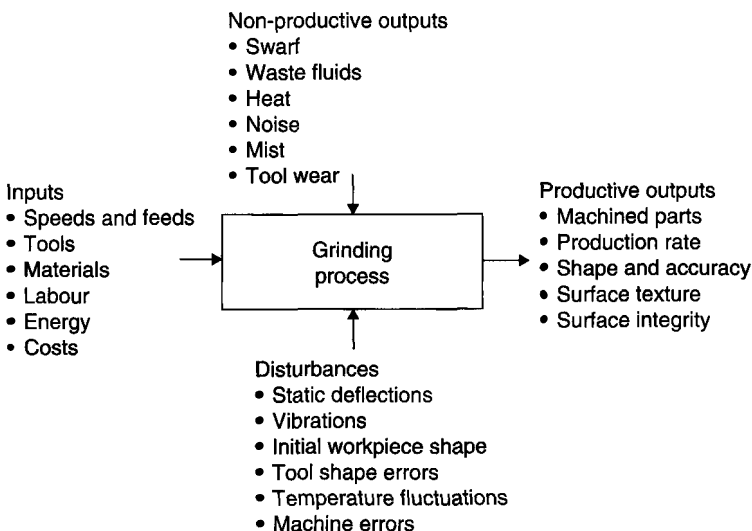


Figure 1.6 Inputs and outputs of a grinding process.

- Atmospheric environment: Temperature, humidity, and effect on environment.
- Health and safety: Risks to the machine operators and the public.
- Waste disposal.
- Costs.

Grinding Machine

The importance of the grinding machine is clear. The machine structure provides static and dynamic constraint on displacements between the tool and the workpiece. A well-designed machine limits vibrations and provides high accuracy movements. The specification, design, and manufacture of the grinding machine is therefore key to grinding performance. A chapter on grinding machine developments outlines the key principles.

Grinding Fluid

The grinding fluid serves three main functions:

- Reduces wheel wear
- cools the workpiece
- flushes away the swarf

As knowledge and awareness of environmental concerns increases, there is a move towards a closer specification of the grinding fluid and the quantities supplied. This issue is addressed in a special chapter.

Atmosphere

The atmosphere is important for effective grinding. Most metals when machined experience increased chemical reactivity owing to two effects:

- Newly created surfaces are more highly reactive than an already oxidised surface.
- High temperatures and rubbing at the interfaces increase speed of reaction.

Oxides or other compounds are formed very rapidly on the underside of the chips and on the new surfaces of the workpiece. Oxides of low shear strength reduce friction. It is important to emphasise that physical, chemical, and thermal aspects all play an important role.

1.4 The Book and Its Contents

The Emphasis

The book explores modern trends in grinding through consideration of the underlying principles that make these machine and process developments possible. The emphasis is on why things happen. Readers will be able to see how to overcome problems and find their own solutions. The book identifies aims and objectives whether these are better quality, increased production rate, lower costs or increased flexibility of manufacture.

Conventional and New Processes

Conventional and new processes are described in this book. New processes include the use of super-abrasives, High Efficiency Deep Grinding (HEDG), speed-stroke grinding, ELID grinding, ultrasonic grinding, and nano grinding. New abrasive structures include the new ranges of micro-crystalline abrasives and high aspect ratio abrasives. Super-abrasives include cubic boron nitride and diamond in resin, vitrified, and metal bonded forms for either conventional grinding or ELID grinding. A substantial chapter describes developments in ultra-precision grinding.

Who Should Read this Book?

The book is aimed at the industrialist, user, teacher, or researcher concerned with developments in grinding technology.

Worked Examples

Numerous worked examples provide scale and magnitude in typical grinding applications.

Book Outline

Basic Removal (Chapter 2)

Basic grinding parameters are introduced together with practical grinding results, principles of material removal, and practical measures for improvement of performance. Results are presented showing that material removal rate can be optimised by selection of suitable grinding conditions.

Grinding Wheels and Dressing (Chapters 3 and 4)

Chapter 3 discusses the types of grinding wheels and grinding wheel developments. Trends towards new abrasives are described including design of wheels for higher speeds and wheels for high accuracy. The latest developments in grinding wheels and dressing are essential for the development of ultra-precision grinding systems. It is shown how modern developments in abrasives and machines have enabled enormous increases in productivity and also achievement of sub-micron tolerances. Chapter 4 introduces the technology of dressing for preparation and use of grinding wheels. Results are presented showing how different dressing conditions affect grinding performance. Techniques are described to cope with modern grinding wheels for both high production rates and extremely high accuracies going into the nano range.

Grinding Wheel Behaviour (Chapter 5)

Wheel contact and wear effects introduce factors that strongly affect grinding wheel behaviour. These factors include the number and sharpness of the abrasive grains in contact with the workpiece and the wheel-workpiece contact conformity. These factors make a wheel glaze or experience self-sharpening behaviour. Another factor is elasticity which can change contact conformity and damp out vibrations. This chapter is essential reading for understanding the performance of grinding wheels in production operations, and it explains different wear rates in different operations. Contact behaviour is analysed in greater depth later in Chapters 12 and 15.

High-Speed Grinding (Chapter 6)

Very high wheel speeds are employed in the pursuit of higher production rate and reduced costs. However, the introduction of high wheel speeds and high removal rate grinding has a number of implications for the user. The different domains of creep grinding, speed-stroke grinding, and high-efficiency deep grinding are distinguished. This chapter also introduces the challenges to maintain workpiece integrity.

Thermal Damage (Chapter 7)

Thermal damage is often the limiting factor for removal rate in high-speed grinding. The types and causes of thermal damage and how to avoid these problems are explained here.

Fluid Delivery (Chapter 8)

Grinding fluids are introduced as well as fluid delivery requirements. Effective fluid delivery is of key importance in avoiding thermal damage and also in the economic achievement of acceptable quality levels. Fluid delivery has become a more critical element in grinding system design. A new treatment of this subject points the way to economic delivery solutions.

Grinding Costs (Chapter 9)

A systematic approach is provided for analysis of costs. This approach allows the evaluation of potential avenues for reducing costs depending on the particular requirements of an application. Experimental results are given showing potential benefits of more expensive abrasives. It is also shown that the number of parts per dress can be critical for the selection of economic grinding conditions.

Grinding Machine Developments (Chapter 10)

Design principles are defined for the achievement of high accuracy and high removal rates. Very few grinding machines follow all these principles but it is shown that application of these principles can lead to remarkable improvements in performance. A number of recent developments in grinding machine design are described. This chapter also introduces recent developments for sub-micron and nano grinding.

Process Operation and Control (Chapter 11)

Chapter 11 introduces the control of grinding processes to achieve high accuracy and high removal rates. Modern principles of process control are introduced including automatic process compensation and optimisation.

Vibrations (Chapter 12)

Vibration behaviour is described in Chapter 12 including the methods of avoiding vibration problems. Impulsive vibrations, forced vibrations, and self-excited vibrations are analysed and stability charts are presented.

Centreless Grinding (Chapter 13)

Centreless grinding has rather special characteristics owing to the unique method of workpiece location. Avoiding vibration problems and achieving roundness for centreless grinding is explored. Results are presented showing suitable set-up conditions for achievement of rapid rounding.

Mechanics of Grinding Behaviour (Chapters 14–17)

Factors governing grinding behaviour are explored in greater depth. Chapter 14 deals with material removal by individual grains and relates grain removal to grinding behaviour. Results presented explain how small differences in wheel structure affect surface roughness achieved in grinding and also wheel life. A major difference between grinding and milling lies in the random spread of grinding grits. Ideally, the distribution should be uniformly random. Sometimes, grains clump together giving rise to non-uniform randomness. This changes the way a wheel behaves. Expressions are given for chip size and relationships with surface roughness and forces. Chapter 15 analyses abrasive contact for rigid and elastic wheels. Elastic wheels behave differently from rigid wheels. Chapters 16 and 17 explore the energy required in grinding and how to minimise energy. Chapter 17 describes material behaviour in the process of removal and effects on wheel wear.

Grinding Temperatures (Chapter 18)

Finally, the important subject of workpiece temperature rise is presented simply and with improved accuracy of prediction. Based on many years of research, it is possible to reveal how temperatures vary dramatically in different grinding regimes. For traditional grinding processes, energy mostly goes straight into the workpiece often causing thermal damage. In modern creep-feed grinding and also in high-efficiency grinding, only a very small proportion of the energy enters the workpiece, and quality can be maintained at extremely high removal rates.

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2 Basic Material Removal

2.1 The Removal Process

A grinding wheel cuts through the workpiece material as the workpiece passes underneath. Normal and tangential forces are generated between the grinding wheel and workpiece as in Fig. 2.1. The forces cause abrasive grains of the grinding wheel to penetrate the workpiece.

A grain that cuts deeply into the workpiece carves out a chip whereas a grain that rubs the workpiece very lightly may fail to penetrate the surface. A grain that rubs without penetration causes mild wear of the surface that may be hardly detectible. There is a third situation where the grain penetrates and ploughs the surface causing ridges without necessarily removing material as in Fig. 2.2 (Hahn 1966). Rubbing, cutting and ploughing are three stages of metal removal. Some grains rub without ploughing. Some grains plough without cutting and some grains experience all three stages. The transition from rubbing to ploughing and then from ploughing to cutting depends on increasing depth of grain penetration into the surface.

Many aspects of grinding behaviour depend on the extent of rubbing, ploughing, and cutting involved. Abrasive grains that are mainly rubbing wear differently from grains involved mainly in heavy chip removal. As a consequence, grinding forces, grinding energy, surface texture, and wheel life are all affected, and grinding behaviour can only be explained in terms of the nature of the grain contact and effects on grain wear. The following is an introduction to these effects.

Figure 2.1 illustrates the down-cut grinding direction of wheel rotation. In down-cut grinding, abrasive grains penetrate to a maximum depth immediately after contacting the workpiece and penetration reduces to zero as the grains move through the contact. Up-cut grinding is where the wheel rotates in the opposite direction so that grain penetration steadily increases as the grains pass through the contact. Up-cut and down-cut grinding modes exhibit small differences in grinding energy, grinding forces, surface finish, tendency to burn, and wheel wear (Tawakoli 1993). Partly, these differences are owing to differences in grain impact and the extent of rubbing, ploughing, and cutting.

In down-cut grinding, chip removal occurs at the beginning of contact by an individual grain. In conventional down-cut grinding, forces tend to be lower, and there are advantages for surface roughness and reduced wheel wear. In up-cut grinding, an individual grain coming into contact

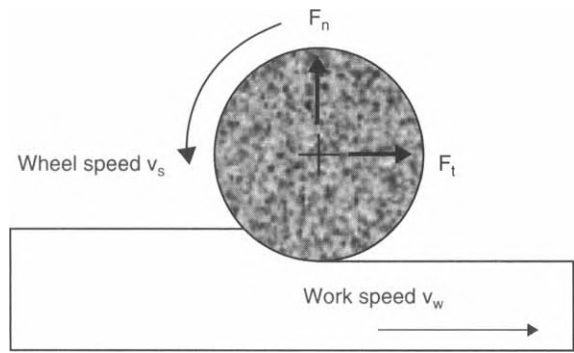


Figure 2.1 Material removal as the workpiece passes the grinding wheel in down-cut grinding.

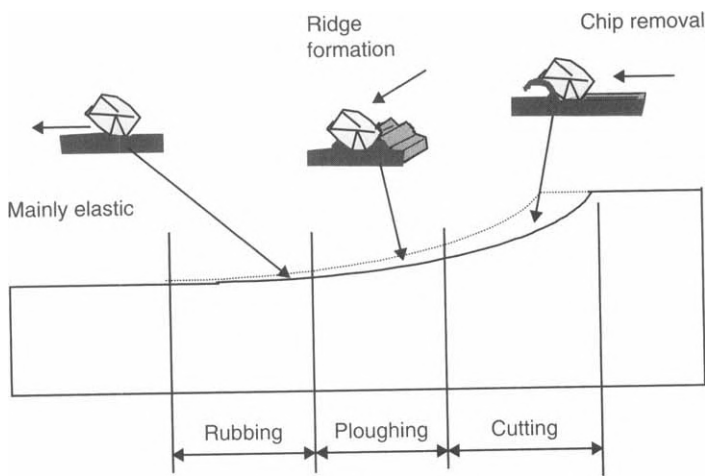


Figure 2.2 Rubbing, ploughing, and cutting at different grain penetration through the arc of contact.

rubs against the workpiece initially and chip removal is achieved later in the passage through contact.

Up-cut grinding tends to be less aggressive towards the abrasive grains. Rubbing continues for a greater extent than in down cut. The grains have a greater tendency to become blunt in up-cut grinding leading to higher grinding forces and higher wheel wear. In down-cut grinding there is a greater initial impact between the grain and the workpiece and a greater tendency for grain micro-fracture. This helps to maintain wheel sharpness and reduces the overall rate of wheel wear. Cooling is more efficient in

up-cut grinding as fluid is carried into the contact on the finished portion of the workpiece.

Depth of grain penetration plays an important role in grinding as argued independently by Guest (1915) and Alden (1914) almost 100 years ago. In practice, it is quite difficult to determine the depth of grain penetration with any degree of accuracy. However, that is less important than being able to predict effects of changing speed, feed, and depth of cut as summarised in the previous paragraph.

Figure 2.3 shows wheel speed v_s , work speed v_w , and depth of cut a_e for four basic grinding operations. Work speed is often termed feed rate and is given in terms of components tangential to the wheel, normal to the wheel, and parallel to the wheel axis. These components may then be labelled as v_{ft} , v_{fn} , and v_{fa} . The depth of cut is sometimes known as the feed increment or the in-feed.

2.2 Depth of Material Removed

The most basic grinding parameter is the real depth of cut a_e . The machine operator sets or programmes a depth of cut a_p . As every operator knows, in a single pass of the grinding wheel across the workpiece, the real depth of material removed is much less than the programmed depth of cut. This is illustrated in Fig. 2.4.

In horizontal surface grinding, the set depth of cut is the down-feed.

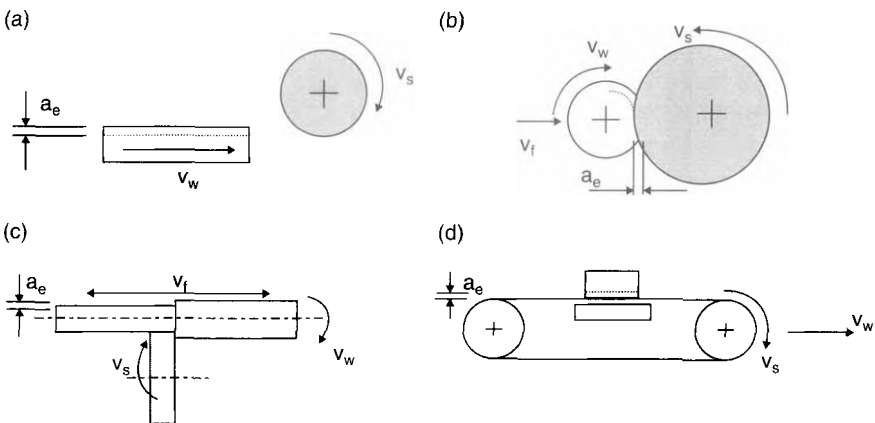


Figure 2.3 Speeds and depth of cut for four basic operations: (a) up-cut surface grinding, (b) plunge cylindrical grinding, (c) traverse cylindrical grinding, and (d) abrasive belt machining.

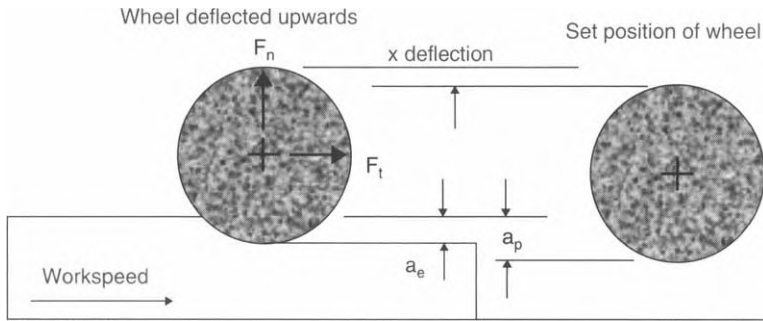


Figure 2.4 Effect of grinding forces on wheel deflection and real depth of cut.

In plunge cylindrical grinding between centres, the set depth of cut is the in-feed per revolution of the workpiece. The time required for one revolution of a workpiece of diameter d_w is $\pi \cdot d_w / v_w$. This gives a depth of cut $a_p = \pi \cdot d_w \cdot v_f / v_w$, where v_f is the in-feed rate and v_w is the work speed. After a number of revolutions the real depth of cut approaches the value of the set depth of cut as analysed below.

In plunge centreless grinding, the set depth of cut is the in-feed per half-revolution of the workpiece. The depth of cut in centreless grinding is $a_p = \pi \cdot d_w \cdot v_f / 2 \cdot v_w$.

It is necessary to measure the workpiece to determine the actual depth of material removed. Typically a_e is approximately a quarter of a_p depending on the workpiece hardness. The fraction also depends on grinding wheel sharpness, machine tool stiffness, grinding wheel stiffness, contact width, work speed, and wheel speed. All of these can affect grinding forces substantially resulting in deflection x of a system. Wheel wear a_s reduces the real depth of cut and thermal expansions x_{exp} of the workpiece, and machine elements usually increase real depth of cut as in Eqn (2.1).

$$a_e = a_p - x - a_s + x_{exp} \quad \text{real depth of cut} \quad (2.1)$$

Example 2.1 The programmed depth of cut in horizontal surface grinding is set by the machine operator first detecting contact between the wheel and the workpiece. The machine operator then sets a down-feed of $25 \mu\text{m}$ (or 0.00098 in.). At the beginning of the pass, the grinding wheel surface deflects upwards by $15 \mu\text{m}$ (or 0.00059 in.). The wheel has not had time to wear and the workpiece has not had time to expand. At the end of the pass in horizontal surface grinding, the grinding wheel has reduced in radius by $4 \mu\text{m}$ (or 0.00016 in.), the grinding wheel surface is deflected upwards by $13 \mu\text{m}$ (or 0.00051 in.),

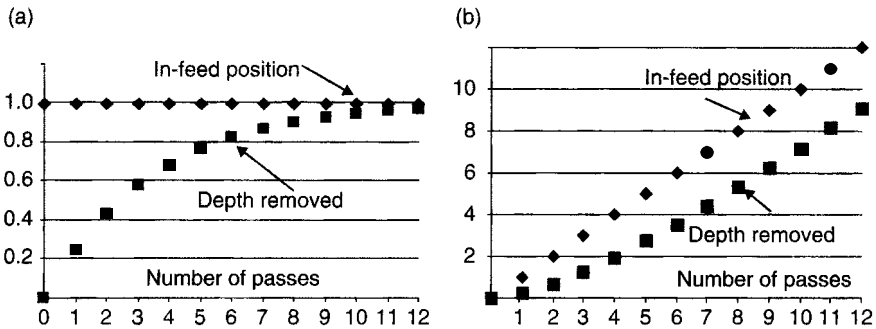


Figure 2.5 Effect of deflections on depth of material removed. (a) A single feed increment and (b) an increment after each pass.

and the workpiece has expanded by 1 μm (0.00004 in.). What is the difference in real depth of cut along the workpiece length?

$$\text{Start } a_e = 25 - 15 - 0 + 0 = 10 \mu\text{m (or 0.000394 in.)}$$

$$\text{End } a_e = 25 - 13 - 4 + 1 = 9 \mu\text{m (or 0.000354 in.)}$$

The difference in real depth of cut along the length is $10 - 9 = 1 \mu\text{m}$ (or 0.00004 in.).

In Fig. 2.5, the first example is for a single in-feed a_p followed by a number of passes without further feed. With successive passes, the total material removed approaches the set value. The second example is for additional feed increments a_p applied after each pass. In this case, deflections build up until the real depth of cut approaches the magnitude of the feed increment.

The Stiffness Factor K

Ignoring wheel wear and thermal expansion, set depth of cut equals real depth of cut plus deflection. That is, $a_p = a_e + x$. The proportion of the set depth removed depends on the machine stiffness and the grinding stiffness. The proportion is termed the stiffness factor K, where $K = a_e/a_p$. Deflection x depends on λ , the overall machine stiffness, as $x = F_n/\lambda$. Normal grinding force F_n depends on how hard it is to grind the workpiece material and is given by $F_n = K_s \cdot a_e$, where K_s is termed the grinding stiffness. It follows that $x/a_e = K_s/\lambda$. The stiffness factor is therefore given by $K = 1/(1 + K_s/\lambda)$. This means that when $K_s/\lambda = 1$, the stiffness

factor $K = 0.5$, and it will be found that real depth of cut is only half the programmed depth of cut. In practice, it is found that grinding stiffness K_s increases proportionally with grinding width. Doubling K_s/λ to a value of 2 reduces the stiffness factor K to 0.333.

In finish grinding, a value $K = 0.4$ represents a stiff machine and moderate grinding forces. A value $K = 0.1$ represents a compliant machine and high grinding forces. In high efficiency deep grinding (HEDG) using high wheel speeds and taking very deep cuts, the value of K is usually much higher.

Size Error

Figure 2.5 illustrates a size error between set depth removed and actual depth removed during spark-out. It can be seen that the size error is given by

$$e = (1 - K)^n \quad \text{size error in spark-out} \quad (2.2)$$

where n is the number of traverse passes after the last feed increment. Increasing the number of spark-out passes reduces the size error. Taking 12 spark-out passes with $K = 0.25$ reduces the error down to 3.2% of a_p . The size error can be reduced by increasing machine stiffness, reducing grinding stiffness, or increasing the number of passes.

Example 2.2 The wheel is given a down-feed of $25 \mu\text{m}$ (or 0.00098 in.) in horizontal surface grinding. The stiffness factor is $K = 0.3$. After 10 spark-out passes without further down-feed, what is the size error due to system deflection?

Set depth of material removed: $25 \mu\text{m}$ (or 0.00098 in.)

Material removed after 1 pass: $25 \times 0.3 = 7.5 \mu\text{m}$ (or 0.0003 in.)

Size error after 1 pass: $25 - 7.5 = 17.5 \mu\text{m}$ (or 0.00069 in.)

Size error: $e = 25 \times (1 - 0.3)^{10} = 0.71 \mu\text{m}$ (or 0.000028 in.)

Example 2.3 In horizontal surface grinding, the wheel is given a down-feed of $25 \mu\text{m}$ (or 0.00098 in.) before each pass. The stiffness factor K is 0.3. After a large number of down-feeding passes, what will be the size error due to deflections?

After a large number of passes, the real depth of cut is equal to the down-feed per pass: $a_e = 25 \mu\text{m}$ (or 0.00098 in.)

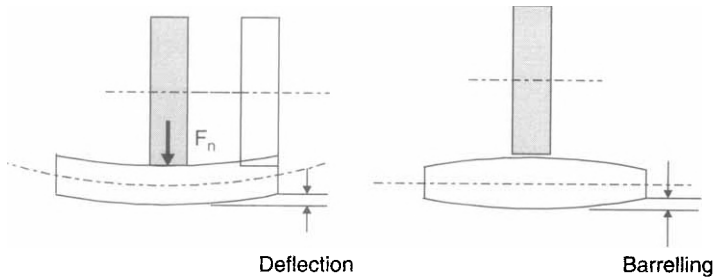


Figure 2.6 Workpiece deflection in traverse grinding leading to barrelling after grinding.

Since a_c is $K \cdot a_p$: $a_p = 25 / 0.3 = 83 \mu\text{m}$ (or 0.00327 in.)

The size error is therefore $83 - 25 = 58 \mu\text{m}$ (or 0.00317 in.)

In plunge cylindrical grinding between centres, we can perform the same error calculation, but n is now the number of complete workpiece revolutions. In centreless grinding, the diameter is adjusted approximately twice per revolution. In this case n is the number of half revolutions.

Barrelling

In traverse cylindrical grinding, the depth of cut is further affected by workpiece bending. In this case the total deflection is larger when grinding at the mid-point along the workpiece length. As a consequence, the depth of cut is larger at the ends of the workpiece than in the middle. The workpiece becomes barrel-shaped as illustrated in Fig. 2.6.

Barrelling can be reduced by taking a large number of passes for spark-out as described above. Unfortunately, this is time-consuming. To reduce the time taken, a work-steady can be employed to support a long workpiece at the mid-point.

2.3 Equivalent Chip Thickness

The depth of material removed a_c is very much larger than the thickness of the layer emerging from the grinding zone at wheel speed. The material is speeded up from work speed to wheel speed, and if the material emerged as a solid extruded sheet it would have a thickness correspondingly reduced to the value known as the equivalent chip thickness h_{eq} .

$$h_{eq} = a_e \cdot \frac{v_w}{v_s} \quad \text{equivalent chip thickness} \quad (2.3)$$

Example 2.4 The real depth of cut after a number of revolutions of the workpiece in a plunge cylindrical grinding operation is 10 μm (or 0.00039 in.). The grinding wheel speed is 60 m/s ($\approx 12,000$ ft/min) and the work speed is 0.3 m/s (or 0.98 ft/min). What is the equivalent chip thickness?

$$\begin{aligned} \text{Working in consistent units of mm: } h_{eq} &= 0.010 \times 300/60,000 \\ &= 0.00005 \text{ mm or } 0.05 \mu\text{m (or } 0.000002 \text{ in.)} \end{aligned}$$

Equivalent chip thickness is often used as a proxy for actual chip thickness as the latter cannot be easily defined or measured (Snoeys et al. 1974). Equivalent chip thickness has been found to be particularly valuable for correlating easily measured grinding parameters with removal rate parameters for a particular grinding wheel type. It can be seen that increasing depth of cut and work speed tends to increase the equivalent chip thickness whereas increasing wheel speed reduces it. Increasing equivalent chip thickness implies increasing the stress on the abrasive grains whereas reducing equivalent chip thickness reduces the stress on the abrasive grains. This gives an immediate explanation for the trend to increase wheel speeds.

Of course, the material does not emerge as a solid sheet. It is cut into many smaller chips. The thickness of the chips must greatly exceed the equivalent chip thickness to account for the discrete nature of material emerging. Factors which affect chip thickness include the distribution of cutting edges on the wheel surface; the effect of chip thickness on grinding behaviour is discussed further in Chapter 14.

2.4 Material Removal Rate

Material removal rate in grinding is usually quoted in terms of removal rate per unit width of grinding contact. Removal rate per unit width is known as specific removal rate Q' . Using the specific removal rate reduces the number of variables and allows direct comparison of removal efficiency across a wide range of operations (Fig. 2.7).

A moderate removal rate of $Q = 50 \text{ mm}^3/\text{s}$ over a 25-mm wide cut is quoted as $Q' = 2 \text{ mm}^3/\text{s}$ per mm width or $2 \text{ mm}^2/\text{s}$ equivalent to $0.186 \text{ in.}^2/\text{min}$ in British units. In HEDG, a possible removal rate as high as $Q = 1200 \text{ mm}^3/\text{s}$ over a grinding width of 2 mm translates to $Q' = 600 \text{ mm}^2/\text{s}$

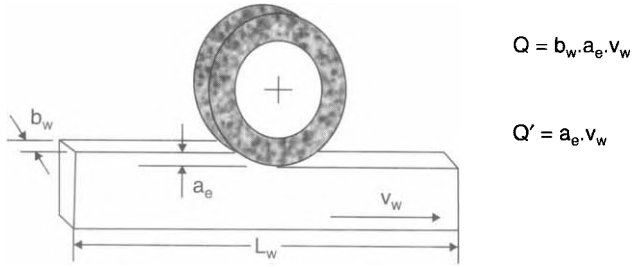


Figure 2.7 Removal rate Q and specific removal rate Q' .

(or 55.8 in.²/min). Such high removal rates create high stresses on the grinding wheel grains and require appropriate grinding wheel design to avoid rapid wear. The HEDG operation removes material 300 times faster.

Another way to increase removal rate without increasing the stress on the grinding wheel grains is to increase the active surface area of the grinding wheel in grinding contact. Large vertical axis surface grinding machines, for example, are used for very high removal rates.

The rate at which material is removed is the product of depth of cut, work speed, and contact width:

$$Q = b_w \cdot a_e \cdot v_w \quad \text{removal rate} \quad (2.4)$$

Removal rate per unit grinding contact width allows data to be presented in a more general way and is known as specific removal rate Q' . This form will be widely quoted in the following chapters.

$$Q' = a_e \cdot v_w \quad \text{specific removal rate} \quad (2.5)$$

Example 2.5 The width of grinding contact in a horizontal surface grinding machine is 15 mm (or 0.394 in.), the real depth of cut is 10 μ m (or 0.000394 in.) and the work speed is 300 mm/s (or 709 in./min). What is the removal rate and what is the specific removal rate?

$$Q = 15 \times 0.010 \times 300 = 45 \text{ mm}^3/\text{s} \text{ (or } 0.165 \text{ in.}^3/\text{min)}$$

$$Q' = 0.010 \times 300 = 3 \text{ mm}^3/\text{mm s} \text{ or } 3 \text{ mm}^2/\text{s} \text{ (or } 0.28 \text{ in.}^2/\text{min)}$$

2.5 Specific Grinding Energy

Grinding energy provides a further valuable measure of the ability of a grinding wheel to remove material. Grinding energy depends on the

sharpness of the grinding wheel and the grindability of the workpiece material. The grinding energy required to remove a volume of material is given by the grinding power P divided by the removal rate Q . This quantity is generally known in manufacturing technology as the specific cutting energy e_c . Since we are considering the grinding process, it will also be known as the specific grinding energy or simply as specific energy.

$$e_c = \frac{P}{Q} \quad \text{specific grinding energy} \quad (2.6)$$

Example 2.6 The maximum grinding power in steady grinding after subtracting the no-load power and the power required to accelerate the grinding fluid has a mean value of 2 kW (or 2.68 hp). The removal rate is 50 mm³/s (or 0.183 in.³/min). What is the specific grinding energy?

$$e_c = 2000 / 50 = 40 \text{ J/mm}^3 \text{ (or 14.7 hp min/in.}^3\text{)}.$$

Specific energy is typically between 15 and 700 J/mm³ (equivalent to 5.5 to 256 hp min/in.³). The value of specific energy depends particularly on workpiece hardness and wheel sharpness. A high value is typical of a difficult-to-grind material and a low value of an easy-to-grind material. In HEDG, specific energy values lower than 10 J/mm³ (or 3.7 hp min/in.³) may be found.

Internationally, specific energy is always quoted in Joules per cubic millimetre. It is a straightforward conversion from SI units to evaluate horsepower required for removal rate quoted in cubic inches per minute using the factor 1 J/mm³ is equivalent to 0.3663 hp min/in.³.

Specific energy values reduce with increasing removal rate as found by many researchers (Fig. 2.8). The example shown is for HEDG of crankshafts (Comley et al. 2004). Using electroplated cubic boron nitride (CBN) grinding wheels, extremely high specific removal rates were achieved up to 2000 mm²/s (or 186 in.²/min). It can be seen that specific energy values decreased down towards 10 J/mm³ (or 3.66 hp min/in.³) at these removal rates.

Grinding energy can be identified by monitoring wheel spindle power during a grinding cycle. An example is shown schematically in Fig. 2.9 for plunge cylindrical grinding.

Initially with the grinding wheel running, a no-load power P_{NL} is dissipated in the spindle bearings and by motor windage. With the grinding fluid switched on and the grinding wheel close to the workpiece, additional power P_f is dissipated by grinding fluid drag on the grinding wheel.

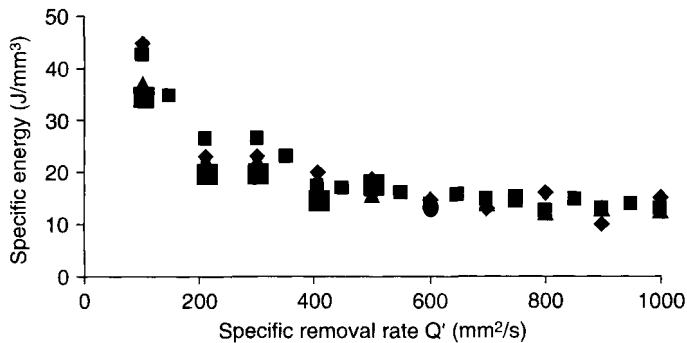


Figure 2.8 Specific energy in HEDG grinding of camshaft webs using electro-plated CBN wheels. Data based on Comley et al. (2004).

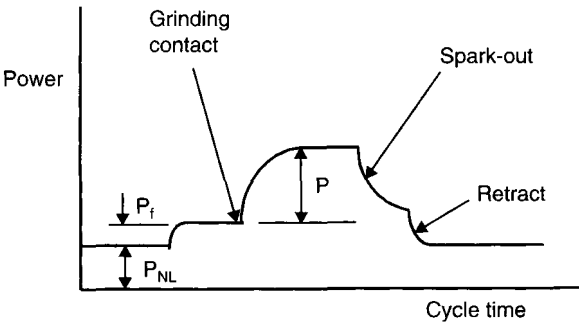


Figure 2.9 Identification of the grinding power P in plunge cylindrical grinding.

After contact is made between the grinding wheel and the workpiece, the depth of cut builds up and hence the spindle power. The grinding power P can be identified by subtracting no-load power and fluid drag power from the maximum power. It is best to identify P after a steady level of maximum power has been achieved.

2.6 Forces and Power

Grinding Power

Grinding power can also be identified by measuring grinding forces. Grinding force resolved into three components: tangential force F_t , normal force F_n , and axial force F_a (see Fig. 2.10).

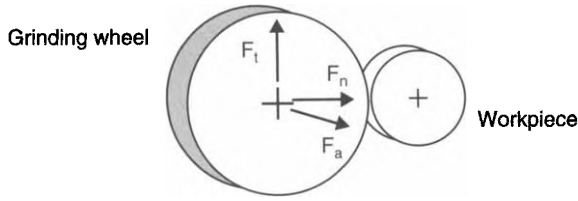


Figure 2.10 The three grinding force components.

Grinding power is given by

$$P = F_t \cdot (v_s \pm v_w) + F_n \cdot v_{fn} + F_a \cdot v_{fa} \text{ grinding power} \quad (2.7)$$

The plus sign applies to up-cut grinding where the workpiece motion opposes the grinding wheel motion, and the minus sign applies to down-cut grinding where the workpiece motion assists the grinding wheel motion. In practice, taking account of the workpiece speed has a small effect as v_s is typically 60–200 times larger than v_w . The normal and axial feed speeds v_{fn} and v_{fa} , respectively, are much smaller than the wheel speed v_s so that grinding power is given quite closely by $F_t \cdot v_s$.

Grinding Force Ratio

Grinding force ratio is another useful parameter that gives indirect information about the efficiency of grinding. The force ratio is defined as

$$\mu = F_t / F_n \text{ grinding force ratio} \quad (2.8)$$

When grinding with sharp wheels, the grinding force ratio is high as the normal force is low when compared to the tangential force. Conversely, when grinding with blunt wheels, the grinding force ratio is low. The reader will notice that the grinding force ratio is similar to friction coefficient and employs the same symbol. This is because of similarities in the mechanics of friction and grinding. Whereas an efficient grinding wheel removes material from the workpiece, an efficient slider or bearing is expected to minimise wear of the sliding surfaces.

Typical Forces

Figure 2.11 shows typical grinding forces when grinding a grey cast iron with a medium size 60 mesh grit alumina wheel using 2% synthetic oil in water emulsion as the grinding fluid.

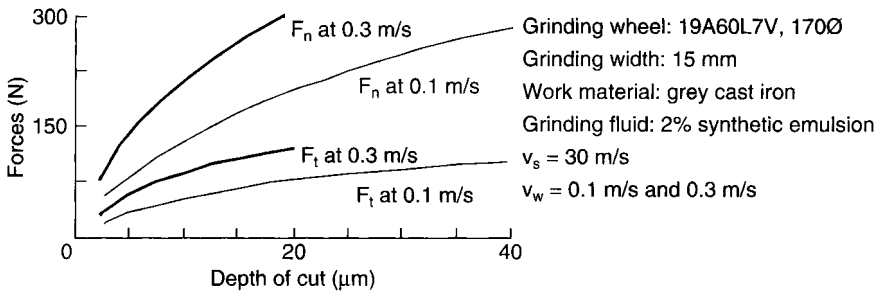


Figure 2.11 Typical grinding forces for grey cast iron.

Example 2.7 Calculate the specific energy and force ratio at 15 μm depth of cut (or 0.00059 in.) for 0.3 m/s work speed (or 0.197 in./min) and for 30 μm depth of cut (or 0.0012 in.) at 0.1 m/s work speed (or 0.066 in./min) using the values in Fig. 2.11 for grinding grey cast iron with an alumina grinding wheel at 30 m/s. Give the values in SI and British units.

At $v_w = 0.3 \text{ m/s}$ and $a_e = 0.015 \text{ mm}$:

$$Q_w = 300 \times 0.015 \times 15 = 67.5 \text{ mm}^3/\text{s} \text{ (or } 0.247 \text{ in.}^3/\text{min)}.$$

$$F_t \approx 110 \text{ N or } 24.7 \text{ lbf} : P = 110 \times 30 = 3300 \text{ W (or } 4.42 \text{ hp)}$$

$$\text{Specific energy: } e_c = 3300/67.5 = 48.9 \text{ J/mm}^3 \text{ (or } 17.9 \text{ hp min/in.}^3)$$

$$F_n \approx 265 \text{ N (or } 59.6 \text{ lbf)}$$

$$\text{Grinding force ratio: } \mu = 110/265 = 0.41$$

At $v_w = 0.1 \text{ m/s}$ and $a_e = 0.030 \text{ mm}$:

$$Q_w = 100 \times 0.03 \times 15 = 45 \text{ mm}^3/\text{s} \text{ (or } 0.165 \text{ in.}^3/\text{min)}$$

$$F_t \approx 97 \text{ N (or } 21.8 \text{ lbf)} : P = 97 \times 30 = 2910 \text{ W (or } 3.90 \text{ hp)}$$

$$\text{Specific energy: } e_c = 2910/45 = 64.7 \text{ J/mm}^3 \text{ (or } 23.7 \text{ hp min/in.}^3)$$

$$F_n \approx 235 \text{ N (or } 52.8 \text{ lbf)}$$

$$\text{Grinding force ratio: } \mu = 97/235 = 0.41$$

Removal rate was 50% higher at the higher work speed. Specific energy was 32% higher at the lower work speed. This confirms that higher removal rates are much more efficient.

Grinding force coefficient is less sensitive to removal rate unless the wheel surface is significantly affected. The value 0.41 is typical of grinding

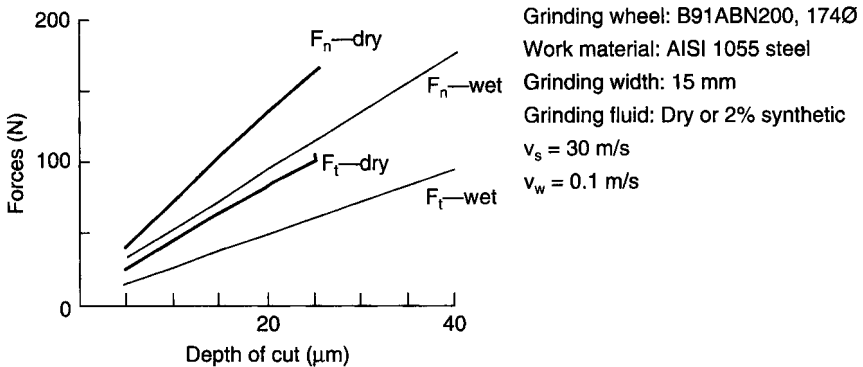


Figure 2.12 Wet and dry grinding.

practice. There were no changes at the two different removal rates suggesting that wheel sharpness was unchanged.

Figure 2.12 shows the typical forces for wet and dry grinding. Results are for grinding a general-purpose medium carbon steel with a fine 200 mesh grit CBN wheel. Forces were considerably reduced using a grinding fluid. Such large reductions are not always found.

The forces displayed in Fig. 2.12 increased linearly with depth of cut, although there appears to be a small threshold force. Threshold force is the force required to initiate chip removal. These results are different from the results in Fig. 2.11 where forces increased non-linearly with depth of cut. The following examples illustrate the different effects of the two abrasives.

Example 2.8 Calculate the specific energy and force ratio at a $20 \mu\text{m}$ depth of cut (or 0.00079 in.) for dry grinding and for wet grinding using the values in Fig. 2.12 for grinding AISI 1055 with a CBN grinding wheel.

Dry grinding

$$Q_w = 100 \times 0.020 \times 15 = 30 \text{ mm}^3/\text{s} \text{ (or } 0.11 \text{ in.}^3/\text{min})$$

$$F_t \approx 84.5 \text{ N (or 19 lbf)}$$

$$v_s = 30 \text{ m/s (or 5900 ft/min)}$$

$$P = 84.5 \times 30 = 2535 \text{ W (or 3.4 hp)}$$

$$\text{Specific energy: } e_c = 2535/30 = 84.5 \text{ J/mm}^3 \text{ (or } 31 \text{ hp min/in.}^3)$$

$$F_n \approx 133 \text{ N (or 29.9 lbf)}$$

$$\text{Grinding force ratio: } \mu = 84.5/133 = 0.63$$

Wet grinding

$$F_t \approx 49 \text{ N (or 11 lbf)}$$

$$P = 49 \times 30 = 1470 \text{ W (or 1.97 hp)}$$

$$\text{Specific energy: } e_c = 1470/30 = 49 \text{ J/mm}^3 \text{ (or 18 hp min/in.}^3\text{)}$$

$$F_n \approx 93 \text{ N (or 20.9 lbf)}$$

$$\text{Grinding force ratio: } \mu = 49/93 = 0.53$$

Other observations when grinding the above materials are as follows.

Wet Grinding

Specific energy was lower in wet grinding due to the lubrication of the cutting action. Usually, a fine grain wheel requires higher specific energy. Dry grinding medium carbon steel with the 60 mesh grit alumina wheel required 50 J/mm^3 (or $18.3 \text{ hp min/in.}^3$). Under identical conditions the 200 mesh grit CBN wheel required 76 J/mm^3 (or $27.8 \text{ hp min/in.}^3$).

Effect of Abrasive Type

Specific energy was lower when a very sharp CBN wheel was used as compared to when a less sharp alumina wheel was employed.

The sharpness of the CBN wheel in Example 2.8 is confirmed by high values of grinding force ratio. The force ratio indicates that the tangential force is high in comparison to the normal force. The tangential force is the force more directly removing material whereas the normal force has to make the wheel grains penetrate into the workpiece. A blunt wheel increases the normal force more rapidly than the tangential force.

When grinding grey cast iron with the alumina wheel, 70 J/mm^3 of energy was required (or $25.6 \text{ hp min/in.}^3$) compared with 50 J/mm^3 (or $18.3 \text{ hp min/in.}^3$) using the same wheel to grind medium carbon steel. It is clear that grey cast iron is more difficult to grind with this wheel as it tends to adhere to the abrasive grains of the grinding wheel which reduces the grain sharpness.

2.7 Maximising Removal Rate

Process Limits

Initial trials to ascertain the limits of removal rate are essential as a first step to process optimisation. In practice, there are constraints on the removal rate, such as machine power available, machine capability, accuracy required, or heat generation. Rapid wheel wear may also present a process limit. In short, the process limits are usually roughness, chatter, burn, wheel wear, or power available. Process optimisation requires that machining conditions are selected within the process limits (Rowe et al. 1985).

Examples of limit charts for plunge feed centreless grinding are given in Fig. 2.13 for AISI 1055 and Fig. 2.14 for grey cast iron. The charts show the benefit of higher wheel speed. Doubling wheel speed from 30 m/s to 60 m/s ($\approx 6000\text{--}12,000$ ft/min) delivered more power to the process and tripled the removal rate. High grinding wheel speeds allow higher in-feed rates to be employed for the same grinding forces and thus allow higher removal rates.

The optimum work speed for both materials was approximately 0.25 m/s (or 590 in./min). At this work speed, it was possible to increase the in-feed rate up to the maximum power available without offending the roughness, burn, chatter, or wheel wear constraints. Surface roughness

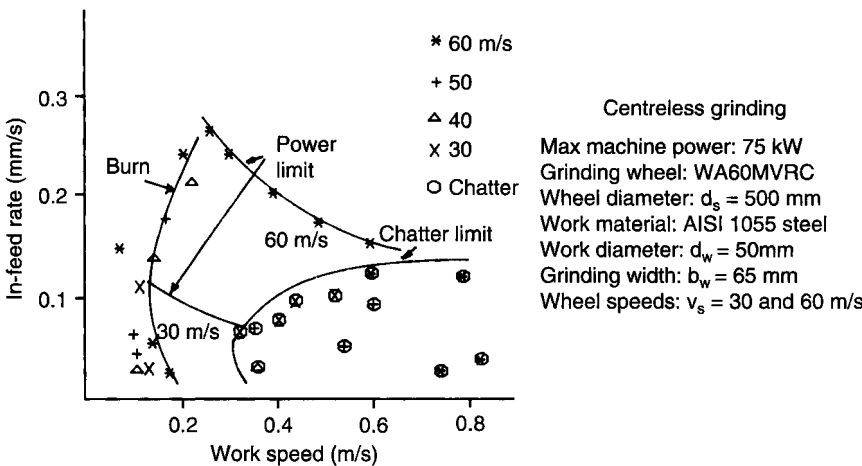


Figure 2.13 Process limit chart for centreless grinding AISI 1055 steel.

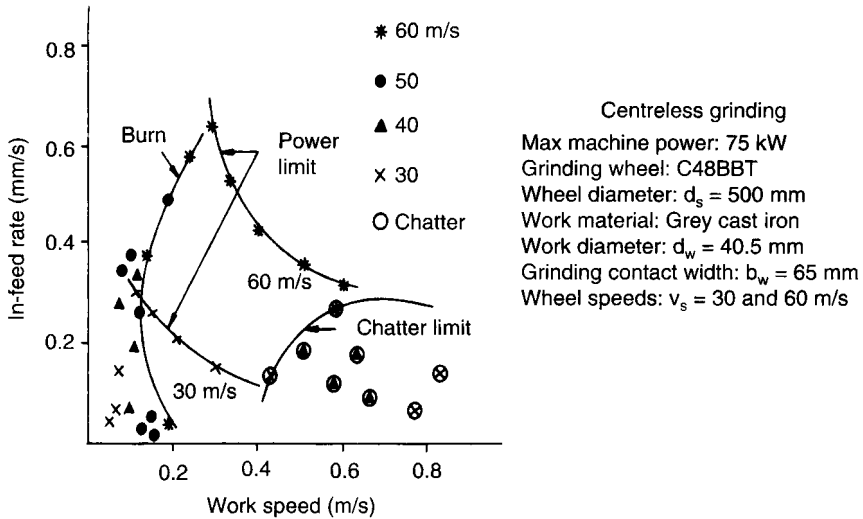


Figure 2.14 Process limit for centreless grinding grey cast iron.

depends primarily on the grinding wheel employed and the dressing process.

Limit Charts

Process limits define the permissible range of speed conditions for stable grinding. Grey cast iron is an easy-to-grind work material using a silicon carbide wheel. Specific material removal rates were achieved in excess of $40 \text{ mm}^2/\text{s}$ at 60 m/s (or $3.72 \text{ in.}^2/\text{min}$ at $12,000 \text{ ft/min}$). Grinding AISI 1055 steel with an alumina wheel, the specific removal rate achieved was $20 \text{ mm}^2/\text{s}$ (or $1.86 \text{ in.}^2/\text{min}$).

The charts show that high work speeds increase the probability of chatter and low work speeds increase the probability of burn. Low work speeds concentrate the process energy in the contact zone for a longer period increasing susceptibility to thermal damage.

Example 2.9 Grey cast iron was ground with a resin-bonded C48BBT carbide wheel, a process that is very efficient in energy terms. The machine power required was 32 kW (or 42.9 hp) at a work speed of 0.3 m/s (or 709 in./min) and a wheel speed of 60 m/s (or $12,000 \text{ ft/min}$). The in-feed rate was 0.58 mm/s (or 1.37 in./min). The workpiece diameter was 40.5 mm (or 1.595 in.), and the grinding width was 65 mm (or 2.56 in.). What are the depth of cut after a number of

revolutions, the specific removal rate, the specific power, and the specific energy?

From Section 2.2,

$$a_e = 3.142 \times 40.5 \times 0.58 / (2 \times 300) = 0.123 \text{ mm (or 0.00484 in.)}$$

$$Q'_w = 0.123 \times 300 = 36.9 \text{ mm}^2/\text{s (or 3.43 in.}^2/\text{min)}$$

$$P' = 32000 / 65 = 492 \text{ W/mm (or 16.75 hp/in.)}$$

$$e_c = 492 / 36.9 = 13.3 \text{ J/mm}^3 \text{ (or 4.87 hp min/in.}^3\text{)}.$$

There are clear benefits from increasing wheel speed. It is tempting to assume that higher wheel speeds automatically increase efficiency. Unfortunately, increasing wheel speed without changing other grinding conditions will reduce efficiency and will not increase removal rate.

To increase the removal rate, we need to increase the feed rate. The aim is to reduce specific energy, increase removal rates, and maintain workpiece quality. This may be achieved by considering the effect of changing one speed at a time:

1. *In-feed rate* Increasing in-feed rate, keeping other speeds constant, increases grinding forces, increases roughness, reduces redress life, and reduces specific energy. The process becomes more energy efficient until excessive in-feed rate leads to high wheel wear, a low grinding ratio, and rapid wheel breakdown. These effects are explained in the following chapters.
2. *Wheel speed* Increasing wheel speed has the opposite effect. Increasing wheel speed, keeping other speeds constant, reduces grinding forces, reduces roughness, increases redress life, and increases specific energy, thus reducing process efficiency. The purpose of increasing wheel speed is to allow the in-feed rate to be increased, thus increasing production rate while maintaining quality levels and process efficiency.
3. *Work speed* Increasing work speed, at constant removal rate, has a relatively small effect on the process within the stable range. High work speeds increase the probability of chatter so there is a maximum work speed limit. These results are for a particular workpiece diameter. It is therefore necessary to undertake trials for particular workpieces to be produced.

Centreless grinding—AISI 1055 steel

Max machine power: 75 kW

Grinding wheel: WA60MVR

Wheel diameter: $d_s = 500$ mm

Work diameter: $d_w = 50$ mm

Grinding width: $b_w = 65$ mm

v_s/v_w : 200

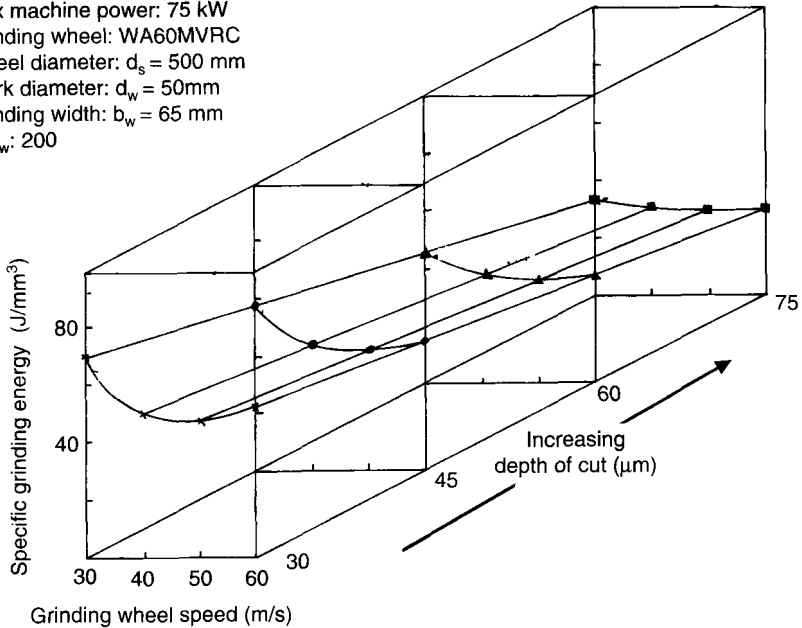


Figure 2.15 Minimising specific energy and maximising removal rate.

At low work speeds, the probability of thermal damage to the workpiece increases. The burn boundary can be moved outwards by using a sharper abrasive to reduce the specific energy.

Figure 2.15 summarises the conditions for maximum removal rate. Increasing depth of cut reduces specific energy. A grinding wheel speed of 45 m/s (or 9000 ft/min) requires minimum energy at 30 μm depth of cut (or 0.0012 in.). At 75 μm depth of cut (or 0.003 in.), the optimum wheel speed was ~60 m/s (or 12,000 ft/min).

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3 Grinding Wheel Developments

3.1 Introduction

New grinding wheels and grinding wheel designs have been introduced in recent decades, rapidly changing modern grinding practice. Removal rates and accuracies are achieved that previously could only have been dreamed about. New abrasives include seeded gel abrasives and superabrasives of resin, vitrified, and metal-bonded forms. Porosity varies from extremely open to completely closed structures depending on process requirements. Users benefit from a close liaison with abrasive manufacturers in either planning a new grinding system or optimising an existing grinding system.

Developments in abrasives and grinding wheels have allowed increased removal rates particularly for high precision grinding. Individual abrasives are engineered to best suit a particular work material and grinding conditions. Simultaneous development has to take place to achieve the right bond, porosity, and wheel design. This chapter shows how these properties work together. Important features of abrasive materials are also described in depth by Marinescu et al. (2006).

A grinding wheel surface consists of abrasive grains that form the cutting edges, bond material to retain the grains in position, and surface pores that allow space for material removal from the work surface. The wheel surface is usually prepared by a truing or dressing operation as described in Chapter 4. The nature of the wheel surface and contact effects are introduced in Chapter 5, after this basic introduction to abrasives, bond materials, and wheel types.

In this chapter, the basic characteristics of conventional and superabrasive grinding wheels are described and directions for grinding wheel developments including high-speed wheel design and application of novel abrasives are provided.

3.2 Abrasives

The most important property of an abrasive is hardness. It is important that hardness is retained at high temperatures and that the abrasive does not react chemically or diffuse too readily into the workpiece material. Hardness

values of the most common abrasives are usually quoted as Knoop hardness. Some typical values provided by manufacturers are:

Superabrasives

Diamond	6500 kg/cm ²
Cubic boron nitride	4500 kg/cm ²

Conventional abrasives

Silicon carbide	2500 kg/cm ²
Aluminium oxide	1370–2260 kg/cm ²
Cemented carbides	1400–1800 kg/cm ²
Quartz	800 kg/cm ²
Glass	300–500 kg/cm ²

Friability is a term used to describe the tendency of a grain to fracture under compression. Grains with greater friability are better for low grinding forces. Fracture produces sharp new edges and hence friability is an advantage for maintaining wheel sharpness.

The thermal properties of abrasive grains are important for abrasive wear resistance and grinding temperatures. Some typical values are given in Table 3.1. The thermal conductivity of superabrasives is extremely high but depends on the purity. The highest values given are for the pure abrasive. With small traces of other elements the conductivity is greatly reduced, although it is still high compared to conventional abrasives. A range is given for diamond and cubic boron nitride (CBN).

Abrasives are crystalline in nature and their properties vary depending on the crystalline structure, which in turn is affected by their preparation or by the added elements of other minerals. Wear resistance of an abrasive depends not only on the hardness of the abrasive at the high contact

Table 3.1 Typical Thermal Properties of Abrasive Grains at Ambient Temperatures

	Conductivity (W/m K)	Density (kg/m ³)	Specific Heat (J/kg K)	Diffusivity (mm ² /s)
Diamond	600–2000	3520	511	333–1110
Cubic boron nitride	240–1300	3480	506	136–738
Silicon carbide	100	3210	710	44
Aluminium oxide	35	3980	765	11.5

pressure, but also on the hardness and chemical composition of the work material. An abrasive used on different materials can show differences in wear rates of 100–1000 times. The abrasive must be suitable for the chemical composition of the work material. The converse process also takes place. Designers try to select workpiece materials for their products that ease the manufacturing process. This can reduce cost and provide greater assurance of maintaining product quality.

Abrasives are usually classified as conventional abrasives or superabrasives. Diamond and cubic boron nitride being much harder than conventional abrasives are termed superabrasives. Superabrasives are much more expensive than conventional abrasives but will be economic for many applications for either of the following reasons: In many cases, the grinding operation is possible only using a superabrasive. In other cases, increased redress life using a superabrasive reduces overall cycle time and hence reduces grinding costs, as demonstrated in Chapter 9.

Superabrasives

Diamond

Diamond is the hardest material known and is used to grind the hardest ceramics. One of the advantages of diamond as an abrasive is the retention of hardness at high temperatures. Diamond is thermally stable up to 800°C in air and to over 1400°C in vacuum. However, because diamond is a form of carbon it is unsuitable for grinding steels. The solubility of carbon in iron causes rapid wear of the diamond abrasive, an effect that is accelerated with temperature. Chemical–thermal degradation generally makes diamond unsuitable for steels and nickel-based alloys (Marinescu et al. 2004). Diamond is extremely resistant to mechanical rubbing wear. Wear tends to be associated with chemical–thermal degradation in the presence of oxygen at higher temperatures.

Diamond has very favourable thermal properties that help to reduce grinding temperatures. The thermal conductivity is the highest of any material with values between 600 and 2000 W/m K at ambient temperatures. The thermal conductivity falls to 70 W/m K at 700°C.

There are other characteristics of diamond of which the user should be aware. The hardness of a diamond crystal varies with the direction of testing by almost a factor of 2, so it is difficult to give a precise figure for its hardness. Some associated consequences are that wear resistance varies with the plane of sliding by a factor of up to 40 times with small changes of angle. Diamond has cleavage planes and is brittle along these planes,

so mechanical impact should be avoided. Diamond is also vulnerable to thermal shock and it is therefore important to avoid sudden application of grinding fluid to a red-hot diamond. This can easily happen, for example, when using diamond tools to dress grinding wheels.

Synthetic diamonds have rapidly taken over a large proportion of the industrial market for grinding wheels and abrasives. Natural diamonds are still used for some applications in spite of their relative cost. Natural diamonds are used particularly for single-point dressing tools and dressing rolls.

Cubic Boron Nitride

CBN is the second hardest material and is widely used for grinding steels. Although CBN is much more expensive than conventional abrasives, costs of CBN have become relatively much lower due to economies of scale. CBN is increasingly replacing conventional abrasives for precision grinding of hardened steels due to its low rate of wear and the ability to hold close size tolerance on the parts produced. Electroplated CBN has played a large part in the development of high efficiency deep grinding (HEDG).

CBN is thermally stable in inert atmospheres up to 1500°C. In air, CBN forms a stable layer of boron oxide that prevents further oxidation up to 1300°C. However, this layer dissolves in water, so CBN wears more rapidly when water-based fluids are used than with neat oil fluids. However, this does not prevent CBN from being used very successfully with water-based coolants. Due to chemical–thermal degradation, CBN wears five times more rapidly than diamond when grinding aerospace titanium alloys.

Conventional Abrasives

Conventional abrasives used in grinding wheels mainly include formulations of aluminium oxide, silicon carbide, and zirconia alumina. Some examples are shown in Table 3.2. There are other natural abrasives such as emery, sandstone, flint, iron oxide, and garnet, but these are not normally used in grinding wheels.

Silicon Carbide

Silicon carbide is the hardest of the conventional abrasives, but has lower impact resistance than aluminium oxide and shows a higher wear rate when used for grinding steels. Silicon carbide wears more rapidly when used to grind metals that have an affinity for carbon such as iron and

Table 3.2 Mechanical Properties of Typical Silicon Carbide and Alumina Abrasives

Abrasive	Knoop Hardness	Relative Toughness	Morphology	Application
Green SiC	2840	1.6	Sharp, angular, glassy	Carbides, ceramics
Black SiC	2680	1.75	Sharp, angular, glassy	Cast iron, ceramics, ductile non-ferrous metals
Ruby Al ₂ O ₃	2260	1.55	Blocky, sharp-edged	HSS and high alloy steel
White Al ₂ O ₃	2120	1.75	Fractured facets, sharp	Steels, ferrous, precision
Brown Al ₂ O ₃	2040	2.8	Blocky, faceted	General purpose
Al ₂ O ₃ /10%ZrO	1960	9.15	Blocky, rounded	Heavy duty grinding
Al ₂ O ₃ /40%ZrO	1460	12.65	Blocky, rounded	Heavy duty, snagging
Sintered Al ₂ O ₃	1370	15.4	Blocky, rounded	Foundry billets and ingots

Al₂O₃: Aluminium oxide; HSS: High-speed steel; SiC: Silicon carbide; ZrO: Zirconium oxide.

nickel. It is therefore used primarily for non-ferrous materials. Green silicon carbide is of higher purity than black silicon carbide. Green silicon carbide is sharp and friable, which makes it a good abrasive. It is the hardest of the conventional abrasives and is used to grind less ductile materials of lower tensile strength such as carbides and ceramics. Black silicon carbide is slightly less hard and is used for abrasive workpiece materials such as ceramics and for ductile non-ferrous materials. It is also used for iron with higher carbon content such as gray cast iron.

Aluminium Oxide

Aluminium oxide or corundum is used for a wide range of ferrous materials including steels. Depending on purity, and preparation of the abrasive, the grains may be either blocky or sharp. Grains that are blocky with high impact resistance are better for heavy stock removal operations. Grains that micro-fracture are more durable because the grains are kept sharp while minimising the forces on the grains and minimising the volume

of grain lost due to fracture. Very tough grains such as zirconia alumina need to be used for heavy removal rates in order to promote micro-fracture. Pink or ruby alumina contains chromium oxide which colours the white alumina. The addition of 0.5–5.0% chromium oxide increases friability. The addition of 2% titanium oxide (TiO_2) increases toughness. Brown alumina is a general purpose abrasive used for resinoid and vitrified wheels for rough grinding.

Sintered Alumina

In recent decades, there have been several exciting developments of aluminium oxide abrasives. Crystallite size has been greatly reduced by employing chemical precipitation and sintering techniques.

In 1986, Norton Company produced a seeded gel abrasive they termed SG (Fig. 3.1). This class of abrasives is commonly termed “ceramic.” The grains are produced with a crystallite size of about $0.2\ \mu\text{m}$ (Marinescu et al. 2006). The fine crystallite structure is achieved by using very small seed grains in a chemical precipitation process. This is followed by compaction and sintering. The resulting abrasive is very tough and also self-sharpening, because micro-fracture is engineered into the grain at the micron level. About 10–50% SG grain is blended with conventional fused abrasive to achieve the required wheel hardness characteristics. The wheels allow greatly extended wheel life and high removal rates. Blocky grains or high aspect ratio grains for better wheel sharpness can be produced.

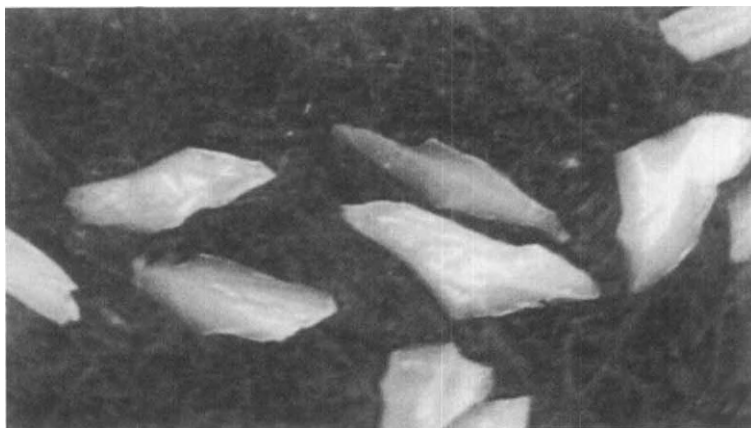


Figure 3.1 Typical seeded gel grains. Courtesy Saint-Gobain Abrasives.

In 1981, 3M Company produced an alumina abrasive material by the sol-gel process, which they called Cubitron. This grain also has a sub-micron crystallite structure produced by chemical precipitation and sintering but does not involve the use of seed grains. The abrasive properties can be further modified with addition of magnesia and rare earth elements. The new range of abrasives was eventually incorporated into grinding wheels to achieve longer wheel life than conventional fused abrasives.

In 1999, Norton introduced new extruded SG grains which they termed TG and TG2 grains. The new cylindrical grains have an aspect ratio (length/diameter) of 4:1 for TG and 8:1 for TG2. The extra long TG2 grains form bent and twisted fibres that pack together closely while allowing extremely high porosity in the wheel structure. The high porosity wheels have much higher retention strength than possible with grains of conventional shape. The new structures have allowed wheels to be developed for high wheel speeds and high removal rates (Klocke and Muckli 2000).

3.3 Wheel Bonds

Wheel bond types fall into three main classes:

- Vitrified bond wheels
- Organic or resin bond
- Metal bond wheels

Organic Bonds

Organic bond wheels tend to be more elastic than other wheels. Elasticity is usually a factor in the selection of an organic bond. Elasticity can be useful for safety at high speed or with unusual load application or for achieving a more polished surface. Organic bonds are mainly used with conventional abrasives, but are also used with superabrasives to achieve extremely low roughness.

Being organic in nature, these wheels have a limited shelf life even before use. They are date-stamped and care should be taken to observe the shelf life for safety reasons. Organic bonds are available in a wide range of bond types. Plastics include epoxy or polyurethane plastics. Plastic bonds employed in a soft wheel using conventional abrasives may be used to avoid burn in burn-sensitive applications such as knife

grinding and chatter in other operations on steel. Other resins include phenolics and polyamide bonds. Some are used for heavy stock removal and shock loading situations. Resinoid wheels may also be used where the grinding operation puts heavy twisting loads on the side faces of the wheel as in drill flute grinding or where it is necessary to withstand interrupted cuts.

All organic bond wheels wear with high temperatures. Often, a new wheel will not grind efficiently until heat from the grinding process has removed some of the surface bond material to create a more open cutting surface. This allows grinding forces to reduce and grinding temperatures to moderate. Polyamide bonds were developed to withstand heat better than phenolic bonds. Polyamide bonds have been developed that can withstand temperatures up to 300°C.

Rubber wheels tend to be used for cut-off wheels where the requirement is for durability. They wear rapidly at high temperatures. Rubber wheels are also used for control wheels in centreless grinding.

Shellac wheels are used for finishing operations. Being softer and more flexible, they polish the surface with less risk of scratching.

Vitrified Bonds

A vitrified wheel is a structure of abrasive grain, bond material, and pores as described in Chapter 5. The bond is much harder than organic bonds but considerably softer than metal bonds. This type of structure allows considerable flexibility in varying the nature of the cutting surface for different workpiece materials. The great advantage of a vitrified wheel is that it can be trued to produce a form for grinding various profiles. Truing also allows the wheel to be re-sharpened when the wheel becomes too blunt or too irregular.

Most conventional grinding is carried out using vitrified wheels and most of these are vitrified alumina wheels. For superabrasive grinding, most wheels are vitrified CBN.

Vitrified bonds are prepared from a mix of glass frits, clays, and fluxes such as feldspar and borax. The bond material is mixed with water and a binder such as dextrin. The required proportions of bond material and abrasive are mixed and then compacted in a mould. Fillers may also be used to create porosity. The wheel is then heated in an oven under a carefully controlled heating and cooling cycle at temperatures up to 1300°C. At temperatures of approximately 1100°C, the bond becomes glassy and starts to flow.

The temperature control is absolutely critical to ensure sufficient flow, but not too much flow.

Metal Bonds

Metal bonds are used for superabrasives. Diamonds or CBN grains can be applied in a single layer onto a metal disk or as a multi-layer abrasive in a sintered cast iron bond.

Single-layer wheels are very expensive due to the time required to set the grains accurately on the surface. A common method of fixing grains onto the wheel disk is by appropriate coating and electroplating. An alternative method for some operations fixes the grains by brazing. This is a much higher temperature process than electroplating and there is a danger of damaging the grains.

Single-layer superabrasive wheels that employ larger grains give durability in service for grinding the hardest materials. It is not possible to dress a single-layer wheel in the same way that a vitrified wheel would be dressed to achieve low run-out. The setting operation and wheel mounting must therefore be carried out with extreme accuracy. Despite the difficulties and expense, electroplated CBN wheels have been highly successful for high-speed precision grinding, high removal rates, and long wheel life.

Metal bond diamond wheels are often used wet for grinding ceramics and brittle abrasive materials. Multi-layer wheels using very small diamond grains are used to produce very high accuracy and low surface roughness. A new electrolytic in-process dressing system of grinding (ELID grinding) allows multi-layer wheels bonded in a conductive metal to be dressed to maintain sharpness and form.

3.4 Grinding Wheels

A modern high-speed vitrified CBN grinding wheel is shown in Fig. 3.2(a). Conventional abrasives such as alumina and silicon carbide usually have a thick abrasive layer as shown in Fig. 3.2(b), whereas metal bond superabrasive single-layer wheels have a thin layer as shown in Fig. 3.2(c). Single-layer grinding wheels are used for the highest speeds and often give long redress life due to the open grain spacing and larger grains employed. Over recent decades, the introduction of high-speed vitrified wheels has led to segmented designs with intermediate thicknesses of the abrasive layer, as

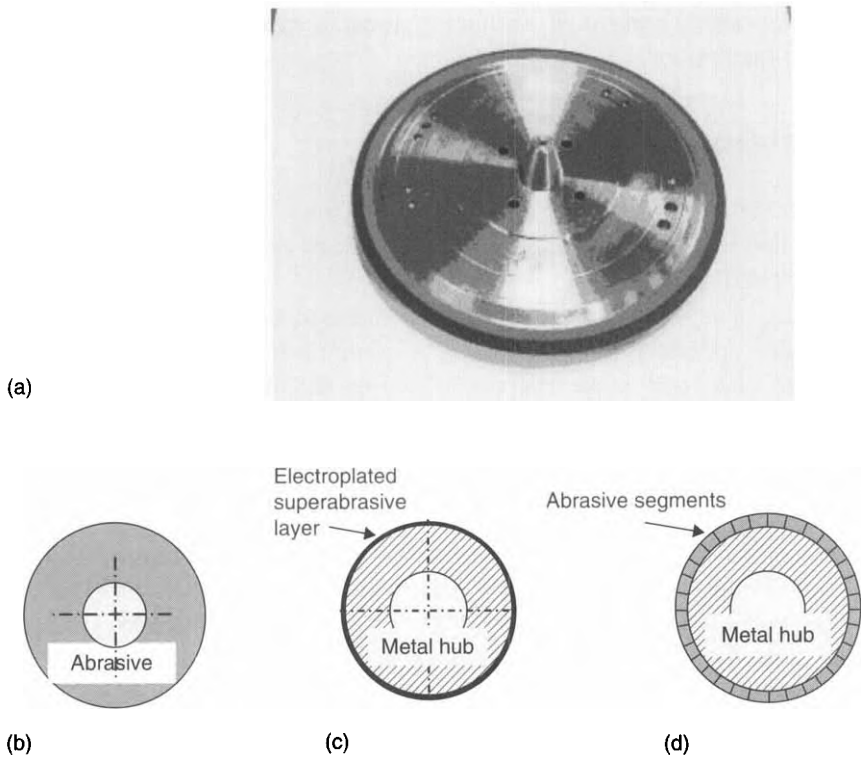


Figure 3.2 (a) Photograph of high-speed vitrified CBN grinding wheel; schematic representations of: (b) conventional abrasive wheel, (c) single-layer superabrasive wheel, and (d) segmented wheel suitable for high speeds using conventional abrasives.

illustrated in Fig. 3.2(d). Segmented wheels can be designed to avoid excessive hoop stresses in the abrasive layer that would occur with a thick continuous layer.

3.5 Wheel Specification

The abrasive layer consists of an array of grains, bond bridges, and pores between the grains. The strength and proportions of the grains and the bond bridges determine the behavioural characteristics of the grinding wheels in use. Manufacturers provide a guide to these characteristics through the wheel specification. These are marked on the wheels together with other information such as the maximum speed and are known as the marking systems. Examples of marking systems are illustrated in Figs. 3.3 and 3.4

Standard Marking System for Conventional Abrasive Wheels

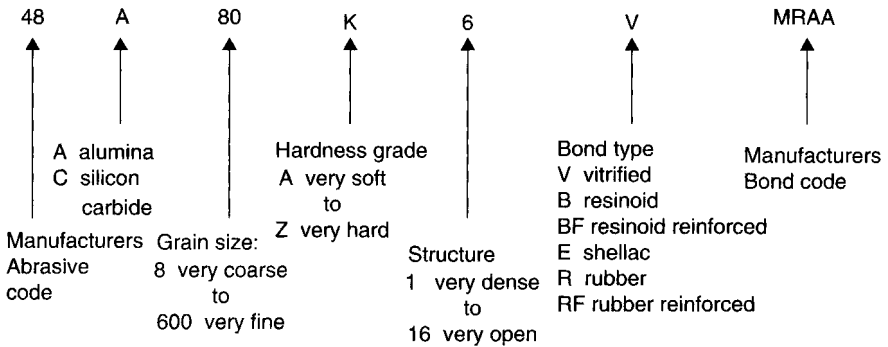


Figure 3.3 Standard marking system for grinding wheels using conventional abrasives.

Marking System for Superabrasive Wheels

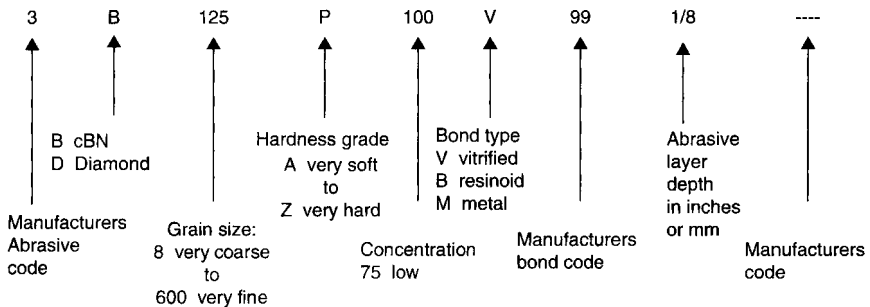


Figure 3.4 Marking system for superabrasive grinding wheels.

The main features of the specification are abrasives type, grain size, grade, structure or concentration, bond type, and manufacturers' codes for variations within these headings.

Grain Size

A coarse grit is used for heavy stock removal. Since surface roughness increases with grit dimension, the surface roughness will increase. A fine grit is used for low surface roughness. Fine grit wheels tend to be stronger than coarse grit wheels for the same volume of the bond.

Not all manufacturers use the same system of specifying grain size. There are two standards for grain size used (Marinescu et al. 2006). These are the ANSI (American National Standards Institute) US standard and the FEPA (Federation of European Producers of Abrasives) ISO standard. The ANSI standard is used more widely for conventional wheels and the FEPA standard for superabrasive wheels. The FEPA standard gives a measure of actual grain size in microns, whereas the ANSI standard gives a measure of mesh size as described above. The two systems are compared in Table 3.3. Although the two standards do not exactly correspond, no discernible difference was detected in comparable wheels of either FEPA or ANSI designations (Hitchiner and McSpadden 2004).

The meaning of a particular grain size can vary from one specification to another. This is because it is impossible to specify grain size within tight limits and it may even be undesirable. A grinding wheel contains a range of grain sizes that will pass through one sieve but not through the next finer sieve. Each grain size for conventional abrasives is specified with reference to the mesh number of the sieves used in sorting the grains. The mesh

Table 3.3 Comparison of Grain Size Designations

FEPA Designation	ANSI Designation
1181	16–18
1001	18–20
851	20–25
711	25–30
601	30–35
501	35–40
426	40–45
356	45–50
301	50–60
251	60–70
213	70–80
181	80–100
151	100–120
126	120–140
107	140–170
91	170–200
76	200–230
64	230–270
54	270–325
46	325–400

number indicates the number of wires per inch in the sieve. A larger mesh number indicates a smaller grit dimension. Malkin (1989) gives approximate relationships to relate grit diameter d_g to mesh number M .

$$d_g \text{ (inches)} = 0.6/M \quad \text{approximate grain size in inches} \quad (3.1)$$

$$d_g \text{ (mm)} = 15.2/M \quad \text{approximate grain size in mm} \quad (3.2)$$

Example 3.1 What is the approximate average grain size of the abrasive in a wheel specified 19A60L7?

Grit mesh size: $M = 60$

Average grain size: $d_g = 15.2/60 = 0.253 \text{ mm (or } 0.01 \text{ in.)}$

Malkin cautions that large variations from these values can apply. A definition becomes even more difficult when the grits have high aspect ratios. With high aspect ratios, the grit dimension bears more relationship to fibre diameter than fibre length.

In some cases, manufacturers add grits of different nominal grit size. For example, a 602 grit size has an extra digit added to indicate a mix of grain sizes.

Grade

Wheel grade is generally indicative of the way a wheel wears. A soft wheel wears quickly, and a hard wheel wears slowly. Grade is affected by the volume of the bond. A greater proportion of bond makes a wheel harder. These characteristics can be altered to a limited extent by the dressing procedure employed. Coarse dressing tends to provide a more open surface on the grinding wheel, thus making the wheel effectively softer.

Wheel grade is indicated by a letter in the range A to Z. A letter higher in the alphabet indicates greater hardness than a lower letter. Manufacturers attempt to make these grades comparable, but differences occur. There have been a number of attempts to correlate grade letters with measured hardness, with only limited success. Indentation tests on a wheel similar to conventional hardness testing have been tried with limited success. Screw-driver tests have been tried where a chisel edge is loaded with constant force against a wheel and the torque required to dislodge grains is measured. This type of testing is more successful.

A further method is to use ultrasonic probes to measure the effective E modulus of the wheel. This method has also had some success and is claimed to be reliable (Peters et al. 1970). Breckner (1973) confirmed this

for vitrified wheels, but considered static bend tests were better for resinoid wheels because of their high damping.

The big users tend to take the indicated grade as a relative measure for a particular manufacturer's wheels of a particular grit size, bond, and structure. Consistency from wheel to wheel is important. Consistency depends on process control in the mixing, compaction, and firing stages of wheel manufacture. The particular grade selected is optimised on the basis of grinding trials.

Structure Number

Wheels having an open structure allow better swarf removal and give better access for grinding fluid. Wheel structure relates to the packing density of the grains. Structure is designated by a number between 0 and 25. A low structure number below 4 is very dense and a structure number higher than 14 indicates a wheel where the grains are widely spaced. Structure is defined by manufacturers in terms of the volume of the abrasive. Typically, more than 60% volume of the abrasive corresponds to a very dense structure where the grains are packed very closely together. With a higher structure number, the grains are separated by a greater distance.

Porosity

Porosity and structure are related. Porosity is also governed by the proportion of bond in the mix. A highly porous wheel will have an open structure and a lower proportion of bond than a normal porosity wheel of the same structure. A wheel with high porosity will tend to act "soft," whereas a wheel with low porosity will tend to act "hard." A highly porous wheel allows grains to be dislodged more easily. This can lead to a high rate of wheel wear.

Recent developments of more porous wheels that can also withstand rapid wear have been important for increasing removal rates. Porous wheels have been particularly important in the development of high removal rate creep-feed grinding where the issues of lubrication and cool operation are of particular relevance.

High porosity is an advantage when grinding materials that produce long chips. The long chips have to be accommodated in the pore space without becoming impacted. Porosity also helps transport grinding fluid into the grinding contact area. Better fluid delivery assists in maintaining a clean abrasive surface and in keeping grinding temperatures down. Low temperatures also help to avoid chip adherence to the wheel.

Concentration

Concentration is used to designate the amount of diamond or CBN in superabrasive wheels based on carats/cm³. Most diamond wheels have a concentration in the range 12–100.

With CBN, a concentration of approximately 100 is typical for outside diameter grinding and a slightly higher concentration up to 150 is typical for internal grinding. A concentration of 100 corresponds to 4.4 carats/cm³ and 25% proportion by volume. A concentration of 150 corresponds to 6.6 carats/cm³ and 37.5% proportion by volume.

3.6 Wheel Design and Application

Figure 3.2 illustrated three basic wheel designs. The basic designs can vary considerably depending on such factors as abrasive, bond, and wheel speed. A much greater variety of wheel shapes are available, designed for particular workpiece shapes and machine types. For example, there are profile wheels used for grinding cutting tools, gears, and screw threads; large face wheels for vertical face grinding; long wheels for through-feed centreless grinding; cup wheels for face grinding; and almost every imaginable variation for a range of grinding operations. The wheel manufacturers will provide advice for particular applications.

The following features highlight the basic principles for a safe approach to application of grinding wheels and use of high speeds.

Safety

It is important that users follow the safety requirements for each country of operation. These control such aspects as risk assessment, training, and supervision of machine operators and setters; design, manufacture, and testing of abrasive products; wheel mounting; wheel balancing; shelf life of abrasives; and machine guarding. There is a responsibility to check compliance with all necessary procedures for safety within the working environment. Special consideration is necessary for guarding. This is even more important for high-speed wheels.

Wheel Mounting

Figure 3.5 illustrates a standard plain wheel mounted on a hub and clamped between wheel flanges using a paper washer or “blotter” to prevent undue local stresses on the abrasive. When the wheel is bolted between the

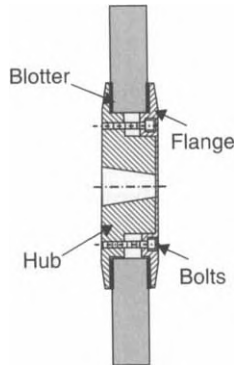


Figure 3.5 Mounting flanges for a plain grinding wheel.

flanges, it is important that the correct tightening up procedure is followed to ensure sufficient grip to prevent wheel slip and an even pressure around the flange to avoid stress concentrations. The bolts should not be overtightened. The design of wheel flanges and the use of grinding wheels are controlled by standards in every country. For example, the relevant standards in UK include BS 4481: Part 1:1981 “Bonded abrasive products” and BS 4581:1970 “The dimensions of flanges for the mounting of plain grinding wheels.”

Clearance is required between the grinding wheel bore and the hub to avoid placing radial and hoop stresses on the wheel. The clearance has to be sufficient to cope with manufacturing tolerances on the wheel bore. Too much clearance will lead to increased run-out of the wheel after mounting.

The wheel flanges in Fig. 3.5 can be used for all three wheel designs shown in Fig. 3.2. However, for high wheel speeds, further consideration needs to be given to the design. Some of these issues are outlined below.

The flanges serve several purposes. These include:

- friction to accelerate, brake, and overcome grinding forces;
- balancing features;
- radial and axial positional constraint while avoiding stress concentrations;
- optimum clamping can reduce the maximum rotational stresses experienced.

Balancing

After mounting, the wheel assembly must be balanced. For lower wheel speeds and medium accuracy, it is sufficient to carry out balancing in static

ways using a dummy spindle, allowing the wheel to rotate to find the out-of-balance position. The wheel hub usually incorporates provision for adjusting the angular position of at least three balance weights. When the weights are arranged at 60° intervals around the wheel flange, the weights are exactly in balance with each other. If two weights are moved closer together opposite the third weight, an out-of-balance is achieved. The position and magnitude can be adjusted to balance the wheel out-of-balance. For wide wheels, consideration should be given to balancing in two planes to avoid setting up a conical whirl.

For precision work and for higher wheel speeds, it is essential to balance the wheel using a balancing device that provides corrective out-of-balance at wheel speed. Manufacturers provide balancing devices that can be incorporated into the wheel-hub assembly. The usual procedure is to dress the wheel to minimise run-out, then balance the wheel, and finally redress the wheel to correct for any remaining run-out. There is a danger if a new wheel is run straight up to maximum speed such that out-of-balance forces will cause excessive stresses on the wheel and machine bearings. For high wheel speeds, it is advisable to balance the wheel at moderate speed and then increase wheel speed and rebalance. Several iterations may be required.

A frequent cause of severe unbalance is when grinding fluid is absorbed into the wheel. It is very important that the wheel is spin dried for at least half an hour after the fluid is turned off. Failure to spin dry the wheel effectively leads to the lower part of the wheel circumference being heavily unbalanced. Due to the capillary effect, fluid does not empty from the wheel under gravity even after long periods of standing.

3.7 High-Speed Wheels

Unbalanced Stresses

It is absolutely essential that high-speed wheels are balanced, as unbalanced forces create heavy stresses and large vibrations in the whole system. This is sometimes the cause of premature grinding wheel failure, a situation to be avoided at all costs. For a conventional wheel as in Fig. 3.2(a), the energy in a bursting wheel can be exceedingly dangerous.

Balanced Stresses

Even in a perfectly balanced wheel, rotational stresses arise and increase with the square of wheel speed. As wheel speeds increase, wheel designs

move away from the conventional design in Fig. 3.2(a) to the metal bond wheel design of Fig. 3.2(b) or the segmented designs for vitrified wheels in Fig. 3.2(c). There are also other designs that achieve intermediate speeds based on reinforced and bonded hubs.

Rotational stresses arise, even in a balanced grinding wheel, due to the centripetal accelerations associated with high speeds. For a uniform isotropic material, these stresses can be predicted with good accuracy using the equations of elasticity for a rotating disc. A factor of safety is required for a grinding wheel to allow for reduced homogeneity of an abrasive structure. The maximum operating speed of a grinding wheel should be no greater than 50% of the speed necessary to burst a wheel. Since it is impossible to test wheels up to bursting speed without damage, wheels are proof tested at 50% above the maximum operating speed. The bursting speed of a wheel depends on maximum crack length near the bore. Burst speeds are therefore subject to the laws of fracture mechanics, implying that not all wheels will fail at exactly the same speed. The maximum speed rule implies a safety factor of four on maximum stress, which is sufficient to allow for the variations in wheel life under normal operating conditions.

A wheel having a fine grit and a closed structure can be operated at higher speeds than wheels that are coarse and have an open structure. The elasticity and yield strength of an abrasive structure can be determined by mechanical testing of samples cut out of a grinding wheel.

A better understanding of the factors governing wheel design can be gained from a consideration of the stress equations.

The element of radius r in Fig. 3.6 is subject to tensile radial stresses p and tensile circumferential stresses f . The radial equilibrium, as the element

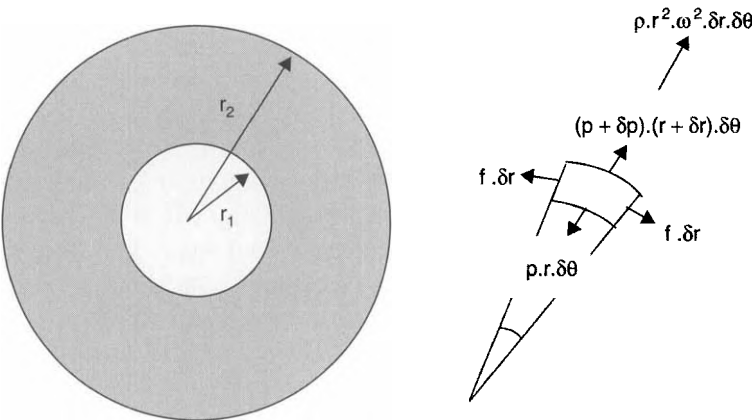


Figure 3.6 Stresses on an element of a free rotating wheel.

becomes infinitesimal, reduces to $f - p - r \cdot dp/dr = \rho \cdot r^2 \omega^2$ where ρ is the material density and ω is the angular wheel speed. The radial element is subject to radial shift u . The radial and circumferential strains are related to the stresses and the elastic constants. These relationships are $E \cdot du/dr = p - \nu \cdot f$ in the radial direction and $E \cdot u/r = f - \nu \cdot p$ in the circumferential direction. The Young's modulus E and Poisson's ratio ν are the elastic constants of the abrasive. Eliminating u from the equations and integrating leads to general equations for radial and tensile stresses. These are $p = A - B/r^2 - (3 + \nu)(\rho \cdot r^2 \cdot \omega^2/8)$ and $f = A + B/r^2 - (1 + 3\nu)(\rho \cdot r^2 \cdot \omega^2/8)$ where the values of A and B can be determined from boundary conditions. Assuming zero radial stress at the inside and outside radii for the wheel shown in Fig. 3.6 and assuming zero axial stress,

$$p = (\rho \cdot \omega^2/8)(3 + \nu)(r_1^2 + r_2^2 - r_1^2 r_2^2 / r^2 - r^2) \quad \text{radial stress} \quad (3.3)$$

$$f = (\rho \cdot \omega^2/8)[(3 + \nu)(r_1^2 + r_2^2 - r_1^2 r_2^2 / r^2 r) - (1 + 3\nu)r^2] \quad \text{circular stress} \quad (3.4)$$

The maximum rotational stress is found to be the circumferential stress at the inner radius where $r = r_1$. As the size of the bore is increased, maximum stress also increases. The maximum circumferential stress is given by

$$f = (\rho \cdot \omega^2/4)[(1 - \nu)r_1^2 + (3 + \nu)r_2^2] \quad \text{maximum circumferential stress} \quad (3.5)$$

Example 3.2 Calculate the maximum circumferential stress for a grinding wheel of 400 mm diameter having a bore diameter of 100 mm at a speed of 1500 rev/min. Assume an average value of density of 2200 kg/m³ and a value of Poisson ratio for the abrasive structure of 0.22.

$$\omega = \frac{1500 \times 2 \times \pi}{60} = 157.1 \text{ radians/s}$$

$$r_1 = 0.1/2 = 0.05 \text{ m}$$

$$r_2 = 0.4/2 = 0.2 \text{ m}$$

$$\begin{aligned} f &= \frac{2200 \times 157.1^2}{4} \times [(1 - 0.22) \times 0.05^2 + (3 + 0.22) \times 0.2^2] \\ &= 1.78 \times 10^6 \text{ N/m}^2 \text{ (or } 258 \text{ lbf/in.}^2\text{)} \end{aligned}$$

Stresses and strains may be easily calculated using the above equations. Values for a typical vitrified grinding wheel are shown in Fig. 3.7. It can be seen that the radial stress p is much lower than the circumferential stress f .

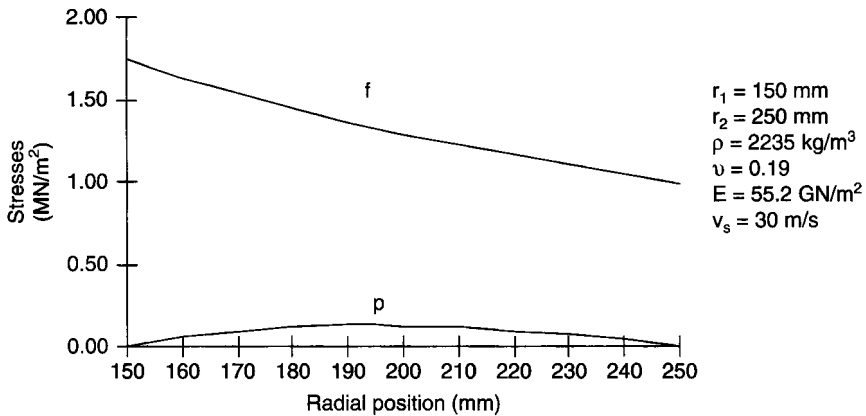


Figure 3.7 Rotational stresses in a vitrified grinding wheel.

The maximum circumferential stress is 2.78 MN/m^2 , which is approximately one-tenth of the failure strength of a typical 60 grit vitrified alumina. The position where fractures usually initiate is at the bore. The average radial strain for the case in Fig. 3.7 is $4.5 \mu\text{m}$.

Further discussion of high-speed wheel design is available in the literature (Barlow and Rowe 1983; Barlow et al. 1995).

Practical Considerations for Design of High-Speed Wheels

There are several possible ways of achieving increased maximum operating speed. Most of these methods have been employed in practice. Some methods may be summarised as follows.

A Solid Wheel

Use a solid wheel without a central hole. Even a small hole doubles the maximum stress. This method has been employed for solid vitrified wheels. However, the wheel has to be attached to a drive and it may not be easy to avoid introducing stress concentrations.

Central Reinforcement

Reinforce the central region near the bore to restrain radial movement. Reinforced wheels have been used successfully to raise wheel speeds. The

reinforcing can be provided by a ceramic material or by using an appropriate metal. Ideally, the material should have high strength, high stiffness, and low mass. The benefit of the reinforcement increases with increasing depth of radial reinforcement.

A Tapered Wheel

Use a tapered wheel that is wider at the centre than at the outer radius. This is another way of providing restraint to radial movement. It is not widely used.

Bonding to a Metal Hub

Use a metal hub and bond the abrasive to the hub. This is a development of the idea of reinforcing the central region and allows further increase in speeds.

Bonded Segments

Use a metal hub and bond narrow segments of abrasive to the hub as in Fig. 3.2(c). This method has been highly successful. The division of the abrasive layer into separate segments reduces circumferential stresses. A major attraction of the segmented wheel is that if a segment fails, the energy released is a small fraction of the energy released when a wheel of conventional design fails. This is because the mass of the segment released is a small fraction of the mass of a conventional wheel. A flying segment, although dangerous, is much more easily contained by machine guarding. Another advantage of the segmented design is that balance problems are reduced. The selection of an appropriate adhesive is an important aspect of the design process. The life of the adhesive becomes an important consideration.

Metal Bond

Use a metal bond to directly adhere the abrasive to a metal hub. This method allows the highest wheel speeds to be achieved with single layers of abrasive and is commonly used for diamond and CBN wheels. The disadvantage of a single-layer wheel is the considerable expense and the need for great accuracy in wheel manufacture and wheel mounting. Single-layer wheels have been highly successful for high removal rate processes such as crankshaft grinding.

Dressable Metal Bond

Dressable metal bond wheels also allow high wheel speeds. These wheels are mainly used for fine grinding of brittle and very hard materials using superabrasives. Such wheels are not necessarily used at high wheel speeds since accuracy may take precedence over removal rate. The modern way of dressing metal bond wheels is by ELID.

Metal bond wheels used for ELID grinding are described in more detail in Chapter 4. ELID grinding wheels are often used for super-finishing and nano-grinding applications. ELID grinding is a process that allows the successful grinding of ceramics and can be used to achieve extremely close tolerances. For such applications, extremely small abrasive grain sizes are employed. The abrasive grains are contained within a dense metal bond. A cutting surface is achieved by machining away the metal bond surrounding the abrasive asperities using ELID.

3.8 Wheel Elasticity and Vibrations

Users report slightly higher roughness values on ground workpieces when using very stiff wheels rather than more elastic wheels. This may be noticed using superabrasive wheels with stiff metal hubs. A stiff grinding system impresses abrasive grits into the workpiece more firmly than a soft system. A soft wheel has more of a polishing action than a stiff wheel. A similar conclusion was reported for vibrations by Rowe et al. (1965). Forced and self-excited vibrations may be more firmly impressed on the surface by a stiff system than a more elastic system. This effect is illustrated in Fig. 3.8.

The following radial contact stiffness values were obtained by Frost for conventional and CBN vitrified wheels (Marinescu et al. 2006).

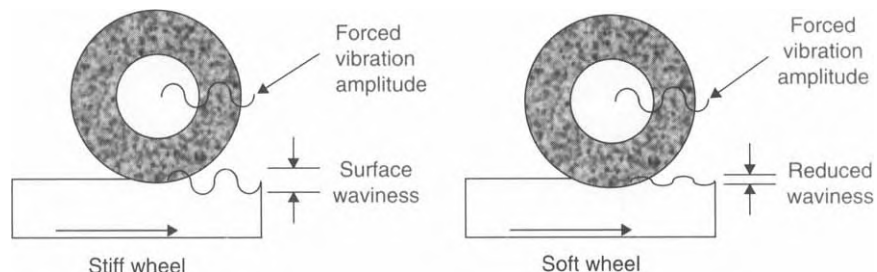


Figure 3.8 A stiff wheel impresses forced vibrations into the surface whereas a soft wheel reduces the resulting waviness of the workpiece.

47A100 L6YMRAA	0.06 N/ $\mu\text{m} \cdot \text{mm}$ (or 8,700 lbf/in. $\cdot$ in.)
5B46 P50 VSS	0.78 N/ $\mu\text{m} \cdot \text{mm}$ (or 113,100 lbf/in. $\cdot$ in.)
5B76 P50 VSS	0.31 N/ $\mu\text{m} \cdot \text{mm}$ (or 44,960 lbf/in. $\cdot$ in.)

It can be seen that the conventional alumina wheel has a much greater elasticity than the thin layer vitrified CBN wheels. The CBN wheels would therefore give slightly higher roughness and greater waviness if other factors remain unchanged. In practice, CBN wheels are usually employed with different speed conditions and on superior machines so that surface waviness is actually reduced.

There are several ways in which elasticity can be introduced into a wheel without substantially reducing major resonant frequencies of the grinding system. It is important to avoid introducing elasticity into the main structure of the machine without considering the effect on the overall machine responses. However, elasticity can usually be safely introduced into the wheel near the contact with the workpiece. Thus, a vitrified wheel or a resin bond wheel will have useful elasticity. Sometimes extra elasticity can be added into the wheel bond or into the wheel hub. For example, it was found that chatter was reduced when grinding steel with resin CBN wheels by the use of a nickel-foam hub material with a radial stiffness of 0.5 N/ $\mu\text{m} \cdot \text{mm}$ (or 72,520 lbf/in. $\cdot$ in) (Sexton et al. 1982). This was compared with values of 4–10 N/ $\mu\text{m} \cdot \text{mm}$ (or 580,100–1,450,000 lbf/in. $\cdot$ in) for standard phenolic or aluminium-filled phenolic hubs.

There are two main reasons for the effect of elasticity. The first is the deflection of the wheel surface away from the workpiece due to the grinding force and the other is mechanical interference between the shape of the wheel and the waviness of the workpiece (Rowe et al. 1965).

Malkin (1989) describes suppression of waviness by mechanical interference due to the wheel shape. High frequencies of waviness on the workpiece f_w are attenuated due to the contact length l_c being longer than the wavelength λ_w of the surface waves. The break frequency above which amplitudes are attenuated on the workpiece is

$$f_w = v_w / 2 \cdot l_c \quad \text{waviness break frequency} \quad (3.6)$$

Example 3.3 Calculate the break frequency for workpiece waviness where the work speed v_w is 100 mm/s and the contact length is 1 mm.

Vibrations are attenuated above $100/2 = 50$ Hz.

A more elastic wheel increases contact length as described in Chapter 2. This has the effect of reducing the maximum frequencies of waviness. For

example, doubling contact length halves the break frequency. Reducing work speed also reduces waviness.

We can go a step further and evaluate the maximum amplitude of surface waviness a_{sw} for a particular wavelength λ_w on the workpiece allowed by the local curvature of the wheel. Based on the principle of intersecting chords of a circle, the maximum amplitude of unattenuated waviness on the workpiece surface is

$$a_{sw} = \frac{1}{2 \cdot d_e} \cdot \left(\frac{\lambda_w}{2} \right)^2 \quad \text{maximum waviness} \quad (3.7)$$

Example 3.4 What is the maximum amplitude with an effective wheel diameter of 200 mm for a surface wave of 2 mm wavelength?

$$a_{sw} = (1/2) \cdot (1/200) \cdot (2/2)^2 = 0.0025 \text{ mm (or } 0.000098 \text{ in.)}$$

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4 Grinding Wheel Dressing

4.1 Introduction

Dressing is performed on a grinding wheel in preparation for grinding. The aspects of dressing include

- truing to eliminate deviations from specified form or straightness;
- dressing to achieve a sharp cutting surface and a uniform random distribution of cutting edges;
- conditioning to remove the bonds surrounding the abrasive grains and create a more open wheel surface. This is particularly important for resin-bonded and vitrified super-abrasive wheels. Sometimes conditioning is attempted with carbide or alumina abrasive sticks. Aggressive use of abrasive sticks wears the abrasive in the wheel and shortens redress life. Another technique is to carry out a reduced removal rate grinding process with the wheel until it has opened up;
- cleaning up to remove a layer of abrasive that is loaded with workpiece material.

Vitrified and other bonded wheels are always dressed before performing a grinding operation. Electroplated wheels are not usually dressed as these wheels contain a single layer of abrasive grains. Removing the grains destroys the wheel. Occasionally, electroplated wheels may be trued initially, carefully removing 10–20 μm , or may at times be conditioned lightly with a dressing stick to remove loaded metal.

Metal-bond, multi-layer super-abrasive wheels are sometimes dressed using a carbide wheel. Also, electrolytic in-process dressing has been used with considerable success (Ohmori and Nakagawa 1990). Dry electro-discharge truing has also been proposed and demonstrated for micro-truing combined with a rotary carbide dressing tool (Xie and Tamaki 2008).

There are two basic types of dressing tools (Marinescu et al. 2004, 2006): stationary tools and rotary tools.

4.2 Stationary Dressing Tools

Stationary dressing tools include single-point diamonds and impregnated diamond dressing tools and are mainly used for dressing conventional

abrasives. Such tools come in a variety of shapes including round- and knife-shaped tools for form dressing.

Multi-Point Diamond Tools

Multi-point diamond dressing tools share the dressing action over a number of cutting points and give greater life. These tools have a number of advantages. It is possible to create a range of dressing tool shapes to cope with wheel profiles. A dressing tool with a chisel edge allows the tool to follow a generated profile on the grinding wheel.

Form Dressing Tools

A dressing tool can be formed as a block to the reverse shape required on the grinding wheel. This allows the grinding wheel shape to be formed across the whole width in one pass.

Single-Point Diamond Tools

Single-point diamonds are set in a tool shank that is periodically rotated to present a different edge for the dressing operation. If the shank is not rotated, the diamond will become heavily worn on one edge leading to serious loss of dressing efficiency.

Single-point diamonds can give excellent dressing performance when used correctly. For larger wheels, it is necessary to use a larger diamond to cope with the dressing action. For a 500-mm diameter wheel (or 20-in. diameter), a 1-carat diamond is a minimum size dressing tool.

The Dressing Process

Figure 4.1 illustrates a dressing process, although the process would be much the same if a multi-point tool is used. Figure 4.1(a) shows a diamond set in a tool shank. A large natural diamond is best for truing large wheels. To ensure vibration-free dressing, the tool holder allows the tool shank to be set at a drag angle of the order of 10° . Also, the tool shank must be rigidly mounted in a tool holder

The dressing tool is traversed across the surface of the grinding wheel as in Fig. 4.1(b) to generate the required form and cutting surface. A coolant should be applied during dressing to keep the diamond cool. This may require the tool holder to have its own coolant nozzle. The coolant supply

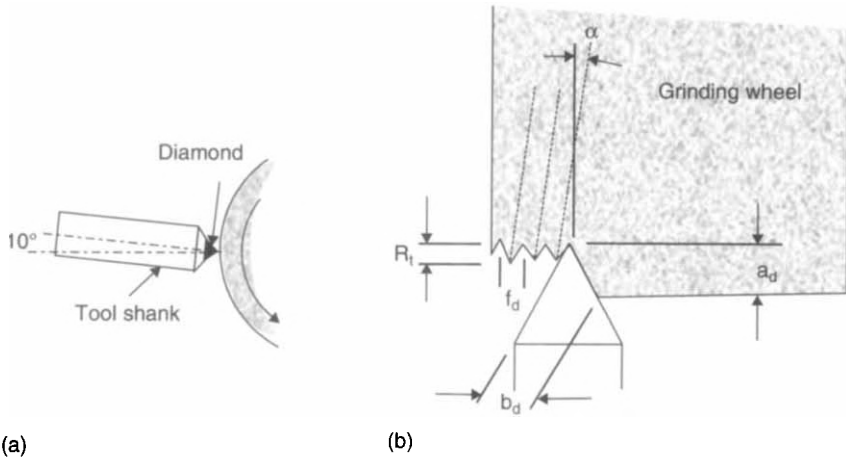


Figure 4.1 Single-point dressing with a stationary non-rotating dressing tool. (a) Single-point dressing tool and (b) single-point traverse dressing.

must be turned on before commencing a dressing pass. If the coolant is turned on during a pass, the diamond will be damaged by thermal shock.

Figure 4.1(b) illustrates how the dressing depth of cut a_d and the dressing feed per revolution of the grinding wheel f_d create a helical groove on the wheel surface. The shape produced also depends on the width of the dressing tool b_d in engagement with the grinding wheel.

Overlap Ratio

The smoothness of the wheel surface depends on the overlap ratio U_d where

$$U_d = b_d / f_d \quad \text{overlap ratio} \quad (4.1)$$

A high value of overlap ratio creates a smoother grinding wheel surface but leads to higher grinding forces and higher specific energy of material removal. A low value of overlap ratio creates a sharper cutting surface and higher surface roughness of the ground workpieces. Usually, the overlap ratio should lie within the range $U_d = 2-20$.

Dressing Tool Sharpness

The smoothness of the grinding wheel also depends on the sharpness of the dressing tool which can be defined by

$$\gamma_d = a_d / b_d \quad \text{dressing tool sharpness ratio} \quad (4.2)$$

Example 4.1 Calculate the overlap ratio and the sharpness for a precision grinding operation where the dressing feed rate is 0.1 mm (or 0.004 in./rev) per revolution of the grinding wheel and the width of the single-point diamond is 1.2 mm (or 0.047 in.) at a dressing depth of 0.015 mm (or 0.0006 in.).

$$\text{Overlap ratio: } U_d = 1.2/0.1 = 12$$

$$\text{Sharpness ratio: } \gamma_d = 0.015/1.2 = 0.0125$$

The sharpness ratio can be viewed as a shape ratio that can be employed even when the dressing tool is not conical. A pointed conical tool as illustrated produces a helix on the grinding wheel surface, whereas a rounded tool having a large radius has a low sharpness ratio and might be expected to produce a smoother wheel surface. However, forces increase during the dressing operation as the dressing tool becomes blunt, and there is an increasing likelihood that the accuracy of the dressing operation will suffer and the smoothness of the wheel diminished. The net result is likely to be poorer surface finish and even dressing chatter marks. These are often identified by wavy markings on the ground workpieces.

Coarse and Fine Dressing

With coarse dressing, large values of a_d and f_d are employed so that the dressing helix is more pronounced and the density of the cutting edges on the wheel surface is reduced. This is because coarse dressing causes macro-fracture of the wheel grains completely removing many grains from the grinding process.

Conversely, with fine dressing, the density of the cutting edges is increased and the wheel surface is more closed. In the grinding which takes place just after dressing, the helix pattern tends to disappear as the grinding wheel wears. The spacing then tends to revert towards the spacing based on the basic structure and composition of the wheel.

In fine dressing operations, both a_d and f_d are small. The dressing operation is often completed with one or more passes where $a_d = 0$. The surface roughness of the workpiece is small immediately after dressing but increases with wheel wear. Fine dressing tends to produce not only a blunt wheel but also cracks in the grains. The result is high initial power and grinding forces and an unstable process. Within a short time, the particles of the grains fracture or pull out of the surface, and the power and forces reduce.

Typical fine dressing values are $a_d = 5\mu\text{m}$ (or 0.0002 in.) and $F_d = 0.05 \text{ mm/rev}$ (or 0.002 in./rev) whereas typical coarse dressing values are

$a_d = 25 \mu\text{m}$ (or 0.001 in.) and $f_d = 0.25 \text{ mm/rev}$ (or 0.01 mm/rev). These values may be adjusted upwards for large grit size or downwards for small grit size wheels. A common mistake is to adopt a dressing feed that is far too small. This causes the overlap ratio to be too high and causes a condition where the wheel grains are damaged by too many interactions with the dressing tool. The grinding forces will be high and the wheel wears more rapidly. The effects of dressing conditions on grinding performance are illustrated in Section 4.4.

4.3 Rotary Dressing Tools

Vitrified CBN and resin-bonded CBN and diamond wheels are usually dressed using rotary dressing tools to avoid problems of rapid dressing tool wear. A rotary dressing tool may be a narrow disk with a layer of diamonds set around the periphery or a cup with a layer of diamonds around the edge. A narrow disk or cup dresser replaces the single-point stationary diamond and is used in much the same way by traversing the disk across the wheel surface. A rotary dressing tool can also be a wider roll used for form dressing. More generally, rotary dressing tools are termed roll dressers irrespective of their width. The best roll dressing tools are driven by a motor so as to bring the whole of the periphery into contact with the grinding wheel. There are also brake roll dressers where the dressing roll is driven by the grinding wheel. In this case, a brake slows the dressing tool down to a fraction of the grinding wheel speed.

A basic scheme for a motor-driven roll is illustrated in Fig. 4.2. Dressing tool life is greatly improved because of the many-times increase in diamond compared to a single-point tool.

A rotary dressing tool can be traversed across the surface of the grinding wheel in the same way as a stationary tool at a dressing feed rate v_{fd} . The selection of dressing feed rate is governed by the same factors as a stationary dressing tool. However, instead of producing a spiral groove on the wheel as in Fig. 5.1, the pattern produced by a roll dresser depends on the diamond spacing in the dressing tool, and a spiral groove is not usually apparent.

Dressing Roll Speed Ratio

As the dressing roll rotates, it is necessary to select an appropriate roll speed. This is achieved by selecting a suitable value of roll speed ratio:

$$q_d = v_d/v_s \quad \text{dressing roll speed ratio} \quad (4.3)$$

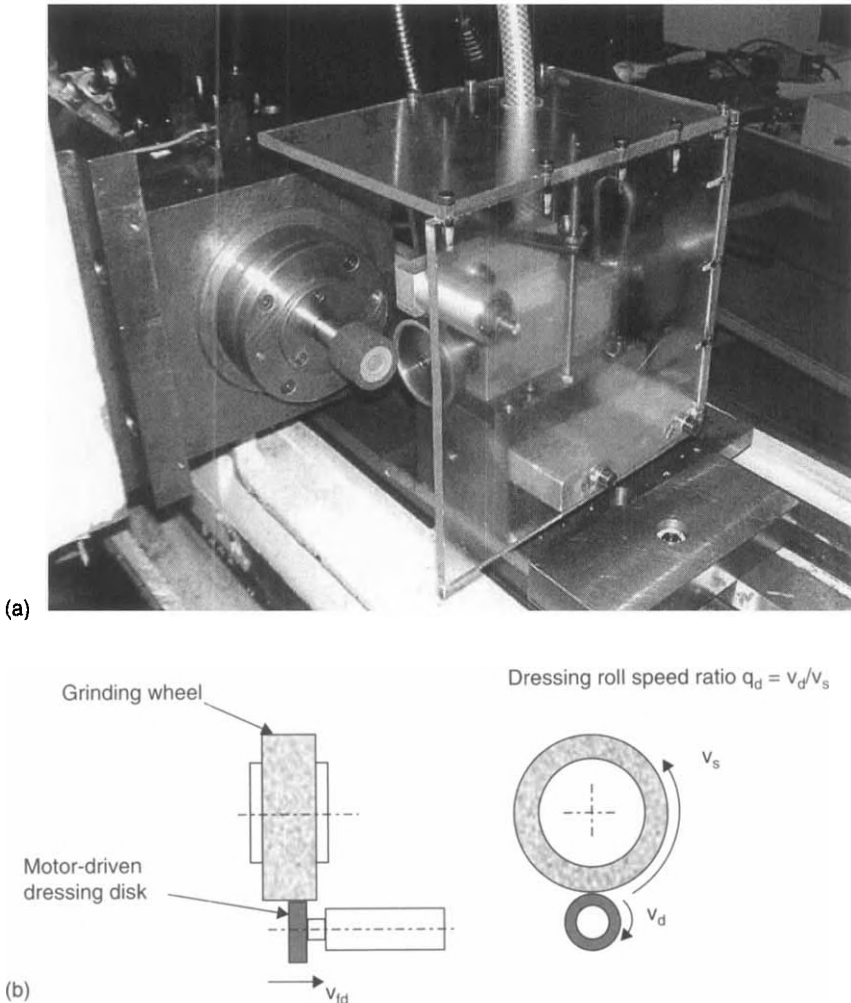


Figure 4.2 Rotary disk dressing. (a) Rotary cup dressing of an internal grinding wheel, and (b) schematic of roll dressing an external grinding wheel.

Roll speed ratio can be either positive or negative. In Fig. 4.2, a positive roll speed ratio is shown. That is, when the wheel and the roll run in the same direction as with gears. Pure rolling makes $q_d = 1$. For most precision grinding operations with conventional wheels, the roll speed ratio is adjusted within the range -0.2 to -0.8 . For CBN wheels, a positive speed range may be employed to provide a more open wheel surface.

Example 4.2 For a wheel speed of 60 m/s and clockwise rotation, what is the dresser speed required to achieve a dressing ratio of (-0.2) ?

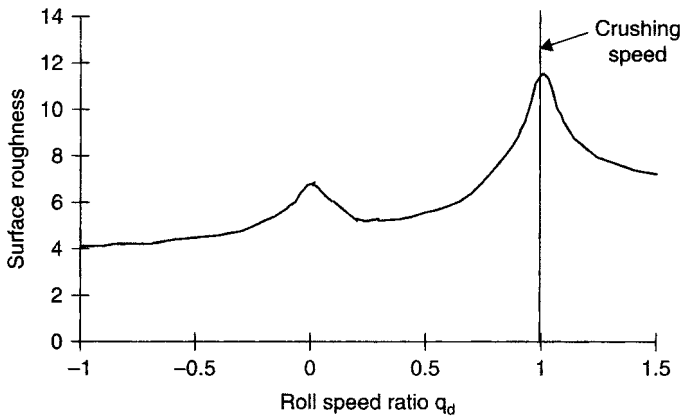


Figure 4.3 Effect of roll speed ratio on surface roughness (based on Schmitt 1968).

$v_d = -0.2 \times 60 = -12$ m/s. The dressing speed is 12 m/s with clockwise rotation.

If the surface speed of the dressing roll is equal to the surface speed of the wheel, the speed ratio is +1 and the process is a crushing action. The surface roughness of the wheel will be very high when crushing as illustrated in Fig. 4.3 for a plunge dressing operation. The normal forces on the roll are also high when crushing. For lower surface roughness and lower dressing forces, the speed ratio must be reduced and, even better, should be negative. In all cases, it is important that the roll dresser is rigidly mounted to avoid deflections and vibrations which will affect accuracy and surface roughness.

Dressing Vibrations

Problems with vibrations can have complex causes. However, there is a simple technique that can help to avoid problems. When a vibration appears, it is very helpful to determine the frequency as a multiple of the grinding wheel speed or workpiece speed. For example, if the grinding wheel rotates at n_s rev/s, it may be found that a vibration occurs at

$$f_v = m \cdot n_s \quad \text{vibration frequency} \quad (4.4)$$

where m can be an integer, non-integer, or a fraction. It is useful to make a note of this frequency. If m is an integer, it may be possible to eliminate the

vibration by adjusting the wheel speed. If the integer indicates that the vibration source is the dressing roll speed n_d , then the vibration can be reduced by adjusting n_d . Many vibration problems can be reduced or even eliminated by avoiding integer relationships between machine speeds and frequencies, dresser speeds and workpiece speeds.

Example 4.3 A grinding wheel of 450 mm diameter rotates at 1500 rev/min. Waves measured on the wheel are spaced approximately 30 mm apart. What is the frequency of the vibration?

Wheel circumference: $3.1417 \times 450 = 1414$ mm or 55.67 in.

Number of waves: $1414/30 \approx 47.1$

Wheel speed: $1500/60 = 25$ Hz

Vibration frequency: $25 \times 47.1 \approx 1178$ Hz

Grinding Wheel Dressing Speed

It is usually recommended that dressing takes place with the grinding wheel running at normal operating speed. This reduces run-out of the wheel due to minor unbalance to a minimum. It is always necessary, of course, to balance the wheel carefully. Sometimes, it is necessary to reduce wheel speed for dressing. In this case, it is important to choose a grinding wheel speed that avoids a vibration mode in the machine. It is relatively straightforward to place vibration sensors at various positions on the machine and check for these frequencies.

4.4 Grinding Performance

Dressing Traverse Rate

Dressing traverse rate affects grinding power and workpiece roughness as illustrated in Fig. 4.4. Surface roughness increases with dressing traverse rate while grinding power decreases. This is because wheel sharpness increases with dressing traverse rate both for a single-point diamond dresser and for an impregnated dressing tool. The wheel sharpness achievable after dressing with a sharp single-point diamond is better than with the impregnated diamond tool. However, the impregnated tool gives consistent wheel sharpness over a longer period whereas the single-point diamond needs more frequent attention.

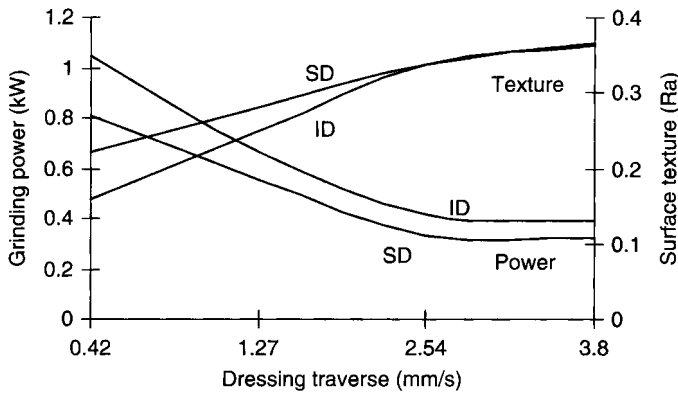


Figure 4.4 Effect of dressing traverse rate on grinding power and surface texture after dressing with a single-point diamond (SD) and with an impregnated diamond dressing tool (ID).

Coarse, Medium, and Fine Dressing

Grinding forces depend strongly on wheel grain sharpness. As grains become blunt, grinding forces increase. However, grains sometimes fracture and pull out, in which case, forces reduce with tool wear. This effect is particularly evident in grinding after fine dressing. As initial wheel wear takes place there is a sharp drop in grinding power as illustrated in Fig. 4.5.

Some grains are damaged in the dressing process. After dressing, damaged grains are initially susceptible to fracture, and grinding power reduces. After the surface of the wheel has stabilised there follows a period in which forces tend to steadily increase as the grains become blunt. The wear behaviour is strongly dependent on the dressing conditions as described by Chen (1995).

With coarse dressing, whole grains are broken out of the surface and the number of active grains on the surface is reduced. The workpiece roughness is much greater than after fine dressing.

As the wheel wears, the power levels for different dressing conditions tend to converge towards the same value. However, dressing too fine or dressing too coarse adversely affects redress life of the grinding wheel where redress life may be measured by the volume of workpiece material ground before it is necessary to redress the wheel. A redress becomes necessary when the workpieces go out of tolerance with respect to parameters such as surface roughness, vibrations, or size-holding.

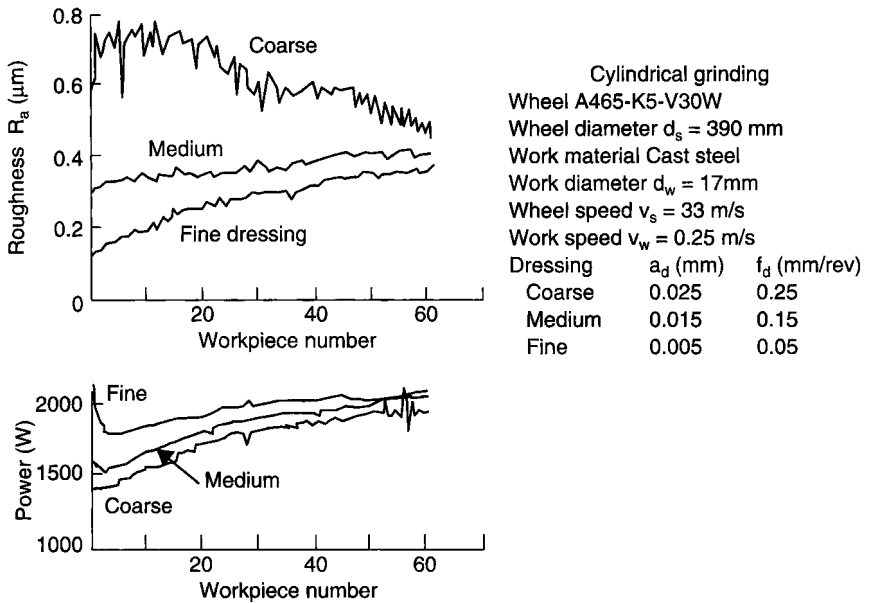


Figure 4.5 Effect of dressing conditions on grinding power and surface roughness.

Dressing Tool Wear

Figure 4.6 shows the effect of dressing tool wear. As the dressing tool wears, the sharpness ratio is reduced. Chen (1995) showed that a wide dressing tool, which corresponds to low sharpness ratio, has a similar effect to coarse dressing in that the grinding power is reduced. High sharpness ratio causes grinding power to be greater than after dressing with low sharpness ratio. A sharp dressing tool leaves a higher number of active cutting edges on the surface than a blunt diamond, which is consistent with the higher initial grinding power. However, the use of a sharp dressing tool also leaves cracks in the grains which lead to micro-fractures and rapid reduction in grinding power with further material removal in the grinding process.

Dressing tool wear causes major problems for a manufacturing process. The process becomes variable, when ideally it should be a constant process. Size, roughness, form, and roundness errors are required to remain within tolerance and are more likely to do so with a constant process. Rotary diamond dressing tools offer much longer tool life than a single-point dressing tool and greater consistency of the grinding process.

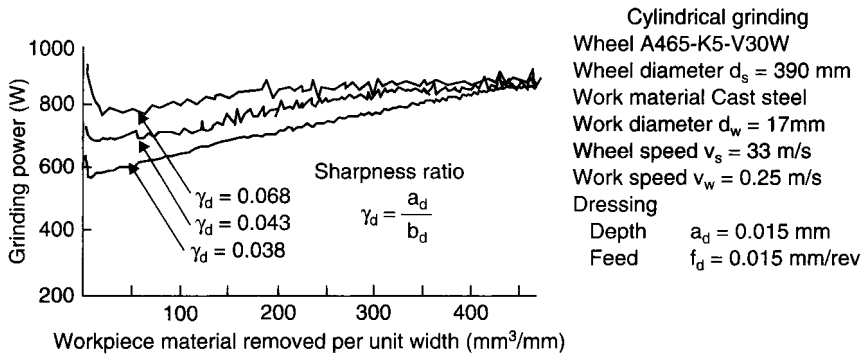


Figure 4.6 Effect of dressing tool sharpness on grinding power.

4.5 Touch Dressing for CBN Wheels

Purpose of Touch Dressing

Touch dressing is a technique of dressing a vitrified CBN grinding wheel with minimal dressing depth, usually <5 μm . A conventional dressing depth applied to CBN wheels is far from ideal. After dressing, the grinding force is high initially and removal rates must be reduced. This is because dressing with a large dressing depth closes up the wheel surface.

Grinding Performance

Figure 4.7 shows grinding results using an 11 mm diameter CBN internal grinding wheel to grind a 50 mm bore. The grinding power after conventional dressing takes a long time to decrease from an initial high value to an acceptable steady lower value. The power decreases because the grinding action erodes away bond material near the wheel surface (Chen et al. 2002). The varying power causes size variations in grinding.

Due to the hardness of CBN, the force required for the dresser to cut through the CBN grains is high. If the dressing depth is large, the large dressing force may pull out grains, leaving bond material at the wheel surface as illustrated schematically in Fig. 4.8. Subsequent grinding is conducted with bond as well as with grains. This is equivalent to grinding with a blunt wheel which increases rubbing and reduces cutting efficiency. Effective grinding can only take place after the bond material is worn away from the grains used for grinding.

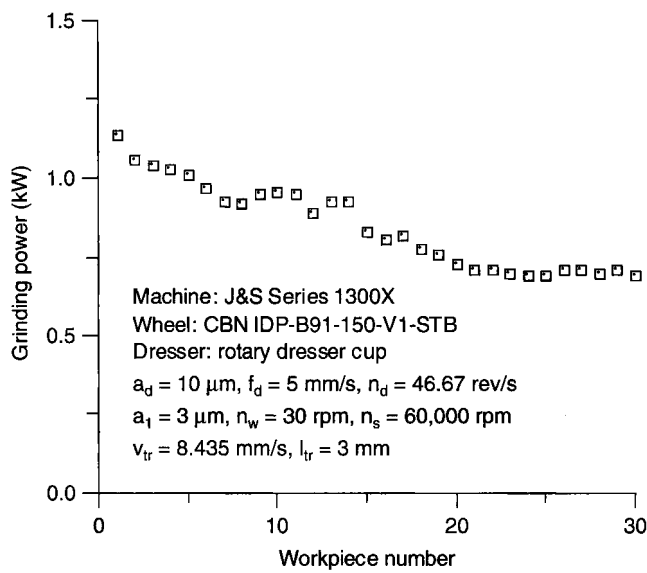


Figure 4.7 CBN grinding power after conventional dressing.

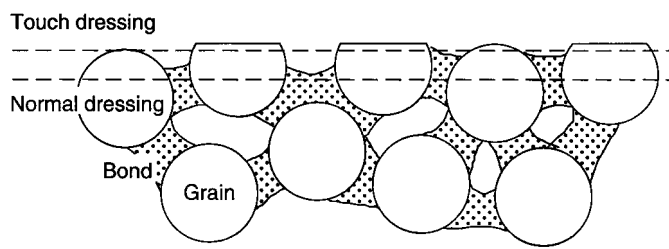


Figure 4.8 Illustrating how a large dressing depth closes up a CBN wheel surface.

In the case of touch dressing, with a very small dressing depth, the dresser may cut through the grains without pulling them out, leaving sharp grains on a more open wheel surface. Therefore, a lower initial grinding power is expected.

Figure 4.9 shows that a touch dressing operation gives lower initial grinding power and that the power remains more stable during subsequent grinding. The decrease of dressing depth increases usable wheel life. The consumption of the wheel using touch dressing is less than one-third of that with normal dressing conditions. This is an important consideration when using a thin layer of expensive abrasive. Figure 4.10 shows that wheel roundness is maintained better with touch dressing. For all of these reasons, touch dressing reduces grinding cost.

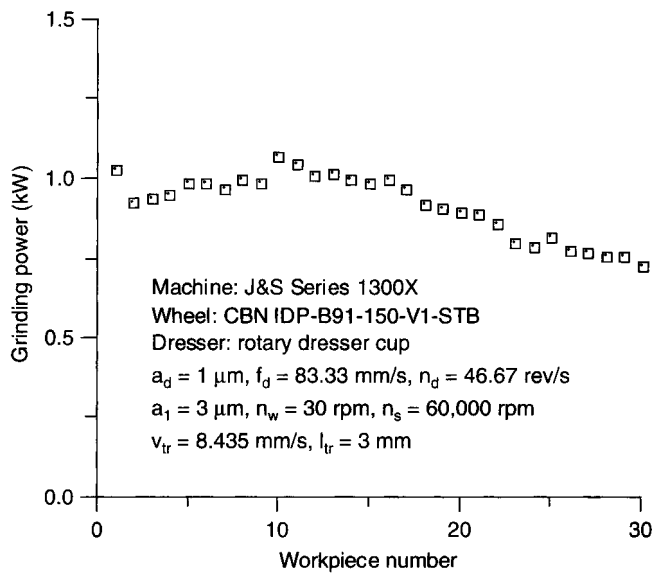


Figure 4.9 Grinding power after touch dressing.

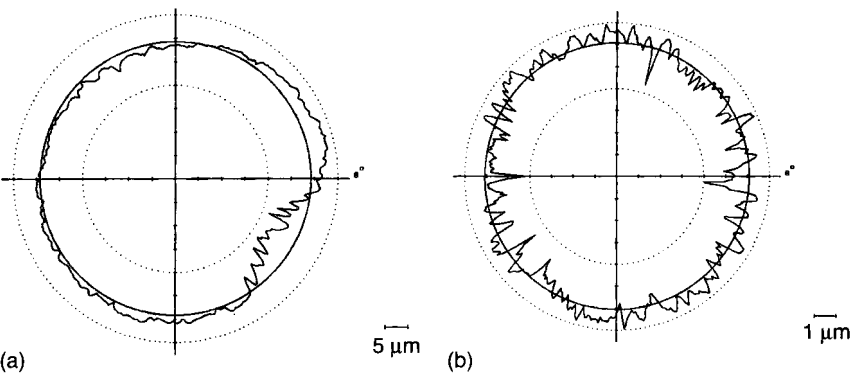


Figure 4.10 Wheel roundness measurement shows that touch dressing provides more grains on the wheel surface: (a) dressing depth is 10 μm shown at low magnification and (b) dressing depth is 3 μm at high magnification.

Touch Dressing Equipment

The advantages of touch dressing are clear, but it requires special equipment to accurately sense contact between the dressing tool and the wheel. A computer numerical control (CNC) machine has the ability to position a machine axis to a high accuracy and to achieve an increment of 1 μm (or 0.00004 in.). The position of the grinding wheel surface relative to the

machine axis positions continually changes due to grinding wheel wear, thermal expansion of the machine tool, and thermal expansion or contraction of the grinding wheel. Diamond wear also changes the position of a diamond dressing tool. The effect of this variability is that the wheel position stored in the CNC is inaccurate by the time it is necessary to re-dress the grinding wheel. To overcome this problem, the machine user often specifies a large dressing in-feed to guarantee that the dressing tool will dress the grinding wheel.

Acoustic Emission (AE) Contact Sensing

AE sensors are used to detect contact between the wheel and the dressing tool. AE is employed because the high frequency signals of dressing contact can be discriminated from background noise. The AE from the contact between the dresser and the abrasive grains contains some very high frequency harmonic elements. The AE signal is characterised using a band-pass filter, a rectifier, and a low-pass filter. Trials on an external grinding machine showed that the detection of dressing depths of cut of $1\text{ }\mu\text{m}$ (or 0.00004 in.) is easily achieved on an external grinding machine.

However, detection of dressing contact for a high frequency internal grinding machine was more difficult because of high frequency background noise associated with harmonics of the high speed grinding wheel spindle and the motor driven rotary dressing cup. The signals for touch dressing should be of a higher frequency range than the background noise to give a satisfactory signal-to-noise ratio. High frequency AE signals from the process are attenuated with transmission through several elements. For an AE sensor mounted on the body of the rotary dressing tool, the AE signal is required to pass from the dressing tool to the sensor via the dressing tool shaft, the support bearings, and the dresser body. This problem was overcome by using a fluid coupling method.

The AE sensor mounting position is shown in Fig. 4.11. The AE signal directly travels to the AE sensor via the coolant. Figure 4.12 shows the AE signals using the coolant coupling method. A $1\text{ }\mu\text{m}$ (or 0.00004 in.) dressing depth was easily identified and the trueness of the grinding wheel shape was also monitored.

Modern CNC grinding systems allow full integration of touch dressing into the grinding process. Dressing passes are made seeking contact with the grinding wheel. One or more dressing passes are then made and a check can be carried out to ensure that full dressing contact has been achieved over the length of the pass. Companies specialising in AE sensor technology offer sensors that can be incorporated into a machine spindle.

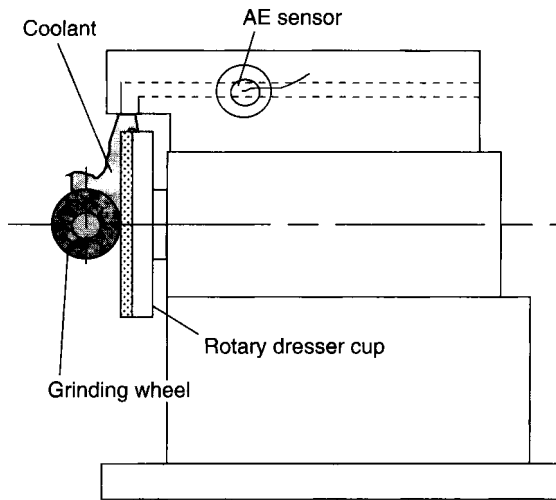


Figure 4.11 Arrangement of a coolant coupling device for AE sensing.

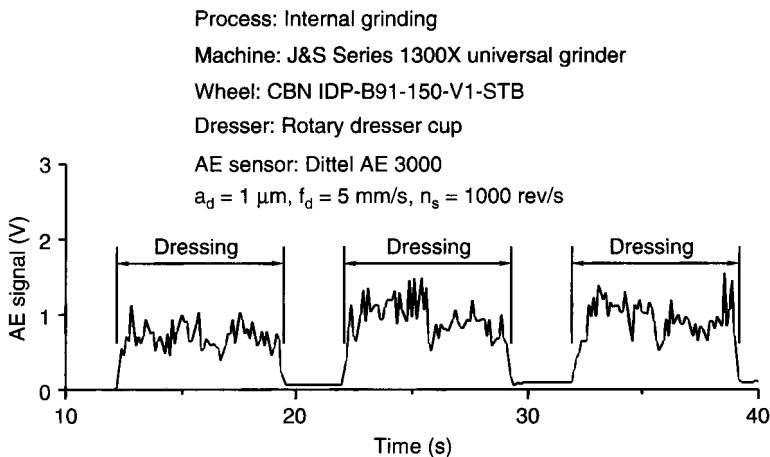


Figure 4.12 AE contact signals using a coolant coupling method.

Wheel Loading

It is important to mention that wheel loading must be avoided when using CBN and touch dressing as this creates a requirement for many more dressing passes to remove the loaded layer. There are various techniques to avoid wheel loading. High wheel speeds and high velocity coolant

delivery are techniques that can be helpful. Attention must also be given to wheel selection and coolant selection.

4.6 Continuous Dressing

Continuous dressing is a specialised technique used for creep-feed grinding where very large depths of work material are removed in a single pass. This process can be highly efficient and is used in the aerospace industry for machining deep forms into components such as the fir-tree roots of turbine blades made from temperature-resistant nickel alloys. These materials retain their hardness at high temperatures and cause heavy grinding wheel wear in the grinding process. This has the consequence of increasing the specific grinding energy in conventional creep-feed grinding as illustrated in Fig. 4.13.

In continuous dress grinding, a rotary dressing tool is steadily fed into the grinding wheel as the grinding process continues. In this technique, it is necessary to synchronise the dressing tool feed and the wheel feed to maintain a constant depth of cut as the grinding wheel reduces in diameter. In Fig. 4.13, the dresser feed rate was $f_d = 0.32 \mu\text{m/rev}$ (or 0.0000126 in.). The effect of continuous dressing is to constantly re-sharpen the wheel, thus maintaining specific energy and grinding forces constant as shown.

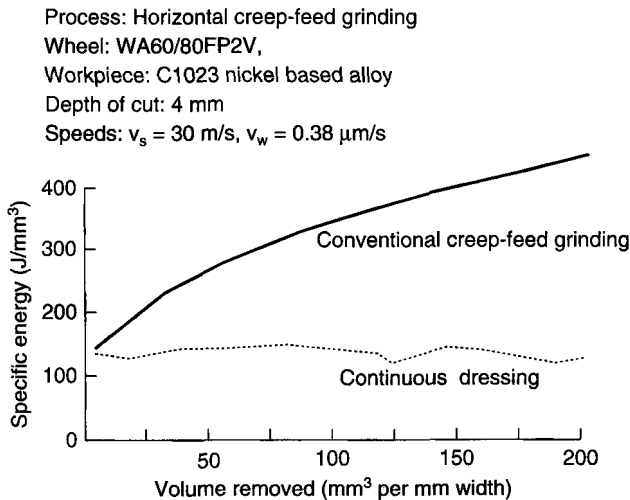


Figure 4.13 The effect of grinding wheel wear on specific energy in conventional creep-feed grinding and in continuous dress creep-feed grinding (based on Andrews et al. 1985).

This technique allows greatly increased removal rates to be maintained while avoiding thermal damage to the workpieces.

4.7 Electrolytic In-process Dressing (ELID)

Electro-chemical dressing was an early process introduced by McGeough (1974). ELID was a further development applied to a range of grinding processes to allow dressing of metal-bonded wheels (Ohmori and Nakagawa 1990). Metal-bond wheels are used for either CBN or diamond super-abrasive wheels. The process has particular application in fine grain wheels where it is used to obtain low surface roughness. ELID grinding is grinding with an integrated electrolytic in-process dressing system. Corrosive chemicals are avoided making ELID grinding a machine-friendly process. For the lowest surface roughness of the order of a few nanometres, very fine-grained wheels are used, typically of 4000–10,000 mesh grit. Much finer grit wheels have also been used to replace lapping processes. ELID grinding is used either for grinding and super-finishing steels using CBN abrasive or for grinding conventional and hard ceramics using diamond abrasive. For hard ceramics, cracking and failure are frequently encountered at conventional depths of cut. Cracking may be avoided when grinding hard ceramics by employing extremely small grain depths of cut employing stiff low-vibration machines. ELID grinding has successfully allowed replacement of other super-finishing processes to achieve mirror surface finishes with improvements in accuracy, surface texture, and production rate. Recent literature for ELID grinding of silicon wafers provides an indication of the state of the art (Liu et al. 2007).

An ELID system for surface grinding is shown in Fig. 4.14. The essential elements are a metal-bond grinding wheel, a power source, and an electrolytic coolant. A metal-bond wheel is connected to the positive terminal of a power supply with a smooth brush contact, and the fixed electrode or cathode is connected to the negative pole. There is an adjustable gap between the wheel and the cathode of 0.1–0.3 mm (or 0.004–0.012 in.).

Electrolysis causes electro-chemical erosion of the grinding wheel bond when a current is passed through the electrolyte into the bond. Electrolysis removes the metal bond and creates a dressing process. The electrolyte can be simply a conducting water-based grinding fluid having a high pH.

Initially the metal-bonded super-abrasive wheel must be trued to achieve a proper wheel shape and remove run-out. This is particularly important when a new wheel is mounted in the machine. There are several ways to carry out precision truing as described earlier. The problem is that most

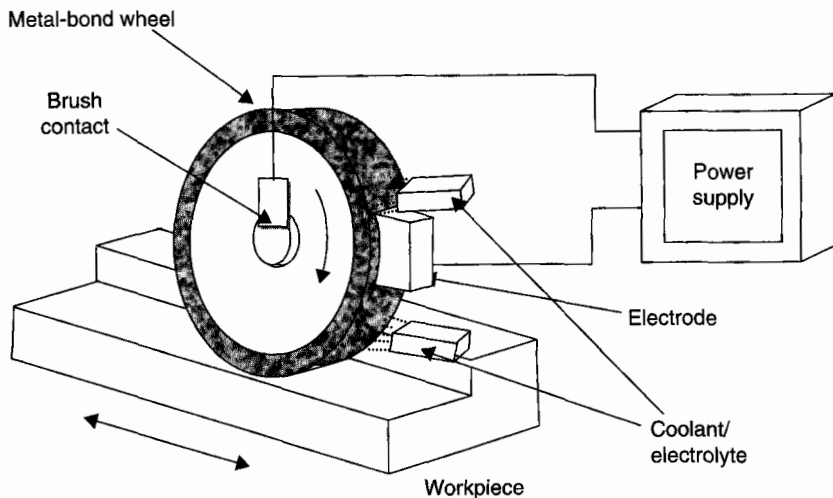


Figure 4.14 Basic elements of an ELID system (Marinescu et al. 2004).

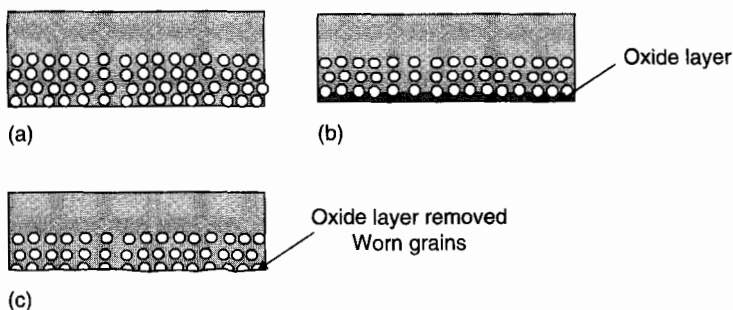


Figure 4.15 Stages in the ELID grinding process. (a) Trued wheel, (b) ELID dressed wheel, and (c) after grinding.

techniques cause damage to the super-abrasive grains. For super-precision wheels, the grains are extremely small compared to conventional abrasive wheels, and an electro-discharge truing method allows precision truing to be performed.

The wheel surface after truing is illustrated schematically in Fig. 4.15(a). A precision truing process may be carried out using a special electrical discharge (ED) abrasive wheel typically made from a bronze-tungsten carbide alloy. The ED wheel is connected to a negative pole and is slowly rotated to perform ED machining on the grinding wheel while it rotates at reduced speed. ED machining does not involve an electrolytic action. Material removal takes place by direct erosion due to electrical discharges between the anode and the cathode.

After truing, dressing is performed by electrolytic means to expose the abrasive grains as illustrated in Fig. 4.15. The dressing operation typically takes 10–30 min (Marinescu et al. 2004). The condition of the wheel surface after electrolytic dressing is illustrated schematically in Fig. 4.15(b). The electrolysis converts iron from a cast iron-bonded wheel into iron oxides that build up on the wheel surface and gradually form an insulating layer causing electrolysis to slow down and eventually cease.

After the initial truing and dressing operations have been performed, grinding and further dressing can be carried out simultaneously. As grinding commences, the oxides are worn away and the grains gradually become blunt. Unless electrolysis is maintained, the wheel condition will be changed by the grinding process as illustrated in Fig. 4.15(c). However, if the electrolytic action is performed simultaneously with grinding, the electrolysis speeds up as the oxides are worn away. Worn grains are removed as bond material is removed by electrolysis, allowing new sharp grains to participate in grinding. The process can be performed with intermittent, pulsed, or continuous electrolytic action in order to optimise the removal process.

An ample flow of electrolyte coolant must be maintained during electrolysis to remove debris and promote electrolysis. Flow rates are typically 20 l/min or more.

The wheels employed are typically cast-iron-powder-bonded diamond, cast-iron-fibre-bonded diamond, and metal-resin-bonded diamond. It is also possible to perform ELID grinding with metal-bonded CBN wheels. An indication of grain sizes employed and corresponding grain size specifications are given in Table 4.1.

Table 4.1 JIS Mesh Sizes and Abrasive Grain Sizes

JIS Mesh Size	Grain size range (microns)	Average grain size (microns)
170	88–134	110
325	40–90	63.0
600	20–30	25.5
1200	8–16	11.6
2000	5–10	6.88
4000	2–6	4.06
6000	1.5–4	3.15
8000	0.5–3	1.76

The benefits of electrolytic conditioning of resin-metal-bonded diamond grinding wheels include the achievement of a soft grinding surface on a hard body wheel for the production of damage-free optical surfaces on glass (Yoshihara et al. 2007).

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5 Wheel Contact Effects

5.1 The Abrasive Surface

Grain Size and Grain Sharpness

Wheels behave very differently depending on the size of the abrasive grains. With close spacing and small grains, the surface roughness produced is low but the grinding forces are increased. With wide spacing and large grains, the reverse is true: roughness is greater but forces are lower. However, it is not only grain spacing that affects grinding behaviour but also grain sharpness. Sharp grains use less energy and forces are reduced. Grain sharpness is affected by grain wear. In this chapter we demonstrate the effects of grain spacing and grain sharpness.

Shape Conformity

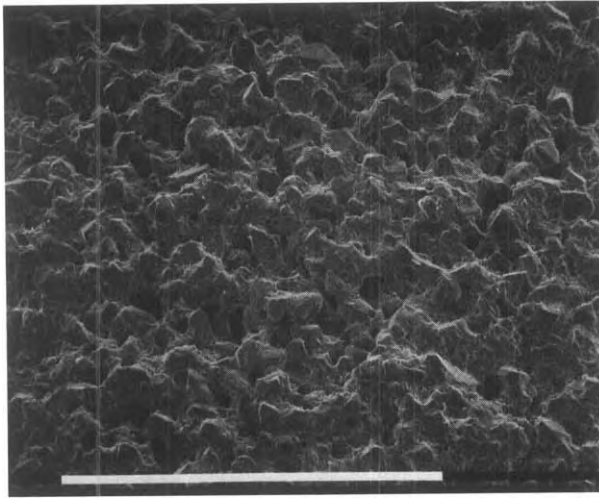
It is further shown how shape conformity between the wheel and the workpiece surface affects grinding behaviour. Contact conformity is important for correct selection of abrasives. Close shape conformity increases the grinding forces and makes a wheel act harder. It is also shown that wheel flexibility plays a significant role in grinding behaviour by increasing wheel conformity and reducing the tendency to chatter.

Abrasive Structure

Figure 5.1 shows the surface of a fine-grain vitrified CBN grinding wheel as revealed by a scanning electron microscope (SEM). This grinding wheel is typical of an internal grinding wheel used to grind bores in M2 tool steel to a fine surface finish of $0.15\text{ }\mu\text{m Ra}$ roughness ($5.9\text{ }\mu\text{in.}$).

The abrasive is actually a matrix of hard abrasive grains, vitreous bonds, and pores as illustrated schematically in Fig. 5.2.

The grains provide hard edges to cut the workpiece. The bond bridges hold the grains in position and the pores provide the space for chip flow. The chips are pieces of swarf removed from the workpiece by grinding.



Field size 4 mm

Figure 5.1 The cutting surface of a fine-grain CBN grinding wheel surface: B91-150V used for internal grinding M2 tool steel to 0.15 Ra roughness.

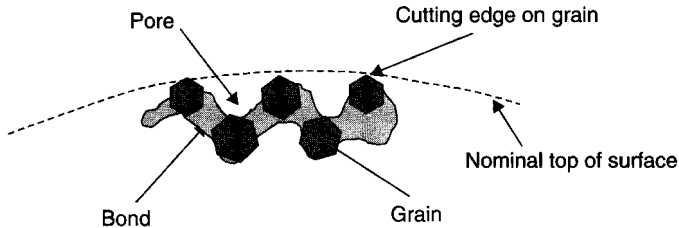


Figure 5.2 The nature of the cutting surface on a grinding wheel.

Grain Spacing and Distribution

The cutting edges are distributed randomly near the top surface as illustrated in Fig. 5.3.

The number of active cutting edges in contact with the workpiece increases with depth of grain penetration into the work surface. Measured at the outermost surface, there is only one cutting edge. It is the same when a grinding wheel is moved down onto a workpiece and commences grinding. At first only one abrasive grain makes contact with the workpiece, as the wheel is lowered further, the number of cutting points increases.

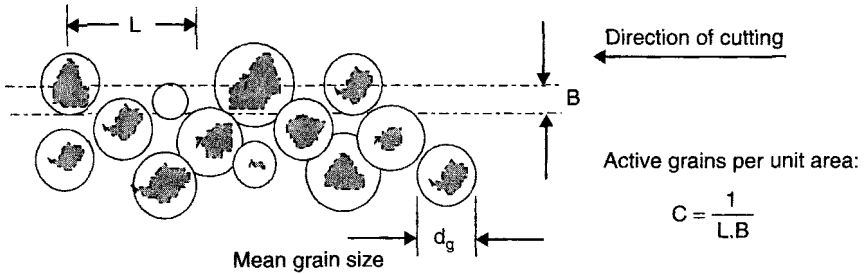


Figure 5.3 Illustrating a section of the abrasive at a depth below the surface.

In Fig. 5.3, the number of active cutting edges in contact with the work-piece per unit area of the abrasive surface is C and is defined as

$$C = \frac{1}{L \cdot B} \quad \text{Number of active grains per unit area} \quad (5.1)$$

This says that on average there is one active grain in an area LB . The average spacing of the grits is shown as L along a line of measurement or rather along a measurement band width B . The average lateral spacing between the cutting edges is $B = C/L$. Shaw (1996) measured grit spacing and found that the number of grains per unit area was less than half the value that was obtained assuming close packing. As a very rough measure of grain spacing, it might therefore be assumed that

$$C = \frac{1}{2.25d_g^2} \quad \text{Approximate grain spacing} \quad (5.2)$$

Example 5.1 Estimate the average grain spacing if the average grain diameter is 1 mm (or 0.0394 in.).

$$C = 1/2.25 \times 1 \times 1 = 0.444 \text{ per mm}^2 \text{ (or 689 per in.}^2\text{)}$$

This is no more than a rough indication as actual grain spacing in the abrasive structure depends on the proportions of grain, bond, and porosity. Also, the number of grain contacts tends to increase as abrasive grains penetrate deeper into the work material. This has the effect that C increases. For these reasons, Eqn (5.2) should not be taken as more than a very approximate measure of grain spacing.

Fine-grain wheels have large values of C and large-grain wheels have small values. It is not easy to measure C because the number of grains engaged in the grinding process increases with increasing depth of cut. Topographical descriptions of the abrasive surface attempt to overcome

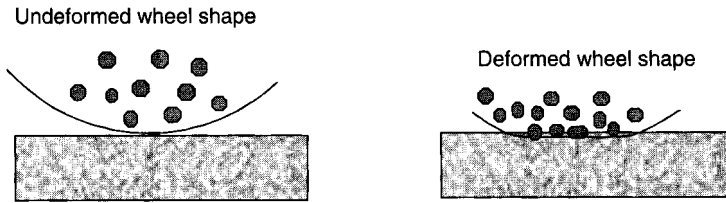


Figure 5.4 Flattening of the grinding wheel due to flexibility of the bond increases contact length and number of grains in contact.

this problem by providing data that can be analysed to provide counts of the abrasive edges with increasing depth into the surface. An area of abrasive is defined and the number of abrasive areas at the required depth is counted and divided by the area to yield a value of C . Techniques for measurement of wheel topography are described by Cai (2002).

Wheel Flexibility

The number of grains actively engaged in grinding also increases as the wheel is flattened against the workpiece. Flattening is mainly due to wheel flexibility. A vitreous bond or resin bond provides flexibility at the wheel surface. The wheel is rather like a car tyre flattened against the workpiece by the normal force. The effect of wheel flexibility is to increase the number of grains in contact with the workpiece. This is illustrated in Fig. 5.4.

A more flexible wheel produces a smoother workpiece surface than a rigid wheel. The volume of material removed by each cutting edge is reduced owing to the greater number of cutting edges removing the material. This reduces the rate of grain fracture.

5.2 Grain Wear

Four basic types of grain wear are illustrated in Fig. 5.5.

The basic type of wear depends on the grinding conditions and on the nature of the abrasive.

Rubbing Wear

Rubbing wear occurs when stresses imposed on a grain are low.

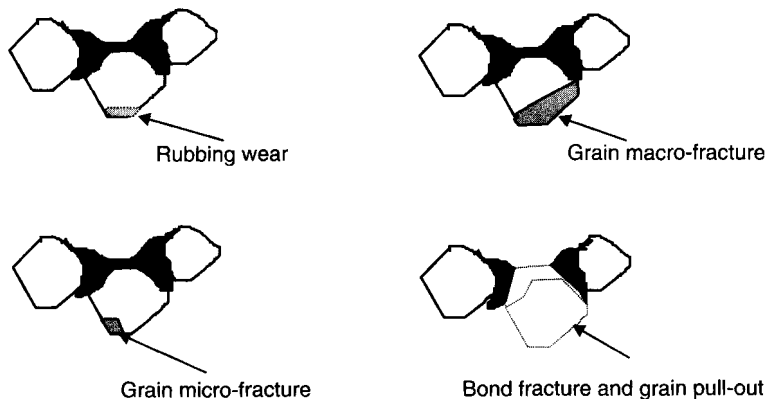


Figure 5.5 Four basic types of abrasive grain wear.

Bond Fracture

Bond fracture occurs when high stresses are applied to a grain and also when bond retention of a grain is weak.

Grain Micro-Fracture

Grain micro-fracture is a favourable type of wear that maintains a sharp grain with a slow rate of wear under conditions of high stress. Micro-fracture depends on the crystalline nature of the grain.

Grain Macro-Fracture

Macro-fracture is where the grain fractures into large fragments. Macro-fracture depends on the crystalline structure of the grain and the grinding stress levels.

Wheel Loading

There is another type of wheel wear phenomenon that has a disastrous effect on grinding performance. This is wheel loading or wheel clogging. Loading occurs when the workpiece material adheres to the tips of the abrasive grains and is brought into repeated contact with the material. Loading also occurs if long workpiece chips fill the pores of the abrasive and are retained there. The consequences of loading and clogging are

extremely poor surface texture of the workpiece, increased grinding forces, and increased grinding wheel wear. To avoid loading, it is important to use ample coolant with effective lubrication properties. Other measures that can help include increasing wheel speed or reducing depth of cut.

Preferred Wheel Wear

A high rate of wheel wear reduces workpiece material removal rate and reduces redress life. Abrasive grains tend to be dislodged increasing surface roughness. If a grinding wheel wears too slowly, the abrasive grains become blunt, grinding forces increase, size errors increase, temperature rise increases, and there is an increased risk of thermal damage to the workpiece. A moderate rate of wheel wear is usually preferred especially if grinding with conventional abrasives as this allows the wheel to remain sharp, thus maintaining stable grinding forces and minimising size variations. With super-abrasives, wheel sharpness may be maintained for long periods in spite of minimal wheel wear. This has the benefit of reducing size variations and increasing wheel life.

Wear Measurement

Wear of the grinding wheel can be measured for test purposes by using part of the wheel surface for grinding so that the wear forms a step. Subsequent to the grinding operation a razor blade can be plunged into the grinding wheel to replicate the step onto the edge of the blade. The step on the blade can then be accurately measured by optical or mechanical means.

G Ratio

A measure of the ability of a grinding wheel to remove material is given by G ratio. An efficient hard wearing grinding wheel will grind an easy-to-grind material for a long time with only a small amount of wheel wear. This corresponds to a high G ratio. The grinding ratio G is defined as the volume of material removed divided by the volume of wheel wear.

$$G = \frac{V_w}{V_s} \quad \text{G ratio} \quad (5.3)$$

Example 5.2 After grinding a depth of 40 μm (or 0.0016 in.) from a workpiece 1 m long (or 39.37 in.), the grinding wheel of 170 mm

diameter (or 6.69 in.) is found to have worn by a depth of 1 μm (or 0.000039 in.). What is the G ratio?

The volume V_w of work material removed after grinding a distance L_w is $V_w = b_w \cdot a_e \cdot L_w$. If in the same time, the wheel wears to a depth a_s , the volume of wheel wear is given by $V_s = b_w \cdot a_s \cdot \pi \cdot d_s$. In this case $G = a_e L_w / (a_s \cdot \pi \cdot d_s)$.

$$G = 0.040 \times 1000 / 0.001 \times 3.142 \times 170 = 74.9$$

High G ratios may be in excess of 5000 whereas for a difficult-to-grind material, the G ratio may be as low as 1. Dressing a grinding wheel causes a further loss of the abrasive layer. In the evaluation of the life of the wheel it is necessary to take this into account. A value of $G = 1$ is very low and implies that the abrasive is not hard enough for the task. A value $G = 5000$ is high and may sometimes correspond to a wheel that is too hard for the task. For example, when the abrasive tool wears too slowly and becomes glazed, machining forces are increased leading to poor size holding and possibly to vibrations and poor surface texture.

Wear Flats

Grinding power is increased as grinding wheel grains become blunt. With slow wear of the grinding wheel, flats are developed where the tips of the abrasive grains rub against the workpiece as illustrated in Fig. 5.6 (Hahn and Lindsay 1967).

As the flats rub against the workpiece surface under high pressure, considerable energy is dissipated in frictional heating. The energy consumed is directly proportional to the true area of contact under the wear flats. Typically, these flats may build up to about 8% of the wheel surface area. Malkin measured wear flat area A as a percentage of the wheel surface area and correlated wear with grinding forces (Malkin 1989). He found that grinding energy increases proportionally as illustrated in Fig. 5.7.

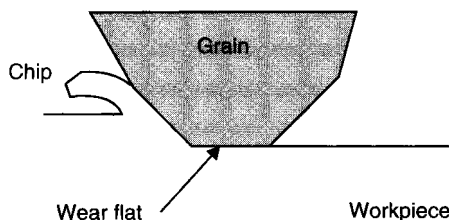


Figure 5.6 Illustrating a wear flat developed on the tip of an abrasive grain.

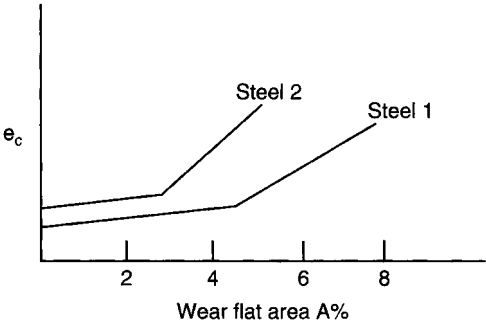


Figure 5.7 Increase of grinding energy with percentage wear flat area.

After burn occurs when grinding steel, it was found that energy increased even more rapidly with wear flat area. When burn occurs, it is necessary to redress the grinding wheel to reduce the wear flat area.

Re-sharpening

The build-up of wear flats on a grinding wheel tends to be self-limiting with self-sharpening wheels and often reverses during a period of constant in-feed due to wheel re-sharpening. This is illustrated in Fig. 5.8. As grain penetration builds up during in-feed, force increases until a maximum is reached. With further in-feed, the forces on the abrasive grains cause fracture and grain re-sharpening. The effect of this is clearly visible in Fig. 5.8. The spark-out phase of the cycle is not shown in the figure. Forces reduce during spark-out, and during a long dwell period, the abrasive grains start to build up wear flat area again.

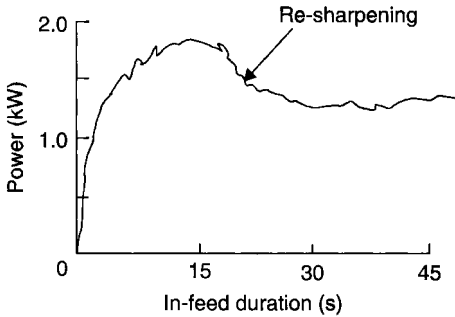


Figure 5.8 Power against time with constant in-feed in cylindrical grinding.

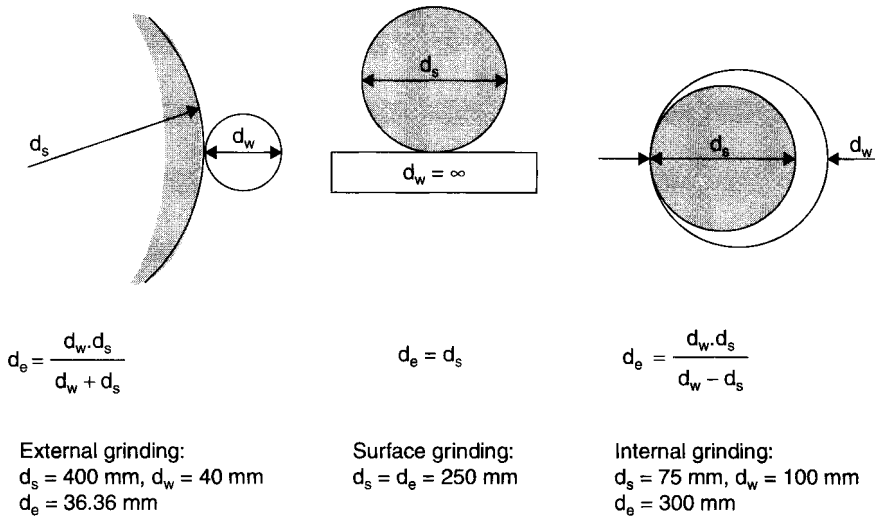


Figure 5.9 Wheel–workpiece conformity.

5.3 Wheel–Workpiece Conformity

Grinding behaviour varies enormously between internal and external grinding due to differences in shape conformity. Conformity is illustrated in Fig. 5.9. External grinding has low conformity contact whereas internal grinding has high conformity. In internal grinding, due to high conformity there is a greater tendency for rubbing contact between the grains and the workpiece over a long arc of contact. Internal grinding requires softer grinding wheels than external grinding in order to cope with extra rubbing wear.

Equivalent Diameter

A measure of conformity is defined in terms of equivalent grinding wheel diameter d_e as shown in Fig. 5.9.

$$d_e = \frac{d_w \cdot d_s}{d_w \pm d_s} \quad \text{Equivalent wheel diameter} \quad (5.4)$$

Example 5.3 The inner work diameter of a cylinder is 100 mm (or 3.94 in.). The internal grinding wheel diameter is 30 mm (or 1.181 in.). What is the equivalent grinding wheel diameter that would give the same conformity in flat surface grinding?

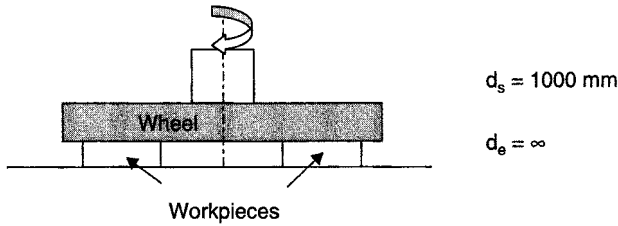


Figure 5.10 Conformity in vertical spindle face grinding.

$$: d_e = \frac{100 \times 30}{100 - 30} = 42.86 \text{ mm diameter (or 1.69 in.)}$$

An advantage of replacing wheel diameter by equivalent diameter is the reduction in the parameters needed to describe a range of grinding processes. In surface grinding, the equivalent wheel diameter is the same as the actual wheel diameter $d_e = d_s$. In external grinding, the equivalent wheel diameter is small due to low conformity whereas in internal grinding, the equivalent wheel diameter is large due to high conformity. Equivalent wheel diameter is derived by simply adding or subtracting the curvatures of the grinding wheel and workpiece as appropriate: $\frac{2}{d_e} = \frac{2}{d_s} \pm \frac{2}{d_w}$. This expression leads to Eqn (5.4).

The equivalent wheel diameter is to some extent a measure of the length of the arc of contact. In internal grinding, a large equivalent diameter spreads the grinding pressure over a larger area. This tends to slow the fracture wear of the grains so that wheels have a greater tendency to glaze. A softer wheel structure must therefore be used for internal grinding. The examples in Fig. 5.9 show that the equivalent wheel diameter can be smaller in external grinding than in internal grinding.

Face grinding produces complete conformity as illustrated in Fig. 5.10 for vertical spindle face grinding. The vertical spindle face grinding process is used because heavy pressure can be absorbed by the grinding wheel. Also, the large area of abrasive grains employed allows high rates of stock removal when grinding an array of workpieces at the same time. The complete conformity of contact is the least favourable for lubrication and cooling. Appropriate abrasive structures must be employed to avoid glazing and wheel loading.

Figure 5.11 illustrates the complete conformity in conventional cylindrical face grinding. The high conformity creates grinding problems. It also creates problems for surface finish because the grains tracks left on the workpiece surface are constantly crossing other grain tracks. The main

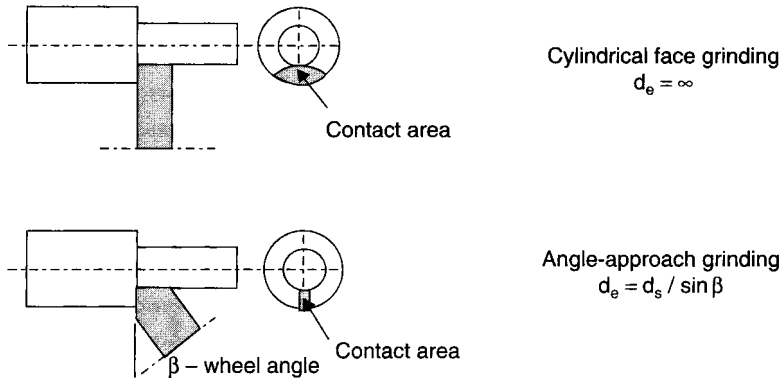


Figure 5.11 Conformity in face grinding and angle-approach face grinding.

reason for the problem is that the material removal is concentrated on the outer corner edge of the wheel. This causes rapid edge breakdown, irregular grain wear, and poor surface roughness. Introducing a corner radius improves the situation but the removal is still concentrated on a small radius. A much better approach is to employ angle grinding.

The problems are overcome in angle-approach grinding because the total contact area is greatly reduced while material removal is spread across the face of the wheel. Angle approach has advantages for control of corner geometry, surface texture, cooling, and redress life of the wheel. The difference in contact geometry is illustrated in Fig. 5.11.

5.4 Contact Length

In Section 5.2, it was shown that power increases with real contact area between the abrasive grains and the workpiece due to the extent of rubbing contact. In the previous section, it was argued that conformity between a workpiece and a grinding wheel is important for similar reasons. With greater conformity, the length of rubbing contact between the grains and the workpiece is increased. Increased contact length increases the slow wear of the abrasive grains leading to a greater tendency to glazing. A short contact length increases the tendency for grain fracture which maintains grain sharpness.

Geometric Contact Length

Geometric contact length l_g in Fig. 5.12 is the arc length AB so that $l_g = \theta \cdot d/2$. This expression is valid for internal, external, and flat surface

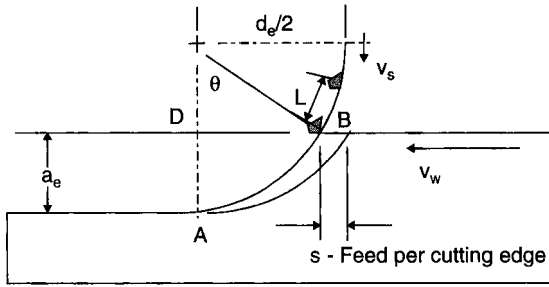


Figure 5.12 The geometric contact arc.

grinding. The geometric contact length is usually evaluated using the principle of intersecting chords $DB^2 = AD \cdot d_c$. For all practical depths of cut, $l_g \approx DB$ and $a_e \approx AD$ so that

$$l_g = \sqrt{a_e \cdot d_c} \quad \text{Geometric contact length} \quad (5.5)$$

Example 5.4 Calculate the geometric contact length for a $30 \mu\text{m}$ depth of cut (or 0.0018 in.) in surface grinding and an equivalent wheel diameter of 100 mm (or 3.94 in.).

$$l_g = \sqrt{.030 \times 100} = 1.732 \text{ mm (or } 0.068 \text{ in.)}$$

For external cylindrical grinding, a similar result is found as justified in Fig. 5.13. The justification for internal grinding follows the same principle.

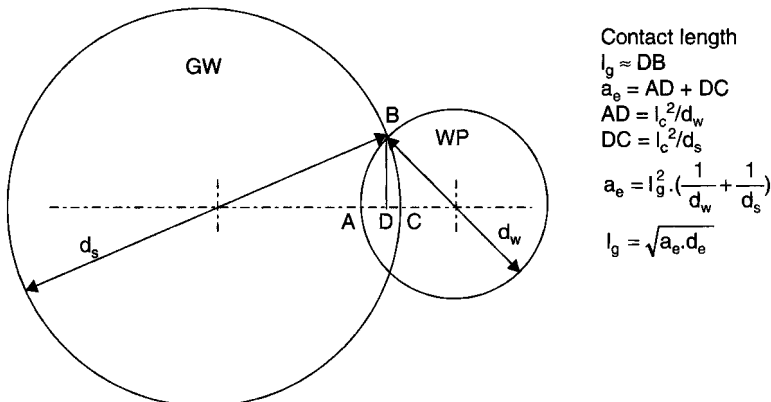


Figure 5.13 Geometric contact length in cylindrical grinding.

Kinematic Contact Length

It is also possible to calculate the contact length allowing for the feed per cutting edge as the grain passes through the contact zone (Fig. 5.12). This gives a so-called kinematic contact length.

$$l_k = \left(1 \pm \frac{v_w}{v_s}\right) \cdot l_g + \frac{v_w}{v_s} \cdot \frac{L}{2} \quad \text{Kinematic contact length} \quad (5.6)$$

The second term on the RHS is negligible at normal grinding speeds.

Example 5.5 Using the value of contact length from Example 5.4, calculate the kinematic contact length where the up-grinding work speed is 0.3 m/s and the wheel speed is 30 m/s. Assume that the grain spacing is 2 mm.

$$l_k = (1 + 0.3/30) \times 1.732 = 1.749 \text{ mm (or 0.069 in.)}$$

The difference due to the speed ratio is usually so small it is hardly worth worrying about.

Deflected Contact Length

Deflected contact length can be very important when using vitrified wheels and even more important when using resin-bond wheels. Deflected contact length takes into account the effect of the normal grinding force on the deflections of the grinding wheel. The contact length, taking account of deflections, can be several times larger than the geometric contact length (Rowe et al. 1993; Marinescu et al. 2004). The deflected contact length l_f due to a normal force is illustrated in Fig. 5.14.

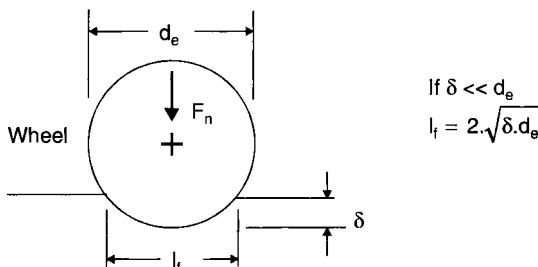


Figure 5.14 Contact length due to a normal force.

Total Contact Length

The total contact length taking geometric and deflection effects into account is derived in Chapter 15. The result is given by Rowe et al. (1993).

$$l_c^2 = l_g^2 + l_f^2 \quad \text{Total contact length} \quad (5.7)$$

The deflection contact length is given by

$$l_f = \sqrt{\frac{8 \cdot R_r^2 \cdot F_n' \cdot d_e}{\pi \cdot E^*}} \quad \text{Deflection contact length} \quad (5.8)$$

where R_r is the roughness factor equal to 1 for a smooth cylinder but typically ranges from 5 to 15 for a grinding wheel, F_n' is the normal grinding force per unit width, d_e is equivalent wheel diameter, and E^* is the combined elastic properties of the grinding wheel and workpiece.

$$\frac{1}{E^*} = \frac{1 - \nu_1^2}{E_1} + \frac{1 - \nu_2^2}{E_2} \quad \text{Combined elastic modulus} \quad (5.9)$$

Example 5.6 Evaluate the contact length for the following surface grinding conditions.

Wheel diameter d_s : 170 mm (or 6.69 in.)

Wheel modulus E_s : 50 kN/mm² (or 7.25×10^6 lbf/in.²)

Wheel Poisson ratio ν_s : 0.22

Work material: AISI 1055

Work modulus E_w : 213 kN/mm² (or 30.1×10^6 lbf/in.²)

Work Poisson ratio ν_w : 0.29

Depth of cut a_e : 0.030 mm (or 0.0012 in.)

Work speed v_w : 0.25 m/s (or 590 in./min)

Grinding contact width b_w : 15 mm (or 0.59 in.)

Measured normal force F_n : 320 N (or 71.9 lbf)

Roughness factor R_r : 10

$$l_g: \sqrt{0.030 \times 170} = 2.26 \text{ mm (or 0.00089 in.)}$$

$$E^*: 1 / \left[\frac{1 - 0.22^2}{50} + \frac{1 - 0.29^2}{213} \right] = 42.68 \text{ kN/mm}^2$$

(or 6.19×10^6 lbf/in.²)

$$F_n : 320/0.015 = 21330 \text{ N (or 4795 lbf)}$$

$$l_r : \sqrt{\frac{8 \times 10^2 \times 21330 \times 0.170}{3.1417 \times 42.68 \times 10^9}} = 0.00465 \text{ m} \equiv 4.65 \text{ mm (or 0.183 in.)}$$

$$l_c : \sqrt{2.26^2 + 4.65^2} = 5.17 \text{ mm (or 0.204 in.)}$$

Many results show that total contact length is typically 1.5–3 times the geometric contact length. This result is important for two reasons. First, it explains why more elastic wheels give lower surface roughness and have a greater tendency to glaze. Second, when calculating grinding temperatures, it is important to use a reasonably accurate value of contact length in the calculations. This is to avoid predicting unduly high temperatures.

Contact Length Ratio

Sometimes, a rough estimate of contact length is made using a contact length ratio R_L so that

$$R_L = l_c/l_g \quad \text{Contact length ratio} \quad (5.10)$$

A value of R_L equal to 1 is applicable for rigid wheels and rigid workpieces. A value of $R_L = 2$ would be a reasonable value for finish grinding of steels with vitrified wheels. It will be found that using an appropriate value of R_L avoids over-estimating temperatures when modelling heat transfer in grinding.

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6 High-Speed Grinding

6.1 Introduction

The trend towards increasingly high speeds in grinding makes it worthwhile to examine the merits of this trend. Several directions are emerging which are loosely termed high-speed grinding domains. Challenges have been addressed to make the technology possible. Often, several parallel developments together enable large improvements in productivity. This chapter presents the rationale for high-speed grinding. Associated developments in process optimisation, machines, wheels, abrasives, and coolants are explored in subsequent chapters.

6.2 Trends in High-Speed Grinding

The main reason for moving to higher grinding speeds is that forces and surface roughness are reduced allowing increases in removal rate.

Quality, Productivity, and Cost

The benefits of moving into a high-speed grinding regime for quality, productivity, and cost are illustrated in Fig. 6.1.

Productivity is increased at the same time as accuracy and product quality. Also, it is possible to reduce manufacturing complexity and improve process reliability. However, these benefits are only achieved with appropriate developments in grinding technology. Simply increasing output from a production operation without a parallel improvement in technology leads to poor quality and accuracy. By improving the technology, the output can be greatly increased while maintaining or even improving quality standards.

Better Removal Rate or Better Accuracy

High wheel speed can either be used to improve accuracy and quality while maintaining productivity or can be used to improve productivity while maintaining accuracy and quality. This point is illustrated schematically in Fig. 6.2.

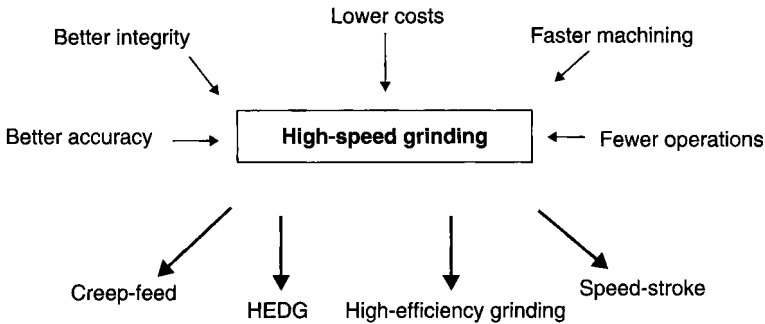


Figure 6.1 Developments in high-speed grinding.

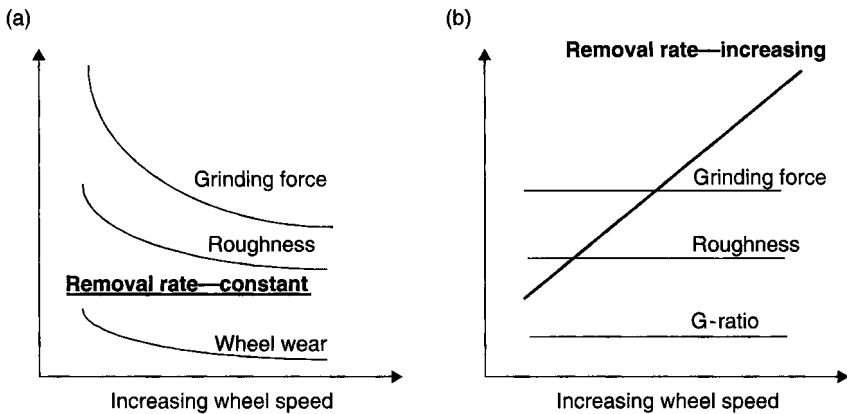


Figure 6.2 Effect of increasing wheel speed (a) at constant removal rate and (b) at increasing removal rate.

The effect of increasing wheel speed with constant depth of cut and constant work speed in Fig. 6.2(a) is that less material is removed by each grain. This reduces wheel wear. Roughness and grinding force are also reduced. As a consequence of lower grinding force, size errors are reduced as explained in Chapter 2 and accuracy is generally improved.

The effect of increasing wheel speed while increasing removal rate is shown in Fig. 6.2(b). Optimum removal per grain can be maintained by increasing work speed in the same proportion as wheel speed. This maintains the grinding force approximately at a constant. Roughness remains approximately constant and wheel wear remains approximately constant when measured in terms of G ratio.

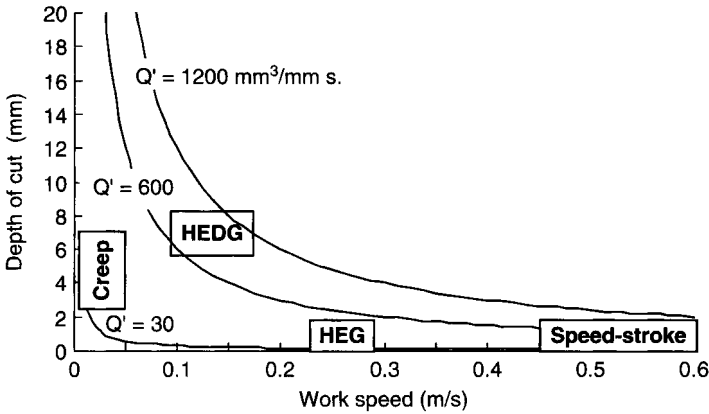


Figure 6.3 High-speed grinding domains.

Although high wheel speed can be used in two different ways—either to increase removal rate or to improve accuracy—often there will be a compromise. Quality and removal rate are both improved.

6.3 High-Speed Domains

High-speed grinding can fall into different domains of material removal as shown in Fig. 6.3. These domains include high-speed grinding with conventional wheels which was dubbed high-efficiency grinding, creep-feed grinding for very deep forms, high-efficiency deep grinding for extremely high removal rates and deep forms, and speed-stroke grinding. As the benefits of each domain become more widely appreciated, there is spin-off for the others so these are not uniquely different. There is also spin-off into high-accuracy grinding and super-finishing.

6.4 High-Efficiency Grinding

Early Development

High-speed grinding with conventional wheels dates back to more than 4 decades. The rationale for high wheel speeds was presented by Opitz et al. (1965). Wheel speeds were achieved up to 100 m/s with bakelite-bonded silicon carbide and up to 80 m/s with bakelite-bonded alumina. Vitrified wheels were also applied using hard bonds and fine grit sizes for strength. When grinding the material Ck45N, a removal rate of $3 \text{ mm}^3/\text{mm s}$

was achieved at 30 m/s using emulsion as the grinding fluid. Removal rate was limited by chatter. Increasing wheel speed to 60 m/s, removal rate was increased to 6 mm³/mm s. In this case, removal rate was limited by burn. Work speed was increased from a high conventional speed of 300 mm/s up to a very high speed of 1000 mm/s. For the same removal rate, this reduced contact length, reduced grain wear and reduced heat generation. In order to further dissipate heat, a high pressure coolant pump was installed. Combining all these measures, the removal rate was increased to 15 mm³/mm s. It was also shown that increasing the wheel speed improved the rate at which finished size was approached and that thin-walled components could be more easily ground at high wheel speed due to reduction in normal forces.

Machine Requirements

Finally, it was shown that costs were lower, even allowing for a more expensive grinding machine. A more expensive machine is necessary to provide essential features for high wheel speeds. The areas that need particular attention are illustrated in Fig. 6.4. These are stronger wheel guards that can contain a wheel burst and appropriate safety features, a stiff machine to prevent increased vibrations at higher operating speeds, higher power to cope with much higher removal rates, higher coolant pressures and flow rates to ensure a clean grinding wheel surface, and more expensive wheels specified for higher wheel speeds.

Emulsion or Neat Oil

Opitz and Gühring (1968) presented further developments in the high-speed process. It was shown that temperatures could be reduced by using

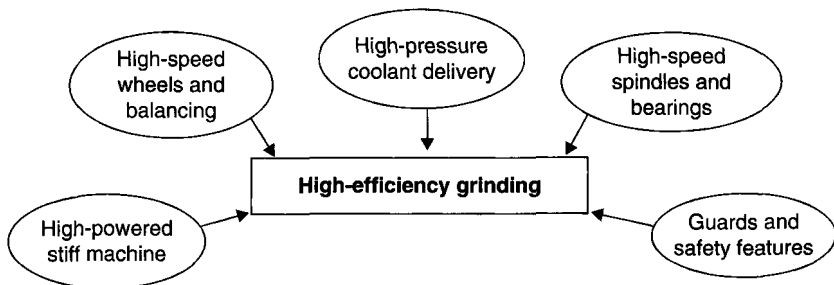


Figure 6.4 Essential areas of machine improvements required for high-efficiency grinding.

neat oil as the grinding fluid instead of emulsion. Oil has a lower thermal conductivity than an oil-in-water emulsion. However, this is offset by better lubrication and higher boiling point of neat oil. This reduces grain wear and grain dulling so that the grinding forces are lower. Neat oil brings a fire risk and it is necessary to consider oils with a suitably high flash point. Safety features to extinguish the spread of a flame point are also important.

Speed Ratio

It was confirmed that higher work speeds allow more heat energy to be dissipated before burn damage occurs. A speed ratio of $v_s/v_w = 60$ was employed. Combining these approaches removal rates were achieved up to $100 \text{ mm}^3/\text{mm s}$ with the same materials and wheel as employed by Opitz et al. (1965).

Rowe et al. (1985) found that work speeds that were too high increased chatter problems. Speed ratios of 120 and 200 were employed.

6.5 Creep-Feed Grinding

Creep-feed grinding developed about the same time as high-speed grinding. Conventional grinding uses high work speeds and very small depth of cut. The concept with creep-feed grinding is to slow the work speed right down to allow deep cuts to be machined. This makes creep-feed grinding more like cutting with a milling cutter. Typical values of work speed are lower than 1 mm/s .

One area of development was in the aero-engine industry to machine difficult materials that could not be machined with conventional cutters. A typical example is to grind the deep fir tree form into the root of a turbine blade. Other examples are found in the tool-grinding industry for broaches, drills, press-tool dies, etc.

The principles of creep-feed grinding were presented by Andrew et al. (1985). Low work speed and large depth of cut lead to a very long arc of contact as illustrated in Fig. 6.5.

The long arc of contact means that the abrasive grains are subject to a greater distance of rubbing wear compared to shallow grinding. The grain depth of cut may be small, but the increased length of contact causes a rapid rate of wheel dulling. This increases grinding forces and hence grinding energy and temperatures. The problem of grinding burn is the main challenge, particularly because at low work speeds, heat has more time to conduct into the workpiece.

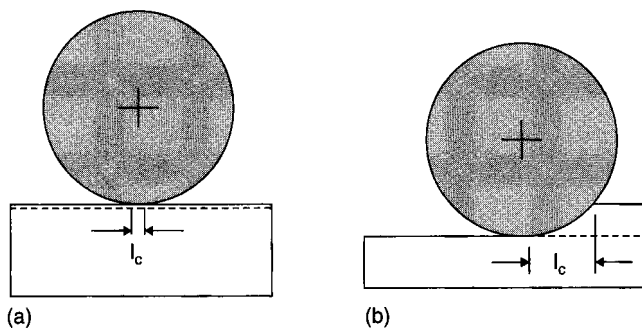


Figure 6.5 Contact length in (a) shallow and (b) deep grinding.

In spite of this, creep-feed grinding has been highly successful. Special measures for dressing have been introduced to maintain grain sharpness such as more frequent wheel redressing and continuous dressing as described in Section 4.6 and Fig. 4.12. A convenient way to achieve redressing after grinding each workpiece is to place a formed dressing tool on the work table in line with the workpiece. Each time the wheel makes a pass, it must first pass across the formed dressing plate. A small down-feed of the table is made before each pass.

The long arc of contact in creep grinding is a great advantage for fluid cooling. The grinding contact area between the wheel and the workpiece is greatly increased. This allows a greater heat convection into the grinding fluid within the contact area so that much less heat remains in the workpiece. It has been found that the heat convected by the grinding fluid is often a large proportion of the total grinding energy. Effective fluid delivery is therefore of prime importance in creep-feed grinding and is discussed in Chapter 8.

6.6 High-Efficiency Deep Grinding (HEDG)

HEDG Development

HEDG combines the improvements achieved in high-efficiency grinding with the benefits of deep grinding. HEDG can be seen as the evolution of creep-feed grinding with much higher wheel speeds (Fig. 6.6) and much higher work speeds (Fig. 6.3). These two measures allow extremely high removal rates and reduce heat transfer into the workpiece (Tawakoli 1993).

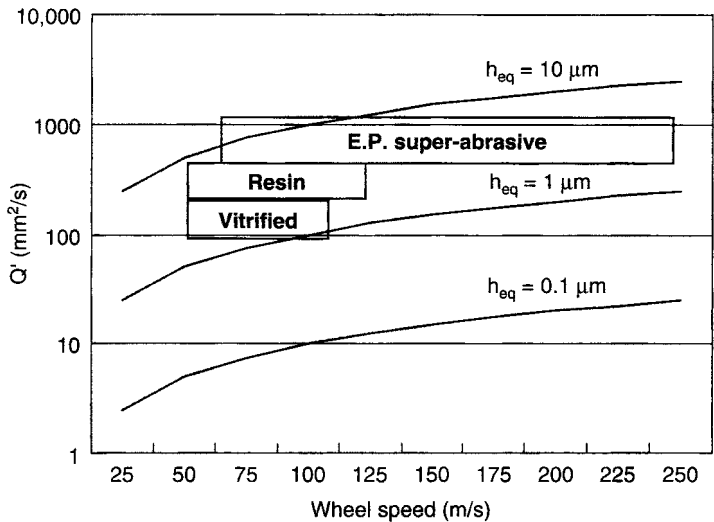


Figure 6.6 Removal rate with wheel speed and equivalent chip thickness.

Extremely high removal rates depend on both wheel speed and equivalent chip thickness as shown in Fig. 6.6. Clearly, the use of high wheel speeds requires appropriate abrasives and wheel designs.

Drill-Flute Grinding

An early example of HEDG was flute grinding for high-speed drills (Opitz and Gühring 1968). Grinding wheels for this application tend to be resin bonded to withstand the combination of high wheel speeds and large side loadings on the wheel.

Crankshaft Grinding

Another important application is crankshaft grinding using electroplated CBN wheels (Marinescu et al. 2006). The use of electroplated CBN in many HEDG applications allows much higher wheel speeds and enables removal rates in excess of $Q' = 1000 \text{ mm}^3/\text{mm s}$.

Chip Thickness

Extremely high h_{eq} values used in HEDG are clear from Fig. 6.6. Values this high require special abrasives that can withstand high grain forces and

an open sharp wheel surface that allows good swarf clearance from the grinding contact zone. An electroplated CBN abrasive provides these qualities. New forms of conventional abrasives have also been developed as described in Chapter 3.

Specific Energy

A feature of HEDG is the low specific energy values. A typical example is shown in Fig. 2.8 where e_c approaches 10 J/mm^3 as Q' approaches $1000 \text{ mm}^3/\text{mms}$. This is not an isolated example. There are many such reports. With such low values of specific energy, grinding temperatures are much lower (Rowe and Jin 2001). However, a further consequence of large chip thickness values is that grinding wheel wear tends to be high. Electro-plated CBN abrasive has good wear resistance to withstand large chip forces and plays a valuable role in much of the reported work.

Stephenson et al. (2002) obtained favourable results in surface HEDG and in cylindrical HEDG using CBN with neat oil as the grinding fluid. Cylindrical grinding allows high work speeds to be more easily achieved.

Viper Grinding

Capes (2000) reports the development of creep-feed grinding into a HEDG process. In this development high-performance vitrified wheels are employed on a machining centre. The process is termed viper grinding, which stands for vitrified performance extreme removal grinding; it was developed by Rolls-Royce in collaboration with Tyrolit and Makino. The need was to grind a variety of features on nickel alloy turbine blades, for instance. The new process does this and achieves removal rates up to $80 \text{ mm}^3/\text{mm s}$ which is extremely fast for a difficult-to-grind material. The aluminium oxide grinding wheels employed are 120–200 mm diameter and are dressed only once prior to the finishing cut. The tools are handled the same way as other tools by the machining centre tool management system. Vitrified wheels were seen as an advantage for grinding nickel materials because of the ability to keep the pores clean. Coolant is delivered at 70 bar and is filtered to $10 \mu\text{m}$.

Temperature Analysis

Chapter 18 describes the analysis of temperatures in grinding. Case studies illustrate the upper limits of removal rate and diminishing returns for very high work speeds.

6.7 High Work Speed Grinding

Cylindrical Grinding

High work speed cylindrical grinding is easily achieved as the workpiece is continually rotating. It therefore has great relevance for cylindrical grinding (Stephenson et al. 2002).

Speed-Stroke Grinding

Speed-stroke grinding seeks to extend the concept of high wheel speeds and high removal rate with extremely high work speeds for lower temperatures (Yui et al. 2004).

Conventional reciprocating grinding employs work speeds up to 0.5 m/s. Speed-stroke grinding attempts to increase work speeds higher than 1 m/s in surface grinding.

In speed-stroke reciprocating grinding, the table has to accelerate up to 1 m/s before the grinding wheel engages the workpiece and then has to decelerate again to reverse the travel. This wastes time and requires sufficient table travel at each end to allow for acceleration and deceleration. Speed-stroke grinding obviously makes more sense for long workpieces where the reversal time is of less importance. The problem of acceleration and deceleration is partly overcome with the help of the latest generation of linear motors for the table drive.

Yui et al. (2004) compared speed-stroke grinding with creep-feed grinding using vitrified CBN wheels. It was confirmed that grinding forces and temperatures were lower but that grinding ratio was also lower and surface roughness was increased.

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7 Avoiding Thermal Damage

7.1 Introduction

Types of Thermal Damage

Low temperature grinding produces compressive residual stresses and improves fatigue life. High temperature grinding of hardened components often produces tensile residual stresses that shorten fatigue life. Other forms of damage can also be caused. Damage includes any of the following:

- grain growth, precipitation, softening
- phase transformations leading to re-hardening
- thermal expansion or contraction, cracking, and tensile residual stresses
- chemical reactions leading to burn marks

Damage Avoidance

Damage can often be avoided through better choice of abrasives or selection of grinding conditions to avoid excessive temperatures. For high-rate production, process monitoring may provide the answer.

Thermal damage can be usually avoided or overcome by correcting the grinding conditions in one of the following ways:

- redressing the grinding wheel to produce a sharper cutting surface
- replacing the wheel with a softer grade or more open structure that has a better self-sharpening action
- replacing the wheel with a sharp super-abrasive
- improving fluid delivery or changing the grinding fluid
- reducing removal rate

Usually, the last recourse is to reduce removal rate since output is reduced. More information on the conditions that lead to high temperatures is given in Chapter 18.

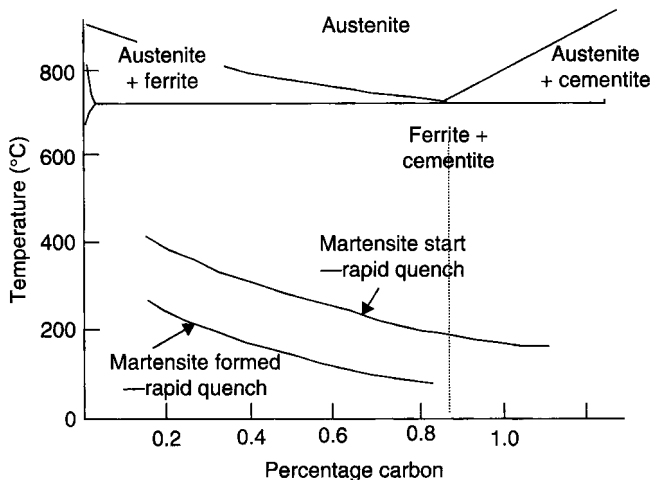


Figure 7.1 Typical iron-carbon diagram.

7.2 The Iron-Carbon Diagram

Explaining Thermal Damage

Thermal effects when grinding ferrous materials depend on the carbon and other alloying content. Some typical effects can be easily demonstrated from a diagram such as Fig. 7.1. Many steel workpieces are finished by grinding because hardened parts can be produced with high accuracy. Hardened steels have relatively high carbon content and can be easily damaged by abusive grinding at high temperatures. Some examples are given below.

7.3 Burn and Temper Damage

Severe Oxidising Burn

Severe oxidising burn is shown in Fig. 7.2. Burn is easily recognised visually in such cases by the appearance of dark blue stripes. The damage was caused by using a low work speed. Increasing the work speed overcame the problem. In many cases, burn is not so readily recognised because burn marks have been removed in the final spark-out period of grinding. However, if the burn is deep enough, removing the external evidence will not be sufficient to remove other aspects of damage such as modified

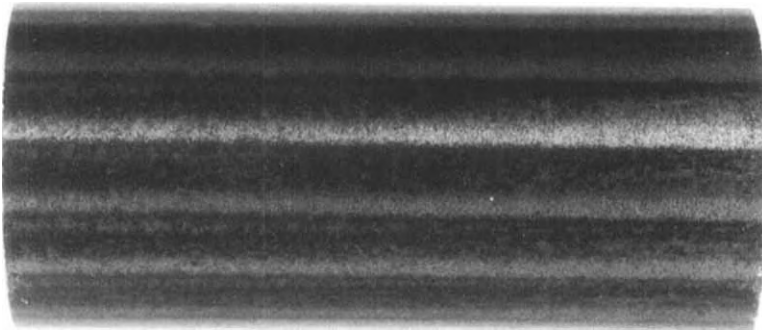


Figure 7.2 A severely burned grey cast-iron workpiece produced by plunge grinding.

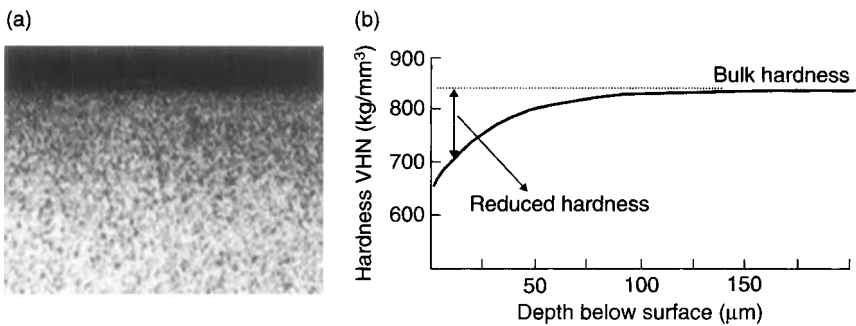


Figure 7.3 (a) Micro-section showing temper damage to a hardened and tempered bearing steel and (b) sub-surface softening due to temper damage.

hardness or even cracks extending deep below the surface. Often burn is accompanied by chatter associated with consequent cyclic softening and force variations.

Temper Damage

Temper damage is a form of burn although workpieces do not necessarily exhibit burn marks or temper colours. Figure 7.3(a) shows an example of temper damage revealed by examining a micro-section. The section was cut from a workpiece taking care to avoid causing further thermal damage. The specimen was set in bakelite, which is the black colour at the top of the section. The section was then carefully polished and etched in 2% nital solution. The micro-structure can be viewed under a microscope. In this example, a magnification of $400\times$ was employed. The workpiece material is a tempered and hardened AISI 52100 bearing steel. The grinding

temperature was sufficiently high to cause diffusion of the carbon in the steel. Typically, temperatures of the order of 450°C are sufficient to cause temper damage. The carbon comes out of solution and shows up as the increased dark areas near the surface. The lighter area below is the unaffected tempered martensite.

Micro-hardness measured across the section, Fig. 7.3(b), shows the degree of softening sufficient to reduce wear resistance of a bearing surface. The damage will often extend to a depth of $100\text{ }\mu\text{m}$ which is probably too much to eliminate during spark-out. A fine feed may remove the layer more safely.

Softening is only a problem with materials which can be hardened. Therefore, softening is not a problem with low carbon steels. It is, for a particular temperature, greater at low work speeds because there is more time for diffusion to take place.

7.4 Re-hardening Damage

Re-hardening damage occurs at a higher temperature than the previous temper damage case. Typically, re-hardening damage occurs when the grinding contact temperatures exceed 850°C .

Figure 7.4 shows re-hardening damage to a hardened AISI 52100 bearing steel at a $100\times$ magnification. This type of damage is potentially more serious. A hard and brittle martensitic white layer can be seen at the surface. In service this layer is wear resistant except that fatigue leads to hard particles breaking away from the surface. The loss from the surface is a direct form of damage and the wear particles can cause further damage

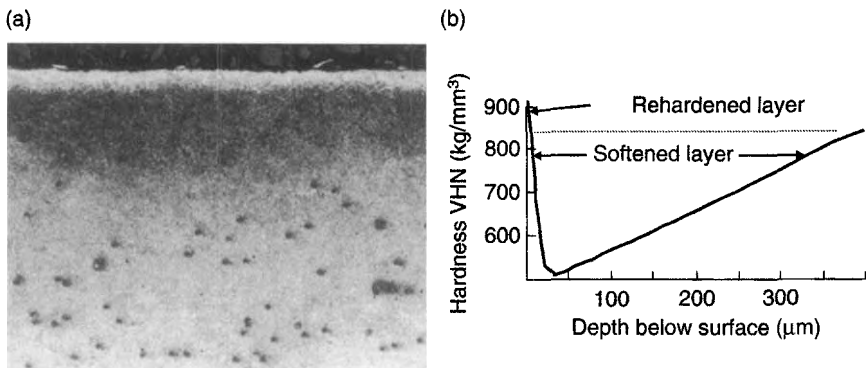


Figure 7.4 (a) Re-hardening damage and (b) hardness variations below the surface with re-hardening damage.

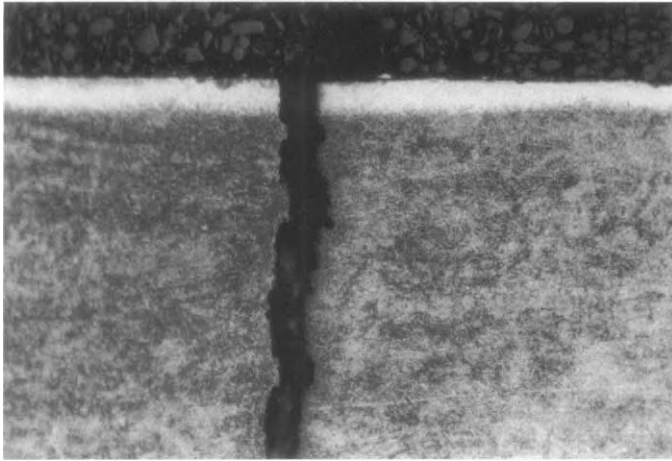


Figure 7.5 Cracking due to severe grinding damage in a bearing steel AISI 52100.

by cutting into the surface. Below the hardened layer is a softened layer where the temperature rise was lower. Deeper still, the temperature rise was insufficient to affect the material structure.

The temperatures for transformation to martensite are shown approximately for a typical iron–carbon diagram in Fig. 7.1. Transformation to martensite takes place in microseconds since no diffusion is required. The material is quenched after grinding due to the low temperature of the bulk material.

Surface Cracks

Heating is accompanied by thermal expansion and contraction. As the wheel passes a point on the workpiece, the surface expands. After the wheel has passed, the surface rapidly cools and is quenched by the bulk material. At this stage the material contracts. If the thermal expansion is sufficient, the subsequent cooling contraction may lead to cracking as illustrated in Fig. 7.5.

7.5 Residual Stresses

The mechanical effect of the abrasive grains is to cause plastic flow under highly compressive conditions. After grinding, compressive residual

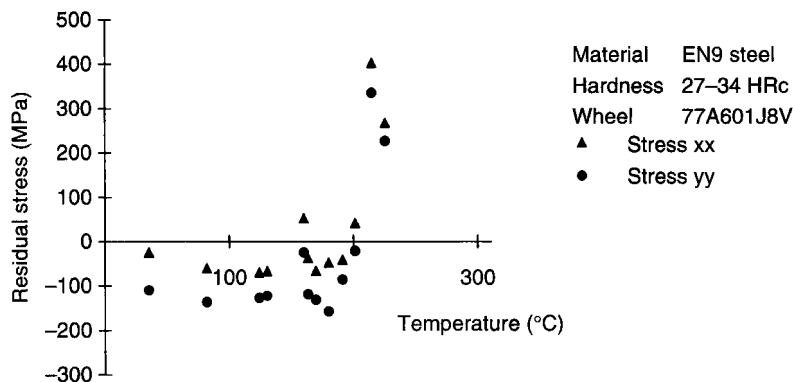


Figure 7.6 Transition to tensile residual stresses for grinding a hardened EN9 steel.

stresses remain in the surface. Moderate compressive stresses are generally considered to be beneficial. Tensile stresses tend to be more damaging.

Tensile stresses may be set up by thermal expansion and contraction as the wheel passes a point on the workpiece. First the temperature rises rapidly and the surface layer expands causing a compressive stress. Then the material contracts causing tensile stress. Another consequence of elevated temperature is that yield stress falls in the region of heating. The yield stress depends on the temperature and also on the previous heat treatment of the material. Tensile stresses can be avoided if low-temperature grinding is performed below the transition temperature for the material throughout the grinding cycle. A typical transition from compressive stresses to tensile stresses is illustrated in Fig. 7.6.

It is often found that grinding temperatures can be kept down by grinding with an appropriate CBN wheel thus allowing compressive stresses to be maintained at higher removal rates. The CBN wheel should be specified to produce low values of specific energy. Although CBN abrasive has a higher thermal conductivity than alumina, which is a great advantage for low grinding temperatures, this can be outweighed by high heat generation if the wrong wheel specification is employed.

The transition temperature for the onset of tensile residual stresses tends to occur close to the softening temperature for the particular material. Typical transition temperatures measured by McCormack et al. (1998, 2001) are listed in Table 7.1. Transition temperatures can vary substantially according to heat treatment. For the AISI 1055, the transition can vary from 100–330°C.

Table 7.1 Onset of Tensile Residual Stresses in Grinding

Material	Typical Transition Temperature (°C)
AISI 1055 (BS070 M55 plain carbon steel)	330
AISI 52100 bearing steel	400
M2 tool steel	560

7.6 Grind Hardening

Transformation to austenite is accompanied by a 1–2% volume reduction which creates a tensile stress at the elevated temperature because volume reduction is opposed by the bulk material deeper. This is the opposite of the dominant general thermal expansion effect. If the material has not been previously hardened, this can avoid the problem of re-hardening damage. On quenching, compressive stresses can be set up at the surface as explained below. In this context, Brinksmeier and Brockhoff (1996) propose to use grinding as a new hardening process. It was demonstrated that high temperature could be used to achieve surface hardening without causing surface cracks (Brockhoff 1999). The explanation offered is that when martensite is formed, where previously there was soft ferrite and pearlite, the resulting martensite occupies a 4% larger volume which leads to a compressive stress at the surface.

Very low work speed, typically 0.008 m/s, is required both to reduce thermal gradients and to allow time for austenitising which is a diffusion process. Cooling then produces an acceptable form of martensite. It remains to be seen whether the process can be industrialised.

7.7 Process Monitoring

Barkhausen Noise Sensors

Barkhausen noise sensors are often used as a non-destructive method to monitor steel parts for thermal damage. A sensor is accurately located close to the surface of the part so as to make the workpiece a conductor of an electromagnetic field. The sensor monitors changes in the electromagnetic properties of the workpiece. Changes in the crystal structure of the workpiece are reflected in the Barkhausen noise signal. The signal tends to increase with softening of the workpiece surface. Re-hardening of the workpiece causes the signal to fall again. The system is calibrated to

give an indication of the safe maximum signal. This system has been used very successfully by many manufacturers. Difficulties can arise with variable thickness of a surface-hardened workpiece as the signal is sensitive to the depth of the hardened layer.

Monitoring Power

Thermal damage occurs because temperatures in grinding are allowed to exceed the safe level. All such problems can be avoided with well-designed grinding conditions which ensure that acceptable energy levels are not exceeded. The grinding conditions to be controlled include wheel selection, speeds, feeds, and grinding fluid application. However, there is always a drive for greater throughput of parts, and the process may well run close to the safe operating limit. Process monitoring can provide warning as the safe limits are crossed and action needs to be taken. The action may be redressing the wheel or simply reducing removal rate. A simple measure to implement is to monitor grinding power. A safe power level can be established for a particular operation.

Process Control

Some users go further and use temperature modelling integrated into a process control system as a way of achieving and maintaining maximum throughput of parts with assured quality as described in Chapter 11.

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8 Application of Fluids

8.1 Introduction

Types of Grinding Fluid

Most grinding is conducted either with oil-in-water emulsions or with neat mineral oil or neat synthetic oil using a low-pressure delivery system as illustrated in Fig. 8.1. For low-speed grinding, the fluid is typically supplied through a nozzle from a pump at a pressure in the range of 1–4 bar (14.5–58 lbf/in.²). For high-speed grinding, much higher pressures and flow rates may be employed.

Functions of a Grinding Fluid

A grinding fluid serves a number of purposes. These include

- mechanical lubrication
- chemo-physical lubrication
- cooling in the contact area
- bulk cooling outside the contact area
- flushing of the debris away from the contact area
- entrapment of abrasive dust and harmful vapours

Contact Area Cooling

Grinding fluids reduce grinding temperatures in two different ways. The first is by directly cooling the process within the grinding contact area. In creep-feed grinding, direct cooling is a powerful agent for reducing grinding temperatures. The second is by maintaining wheel cutting efficiency.

Reduction of Wheel Wear

An important way of reducing temperature is by reducing friction and wear of the grinding wheel, thus minimising specific energy. In shallow-cut grinding, lubrication is often more significant than direct cooling. Lubrication is very important for reducing wheel dulling. By reducing friction

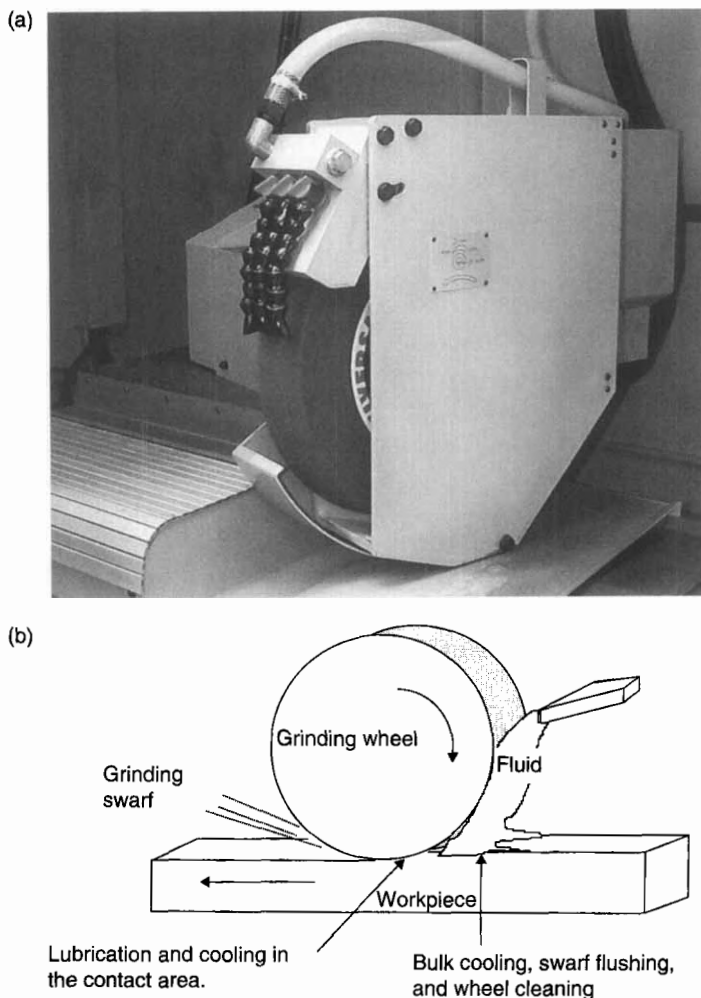


Figure 8.1 (a) Low-pressure flood delivery nozzles for cylindrical angle-head grinding (with permission from Jones and Shipman-Holroyd) and (b) function of the fluid.

and wheel dulling, power is reduced so that less heat is generated and temperatures are reduced. This can greatly improve grinding quality and allow higher removal rates.

Bulk Cooling

A plentiful channel of coolant on and around the workpiece assists bulk cooling. Bulk cooling refers to cooling that takes place outside the

contact area. If the workpiece bulk temperature and coolant temperature are allowed to creep up, size control will be poor and there is increased risk of thermal damage as any rise in the local temperature in the contact area adds to a rise in the bulk temperature.

Swarf Flushing

A plentiful supply of fluid helps by flushing swarf away from the grinding zone. This is essential to prevent re-circulation of swarf and fractured grain particles which can damage the workpiece surface. Re-circulated debris can also damage the wheel surface by adhering to the wheel causing deep scratching in the workpiece surface.

Minimum Quantity Lubrication (MQL)

Lubrication is ineffective unless the fluid actually enters the grinding contact. Even minute quantities entering the contact zone can be beneficial to process efficiency. This has led to a trend towards MQL systems designed to reduce environmental hazards and disposal costs.

Safe Use and Disposal of Fluids

The user has “cradle-to-grave” responsibility for safe use and disposal of grinding fluids. A range of environmental aspects need to be considered (Howes et al. 1991). Legislation at national and international levels increasingly aims to avoid potential health hazards and to protect the environment. The effect of complying with legislation leads to rising costs associated with the use and disposal of process fluids. Consequently, there is pressure to reduce or avoid the use of grinding fluids (Inasaki et al. 1993). It is therefore important to consider how process fluids can be employed safely and as efficiently as possible.

Environmental concerns range from potential effects of ingesting workpiece material or process fluid particles and extend through to irritant effects of metals, oils, and bacteria. These concerns make total machine enclosure with fume extraction systems attractive. However, it is unacceptable to pump fumes into the outside atmosphere. There are competing benefits of oil versus water-based fluids and the desirability of minimum fluid application.

Alternative Lubrication Possibilities

As the most effective grinding fluids are oils and water-based emulsions, it is easy to overlook other possibilities. These include

- mist lubrication
- self-lubricating surface layers deposited on a workpiece before grinding
- solid lubricants introduced into the pores of the abrasive.

Total Life Cycle Costs

It is recommended that the total life cycle costs of a process, including costs of fluid disposal and replacement and costs of workpiece cleaning, be considered.

Oil versus Water-Based Fluids

A well-designed mineral oil system may be better than a water-based emulsion because of the long life of the fluid, good lubrication, low corrosion, and absence of bacterial and fungal growth. However, a water-based fluid has higher thermal conductivity and it is usually preferred if the machine is not fully enclosed. For high wheel speeds, the machine should be fully enclosed to avoid mist in the atmosphere and floor contamination. If oil is used, consideration must be given to fire hazards. The proportion of air in the enclosure must be kept below the level at which flash fires break out.

Water has a much higher heat capacity than oil or air and it has a much higher value of thermal conductivity. Water also absorbs a large volume of heat in the process of evaporation.

Fluid Properties

Typical properties of the main groups of fluids are given in Table 8.1.

Wherever possible, the manufacturing engineer reduces the number of machining fluids employed in the factory to ease the problems of purchase, storage, and disposal. This can significantly reduce operating costs. This means that compromises are made between optimum grinding performance and flexibility of application of a fluid to a number of processes.

Table 8.1 Thermal Properties of Water, Mineral Oil, and Air

Property	Units	Mineral oil	Water	Air
Density	kg/m ³	900	1000	1.2
Specific heat capacity	kJ/kgK	1.9	4.2	1.0
Thermal conductivity	W/mK	130	600	26
Heat of evaporation	kJ/kg	210	2260	0
Boiling temperature	°C	300+	100+	–

Dry Grinding

There are exceptions to the use of liquid coolants mainly where it is necessary to keep the workpiece dry. In this case the use of an air jet may offer improved cooling over grinding without any form of cooling. The presence of water vapour in an air jet may be acceptable and can make a useful contribution to cooling. Water vapour improves grinding efficiency and also provides lubrication particularly when used in an air environment rather than with a nitrogen environment (Howes 1990).

8.2 Water-Based Fluids

Pure water can be used as a grinding fluid for occasional operation where only a small quantity of water is required.

Re-circulating Systems

For most operations, there is a need for re-circulation of the grinding fluid in a closed system. The following comments therefore apply to water-based fluids and emulsions mixed using proprietary concentrates of oils and additives.

Fluid Treatment

There are a number of reasons for a closed system, for example, the machine and the workpieces need to be protected from corrosion. This requires the addition of rust inhibitors to the fluid. The swarf generated in the process must be separated and recycled or sent for disposal. This requires a filtration system. Lubrication properties can be improved through the addition of oily compounds or other synthetic compounds designed for a particular grinding wheel and workpiece material combination.

Bacteria and fungi build up in a closed system and so it is necessary to add bactericides and fungicides. Foaming must be prevented which requires the use of a foam suppressant. Even with a well-designed coolant system, it may be necessary to completely change the coolant mixture on a weekly basis.

Water quality needs to be controlled for use with emulsions. Hard water may undermine the resulting properties of the fluid. A pH value of 6–7 is recommended. De-ionisation may be required for difficult-to-grind materials.

Fluid Compositions

Table 8.2 summarises typical compositions of water-based fluids as loosely classified by Howes (1990). In this classification, emulsifiable oils, often known as soluble oils, contain 50–80% of mineral oil in the concentrate. The term “soluble” is slightly misleading as a substantial proportion of emulsifier is required to achieve miscibility. True solutions do not require emulsifiers. Water-based fluids containing either synthetic or mineral oil are widely known as emulsions. Emulsions offer better cooling than neat oils but need frequent changing where low surface roughness must be maintained.

The average temperature at which boiling commences in an emulsion is approximately 130°C (Howes 1990). A greater proportion of oil in the grinding fluid increases the burn-out temperature. In measurements by the author, maximum temperatures up to 180°C were recorded before it became obvious that complete burn-out had occurred.

8.3 Neat Oils

Neat oils are particularly useful for high-speed grinding due to the high boiling temperature and good lubrication properties. With careful plant design, it may be possible to use neat oils for much longer periods than is possible with water-based fluids. Neat oils have been widely used in Europe. They have been particularly successful for HEDG when used with electroplated CBN wheels and also when used with resin-bonded conventional wheels.

The main advantage of neat oils when compared to water-based fluids is reduced wheel wear. This is due to improved lubrication. Wear flats building up on a grinding wheel surface can be the result of work material loading the wheel surface. The use of neat oil greatly reduces the tendency

Table 8.2 Typical Compositions of Water-Based Fluids

Fluid	% Composition of Concentrate				Dilution with Water
	Mineral oil	EP/Lubrication Additives	Emulsifiers	Coupling Agents	Corrosion Inhibitors
Emulsifiable oils	50-80	0-10	10-40	0.5-3	0-10
Semi-synthetics	5-30	0-10	Up to 50	0.5-3	0-10
Synthetics	Up to 5		0-40	0.5-3	Up to 40 to 1:200 1:10
					1:40 to 1:80 1:50 to 1:80

for wheel loading and in this respect it is even better than water-based fluid for reducing the build-up of wear flats.

Fire and possible explosions can be a hazard when using neat oils. The system should be designed to prevent fires. An automatic system for fire dowsing can be incorporated into the machine to cope with the possibility of incipient fire. One method is to release compressed nitrogen into the grinding wheel enclosure when fire is detected. The flash point of a grinding fluid is the minimum temperature at which the fluid ignites on the surface of the liquid. For grinding fluids, the flash point typically lies in the range 145–210°C (Marinescu et al. 2004).

Neat oils are thermally stable at temperatures up to approximately 200°C but can withstand burn-out at temperatures up to 300°C. Neat oils may be mineral oils or synthetic oils.

Mineral Oils

Mineral oils are refined petroleum-based hydrocarbons. Mineral oils are combinations of paraffins, naphthenes, and aromatic oils. A wide variety of mineral oil compositions are employed in cutting and grinding fluids. The lubrication properties are modified for particular applications using additives. A wide range of additives are employed for different purposes (Marinescu et al. 2004). Extreme-pressure additives are added to improve the lubrication properties.

Synthetic Oils

Synthetic oils may be derived from hydrocarbons or from other chemicals. Synthetic oils tend to be considerably more expensive than mineral oils. These oils tend to be purer and are produced for particular properties such as high temperature stability. Synthetic oils may be hydrocarbons. Types of synthetic oil may include organic esters, silicones, and halogenate organic compounds.

8.4 MQL and Gas-Jet Cooling

MQL with Oil

MQL is designed to eliminate some of the disadvantages associated with conventional fluid delivery. A review of cooling and lubrication methods in grinding reveals the need for more information (Brinksmeier et al. 1999).

Some preliminary results show that applying 4 ml/min ester oil as compared with 11 l/min of mineral oil when grinding 16 Mn Cr₅ material with micro-crystalline aluminium oxide reduced normal and tangential forces. The disadvantage was that grinding wheel wear and roughness were increased (Brunner 1998). Brinksmeier concludes that MQL is only suitable for fine grinding because of reduced cooling and lubrication.

Mist Cooling

A new method proposed involves injecting a small quantity of water mist into an air jet (Babic et al. 2005). This method has the virtue of simplicity and low risk. Early results were encouraging although the limitations are probably similar to those of other MQL methods.

Cryogenic Cooling

Cryogenic cooling with nitrogen has been tried with some success (Paul et al. 1993). Nitrogen plus MQL ester oil has also been tried in order to reduce wheel wear with pure nitrogen (Hoffmeister and Langemeyer 1998).

Ice–Air Jet Blasting

In a remarkable recent publication, it has been shown that very fine ice particles can be used to safely clean surfaces for medical or other applications where solvents are not permissible (Kluz and Geskin 2007). This raises the question of possible development for a range of applications including wheel cleaning. An application demonstrated was the removal of glue from a rubber surface without damage to the rubber surface.

8.5 Pumping System

The fluid delivery system is a major consideration in continuous production operation. The coolant system may take up substantial floor space, consume considerable amount of energy, and require ongoing maintenance of the fluid. The coolant system is also linked to the swarf handling system.

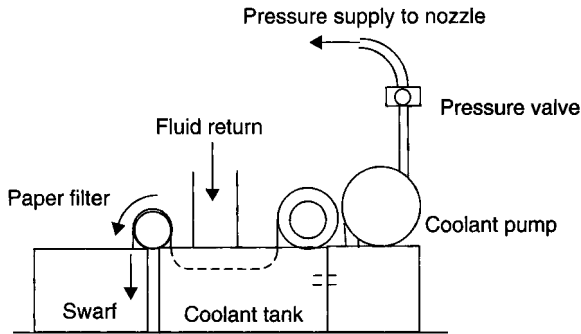


Figure 8.2 Basic elements of a fluid supply system.

Basic Elements

Basic elements include a coolant tank, a pump and pressure control valve, coolant filtration, swarf separation and collection. In practice, the requirements for filtration and separation may involve several elements and stages in precision grinding. Basic elements of a system are illustrated schematically in Fig. 8.2.

Separation

There are several types of separating equipment available. One method is the hydrocyclone where the fluid is whirled through a vortex causing heavy particles to be forced outwards and downwards into a collection box while the fluid is drawn upwards for re-circulation. Another commonly used method is the drum magnetic separator where metallic swarf is drawn towards a drum. A belt filter clarifier illustrated is also commonly employed. A three-phase separator can separate tramp oil from the coolant and separately trap the swarf. There are other methods and variations that may be used singly or in combination.

Heat Exchanger

A heat exchanger is an essential addition for precision grinding which allows the fluid to be properly cooled before re-circulation to the grinding machine. In all cases, the tank capacity should be checked to ensure adequate cooling capability. There is a tendency to under-estimate the cooling requirements when specifying the heat exchanger. This saves on initial

cost of the equipment but has serious repercussions for ongoing process efficiency. Some users employ coolant refrigeration.

The coolant system stabilises the machine temperature. Machine temperature cycles up and down each time the coolant is switched off and on. For high-precision grinding it is important to take this into account in machine warm-up and operation procedures.

Wheel Absorption of Fluid

At the end of a grinding session, it is important to take account of fluid accumulation within a porous wheel. It is necessary to run the grinding wheel for a lengthy period to spin out the fluid. Neglecting this procedure leads to an accumulation of fluid at the bottom of the wheel due to gravity. This results in serious wheel imbalance.

Supply Flow Rate and Pressure

Supply pressure and flow rate of the supply system must be sufficient to overcome the air barrier around the grinding wheel. In high-speed grinding, this requires attention to the fluid delivery system and also to nozzle design.

8.6 Fluid Delivery

One of the most difficult questions to answer in the design of fluid delivery systems is how much flow is required. For cooling, it might be thought that more is better. A large volume of flow helps to keep the bulk temperature of the whole workpiece down and can help to maintain a constant machine temperature. However, pumping large volumes of fluid at elevated pressures generates heat. An adequate supply of cooled fluid is ideal.

Hydrodynamic Effects and Size Control

An example where too much flow can be a disadvantage is in internal grinding. The convergent nip and high conformity in internal grinding lead to a hydrodynamic wedge. The fluid pressure builds up as in a hydrodynamic bearing and pushes the grinding wheel away from the workpiece.

The flexibility of an internal grinding spindle increases the tendency for separation and can lead to size-holding problems. The long arc of contact in internal grinding means that the grinding fluid is important for lubrication and cooling. A solution is to use a small jet for fluid entering the grinding contact and a higher pressure auxiliary jet for wheel cleaning.

Roughing and Finishing Requirements

In high-removal rate external grinding a large volume of fluid is required. However, for finish grinding to close tolerances, hydrodynamic effects should be minimised. Some users switch the flow rate to a lower setting at the final stage of finishing.

Air Barrier

Air entrained around a rotating wheel forms a barrier that impedes fluid delivery. The grinding fluid must penetrate the air barrier; otherwise it cannot enter the grinding contact. The air barrier is a sub-ambient low-pressure layer of fast-moving air as illustrated in Fig. 8.3. The entrained velocity at the surface is equal to the wheel velocity. The velocity is reduced with increasing distance from the surface and the pressure recovers.

The fast-moving layer of air surrounding the wheel cannot all pass through the grinding contact and is diverted either to the sides or in the reverse direction as rejected flow. The same happens with liquid coolant. This was demonstrated by Ebbrell et al. (2000) and the effect is clearly shown in Fig. 8.4.

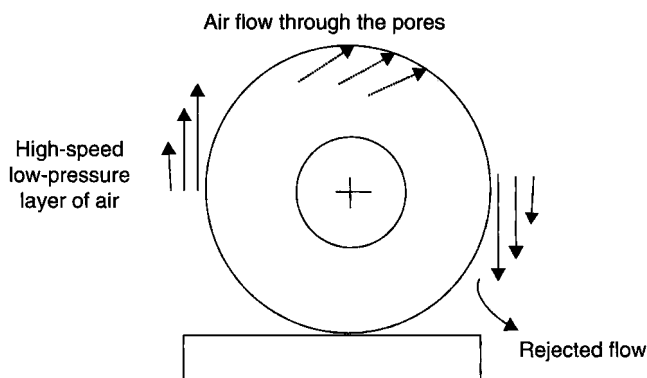


Figure 8.3 Air flow through and around a grinding wheel.

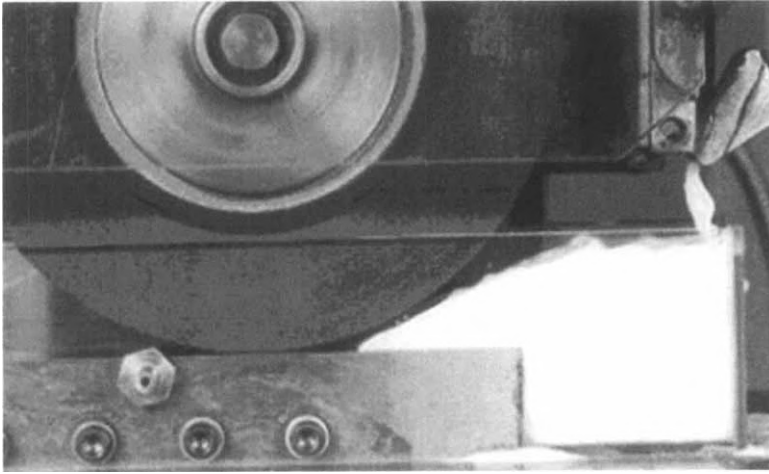


Figure 8.4 The air barrier obstructs the flow of coolant into the grinding contact.

Highly Porous Wheels

The air barrier is a greater problem with highly porous wheels. A porous wheel pumps air out of the pores. The air exits tangentially from the wheel to create the barrier. The wheel acts as a pump drawing air in from the sides of the wheels. This is less of a problem with wide non-porous wheels although an air barrier occurs in all grinding wheels.

Sealing the Wheel

It has been shown that sealing the sides of the wheel with silicone reduces the air flow into the air barrier (Marinescu et al. 2004).

Pore Feeding

In principle, feeding grinding fluid through the pores of the wheel overcomes the air barrier. However, this makes the wheel mounting complex and can cause misting. Also, the fluid must be filtered with very fine filters as the wheel has a tendency to become blocked (Fisher 1965).

Use of an Air Scraper

Physically obstructing the air flow with an air scraper or using the body of the nozzle to obstruct the air flow eases the problem of getting fluid into the contact. It has been shown by several researchers that scraper plates are

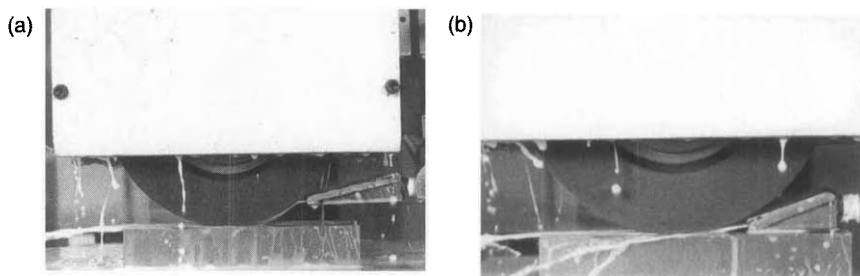


Figure 8.5 Jet delivery of grinding fluid: (a) 34° angle and (b) 0° angle.

useful in overcoming the air barrier particularly at high grinding wheel speeds where the problems become more severe (Campbell 1955; Trmal and Kaliszer 1975). A scraper is simply a plate mounted close to the wheel so as to deflect the air stream. The best place to position the scraper is immediately in front of the position where the coolant jet impinges. This obviously suggests making the air scraper part of the nozzle design. Maintaining the nozzle close to the wheel optimises fluid delivery and scraper performance.

Nozzle Position and Fluid Speed

The usual method advocated for overcoming the problem is to direct the grinding fluid at the grinding contact with a velocity approaching or equal to wheel speed. Figure 8.5 shows examples of a slot-shaped nozzle used successfully to get fluid into the grinding contact at relatively low speed.

In Fig. 8.5, the wheel speed was 27 m/s. Applying the jet above the position of reversed flow at 3.5 m/s with a flow rate of 4.2 l/min at an angle of 34° to the wheel surface successfully penetrated the air barrier. It can be clearly seen that the flow has attached to the wheel inside the low pressure layer and has gone through the grinding contact. Applying the jet tangentially close to the nip was also successful although in this case the nozzle was almost twice the distance from the wheel as in the previous case where the jet was angled at 34° to the wheel surface. The greater stand-off distance between the nozzle and the wheel allows some dispersion of the jet to occur which can be seen on closer examination (see Fig. 8.5).

Nozzle Arrangements

For high wheel speeds, a variety of nozzles and fluid delivery systems have been applied. Nozzle designs mainly fall into one of two groups as illustrated in Fig. 8.6.

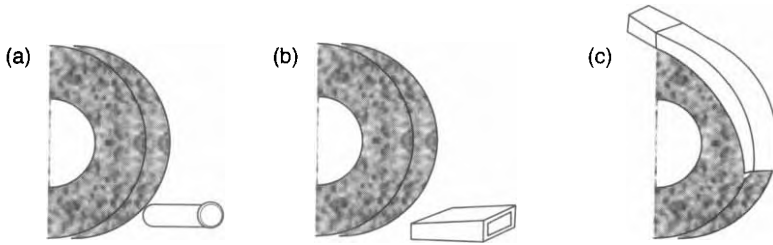


Figure 8.6 Basic nozzle shapes: (a) Round nozzle, (b) rectangular nozzle, and (c) shoe nozzle.

- Jet nozzles which introduce fluid at high velocity
- Shoe nozzles which introduce fluid at low velocity

The two main types of jet nozzles are the round nozzle and the rectangular nozzle. Shoe nozzles are closely fitted at the sides and form a chamber around a portion of the wheel periphery.

For low-speed grinding the shape of the nozzle is less critical. At high wheel speeds, it becomes more important to supply fluid across the whole width of the wheel. The round nozzle is a very effective way of delivering the fluid, but wide wheels require a larger flow rate to supply fluid effectively over the whole width. The slot nozzle can supply the required flow rate uniformly across the width. The shoe nozzle is another way of ensuring fluid is supplied across the whole width although fluid drag can be substantial at high wheel speeds.

Auxiliary Nozzles

High-pressure pumps producing auxiliary high-velocity jets may be employed to improve wheel-cleaning action as illustrated in Fig. 8.7. Pump pressures may be up to 100 bar (1500 lbf/in²). Whereas the primary jet aims to direct the fluid into the grinding contact, the auxiliary cleaning jet may be directed to impinge on the wheel acting against the wheel speed.

Positioning the Jet

The best way to get fluid into the grinding contact is to position the jet close to the wheel surface. It should also be close to the nip. The nip is the name given to the converging gap at the entry to the grinding contact. The jet should be directed tangential or almost tangential to the wheel.

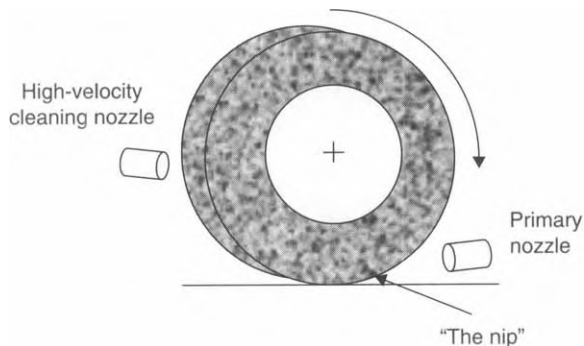


Figure 8.7 Use of auxiliary nozzles for cleaning.

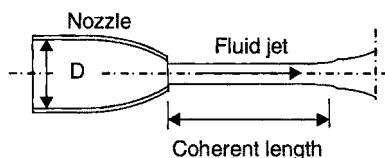


Figure 8.8 Webster/Rouse nozzle.

Too large an angle or distance from the wheel surface causes the fluid to be diverted outwards from the wheel surface or to bounce off it and leads to misting.

Coherence

It may not always be practical to position the nozzle close to the wheel surface. This may be the case when grinding deep forms such as large screw threads. In this case it may be necessary to use a larger jet diameter and substantially greater flow rate to ensure that a coherent stream of fluid penetrates the nip before turbulence causes too much dispersion of the jet (Webster et al. 1995).

Webster Nozzle

Webster developed several nozzle designs to achieve better coherence. The concave internal shape of a converging nozzle, as illustrated in Fig. 8.8, is best for coherence.

The radius of the converging section is $1.88xD$ leading to a 30° exit passage. The actual diameter of the jet of fluid is slightly smaller than the

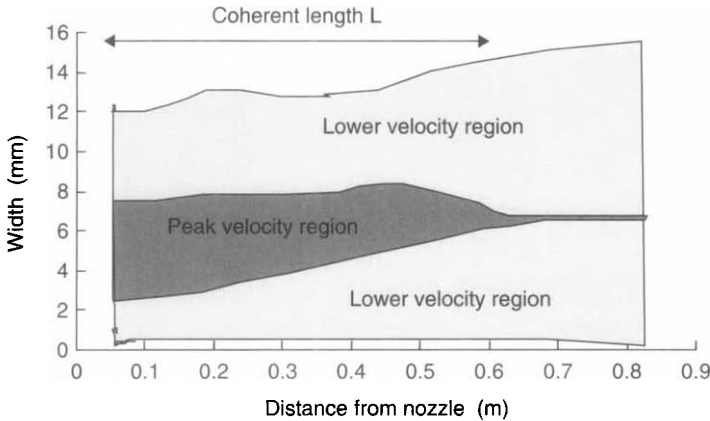


Figure 8.9 Measured jet stream width and definition of coherence length.

exit orifice due to the vena contracta effect. Coherence is better with large contraction ratios and a smooth internal surface. The contraction ratio is the ratio of the inlet diameter to the orifice diameter.

Coherent Length

An example of measured widths of peak jet velocity and overall jet width is shown in Fig. 8.9 (Morgan et al. 2008). The jet slows down faster at the outside due to friction with the atmosphere than in the centre of the jet. Eventually the width of the central jet stream that was at the peak exit velocity diminishes to zero. At this point the jet stream can be considered to have lost its coherence. Typically, this might be within a distance of 300 mm (12 in.). If the nozzle is positioned at this distance from the grinding contact, the jet velocity will be lower than the peak jet velocity right across the grinding width.

Nozzle Comparisons

Figure 8.10 shows comparisons of measured and predicted coherent length for various nozzle shapes. The large Webster nozzle gives the best coherent length and is superior to a straight tapered nozzle and a straight pipe nozzle. However, for a wide wheel it is necessary to use a rectangular nozzle shape to achieve peak velocity across the wheel width. This means that the nozzle needs to be positioned as close as possible to the grinding contact to ensure efficient fluid delivery.

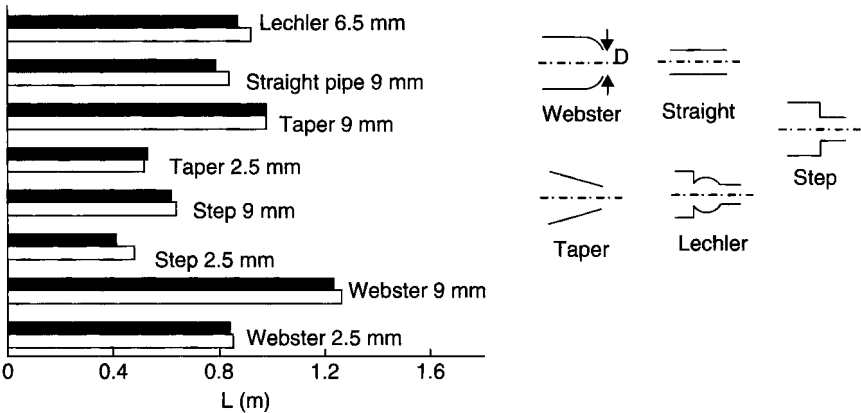


Figure 8.10 Coherent length L for various nozzle shapes and sizes. Lower bars—experiment; upper bars—predicted.

Jet coherence can be improved for all types of nozzles by incorporating flow conditioning tubes or plates that direct the flow within the nozzle (Brinksmeier et al. 1999).

Shoe Nozzle

A shoe nozzle is illustrated in Fig. 8.6. By making the nozzle fit around the sides of the wheel and also by introducing a scraper plate, the air barrier is disrupted (Fisher 1965). This allows the fluid to be entrained with the wheel at much lower pressures and velocities than with a jet nozzle.

The disadvantage of a shoe nozzle is that it takes power from the grinding spindle. The flow rate can be supplied more economically if the flow rate is reduced to the minimum necessary to produce a liquid layer of useful flow approximately 2–3 mm thick (Klocke et al. 2000). The concept of useful flow is introduced in Section 8.8.

Nip

As the grinding wheel commences up-cut grinding or completes down-cut grinding, there is a region in which there is no nip to constrain the fluid in proximity to the wheel. This situation is illustrated in Fig. 8.11. A false workpiece provides a converging wedge and helps to induce fluid into the grinding contact. Using a false workpiece gives higher removal rates before the occurrence of burn (Powell 1979).

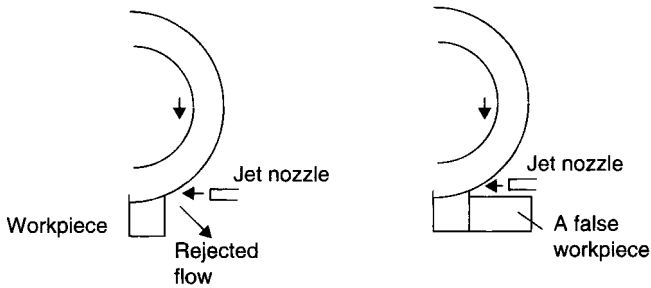


Figure 8.11 The use of a false workpiece to create a nip.

8.7 Nozzle Design Calculations

Turbulence

Turbulence for liquids in pipe flow occurs when Reynold's number $Re = \frac{\rho \cdot v \cdot d}{\eta}$ is greater than 1000–2000. Here η is the dynamic viscosity and ρ is the fluid density. Turbulence in flow through short orifices can occur at lower values of Reynold's number.

Example 8.1 Check that the flow through a round nozzle is turbulent for a water jet velocity $v = 30$ m/s with a 5-mm diameter nozzle.

For water, the density is 1000 kg/m^3 and the dynamic viscosity is 0.001 Ns/m^2 .

$$Re = \frac{1000 \times 30 \times 0.005}{0.001} = 150,000$$

Since Reynold's number here is much greater than 1000, the flow is fully turbulent.

Round Orifice Nozzle

The ideal nozzle has a smooth convergent section leading the flow into a smooth orifice section. The exit from the nozzle should be either a smooth flat face or a smooth chamfer to avoid interfering with the jet stream as in Fig. 8.12. For minimum losses, the length l of the orifice should be smooth and less than $0.4d$.

Pumping pressure energy in the supply line may be equated to the kinetic energy of the fluid jet after exit from the nozzle to determine the jet velocity.

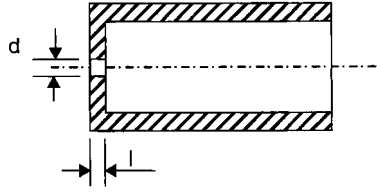


Figure 8.12 Round jet nozzle.

$$v = C_v \sqrt{\frac{2 \cdot p_p}{\rho}} \quad \text{Turbulent flow velocity} \quad (8.1)$$

where C_v is a velocity coefficient usually in the range 0.95–0.98. The flow rate Q_f is given by the jet velocity and the size of the orifice.

$$Q_f = C_d A \sqrt{\frac{2 \cdot p_p}{\rho}} \quad \text{Round jet flow rate} \quad (8.2)$$

where $A = \pi d^2/4$ and C_d is a discharge coefficient having a typical maximum value of 0.65 at $R_e = 200$. C_d is reduced at both higher and lower values of R_e .

The power required to pump the fluid through the nozzle is

$$H_p = p_p \cdot Q_f \quad \text{Pumping power} \quad (8.3)$$

Example 8.2 Determine the supply pressure and the nozzle size required to deliver a water jet velocity of 60 m/s and a flow rate of 90 l/min. What is the pumping power required? Assume $C_d = 0.65$ and $C_v = 0.97$.

$$Q_f = 90 / (1000 \times 60) = 0.0015 \text{ m}^3/\text{s}$$

$$p_p = \frac{\rho \cdot v^2}{2 \cdot C_v^2} = \frac{1000 \times 60^2}{2 \times 0.97^2} = 1913000 \text{ Pa}$$

$$A = \frac{Q_f}{C_d} \frac{1}{\sqrt{2 \cdot p_p / \rho}} = \frac{0.0015}{0.65} \sqrt{\frac{1000}{2 \times 1913,000}} \\ = 0.0000373 \text{ m}^2 \text{ (or } 0.0578 \text{ in.}^2\text{)}$$

$$d = \sqrt{\frac{4 \cdot A}{\pi}} = \sqrt{\frac{4 \times 0.0000373}{\pi}} = 0.00689 \text{ m (or } 0.271 \text{ in.)}$$

$$p_p = 1.913 \times 10^6 \text{ Pa or } 19.13 \text{ bar (or } 281 \text{ lbf/in.}^2\text{)}$$

$$H_p = 1.913 \times 10^6 \times 0.0015 = 2870 \text{ W (or } 3.85 \text{ horsepower)}$$

It takes 2.87 kW to pump the fluid through the orifice. It is also necessary to allow for additional losses in the pump, windage, pipe bend losses, and pressure losses in sudden expansions and contractions in the piping. These losses can represent a substantial increase in pump power.

Round Pipe Nozzle—Transitional Flow

If a long tube is employed to restrict the flow rather than a very short orifice, pressure energy is converted into heat through viscous losses. With long tubes and Reynold's number lower than 1000, it is necessary to allow for viscous losses. Jet velocity is reduced for a given pumped supply pressure.

$$p_p = \frac{32\eta l v}{d^2} + \frac{\rho v^2}{2} \quad \text{Transitional jet velocity} \quad (8.4)$$

The flow rate is given by the size of the bore as mentioned previously (Eqn 8.2).

Example 8.3 Round pipe nozzle. A nozzle is constructed using a copper pipe of 12 mm bore diameter and 180 mm length. The velocity of the jet is to be 4 m/s. The grinding fluid is neat oil having a viscosity of 0.045 Ns/m² and a density of 900 kg/m³. Calculate the pressure required and the pumping power. Assume $C_a = 1$.

$$Re = \frac{900 \times 4 \times 0.012}{0.045} = 960$$

$$Q_f = \frac{\pi \times 0.012^2}{4} \times 4 = 0.000452 \text{ m}^3/\text{s} \text{ or } 27.1 \text{ l/min}$$

$$p_p = \frac{32 \times 0.045 \times 0.18 \times 4}{0.012^2} + \frac{900 \times 4^2}{2} = 14400 \text{ Pa or } 0.144 \text{ bar}$$

$$H_p = 14400 \times 0.000452 = 6.51 \text{ W}$$

Rectangular Nozzle

The rectangular nozzle, Fig. 8.13, is less likely to be fully turbulent than a round orifice nozzle because the gap thickness of the opening is smaller.

The flow rate Q_f is given by the jet velocity and the area of the slot opening

$$Q_f = C_a \cdot w \cdot h \cdot v \quad \text{Slot flow rate} \quad (8.5)$$

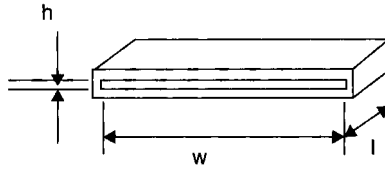


Figure 8.13 The opening of a rectangular nozzle.

where w is the width of the rectangular slot opening and h is the gap thickness. The pressure required including viscous losses is

$$p_p = \frac{12 \cdot \eta \cdot l \cdot v}{h^2} + \frac{\rho \cdot v^2}{2} \quad \text{Slot pressure} \quad (8.6)$$

Example 8.4 Rectangular nozzle. Determine the supply pressure and rectangular slot sizes required for a wheel of 25 mm width to deliver a jet velocity of 20 m/s and a flow rate of 50 l/min. What is the pumping energy required? The viscosity of the oil is 0.045 Ns/m² and the density is 900 kg/m³. The length of the orifice section is $l = 5$ mm. Assume $C_a = 0.7$.

$$h = \frac{50}{0.7 \times 1000 \times 60 \times 0.025 \times 20} = 0.00238 \text{ m (0.094 in.)}$$

$$Re = \frac{\rho \cdot v \cdot h}{\eta} = \frac{900 \times 20 \times 0.00238}{0.045} = 952$$

$$p_p = \frac{12 \times 0.045 \times 0.005 \times 20}{0.00238^2} + \frac{900 \times 20^2}{2}$$

$= 189500 \text{ Pa or } 1.895 \text{ bar (27.9 lbf/in.}^2\text{)}$

$$H_p = p_p \cdot Q_f = \frac{189500 \times 50}{1000 \times 60} = 158 \text{ W (0.21 horsepower)}$$

8.8 Nozzle Flow Rate Requirements

There is no simple principle that states the flow rate requirement for satisfactory grinding performance. Low removal-rate grinding can often be satisfactorily conducted with low grinding fluid flow rate. High removal-rate grinding requires higher flow rates to remove grinding heat and grinding swarf. Increasing flow rate reduces bulk temperatures but when combined with high jet speeds creates heat within the coolant. This heat has to be eliminated through a heat exchanger. As a general rule, high-speed high removal-rate grinding requires useful flow rate to be maximised.

Increasing the flow rate does not necessarily increase the useful flow rate. This section considers how much of the nozzle flow rate supplied becomes useful flow rate and how much nozzle flow rate is required to maximise useful flow rate.

Useful Flow Rate

Useful flow rate is the fluid that actually enters the grinding contact. It can be measured in an experimental set up by capturing the fluid at the exit from the grinding contact. Using a conventional flood nozzle, typical useful flow rates were 5–30% of the nozzle flow rate (Engineer et al. 1992). With high jet velocity, higher utilisation figures can be obtained. Percentage utilisation figures for useful flow rate depend on how well the nozzle is positioned, the jet velocity, the nozzle flow rate, and the wheel porosity. The highest percentage utilisation figures are obtained when jet velocity is equal to wheel speed.

It is usually recommended for high-speed grinding that jet velocity should be equal to the wheel speed for maximum useful flow rate although results given below indicate that lower jet speeds may be employed.

Nozzle Flow Rate

Useful flow rate is greatly different from nozzle flow rate as a large proportion of fluid is dispersed to the sides of the wheel. The rejected flow rate is almost always greater than the useful flow rate. As a rough guide, nozzle flow rate should be four times the achievable useful flow rate. Achievable useful flow rate is discussed below. If the nozzle flow rate is too low, or if the jet is not well positioned, the actual useful flow rate will be substantially lower than the achievable useful flow rate (Morgan et al. 2008). Increasing the nozzle flow rate excessively is counter-productive as it was found that the inevitable increase in rejected flow interferes with the stream of useful flow.

Achievable Useful Flow Rate

The surface pores of a grinding wheel act in a similar way to the chambers in a rotary pump. This is illustrated in Fig. 8.14. Usually, the surface pores are incompletely filled. The surface pores at best are usually only half filled even with a well-directed jet speed equal to wheel speed and adequate nozzle flow.

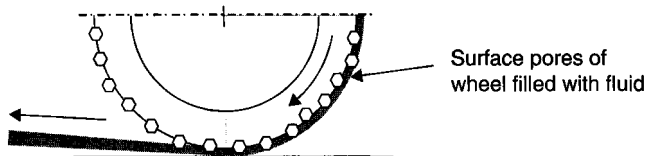


Figure 8.14 Surface pores filled with grinding fluid.

The flow rate required to fill the surface pores depends on the porosity ϕ of the wheel from manufacturer's data and the mean depth of the pores, h_{pores} . The depth of the pores can be roughly estimated from the mean grain size or mean grain length for high aspect ratio grains. Allowing for the pores being only half-filled,

$$Q = \frac{1}{2} \phi \cdot h_{\text{pores}} \cdot b \cdot v_s \quad \text{Achievable useful flow rate} \quad (8.7)$$

where b is the width of the grinding contact and v_s is the wheel speed.

The mean depth of the pores is of the same order of magnitude as the abrasive grain size. The depth is larger for very porous large-grain wheels and smaller for low-porosity small-grain wheels. The porosity of a typical grinding wheel is approximately 30%. A high-porosity wheel might have 50% porosity or greater. This provides a starting point for estimating the achievable useful flow rate.

Example 8.5 Achievable useful flow rate. The mean pore depth of a grinding wheel is approximately 1 mm, and the porosity of the wheel obtained from the manufacturer's data is 30%. Estimate the surface flow rate for a wheel speed of 60 m/s with a wheel 25 mm wide and the mean thickness of the fluid film.

$$Q = 0.5 \times 0.3 \times 0.001 \times 0.025 \times 60 = 0.00022 \text{ m}^3/\text{s} \text{ or } 13 \text{ l/min}$$

The mean thickness of the fluid film is $\frac{1}{2} \phi \cdot h_{\text{pores}} = 0.15 \text{ mm}$ or 0.0059 in.

For such a wheel running at 60 m/s, achievable useful flow rate is roughly 13 l/min. This useful flow rate will be expected given the following conditions: jet speed is 48–60 m/s being 80–100% of wheel speed, nozzle flow rate is 52 l/min being four times the useful flow rate and the jet is well positioned. If the jet velocity is too low or if insufficient fluid is supplied from the nozzle or if the nozzle is not carefully positioned, the actual useful flow rate will be reduced.

Useful flow rate achievable is greater for highly porous wheels and lower for very dense wheels. At best, the grinding fluid usually fills 40–60% of the available surface pore space. The maximum achievable useful flow

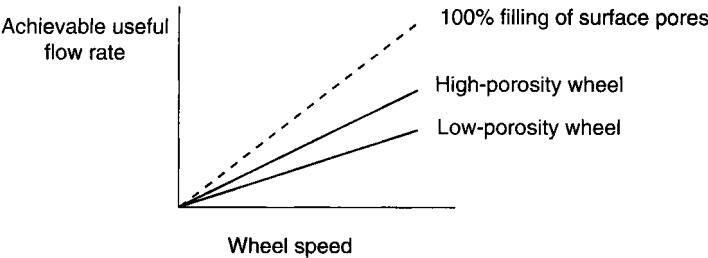


Figure 8.15 Effect of wheel speed and grinding wheel porosity on achievable useful flow rate with jet speed equal to wheel speed and sufficient nozzle flow rate.

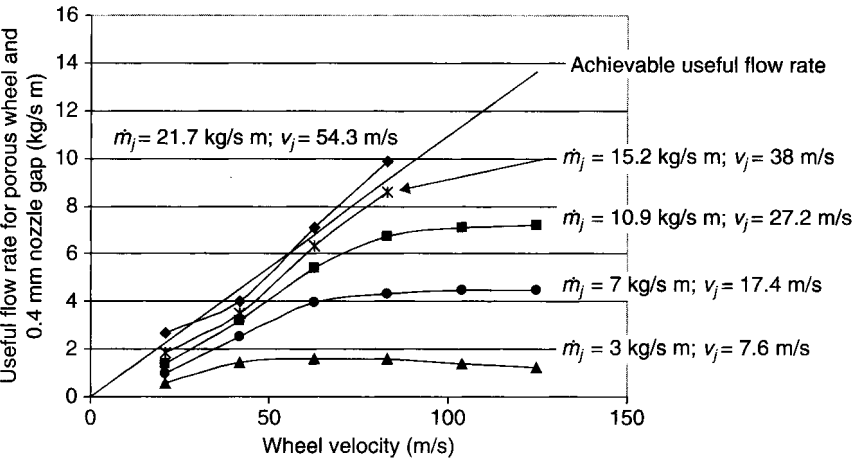


Figure 8.16 Useful flow rates for a rectangular nozzle of 0.4-mm gap thickness.

rate is almost always less than the flow rate for 100% surface pore filling. This is shown in Fig. 8.15.

The relationship between wheel speed and achievable useful flow rate is illustrated in Fig. 8.16 with jet speed equal to wheel speed and sufficient nozzle flow rate. This figure is based on experimental results (Morgan et al. 2008). For a low-porosity wheel, it is more difficult to squeeze air from the pores. At best the pores are slightly more than half full. With a highly porous wheel, the air can be more easily squeezed out and the pores more completely filled. If the jet is entrained, it is even possible to force coolant deeper into the body of the wheel. For further guidance on flow rate system design, it is helpful to consider some actual measurements.

Measured Useful Flow Rate

Figure 8.16 shows measurements of useful flow rate (Gviniashvili 2003). Useful flow measurements were made by collecting the fluid coming out of the grinding contact. The achievable useful flow rate was estimated by measuring the surface of the porous vitrified CBN grinding wheel. The flow rates are given as mass flow rates per unit wheel width. The rectangular nozzle gap thickness in Fig. 8.16 was 0.4 mm.

Results are given for various values of jet velocity. The useful flow rate falls well below the achievable flow rate when the jet speed is low compared to the wheel speed. The useful flow rate matches the achievable flow rate quite closely when the jet speed is equal to the wheel speed. Under these conditions, the useful flow rate achieved is greater than 40% of the nozzle (or jet) flow rate.

Figure 8.17 shows the results for a nozzle with a 2-mm gap. The nozzle flow rates were increased for a given jet velocity. The maximum jet velocity was reduced due to the limitation of the pump capacity. The useful flow rate with a jet velocity of 16 m/s in Fig. 8.17 almost matches the useful flow rate in Fig. 8.16 with a jet velocity of 27.2 m/s. However, the nozzle flow rate at the low jet velocity was trebled. Clearly, the useful flow rate is a function of both nozzle flow rate and jet velocity.

The feature in common for the two sets of results is that the useful flow rate is approximately limited to the value indicated by the achievable useful flow rate as calculated from wheel porosity. It appears that the maximum useful flow rate can be achieved with a jet velocity between 50% and 100%

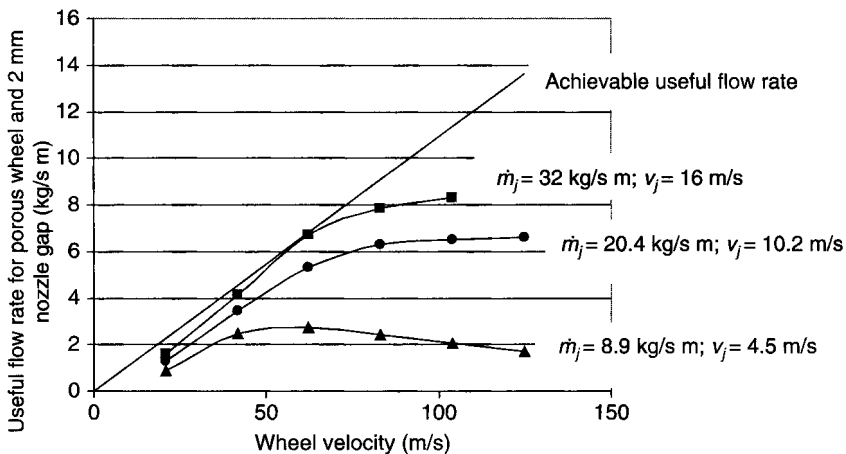


Figure 8.17 Useful flow rates for a rectangular nozzle of 2-mm gap thickness.

of the wheel speed, but at the lower jet speed a much larger nozzle flow rate is required.

Useful flow rates can be increased when jet entrainment is employed to direct the jet flow into the grinding contact using side plates to restrict sideways rejection of the flow (Jackson 2008). This is not the situation that applies in normal grinding practice but is worth exploring further.

Figure 8.18 shows the useful flow rate expressed as a percentage of the nozzle flow rate for a high-porosity Altos grinding wheel. The percentage useful flow rate in this case reached a maximum of 50% of the nozzle supply flow.

It can be seen that actual useful flow rate varies linearly with wheel speed, Fig. 8.18, up to a speed exceeding jet speed if there is sufficient supply flow rate. The useful flow rate levels off when there is insufficient jet flow for the wheel speed due to supply starvation. The straight line superimposed on the results represents achievable useful flow rate over the speed range as calculated by the method given above. Increased jet velocity or increased nozzle supply flow rate in the linear region serves only to increase energy consumption and leads to increased rejected flow.

Figure 8.19 shows that excessive jet supply flow rate fails to significantly increase useful flow rate even though the supply flow rate is almost doubled. Excessive supply flow rate reduces percentage useful flow and increases rejected flow while useful flow rate is only marginally increased. Increasing jet velocity increases the useful flow rate slightly until wheel speed is reached. After this there is reduced benefit. For the medium porosity wheel shown, maximum useful flow rate was typically 25–30%. However, for a high porosity wheel, the maximum useful flow rate could be increased to a maximum of 50%.

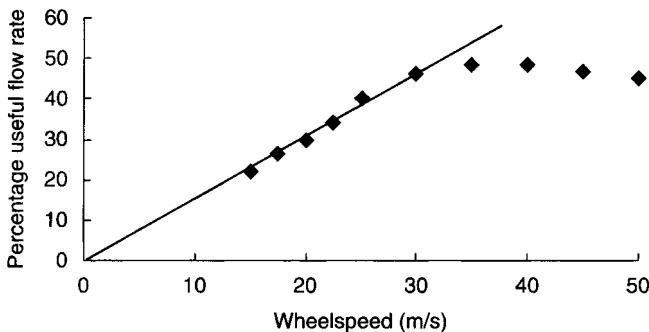


Figure 8.18 Percentage useful flow (%) and prediction. Nozzle flow = 18.9 l/min, jet speed = 24.2 m/s, and Altos high-porosity (54%) alumina wheel with high aspect ratio grains (10:1).

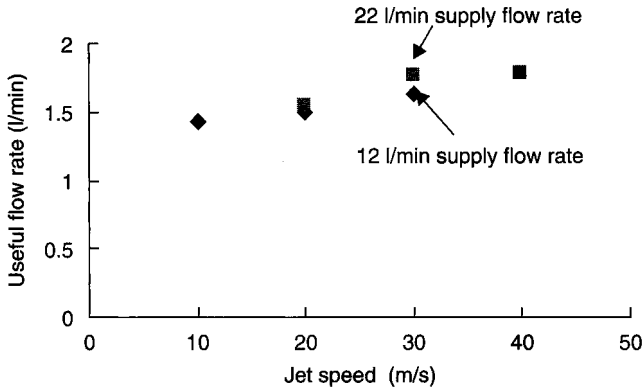


Figure 8.19 Useful flow rate vs jet speed for two different flow rates where $v_s = 30$ m/s, $a = 30$ μ m, $v_w = 10$ mm/s, medium-porosity (44%) Flexovit wheel WA60KVL.

Bulk Cooling

The total flow rate should be sufficient to remove the process heat generated. It is important to maintain a stable machine and workpiece temperature; otherwise sizes will drift as temperatures gradually increase. This requirement is different from simply preventing an unacceptable temperature rise in the grinding contact. Excessive temperature rise can be prevented by deciding a suitable value of temperature increase for the coolant as it floods the workpiece and machine. A simple heat balance gives

$$Q = \frac{P}{\rho \cdot C_p \cdot \Delta T} \quad \text{Bulk cooling flow rate} \quad (8.8)$$

where P is the grinding power, ρ is fluid density, C_p is the specific heat capacity, and ΔT is the acceptable temperature rise.

Example 8.6 Bulk cooling flow rate. Acceptable temperature rise of the fluid between delivery from the nozzle and return to the coolant system is judged to be 10 K. The grinding power is 10 kW. The density of the neat oil fluid is 900 kg/m³ and the specific heat capacity is 1.9 kJ/kgK. Estimate the flow rate required ignoring radiation from the machine elements.

$$Q = \frac{10000}{900 \times 1900 \times 10} = 0.000585 \text{ m}^3/\text{s} \text{ or } 35 \text{ l/min}$$

Fluid splashing against the machine column causes random size errors. These can be prevented by placing a splash guard between the working area and the machine column.

8.9 Power Required to Accelerate the Fluid

Spindle Power

If the jet velocity is equal to the wheel speed, there is no need to accelerate the fluid further and no need for extra power at the grinding wheel spindle. If the jet velocity is greater than the wheel speed, the jet will start to drive the wheel. This is a wasteful and inefficient use of energy. In the usual situation, the jet speed is lower than the wheel speed and creates a drag on the wheel spindle. The wheel spindle has to accelerate the fluid. This effect is substantial with large high-speed wheels. It reduces the energy available for grinding.

The power required to accelerate the fluid can be easily estimated (Gviniazhvili et al. 2004, 2005). Working from Newton's second law, the power input from the grinding wheel is

$$H_f = F.v_s = \rho.Q.(v_s - v_j).v_s/k_f \quad \text{Power from spindle} \quad (8.9)$$

where the factor k_f allows for losses in the conversion process and typically has a value approximately equal to 0.5 for a porous wheel and 0.7 for a non-porous wheel. The flow rate Q is the useful flow rate that is accelerated in the grinding contact. Ignoring k_f we can evaluate an efficiency of the pumping process. The increase in kinetic energy of the useful flow is $\rho.Q.(v_s^2 - v_j^2)/2$. Dividing the increase in kinetic power of the fluid by the spindle power input, the pumping efficiency is

$$E = \frac{v_s + v_j}{2.v_s} \quad \text{Efficiency} \quad (8.10)$$

Efficiency is maximum when the jet speed is equal to the wheel speed.

Total Power

The total power, H_t , required to supply the process fluid is the sum of the pumping power, H_p , and the spindle power, H_f .

$$H_t = p_p.Q_f + \rho.Q_u.(v_s - v_j).v_s/k_f \quad \text{Total fluid power} \quad (8.11)$$

where Q_f is the total pumped flow rate and Q_u is the useful flow rate.

For a jet tilted at an angle α to the wheel tangent, the jet speed in Eqn (8.11) might be replaced by $v_j \cdot \cos\alpha$. Intuitively, it can be sensed that energy efficiency will be substantially reduced with increasing angle of the jet.

The extra power supplied by the wheel spindle can be estimated for a shoe nozzle or for a jet nozzle applied perpendicular to the wheel surface by assuming that the useful flow rate supplied through the nozzle is accelerated up to the peripheral speed of the wheel. A significant proportion of the flow rate is not accelerated up to wheel speed. This means there are additional losses due to re-circulation within the shoe nozzle chamber. It is therefore important to minimise the total flow rate as far as possible while not starving the wheel of fluid which will reduce the useful flow rate (Klocke et al. 2000). Further losses occur due to drag between the shoe and the sides of the wheel. For both reasons, shoe nozzles are not as energy efficient as might first appear.

Equation (8.11) can also be used for shoe nozzles. In this case $v_j = 0$ and the pumping power term is likely to be very small. The spindle power to accelerate the fluid is substantially increased as shown by the examples that follow and may even be larger than the grinding power.

Example 8.7 Shoe nozzle. A grinding wheel 25-mm wide has a 1-mm pore depth and a porosity of 50%. The wheel speed is 100 m/s. The coolant pump supplies 300 l/min of water-based fluid at a pressure of 0.5 bar to a shoe nozzle. The fluid density is 980 kg/m³. Estimate the power requirements associated with fluid delivery.

$$Q_u = 0.5 \times 0.001 \times 0.025 \times 100 = 0.00125 \text{ m}^3/\text{s} \text{ or } 75 \text{ l/min}$$

$$Q_f = 300/1000/60 = 0.005 \text{ m}^3/\text{s}$$

$$H_p = 0.5 \times 10^5 \times 0.005 = 250 \text{ W}$$

$$H_f = 980 \times 0.00125 \times (100 - 0) \times 100/0.5 = 24500 \text{ W or } 32.8 \text{ horsepower}$$

This example demonstrates that a shoe nozzle may demand a substantial increase in power input to the grinding spindle.

The following example, using the same methodology, is for jet delivery.

Example 8.8 Jet nozzle. For the same grinding conditions as in Example 8.7, estimate the power requirements using a round jet nozzle to supply 300 l/min at a velocity of 100 m/s assuming negligible viscous losses and $C_v = 1$.

$$Q_u = 0.00125 \text{ m}^3/\text{s} \text{ as previously}$$

$$Q_f = 300/1000/60 = 0.005 \text{ m}^3/\text{s}$$

$$p_p = \frac{\rho \cdot v^2}{2} = \frac{980 \times 100^2}{2} = 4.9 \text{ MPa or 49 bar (720 lbf/in.}^2\text{)}$$

$$H_p = p_p \cdot Q_f = 4.9 \times 10^6 \times 0.005 = 24500 \text{ W}$$

$$H_f = \rho \cdot Q \cdot (v_s - v_j) \cdot v_s / k_f = 980 \times 0.00125 \times (100 - 100) \times 100 = 0 \text{ W}$$

This example demonstrates the substantial power input required to pump a large flow rate through a nozzle at 100 m/s.

In this example, substantial saving of energy can be made if the required useful flow rate is achieved with reduced nozzle flow rate at the same velocity. Halving the nozzle flow rate halves the pumping power.

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9 Cost Reduction

9.1 Introduction

Output, Quality, and Cost

A manufacturing process has to meet three sometimes conflicting requirements illustrated in Fig. 9.1. An optimum process is where quality levels meet the required standard, output meets the required number of parts per hour, and cost per part is minimum. The best process developments are the ones that improve all three requirements.

A systematic cost analysis aims towards increasing output while reducing costs and improving quality (Ebbrell 2003, Rowe and Ebbrell 2004). A number of industrial examples have been described by Marinescu et al. (2007). For example, it was shown that changing to CBN grinding for internal profile grinding of an aero engine shroud assembly reduced cost by more than two-thirds. This gave a saving of \$570 per part.

Total Life Cycle Costs

A total life cycle approach should be used. In addition to basic costs it is necessary to add disposal costs of waste products, materials, and machines at the end of their useful life. This does not change the approach taken below but it means that the relevant overheads should be included.

Cost Variables

Factors that affect cost are illustrated in Fig. 9.2. The factors include mean cycle time, dwell time for spark-out, and dressing time. Costs include labour cost, machine costs, grinding wheel cost, and dresser cost. Other factors are permitted size deviations, shape deviations, roughness, and surface integrity.

High wheel speeds allow higher removal rates for the same quality. Grinding machine costs are also increased. More advanced designs are required for spindle drives and for spindle bearings, also for wheel guarding, wheel dressing, fluid delivery, and machine stiffness. Advantages are often obtained by introducing super-abrasive wheels to allow higher

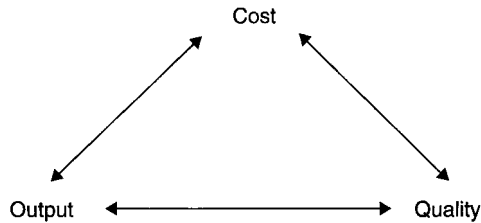


Figure 9.1 Cost, quality, and output are complementary requirements of an optimum process.

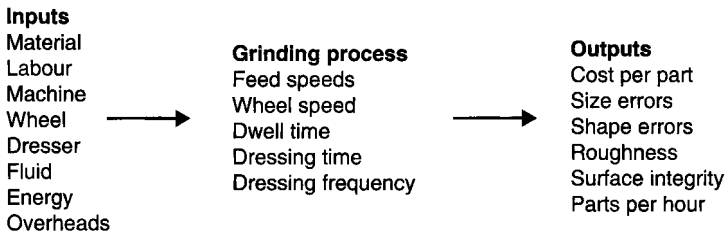


Figure 9.2 Factors involved in cost reduction.

wheels speeds and long re-dress life. This increases the cost of the abrasives but will usually reduce cost per part.

A study of costs in cylindrical grinding demonstrates that some cost increases may be worthwhile to bring down the total cost per part.

Overhead Costs

Some costs are often grouped as overheads. For example, many costs are loaded onto labour costs or onto machine costs. Machine costs are often loaded with energy costs, cost of the building, and cost of machine maintenance. Labour costs are often loaded with management costs. Grouping costs in this way simplifies cost comparisons. However, there is a danger that it reduces motivation to investigate potential benefits of introducing new machines and equipment. Overhead costs will also include the cost of disposal of waste products, materials, and machines at the end of their useful life.

Wheel, Machine, and Labour Costs

The following examples group costs under labour cost, machine cost, and wheel cost. Workpiece quality and re-dress life are related through the

number of parts ground before roughness or roundness tolerance is exceeded. Output is related through the cycle time.

9.2 Analysis of Cost per Part

Cost Elements

The first step is to establish costs for comparison. For the following study, we compare CBN and alumina abrasives for grinding typical aerospace materials. Since CBN requires high wheel speeds to achieve the best output, we also look at wheel speed as a variable and the consequent increase in machine cost.

Three convenient cost groups are machine, labour, and grinding wheel costs. Grinding fluids are grouped with machine costs. Material costs are ignored since material cost per part remains constant. The user can easily adapt these groups to examine more closely the effects of a particular item. For example, grinding fluids could be considered separately to examine the benefits of investing in a much more expensive fluid.

Total Cycle Time

The second step is to establish the total cycle time. Total cycle time depends on the grinding cycle time plus the dressing cycle time.

$$t_t = t_s + t_d/N_d \quad \text{Total cycle time} \quad (9.1)$$

where t_s is the grinding cycle time and t_d is the dressing cycle time. The number of parts ground before the wheel requires dressing is N_d .

Example 9.1 The grinding cycle time for grinding a part from loading one part to loading the next is 86 s. The wheel requires to be dressed every four parts. The dressing operation including adjusting the machine feed position takes 56 s.

$$\text{Total cycle time} = 86 + 56/4 = 100 \text{ s}$$

Grinding Cycle Time

Grinding cycle time in cylindrical grinding includes feed time for the stock removal d_{vw} , the stand-off distance d_{ss} , and the dwell time for spark-out t_{so} as in Fig. 9.3.

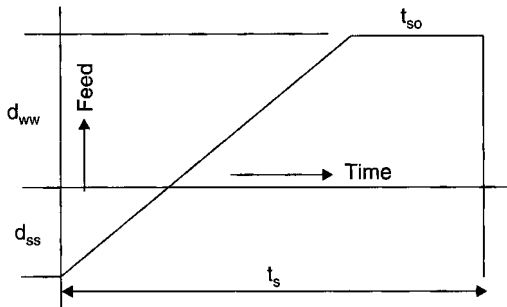


Figure 9.3 Basic grinding cycle time for a single in-feed and spark-out cycle.

$$t_s = \frac{(d_{ww} + d_{ss})\pi d_w}{Q'_w} + t_{so} \quad \text{Grinding cycle time} \quad (9.2)$$

where Q'_w is specific removal rate and d_w is the workpiece diameter.

Example 9.2 The stock to be removed by cylindrical grinding of 40 mm diameter workpieces is 160 μm . The safe stand-off distance is set as 40 μm . The material removal rate is to be 10 $\text{mm}^3/\text{mm s}$ and the spark-out time allowed is 4 s.

$$\text{Grinding cycle time} = t_s = \frac{(160 + 40) \times \pi \times 40}{1000 \times 10} + 4 = 6.51 \text{ s}$$

Dressing Cycle Time

Dressing cycle time depends on the wheel width b_s , the number of dressing passes n_d , and the dressing feed rate v_{fd} .

$$t_d = \frac{b_s n_d}{v_{fd}} \quad \text{Dressing cycle time} \quad (9.3)$$

The dressing feed rate depends on the effective width of the dressing tool b_d and the dressing overlap ratio U_d . Typically, the overlap ratio should be of the order of 3–10. A higher value gives lower roughness but higher grinding forces.

$$v_{fd} = \frac{b_d}{U_d} \times \frac{v_s}{\pi d_s} \quad \text{Dressing feed rate} \quad (9.4)$$

Example 9.3 A grinding wheel is 25 mm wide and two dressing passes are to be made for each dressing operation. The dressing tool is 120 μm wide at a 10 μm dressing depth. The grinding wheel speed for grinding

with alumina is 40 m/s and the grinding wheel diameter is 450 mm. Calculate the dressing feed rate for an overlap ratio of 3 and the dressing cycle time.

$$\begin{aligned}\text{Dressing feed rate} = v_{fd} &= \frac{0.000120}{3} \times \frac{40}{\pi \times 0.450} \\ &= 0.00113 \text{ m/s or } 0.0445 \text{ in./s}\end{aligned}$$

$$\text{Dressing cycle time} = t_d = \frac{25 \times 2}{1.13} = 44.2 \text{ s}$$

Dressing Frequency

The need for dressing depends on factors such as the abrasive employed, the removal rates, and the accuracy to be maintained. Often, with large diameter wheels, many parts are produced before it is necessary to dress. In other operations with small wheels or very large parts, the wheel may be dressed several times for each part. This greatly increases the cycle time and the cost per part. In small-part production more of the wheel is usually removed by dressing than by wheel wear. Frequent dressing increases the replacement frequency of both the wheel and the dressing tool.

Number of Parts per Wheel

The number of parts per wheel depends on the number of times the wheel is dressed for each part produced. The number of parts that are produced before the wheel is reduced to the minimum safe size is given by

$$N_w = \left[\left(\frac{d_{s\max} - d_{s\min}}{2} \right) / (r_s + a_d n_d) \right] N_d \quad \text{Number of parts per wheel} \quad (9.5)$$

where $d_{s\max}$ is the maximum wheel diameter, $d_{s\min}$ is the minimum wheel diameter, r_s is the radial grinding wear per dress, a_d is the dressing depth, n_d is the number of dressing passes, and N_d is the number of parts per dress.

Example 9.4 An alumina grinding wheel when new is 500 mm diameter. The minimum diameter permitted is 400 mm. The radial wheel wear over the re-dress period is estimated as 15 μm . The re-dress life of the wheel is 5 parts per dress. The dressing procedure calls for two passes taking a dressing cut depth of 25 μm . Determine the number of parts per wheel.

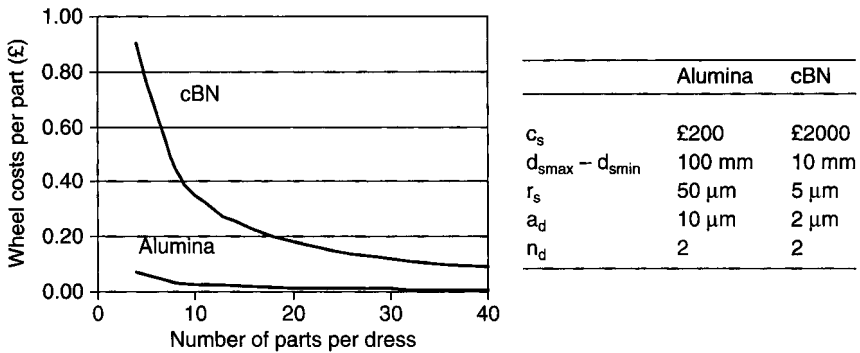


Figure 9.4 Comparison of typical wheel cost per part for alumina and CBN wheels.

The number of parts per wheel is

$$N_w = \left[\left(\frac{500 - 400}{2} \right) \right] / \left(\frac{15 + 25 \times 2}{1000} \right) \times 5 = 3846 \text{ parts per wheel}$$

Wheel Cost per Part

The wheel cost per part C_s is

$$C_s = \frac{c_s}{N_d} \quad \text{Wheel cost per part} \quad (9.6)$$

where c_s is the wheel cost. Assuming that the cost of the wheel is £200 or 20,000 pence, the wheel cost per part for the above example is $20,000/3846 = 5.2$ pence.

Figure 9.4 shows a typical wheel cost/part varying with the number of parts/dress for alumina and CBN wheels. Re-dress life has a strong effect using cBN due to initial cost but cost/part may be offset by longer re-dress life. A high wheel speed increases re-dress life by reducing chip thickness. Wheel cost/part becomes negligible with long re-dress life.

The benefit of CBN for reducing total cost will be seen below when wheel, labour, and machine costs are added together.

Labour Cost/Part

Labour rate includes a general overheads cost element. Labour cost per part C_l is the product of labour rate c_l and the total cycle time t_c . Multiplying the total cycle time by the labour rate gives the labour cost/part.

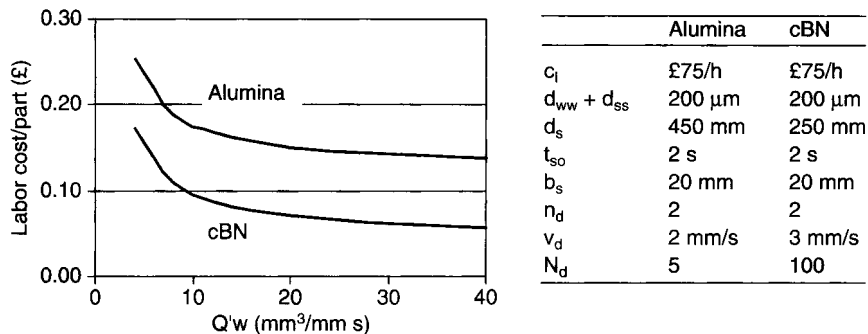


Figure 9.5 Effect of removal rate on labour cost per part.

$$C_l = c_l \cdot t_t = c_l \cdot \left[\frac{(d_{ww} + d_{ss}) \pi d_w}{Q'_w} + t_{so} + \frac{b_s n_d}{v_{fd} N_d} \right] \quad \text{Labour cost/part} \quad (9.7)$$

Labour cost/part is affected by the total cycle time. It therefore depends on removal rate, spark-out time, number of dressing passes, dressing feed rate, and by number of parts/dress. The above analysis shows the importance of dressing frequency and dressing time in cost per part. With many parts/dress, the last term of Eqn (9.7) becomes negligible. With several dressing operations per part, the last term becomes large.

Example 9.5 Labour cost includes an overhead element of £75/h. The total cycle time is 100 s.

$$\text{Labour cost per part is } C_l = \frac{75}{60 \times 60} \times 100 = £2.08$$

Figure 9.5 shows the effect of removal rates using a labour rate with overheads based on £75/h. It can be seen that high removal rates reduce the labour cost per part.

Figure 9.6 shows that re-dress life can strongly affect labour cost per part. In this case re-dress life is represented by parts per dress. Re-dress life is particularly important if it is short. In this example, dressing cost/part becomes negligible for more than 3 parts/dress.

Machine Cost/Part

Machine cost per part C_m is given by the cost of the machine C_{mc} times the total cycle time divided by the payback time y_t .

$$C_m = C_{mc} \cdot t_t / y_t \quad \text{Machine cost per part} \quad (9.8)$$

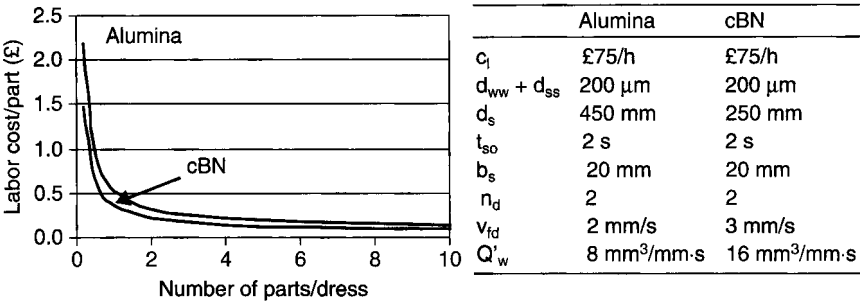


Figure 9.6 Effect of the number of parts per dress on labour cost per part.

Example 9.6 A cylindrical grinding machine with CNC control costs \$150,000 and the payback period required is 2 years at 40 h/week for 50 weeks/year operation. The machine is to be used to grind parts with a total cycle time of 100 s.

Machine cost per part = $150000 \times 100 / (2 \times 40 \times 50 \times 60 \times 60)$
= \$1.042

Total Variable Cost/Part

Total cost per part is the sum of wheel cost/part, labour cost/part, and machine cost/part. Material cost/part does not affect the selection of process conditions and is therefore left out of this discussion.

The total cost/part for wheels, labour, and machine is therefore $C_t = C_s + C_l + C_m$, or in terms of the grinding conditions:

$$C_t = \frac{2c_s (r_s + a_d n_d)}{(d_{smax} - d_{smin}) N_d} + \left[\frac{(d_{ww} + d_{ss}) \pi d_w}{Q'_w} + t_{so} + \frac{b_s n_d}{v_d N_d} \right] \left(c_l + \frac{C_{mc}}{y_t} \right)$$

Total cost per part (9.9)

The number of parts/dress is important in all three elements contributing to the total cost. This explains why re-dress life becomes important in any process optimisation.

9.3 Cost Reduction Trials

When large numbers of parts are to be produced in batches over years it is important to conduct grinding trials for cost reduction. Trials are

necessary to determine the effect of grinding conditions on re-dress life. The procedure ideally requires three stages:

- Basic trials
- Estimate best conditions to meet process requirements
- Confirmation trials

A possible fourth stage allows alternative selections of grinding conditions to be tried and compared for costs. The above three-stage procedure provides an approximately optimised set of grinding conditions for a particular grinding machine and abrasive.

In the following analysis we go much further, we compare the following different set-ups for an easy-to-grind material AISI 52100:

- Vitrified alumina on a conventional cylindrical grinder
- Vitrified CBN on a conventional cylindrical grinder
- Vitrified CBN on a high-speed cylindrical grinder with rotary-disk dresser

Further cost comparisons are made for grinding a difficult-to-grind material: Inconel 718.

For the following cost comparisons, re-dress life was defined as the number of parts ground before any of the quality levels were breached. For example, roughness was required to be less than $0.25\text{ }\mu\text{m Ra}$ and roundness error less than $1\text{ }\mu\text{m}$ for the AISI 52100 material. By these means, specified quality levels are built in to the test procedure.

The following procedure allows a comprehensive analysis to be performed with a minimum number of grinding and measuring trials. The procedure follows trials that were actually conducted so that typical results can be shown.

Basic Trials

With a large number of process variables, the testing time could become prohibitively expensive. For example, 8 variables require 256 trials for a full set of tests. Therefore, an economic testing procedure must be employed that identifies the most important factors and preferred values with as little time as possible required for testing.

An example of an experimental design for a two-level investigation of seven variables using eight trials is given in Table 9.1. In the example shown four trials are conducted for each value of a parameter as can be

Table 9.1 An $L_8 2^7$ Experimental Plan

Trial	Parameter						
	A	B	C	D	E	F	G
1	1	1	1	1	1	1	1
2	1	1	1	2	2	2	2
3	1	2	2	1	1	2	2
4	1	2	2	2	2	1	1
5	2	1	2	1	2	1	2
6	2	1	2	2	1	2	1
7	2	2	1	1	2	2	1
8	2	2	1	2	1	1	2

The values 1 and 2 (under columns A–G) indicate Levels 1 and 2, respectively.

seen by considering a single column in Table 9.1. The result for each parameter is the mean value for the combinations of the other parameters shown. The expected range for each variable can be expressed by a low value and a high value. In each case, Level 1 represents the low value and Level 2 represents the high value. A large number of workpieces is ground for each trial, thus allowing re-dress life and G ratio to be assessed.

Variables A to G include all the main factors known to influence quality and re-dress life. These include wheel speed, work speed, spark-out time, dressing depth, dresser speed, dressing overlap ratio, and dressing direction. For most trials an additional variable was introduced: the number of dressing passes.

Figure 9.7 shows roughness results from a single grinding trial. The re-dress life corresponds to the number of parts produced after $4000 \text{ mm}^3/\text{mm}$ of material has been removed. At this point the roughness is at the acceptable limit. Other measurements made were specific energy, size-holding, roundness, and G ratio.

Direct Effects

Results from the basic trials are presented as direct effects. An example of direct effects on size-holding using the high-speed machine is shown in Fig. 9.8.

The levels employed for the variables are given in Table 9.2.

It is clear that high wheel speed allows much better control of size-holding. It is also seen that a small number of dressing passes is better than

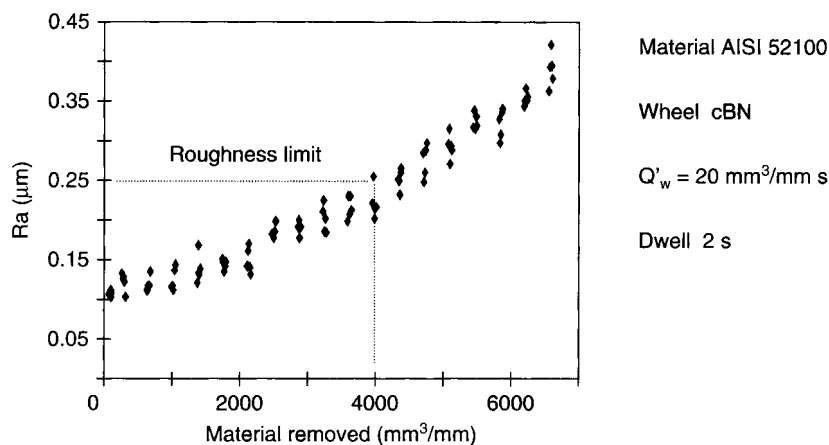


Figure 9.7 Roughness values in a single grinding trial.

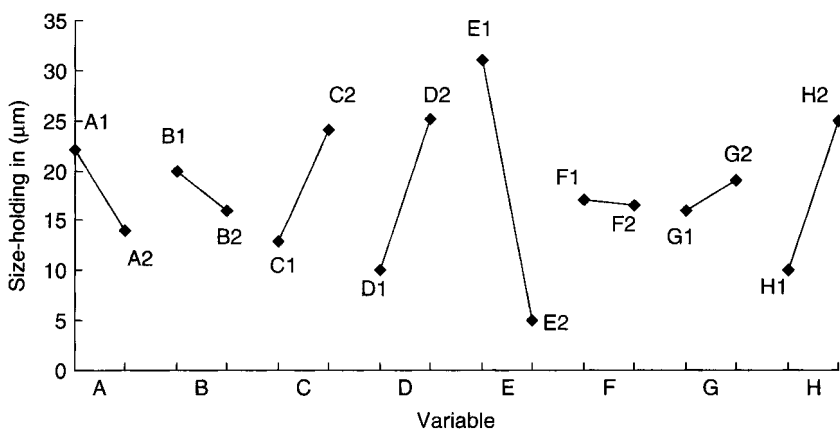


Figure 9.8 Direct effects on size-holding in high-speed CBN grinding of AISI 52100 for the variables listed in Table 9.2.

a large number and a lower dressing speed is better than a higher dressing speed.

Selection of Best Conditions

The best conditions selected will depend on all the measured values: that is, roughness, roundness, size, re-dress life or G ratio, and specific energy. The best conditions selected will depend on the objectives. For example, when there is difficulty in achieving quality specifications, the conditions selected will be chosen to ensure that quality is easily maintained

Table 9.2 Variable Levels for High-Speed CBN Grinding of AISI 52100

		1	2
A	Dressing direction	Down	Up
B	Dressing overlap	2	10
C	Dressing depth (µm)	2	10
D	Dressing passes	2	10
E	Wheel speed (m/s)	60	120
F	Work speed (m/min)	36	54
G1	Finish feed	10 µm @ 1µm/s	
G2	Dwell (s)		10
H	Dresser speed (m/s)	42	72

without risking short re-dress life. Sometimes quality will be readily achieved and it is possible to select conditions that increase removal rate.

The results above are given for high-speed CBN grinding. In this case, the best conditions selected were up dressing at 42 m/s with 2 dressing passes at 2 µm depth and a dressing feed rate of 1.7 mm/s corresponding to a dressing overlap of 10. The best grinding conditions were 120 m/s wheel speed, 26 m/min work speed, spark-out for 2 s, and a removal rate of 20 mm³/mm/s.

Confirmation Trials

A trial should be conducted to confirm that the best conditions selected achieve the objectives. The confirmation trial is a much smaller experiment than the full set of basic trials, but it is important to ensure that quality levels are acceptable and to check that the re-dress life is acceptable. The results from the confirmation trials are taken as the optimised results for a particular set up and abrasive.

9.4 Cost Comparisons for AISI 52100

In this section optimised results are compared for different abrasives and two different machines.

The cost factors and variables used for the trials are given in Table 9.3. A high-speed vitrified CBN wheel was tested on a high-speed grinding machine for speeds up to 140 m/s. The results were compared with conventional alumina wheels and CBN wheels tested on a conventional machine at speeds up to 45 m/s.

Table 9.3 Conditions for Cost Comparisons

Wheels	All Al ₂ O ₃	SG	CBN	CBN (High Speed)
Wheel speed (m/s)	45	45	45	60–120
Wheel cost (£)	200	320	3000	1700
Wheel dia—max (mm)	450	450	450	250
Wheel dia—min (mm)	350	350	438	240
Labour rate (£/h)	75	75	75	75
Total feed (mm)	0.2	0.2	0.2	0.2
Work dia (mm)	40	40	40	40
Wheel width (mm)	25	25	25	17
Machine cost (£)	100,000	100,000	100,000	250,000
Payback period (h)	1,920	1,920	1,920	1,920

Table 9.4 Grinding Wheels for AISI 52100

Abrasive	Specification	Wheel Speed (m/s)
Vitrified Al ₂ O ₃	A46 K5V	45
Vitrified Al ₂ O ₃	A80 J6V	45
Vitrified SG	A60 J8V	45
Vitrified CBN	B91 (medium speed)	45
Vitrified CBN	B91 (high speed)	60–120

The high-speed grinding machine was designed to employ smaller diameter wheels. Small CBN wheels are considerably less expensive than large CBN wheels as seen in Table 9.3. The cost of the high-speed machine was approximately two and a half times the cost of the conventional cylindrical grinding machine.

Grinding Wheels

Five grinding wheels were employed for easy-to-grind bearing steel AISI 52100. The wheels are listed in Table 9.4.

Best Conditions

For best results, the following dressing and grinding conditions were selected from the basic trials for each abrasive and confirmed by confirmation trials. The best conditions found were as follows.

Conventional-Speed Al_2O_3 and SG Wheels

A single-point diamond dressing tool was used with dressing depth $a_d = 10 \mu\text{m}$, dressing feed rate $v_{fd} = 3.18 \text{ mm/s}$, and dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 45 \text{ m/s}$, work speed $v_w = 20 \text{ m/min}$, and spark-out $t_{so} = 10 \text{ s}$. For the basic aluminium oxide wheels, specific removal rate $Q'_w = 1 \text{ mm}^3/\text{mm}\cdot\text{s}$. For the SG wheel $Q'_w = 2.5 \text{ mm}^3/\text{mm}\cdot\text{s}$.

Conventional-Speed CBN Wheel

The conventional-speed B91 CBN wheel was dressed with a rotary disk dresser. The wheel was up-dressed at a speed $v_r = -12.6 \text{ m/s}$, dressing depth $a_d = 2 \mu\text{m}$, dressing feed rate $v_{fd} = 3.9 \text{ mm/s}$, and dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 45 \text{ m/s}$, work speed $v_w = 26 \text{ m/min}$, spark-out $t_{so} = 10 \text{ s}$, and removal rate $Q'_w = 4 \text{ mm}^3/\text{mm}\cdot\text{s}$.

High-Speed B91 CBN Wheel

The high-speed B91 CBN wheel was up-dressed at a speed $v_r = -42 \text{ m/s}$, dressing depth $a_d = 2 \mu\text{m}$, dressing feed rate $v_{fd} = 1.7 \text{ mm/s}$, and dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 120 \text{ m/s}$, work speed $v_w = 26 \text{ m/min}$, spark-out $t_{so} = 2 \text{ s}$, and removal rate $Q'_w = 20 \text{ mm}^3/\text{mm}\cdot\text{s}$.

Cost Comparison

The results presented in Fig. 9.9 show that in all cases wheel costs were very small. Two conventional wheel structures A46 and A80 gave very similar costs at a removal rate of $1 \text{ mm}^3/\text{mm}\cdot\text{s}$. An A60-SG abrasive gave the same quality level at a higher removal rate of $2.5 \text{ mm}^3/\text{mm}\cdot\text{s}$ and

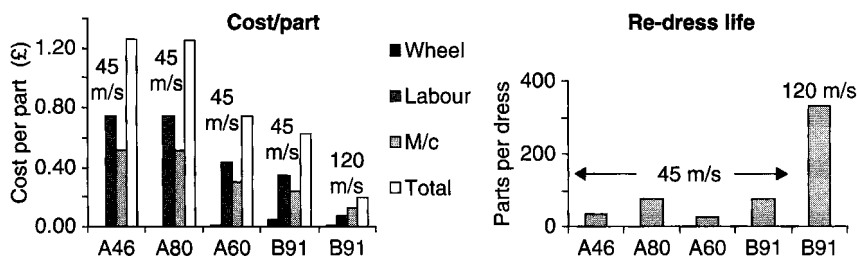


Figure 9.9 Cost comparisons and re-dress life for different abrasives and machines for grinding AISI 52100.

reduced costs. Vitrified CBN at 45 m/s allowed an even higher removal rate of 4 mm³/mm·s, further reducing costs. CBN at 120 m/s allowed the highest removal rates of 20 mm³/mm·s and produced the lowest costs in spite of the more expensive machine.

Re-dress Life

Re-dress life was longest using the high-speed wheel at 120 m/s. More than 300 parts per dress were achieved at removal rates 20 times higher than used for the basic alumina wheels at 45 m/s. With long re-dress life, abrasive cost, re-dress time, and machine cost become relatively unimportant. Minimum cost depends much more on achieving high removal rate and short spark-out time.

9.5 Cost Comparisons for Inconel 718

Grinding Wheels

Inconel 718 is a more difficult material to grind. It is a high-strength, high-temperature aerospace alloy and has a tendency to clog the grinding wheel. The three grinding wheels listed in Table 9.5 were each selected after initial testing. These wheels were then used in trials to determine the most favourable conditions for each wheel.

High-porosity wheels are often used to overcome wheel loading. High-porosity wheels tend to wear more quickly but have the advantage that more fluid can be carried into the grinding contact. The higher wear rate can be offset by higher wheel speeds. This also tends to offset the loading tendency. An A80 aluminium oxide wheel having 42% porosity gave better results than other aluminium oxide wheels tested for grinding Inconel 718. The CBN wheels listed also had increased porosity, 40% compared with 35%, and showed improved grinding performance for Inconel 718 (Cai 2002).

Table 9.5 Grinding Wheels for Inconel 718

Abrasive	Specification	Wheel Speed (m/s)
Vitrified Al ₂ O ₃	A80 J6V	45
Vitrified CBN	B151 (medium speed)	45
Vitrified CBN	B151 (high speed)	60–120

Best Conditions

Grinding trials were conducted for each of the three grinding wheels. Best dressing and grinding conditions were selected for each of the three wheels.

Conventional-Speed Al₂O₃ Wheel

Single-point diamond dressing was employed, dressing depth $a_d = 2 \mu\text{m}$, dressing feed rate $v_{fd} = 1.2 \text{ mm/s}$, dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 45 \text{ m/s}$, work speed $v_w = 20 \text{ m/min}$, spark-out $t_{so} = 10 \text{ s}$, and removal rate $Q'_w = 2 \text{ mm}^3/\text{mm}\cdot\text{s}$.

Conventional-Speed Vitrified B151 CBN Wheel

The wheel was rotary disk dressed at a dressing speed $v_r = -12.6 \text{ m/s}$, dressing depth $a_d = 2 \mu\text{m}$, dressing feed rate $v_{fd} = 1.4 \text{ mm/s}$, dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 45 \text{ m/s}$, work speed $v_w = 20 \text{ m/min}$, spark-out $t_{so} = 10 \text{ s}$, and removal rate $Q'_w = 2 \text{ mm}^3/\text{mm}\cdot\text{s}$.

High-Speed B151 CBN Wheel

The wheel was rotary disk dressed at a dressing speed $v_r = -42 \text{ m/s}$, dressing depth $a_d = 2 \mu\text{m}$, dressing feed rate $v_{fd} = 8.5 \text{ mm/s}$, dressing passes $n_d = 2$. Grinding conditions were wheel speed $v_s = 120 \text{ m/s}$, work speed $v_w = 26 \text{ m/min}$, spark-out $t_{so} = 2 \text{ s}$, and removal rate $Q'_w = 2 \text{ mm}^3/\text{mm}\cdot\text{s}$.

Re-dress Life and Cost Comparison

Cost comparisons for grinding Inconel 718 are given in Fig. 9.10. The difficulty of grinding Inconel 718 is reflected in a relatively short re-dress

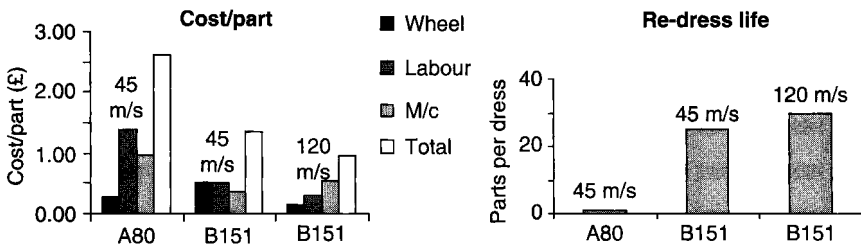


Figure 9.10 Cost comparisons and re-dress life for grinding Inconel 718 with different abrasives and machines.

life. In the case of alumina, it was necessary to re-dress after each part. With CBN at 45 m/s, a re-dress life of 25 parts was achieved and at 120 m/s, this was further increased to 30 parts. For all three cases, removal rate was limited to 2 mm³/mm·s.

The key to reducing costs was to increase the number of parts before re-dress was required. This was achieved by limiting removal rates and raising wheel speed. High wheel speed also allowed spark-out time to be reduced.

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10 Grinding Machine Developments

10.1 Machine Requirements

The main machine requirements are to

- produce accurate parts
- achieve a high output
- be simple to operate
- require minimal maintenance and infrequent overhauls

Consequent to these requirements is the need for

- high machine stiffness and high load capacity
- high accuracy of machine movements
- measures to cope with thermal deflections
- measures to cope with or avoid machine wear
- measures to cope with wheel wear and dresser wear

Stiffness

High stiffness is required to tolerate variations in grinding force and to allow higher removal rates, with small deflections and low vibrations, so as to avoid compromising on workpiece accuracy.

Accuracy

High accuracy of machine movements is required so that the machine can be accurately positioned or programmed to allow accurate control of size, shape, and generated profiles. In peripheral grinding, grinding wheel run-out must be less than 1 μm for micro-grinding and even smaller for nano grinding. In face grinding, face run-out is more important than peripheral run-out. The slide-way motions must avoid tilt errors that may consist of yaw, pitch, and roll errors.

Thermal Deflections

Thermal deflections must be controlled to avoid size variations during a production run. There are two ways to cope with thermal deflections. Either the machine must have very close temperature control or there must

be a way of adjusting machine position for thermal deflections. This latter option is often provided through in-process gauging.

Wear

Precision grinding machines, as with all production machines, are subject to wear. This means there must be a programme of planned maintenance or checking for sustained quality and output. Some machine designs are less prone to the effects of wear than others and this can be an important consideration in machine selection. In-process gauging can overcome the effects of wear to some extent but some types of wear are more serious than others. For example, wear of rolling contact bearings causes irregularity of motion and can seriously detract from the ultimate surface quality. Accuracy of spindle bearings and spindle-bearing condition are critical as little can be done to correct for poor spindle performance.

Table 10.1 lists the elements involved in grinding accuracy. The elements include the machine, the slide positioning, fixtures/work mounting arrangements, the grinding wheel, and the workpiece.

10.2 Grinding Machine Elements

The next few sections consider how machine elements influence accuracy and removal rate. Basic elements and photographs of typical grinding machines are illustrated in Fig. 10.1(a–c).

Basic elements of a horizontal surface grinding machine include

- base and column
- work table, slide-ways, and drives
- wheel head including spindle bearings and down-feed drive
- wheel motor

A cylindrical grinding machine will also include as shown a

- work head including spindle bearings and spindle drive
- tailstock

10.3 Machine Stiffness and Compliances

Definition of Static Stiffness

We must also consider how stiffness affects accuracy. We are particularly interested in the change in workpiece size that results from deflection

Table 10.1 Elements Involved in Grinding Accuracy

Measuring system (measurement resolution, accuracy, and repeatability)
Positioning servo
Machine
Smallest increment of movement (movement resolution, accuracy, and repeatability)
Backlash
Compliances
Geometric accuracy
Thermal deflections
Fixture
Compliance
Thermal deflection
Geometric accuracy
Grinding wheel
Wear
Dressed roundness/run-out/topography
Dressing tool
Wear
Dresser sharpness
Workpiece
Compliance
Thermal deflection

caused by the application of the grinding force. The higher the machine stiffness, the smaller the size error. The main stiffness of a grinding machine is usually defined as normal force/normal deflection.

$$k = \frac{F_n}{x} \quad \text{Static stiffness} \quad (10.1)$$

The deflection of the machine is the sum of all the deflections of the machine elements around the force loop described in greater detail below. In mathematical terms, the deflections add up so that the total deflection $x = x_1 + x_2 + x_3 + \dots$. If there is a large deflection in any single element, the overall stiffness of the machine will be low. Deflection divided by force is the inverse of stiffness and is termed compliance. Compliances like deflections can usually be added. Deflections arise mainly from the bending of machine elements and bearing deflections. In the sub-sections below, the elements around the force loop contributing to the total deflection are each considered in turn.

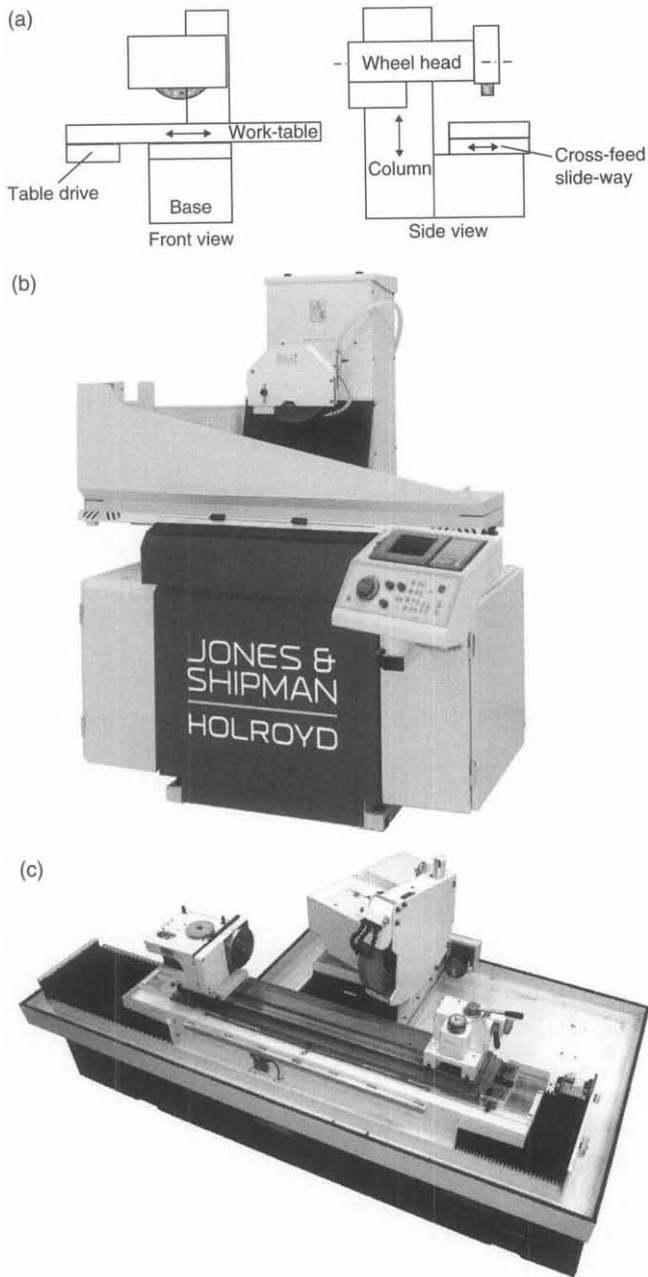


Figure 10.1 (a) The main elements of a horizontal grinding machine, (b) Jones & Shipman—Holroyd 524 CNC surface grinder (with permission), and (c) the basic elements of Jones & Shipman—Holroyd angle-head cylindrical grinder (with permission).

Example 10.1 The normal grinding force is 40 N (or 9 lbf) and the resulting deflection of the grinding machine normal to the work surface is 0.006 mm (or 0.000236 in.). Evaluate the machine stiffness.

$$\begin{aligned}\text{Machine stiffness} &= 40/0.006 \times 1000 \\ &= 6.67 \times 10^6 \text{ N/m or } 38,100 \text{ lbf/in.}\end{aligned}$$

Damping

The ability of a machine to withstand vibrations depends not only on machine stiffness but also on machine damping. Damping is a term used to describe the mechanism of energy dissipation within a machine. An example of a damping mechanism is shear of an oil film or shear of a grease film. Hydraulic damping mechanisms are employed in automobile suspensions to damp out road bounce. Another damping mechanism is plastic shear of the asperities on a friction surface. In all these examples, energy is dissipated as heat.

A highly damped machine feels much stiffer than a lightly damped machine even if stiffness is the same. Vibration amplitudes tend to be smaller. Joints, bearings, and slide-ways reduce stiffness of a machine but compensate by providing damping. Damping also arises from internal friction within the materials from which the machine is constructed. A welded steel structure will be lightly damped. A cast iron machine has slightly greater damping. Composite materials tend to be more highly damped. But, in practice, damping mainly arises from the joint interfaces in slide-ways and through clamped joints.

C-Frame and U-Frame Structures

Machines can often be approximately represented as a C-frame as in the case of a horizontal grinder or as a U-frame as in the case of a cylindrical grinder. Both shapes have cantilevered members. Normal grinding force tends to open out the gap between the cantilevers as illustrated in Fig. 10.2. The horizontal grinding machine very roughly forms a letter C as represented in Fig. 10.2.

Slide-Ways and Bearing Deflections

Deflections associated with slide-ways and spindle bearings may be substantial. For example, the effect of deflection in the wheel head height adjustment slide-way is illustrated in Fig. 10.3.

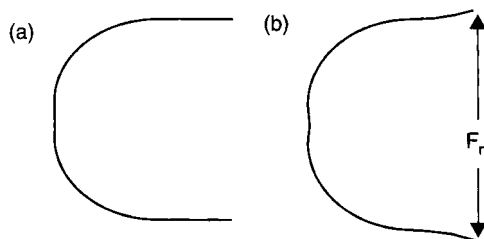


Figure 10.2 Deflections due to bending in a C-frame. (a) Undeformed shape and (b) Deformed shape.

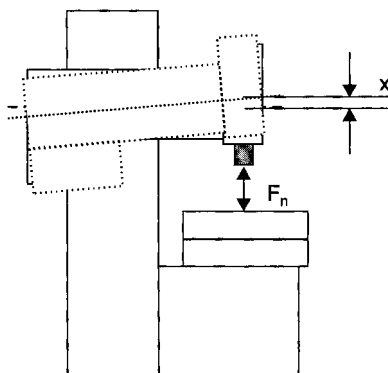


Figure 10.3 The effect of deflection in the wheel head height adjustment slide-way on normal deflection at the grinding point.

Compliances and the Force Loop

Overall stiffness of a machine depends on all elements acted on by the normal grinding force. For example, Fig. 10.4 shows the main elements of a centreless grinding machine (Rowe 1974; Marinescu et al. 2007, chapter 19).

The main elements comprise a force loop shown schematically in Fig. 10.5. The elements in the force loop include

- the machine base and table
- the grinding wheel head and spindle bearings
- the control wheel head and spindle bearings
- the in-feed drive

Each element in the force loop adds to the total compliance. Typical values of compliance are shown in Fig. 10.6 for each element. Low-speed plain control wheel bearings are seen to be very compliant particularly at light loads under finishing conditions. At higher grinding forces, the

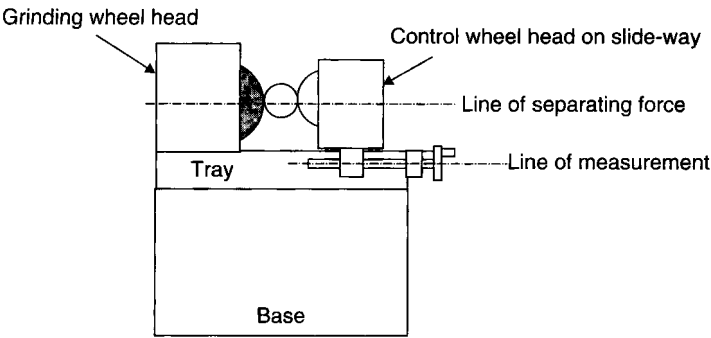


Figure 10.4 The main elements of a centreless grinding machine.

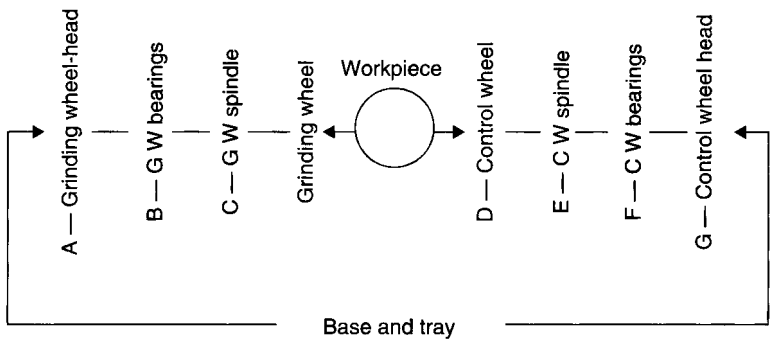


Figure 10.5 Elements comprising a force loop for centreless grinding.

compliance is reduced. Unfortunately, high compliance under finishing conditions makes it difficult to achieve a final size. Under finishing conditions small variations in grinding force lead to large size errors. Replacing the control wheel spindle unit by a stiff hydrostatic bearing spindle greatly reduced compliance and gave much improved size-holding capability.

The next most compliant element is the rubber-bonded control wheel. The grinding wheel compliance is not shown although this is slightly lower than the compliance of the control wheel (Rowe 1974).

Example 10.2 Estimate the overall stiffness of the centreless grinding machine shown in Fig. 10.6.

$$\begin{aligned} \text{Total compliance} &= 0.007 + 0.012 + 0.012 + 0.047 + 0.015 \\ &\quad + 0.078 + 0.015 = 0.186 \mu\text{m/N} \\ \text{Overall stiffness} &= (1/0.186) \times 1000 = 5400 \text{ N/mm or } 30700 \text{ lbf/in.} \end{aligned}$$

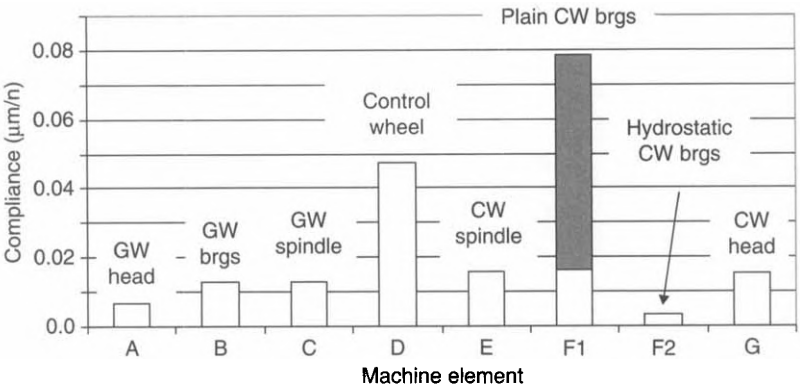


Figure 10.6 Measured compliances for a centreless grinding machine.

Improvement of Grinding Performance

The grinding performance of a grinding machine can be greatly improved by increased machine stiffness. The effect of stiffness in a real grinding situation can be shown by the stiffness factor K described in Chapter 2, where K is the ratio of real depth of cut to set depth of cut. A low stiffness factor reflects high grinding forces and low machine stiffness. The following values were measured for a conventional centreless machine compared with an improved stiffness machine. The values were obtained when plunge grinding steel workpieces of 25 mm diameter by 50 mm length over a 20 s grinding cycle. It can be seen that replacing the compliant bearings by stiff hydrostatic bearings more than doubled the stiffness factor.

Conventional machine $K = 0.23$

Improved stiffness machine $K = 0.44$

The effect on spindle deflections during a grinding cycle is shown in Fig. 10.7. The stiff machine deflections reduced rapidly in the spark-out period allowing close size control. The compliant machine deflections reduced slowly and deflections remained at the end of the spark-out period.

Improvement during Spark-Out Time

Roughness, roundness, and size variations all reduce during spark out. Spark out is a term used to describe a period of dwell after in-feed and is

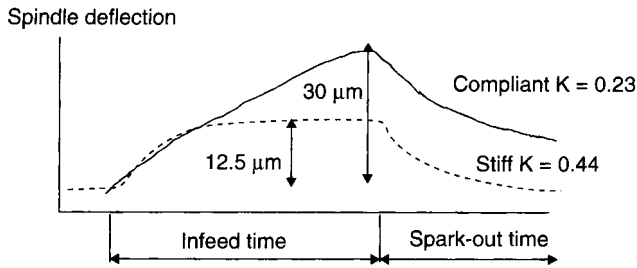


Figure 10.7 Comparison of regulating wheel spindle deflections during a plunge grinding cycle.

employed to achieve a required size tolerance or roundness quality. A stiff machine reduces errors to a negligible level during spark out much faster than a compliant machine. Figure 10.8 shows the improvements in accuracy gained during spark out with a higher stiffness machine compared to a compliant centreless machine.

Size variations and roundness errors are reduced to a negligible level in half the dwell period required by the compliant machine.

Although surface roughness reduces during spark out, it is slightly greater for a stiff machine than for a compliant machine. Experience shows that stiff systems tend to produce slightly higher roughness in grinding. It appears that a stiff system imposes vibrations into the surface more than a soft system. However, a soft system requires a longer cycle time, which is undesirable. The difference in roughness is small and is easily reduced if required using a slightly harder bond or a slightly smaller-grain wheel. The requirement for low roughness with a stiff system is that abrasive grain cutting depths are small and uniform. This can be achieved with high quality small-grain wheels and a low level of vibrations. An advantage of stiffer machines is better capability of using finer abrasives for a given removal rate. Thus, high stiffness is helpful for low roughness, improved accuracy, and improved production rate.

10.4 Design Principles for Machine Layout

For high accuracy and removal rate, structural design should be guided by the following principles:

- The line of measurement should be coincident with the line of separating force for grinding (Abbé Principle minimises tilt errors)

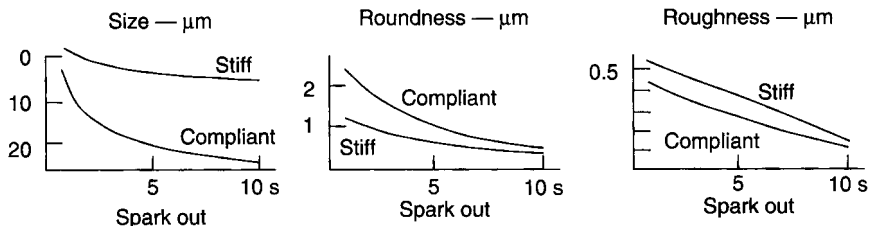


Figure 10.8 Improvements in accuracy during spark out by comparing a stiff centreless grinding machine with a compliant machine.

- The neutral axis for bending should be coincident with the line of separating force (minimises errors due to bending)
- The dressing point should be along the line of separating force (minimises tilt errors in dressing)
- Bearings should have high stiffness and be subject to low wear
- Feed drives should have high accuracy position resolution and be well damped
- Temperature control should provide stable positioning
- Size measurement should allow automatic size compensation
- Access to the machine working area for part feeding and machine set-up needs to be convenient

These principles apply equally to high production rate machines and to ultra-precision grinding machines. Unfortunately, some of these principles offer conflicting challenges. For example, several attempts to build ideal machines based on new kinematic principles have failed to be truly successful because of poor machine access for part loading, wheel changing, and machine setting.

An example of a centreless grinding machine originally designed in 1972 for high stiffness and high accuracy is shown in Fig. 10.9 (Rowe 1979). The frame of the machine rests on a base but the frame has its own structural integrity. This ensures that the separating force due to grinding lies along the neutral axis for bending. In pure bending, the neutral axis remains the same length. As a consequence, machine bending does not affect workpiece size or roundness.

The line of measurement is the line that senses position and provides feed motion. Application of the Abbé Principle means that the measured position coincides with the critical dimension for workpiece accuracy and ensures the highest accuracy possible.

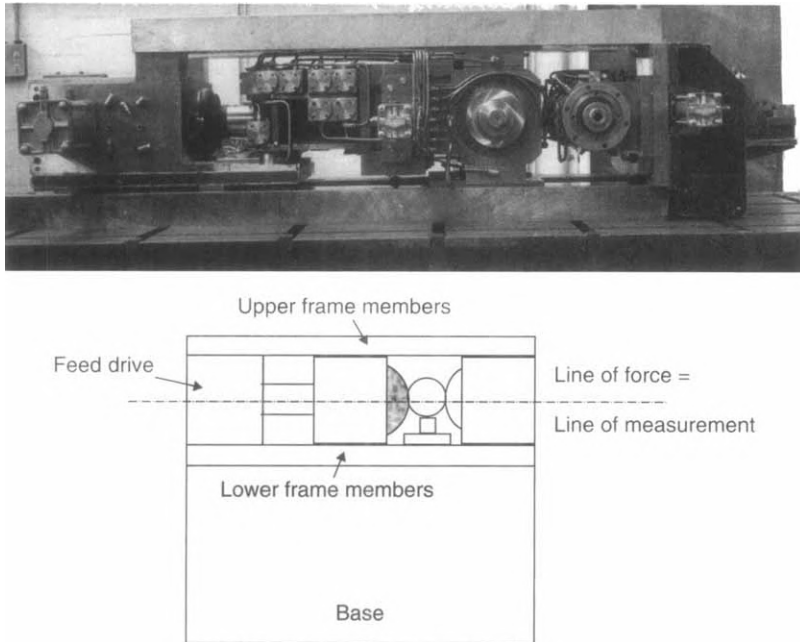


Figure 10.9 A centreless grinding machine designed in 1972 for high stiffness and accuracy.

The stiffness of the above machine was 35 MN/m or 4×10^6 lbf/in. This stiffness is five times higher than the stiffness of a similar sized machine of the layout in Fig. 10.4. The resonant frequency was 500 Hz compared with 78 Hz for the conventional design. This remarkable improvement arises partly owing to the fact that wheels were supported symmetrically between bearings and partly owing to the box structure. These features greatly reduce bending deflections and tilt errors. Errors inherent in a conventional C-frame layout are avoided in a symmetrical box structure. Other features of the high-stiffness machine were the use of water-cooled oil-lubricated hydrostatic spindles and a very accurate hydrostatic feed drive described in Section 10.14.

The high resonant frequency had the great benefit of roundness accuracy being no longer limited by work-regenerative errors as with a conventional machine. The benefits of high stiffness were reflected in improved grinding accuracy over a much wider range of work speeds than is possible with the conventional layout.

10.5 Spindle Bearings and Wheel Heads

Spindle Elements

The spindle-bearing assembly is often termed the heart of a grinding machine because for high-precision machines, the spindle unit may be the limit on attainable accuracy in the grinding process. The whole wheel head consists of

- a spindle
- radial and axial bearings
- bearing seals to prevent lubricant leaking out or grinding fluid leaking in
- a transmission that connects spindle and drive motor
- a wheel head casing for structural stiffness of the wheel head assembly

Spindle Roundness

The spindle must be ground to a high order of roundness. Any roundness error in the spindle leads to motion errors as the spindle rotates. For example, if the spindle is slightly elliptical, the spindle will vibrate at twice the wheel speed.

Spindle Types

There are basically four classes of spindle bearings:

- plain hydrodynamic bearings
- rolling element bearings
- hydrostatic and hybrid bearings
- air bearings

Each has its own advantages and disadvantages as given below (Rowe 1967; Neale 1973; Van Beek 2006).

10.6 Plain Hydrodynamic Spindle Bearings

There are many designs of plain hydrodynamic spindle bearings used for grinding wheel spindles. An example of a simple plain spindle-bearing

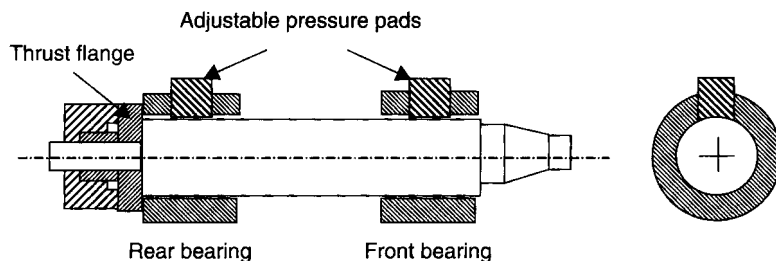


Figure 10.10 Example of a plain hydrodynamic spindle-bearing arrangement.

arrangement is shown in Fig. 10.10. The bearing is basically a cylindrical sleeve where the bore diameter is slightly larger than the spindle diameter to provide a radial clearance. Pressure pads can be screwed down with clearance adjustment screws. Tightening the adjustment screws provides preload against the lower half of the bearing sleeve. Grinding forces in the axial direction are restrained by thrust bearings acting on a thrust flange.

Being a robust and simple construction, it was found that after construction and adjustment the spindle bearings are very reliable in service. Adjustment is not usually necessary for 5000–6000 h in operation.

Hydrodynamic bearings rely on speed to generate hydrodynamic pressure in an oil film. The oil film separates the bearing surfaces and prevents wear. This provides a stiff bearing that has negligible wear during normal running operation. Wear mainly occurs during start-up of the spindle rotation before the oil film is established. However, as some oil is retained in the bearing gap, starting and stopping wear is usually very small.

At one time, plain bearings were often used for work head spindles. At lower speeds of the work head, a plain bearing has lower oil-film stiffness.

The main disadvantage of plain bearing spindles is the high temperature rise that is often experienced. Temperatures may rise as high as 50°C for hydrodynamic grinding spindles. Oil flow and oil cooling must be sufficient to prevent excessive temperature. Sometimes, it is necessary to introduce water pipes to circulate cooling water around the bearings.

Data for plain bearing design is widely available in the literature (Neale 1973; Van Beek 2006). In practice, it is necessary to build a prototype and test it for satisfactory performance.

10.7 Rolling Bearings

Rolling bearings are widely used in grinding machines. Performance of rolling bearings has steadily improved over the years due to improvements

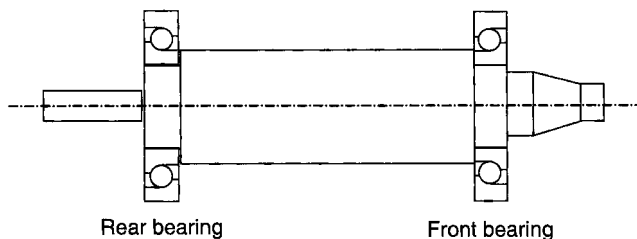


Figure 10.11 Angular contact spindle bearings.

in design. High quality bearing steels give longer bearing life and improved accuracy. In recent years there has been an increased trend towards the use of high-accuracy ceramic balls in hybrid rolling element bearings. Angular contact ball bearings may be pre-loaded to eliminate clearance and provide relatively low temperature operation with reasonable stiffness. Angular contact bearings have the additional advantage that radial and axial forces are restrained. For heavy duty machines, multiple rows of bearings may be employed to provide increased stiffness and load capacity. A spindle arrangement with angular contact bearings is shown in Fig. 10.11.

It must be remembered that rolling element bearings wear progressively in operation. Each ball is subject to a cyclic loading as it makes repeated journeys through the highly loaded position. The cyclic loading causes fatigue wear. As wear develops, vibration levels in rolling element bearings progressively increase until the point of failure. The bearings should be replaced well before failure to maintain optimum performance.

Lubrication requirements for rolling element bearings depend on such factors as the bearing design, speed, and load. Design data for rolling bearings are widely available from manufacturers who supply precision bearings for grinding spindles. Design information is also available from the *Tribology Handbook* (Neale 1973) or from other sources such as Van Beek (2006).

10.8 Hydrostatic and Hybrid Bearings

Advantages

Hydrostatic bearings are applied for demanding applications where conventional plain bearings and rolling bearings cannot meet the exacting demands of a grinding machine. However, hydrostatic bearing systems tend to be relatively expensive and will therefore be avoided if capital cost and machine simplicity are given priority.

Hydrostatic bearings have several advantages:

- absence of bearing wear under stopping and starting conditions
- absence of wear under running conditions
- repeatable high bearing stiffness
- high load capacity
- low temperature operation if correctly designed
- high accuracy of spindle rotation achievable due to averaging of bearing and spindle surface errors.

Basic Design

A hydrostatic spindle layout is shown in Fig. 10.12 consisting of two 4-recess journal bearings and a flat thrust bearing.

Each recess is separately supplied with pressurised oil through a flow control restrictor. For the configuration shown, this requires 10 flow control restrictors. The oil flows from the recesses through the bearing lands and is vented at low pressure back to the supply tank.

The size of the recesses has an effect on load support and bearing flow. The geometry of the recess is shown in Fig. 10.13. The recess should not be too large or the bearing lands will be too fragile nor should it be too small which will detract from zero speed loads. Recesses are typically 1–2 mm deep.

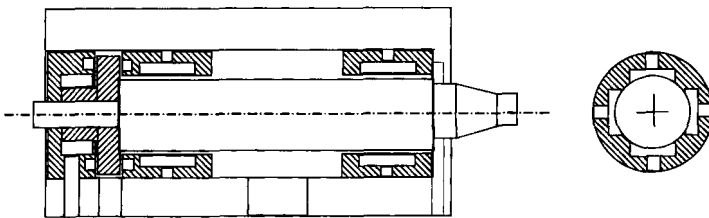
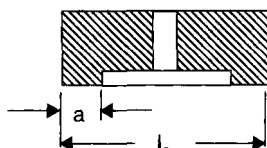


Figure 10.12 A hydrostatic spindle bearing.



Guide to recess size

$$0.1 \leq a / L \leq 0.25$$

Figure 10.13 Guide to recess size.

Filtration is important to prevent restrictor blockage and to prevent ingress of particles that might cause bearing wear.

Restrictors

Restrictors are an essential feature of hydrostatic bearings. Each bearing recess must have a form of flow control device. This can be as simple as an orifice or a capillary. Some designs have more complex control devices designed to provide very high bearing stiffness (Rowe 1970, 1983). Capillary restrictors give best tolerance of temperature rise and are generally satisfactory. The restrictor is usually designed to achieve a recess pressure that is half the supply pressure.

Bearing design is relatively straight-forward (Rowe 1983; Van Beek 2006). Some basic relationships for the main parameters are given below.

Maximum Load

The term “hydrostatic” is applied to a bearing that relies on the supply pressure for load support. The term “hybrid” is applied to a bearing that combines hydrostatic load support with hydrodynamic load support. For a hydrostatic bearing,

$$W = P_s \cdot L \cdot D \cdot \bar{W} \quad \text{Max load for 1 journal} \quad (10.2)$$

where P_s is the oil supply pressure, L is the bearing length, and D is the journal diameter. The load factor $\bar{W}$ depends on maximum allowable eccentricity ratio, length/diameter ratio, number of bearing recesses, recess size, and pressure ratio. Eccentricity ratio is defined as journal displacement divided by radial clearance. Allowable maximum eccentricity ratio is typically 0.5. The pressure ratio β is the recess pressure in the concentric position divided by the supply pressure. The optimum pressure ratio is 0.5. The restrictor for each recess is designed to achieve this value. A typical value of length/diameter ratio is 1. Using these values, the load factor $\bar{W} = 0.25$.

Example 10.3 for max load. Estimate the maximum load carried by a journal bearing $L/D = 1$, $a/L = 0.25$, $L = 60$ mm, $D = 60$ mm, pressure ratio $\beta = 0.5$ at a supply pressure of 2 MPa (or 308 lbf/in.²). Assume a load factor of 0.25.

$$\text{Max load} = 2 \times 10^6 \times 0.060 \times 0.060 \times 0.25 = 1800 \text{ N or } 405 \text{ lbf}$$

Flow rate

Flow rate strongly depends on bearing radial clearance h_o .

$$q = \frac{P_s h_o^3}{\eta} \cdot \bar{Q} \quad \text{Flow rate for 1 journal} \quad (10.3)$$

where h_o is the radial clearance between the journal and the bearing and η is the dynamic viscosity of the oil. The flow rate factor $\bar{Q}$ depends on pressure ratio β , land width a , and bearing diameter D . The flow rate factor is given by $\bar{Q} = \beta \cdot \pi \cdot D / 6 \cdot a$.

Example 10.4 for flow rate. Estimate flow rate for the journal bearing in the previous example where the dynamic viscosity of the oil is 35 cP and the radial clearance is 30 μm .

$$\text{Flow factor } \bar{Q} = 0.5 \times \frac{\pi \times 60}{6 \times 15} = 1.045$$

$$\begin{aligned} \text{Flow rate } q &= \frac{2 \times 10^6 \times 0.00003^3}{0.035} \times 1.045 \\ &= 1.61 \times 10^{-6} \text{ m}^3/\text{s} \text{ or } 0.00161 \text{ l/s} \end{aligned}$$

This is a very low flow rate. For a high speed bearing, the flow rate should be increased to ensure adequate cooling. An adequate level of cooling is assured by optimising the bearing for power ratio as described below. Flow rate can be increased by reducing oil viscosity, reducing land width, and increasing clearance. Clearance has the strongest effect on flow rate.

Average Oil Film Stiffness

A very approximate expression for average bearing stiffness over the whole load range is given by

$$\lambda = \frac{W_{\max}}{h_o} \quad \text{Approx bearing stiffness} \quad (10.4)$$

Example 10.5 for average stiffness. Estimate bearing stiffness for the above bearing example where maximum load is equal to 1800 N and bearing radial clearance is 30 μm .

Rough estimate of bearing stiffness = $1800 / 0.00003 = 60 \times 10^6 \text{ N/m}$
or 340,000 lbf/in.

Concentric Stiffness

More accurate formulae for concentric stiffness based on the type of restrictors, the land widths a and b , and the concentric pressure ratio β are available in the literature (Neale 1973; Rowe 1983). For example, we can

write $\lambda = \frac{P_s(L-a)D}{h_o} \cdot \bar{\lambda}$, where $\bar{\lambda} = \frac{3.82\beta(1-\beta)}{1+\gamma(1-\beta)}$ and $\gamma = \frac{na(L-a)}{\pi Db}$ for a

four-recess journal bearing with capillary control. The inter-recess land width is b . The concentric pressure ratio is β usually made equal to 0.5. Using these expressions, the concentric stiffness is found to be 116 MN/m or 662,000 lbf/in. The concentric stiffness is usually higher than the stiffness at maximum load.

Pumping Power

The power required to pump the oil through the bearing is given by

$$H_p = P_s \cdot q \quad \text{Pumping power} \quad (10.5)$$

Example 10.6 for pumping power. Estimate the pumping power for the above example where supply pressure is 2 MPa and the flow rate is 0.00161 l/s.

$$\text{Pumping power} = 2 \times 10^6 \times 1.61 \times 10^{-6} = 3.22 \text{ W}$$

The pumping power in this example is negligible. As we shall see, continuing on with the design example, flow rate must be increased for a high-speed bearing. A convenient way to do this is to optimise the power ratio.

Friction Power

The friction power H_f dissipated in the journal bearing due to speed depends on speed U , oil viscosity η , radial clearance h_o , and friction area A_f between the journal and the bearing.

$$H_f = \frac{\eta \cdot A_f \cdot U^2}{h_o} \quad \text{Friction power} \quad (10.6)$$

where the friction area is equal to the total bearing area πDL minus $\frac{3}{4}$ of the total recess area.

Example 10.7 for friction power. Estimate the friction power for the above bearing design examples where the rotational speed of the

journal is 2000 rev/min. The axial length of the recesses is 50% of the bearing length and circumferentially the recess is two-thirds, that is, the circumferential land width extends over 30°. The oil viscosity is 35 cP and the radial clearance is 30 µm. The journal diameter is 60 mm.

$$\begin{aligned}\text{Friction area} &= \pi \times 0.06 \times 0.06 \times \left(1 - \frac{3}{4} \times 0.5 \times \frac{2}{3}\right) \\ &= 0.0085 \text{ m}^2 \text{ or } 8480 \text{ mm}^2\end{aligned}$$

$$\text{Bearing speed } U = \pi \times 0.06 \times 2000 / 60 = 6.28 \text{ m/s}$$

$$\text{Friction power } H_f = \frac{0.035 \times 0.0085 \times 6.28^2}{30 \times 10^{-6}} = 390 \text{ W}$$

Power Ratio

While the friction power appears manageable in the above example, it is bad practice to design a hydrostatic bearing where the friction power is 100 times larger than the pumping power. This causes excessive temperature rise and can cause other problems associated with cavitation and load capacity.

The following guidelines apply for power ratio of hydrostatic and hybrid bearings where power ratio $K = H_f/H_p$.

$$1 \leq K \leq 3 \quad \text{Recessed hydrostatic bearings} \quad (10.7)$$

$$3 \leq K \leq 9 \quad \text{Plain hybrid bearings} \quad (10.8)$$

Hydrostatic and hybrid bearings in which the power ratio is adjusted to lie in the recommended range are termed optimised bearings. Such bearings require low power and run relatively cool.

Plain hybrid bearings are hydrostatic bearings having a larger number of small supply holes. Hybrid bearings are designed to take advantage of hydrodynamic pressures that build up on the plain bearing lands which are larger than for conventional recessed hydrostatic bearings (Koshal and Rowe 1980; Rowe et al. 1982). Hybrid bearings carry heavier loads at high speed than pure hydrostatic bearings for the same supply pressure. At low speed, the loads carried are of the same order of magnitude.

Temperature Rise

Temperature rise can be simply calculated from the following expression:

$$\Delta T = \frac{P_s(1+K)}{J \cdot c \cdot \rho} \quad \text{Bearing temperature rise} \quad (10.9)$$

where P_s is the supply pressure, K is the power ratio, J is the mechanical equivalent of heat equal to 1 when using SI units, c is the specific heat capacity of the oil, and ρ is density of the oil.

Example 10.8 For temperature rise. Estimate the temperature rise for a bearing having a supply pressure of 2 MPa and a power ratio $K = 2.0$. Specific heat of the oil is $2120 \text{ Jkg}^{-1}\text{K}^{-1}$ and oil density is 855 kg/m^3 .

$$\text{Temperature rise } \Delta T = \frac{2 \times 10^6 \times (1+2)}{2120 \times 855} = 3.31^\circ\text{C or } 5.96^\circ\text{F}$$

If $K = 100$, the temperature rise would be 110°C . This would be completely unacceptable leading to overheating.

The temperature rise calculated is for a single pass of the oil through the bearing. The oil is then returned and cooled in the bearing supply system before re-entry to the bearing.

10.9 Air-Bearing Spindles

Features

Air bearings have several advantages:

- High rotational accuracy achievable due to averaging of spindle shape errors
- Cool running due to very low friction and refrigeration due to air expansion
- Low drag on the spindle

Air bearings function on principles similar to oil- or water-lubricated bearings. Although air bearings can operate purely hydrodynamically at high speeds, the pressures generated are much lower than in the case of oil-lubricated bearings. To improve load capacity, air-bearing spindles are usually pressurised. Loads carried with external air pressurisation are

lower than with oil hydrostatic bearings because supply pressures are lower. Typical air supply pressures are 3–7 bar (45 – 100 lbf/in.²) whereas oil supply pressures are typically 3–30 times higher.

Compressibility of air is a disadvantage as the spindle can bounce on the air film. Compressibility creates a resonant frequency of the spindle on the air film. This frequency needs to be high enough to avoid problems in grinding. Air bearings are subject to resonance at the natural frequency given by $\omega_n = \sqrt{k/m}$, where k is the bearing stiffness and m is the mass of the spindle and grinding wheel assembly (Powell 1970; Van Beek 2006).

Compressibility can also lead to instability known as pneumatic hammer. Pneumatic hammer is a condition where the spindle vibrates in a self-excited mode on the air film. Pneumatic hammer can usually be avoided by employing a correct design for the bearings.

Basic Design

An example of a layout for an externally pressurised air-bearing spindle is shown in Fig. 10.14. The jet restrictors are shown at an exaggerated size for clarity. Typically, journal bearings will have two rows of eight jets in each bearing. Double row bearings carry much higher loads than single row bearings. The orifices are normally placed at quarter stations along the bearing length as shown. Load capacity is usually based on an eccentricity ratio of 0.5. Further details of air-bearing design can be found in the literature (Powell 1970; Neale 1973).

Restrictors

Restrictors provide the flow control essential to achieve bearing load support. There are three main types of restrictors, as illustrated in Fig. 10.15.

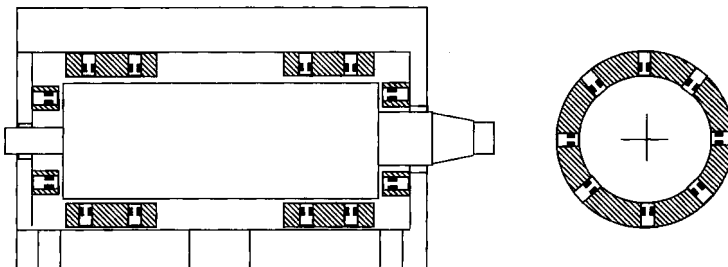


Figure 10.14 Layout of an externally pressurised air-bearing spindle.

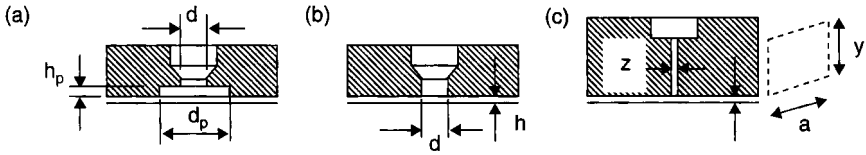


Figure 10.15 Three types of flow restrictors for pressurised air bearings:
(a) Pocketed orifice, (b) Annular orifice, and (c) Slot entry

The pocketed orifice gives the highest bearing stiffness. The orifice diameter may be as small as 0.25 mm (0.01 in.). The orifice area $\pi d^2/4$ must be smaller than the annular entry area into the bearing film $\pi d_p h$, otherwise the restriction is provided by the annulus. While the annular orifice gives lower stiffness, the bearing is less prone to pneumatic hammer. Pneumatic hammer is a condition where the bearing vibrates due to compressibility of the air in the pocket. With pocketed orifices, it is important that the pocket diameter and the pocket depth are very small so as to avoid pneumatic hammer. Typically, the pocket depth is of the order of 1 mm and the pocket diameter of the order of 3 mm.

Slot entry is another type of restrictor used that gives favourable load support by spreading the entry pressures over a larger area of the bearing. The slots that form the restriction are provided by spacers to achieve a very narrow gap z . The gap is of the same order of thickness as the bearing clearance h . The length of the slot is y and the breadth a .

Air Journal Bearing Load

The load capacity with eight jets per row for a double row journal bearing having a length/diameter ratio of 1 is approximately $W = 0.33 \cdot P_s \cdot L \cdot D$. Two single-row bearings each having $L/D = 0.5$ give a combined load of approximately $W = 0.24 \cdot P_s \cdot D^2$.

Example 10.9 Air-bearing radial load. Estimate the load carried by a double-row journal bearing where the diameter is 60 mm, $L/D = 1$, and the supply pressure is 4 bars.

$$\text{Load } W = 0.33 \times 4 \times 10^5 \times 0.06 \times 0.06 = 475 \text{ N or } 107 \text{ lbf}$$

10.10 Machine Base

The machine base has an important function of supporting all the other machine elements. It needs to be a stiff element capable of withstanding deflections applied either through the work table or through the column.

The machine base is essentially a box which is an inherently stiff structure. However, the base is usually hollow to reduce weight and cost. It may also have openings to allow ancillary equipment to be stored inside. Usually, the base allows rocking motions to develop at low frequencies often less than 30 Hz. If the base is not rigid, the distortions may interfere with grinding quality. The base must be sufficiently rigid to provide a stable platform for the machine and may also provide clamping of the machine elements.

It is important that the machine base does not distort when the base is levelled on the machine mounting points on the machine foundation. The machine base is usually mounted on vibration absorbing mounts to prevent low-frequency vibration being transmitted from surrounding machinery. The mounts should absorb vibrations below 10 Hz.

Many machine bases are made from cast iron, although welded steel structures have become more common. Examples of materials used for machine bases and the main advantage include (Marinescu et al. 2007, chapter 15)

- Cast iron—inherent damping properties. Requires a mould and therefore more likely to be used for longer production runs
- Steel fabrication—low damping. Reduced cost for one-off
- Granite—massive natural material. Bolted joints require careful design
- Matrix composites—use of polymer matrix filled with stone

A massive machine base has the advantage that high vibration frequencies are damped out and the base is inherently stiff compared to other elements of the machine. The base should be dimensionally stable with the passage of time to prevent loss of machine alignments.

Other machine elements are bolted to the machine base, and therefore the strength of the fixing bolts must be adequate to cope with machine lifting and repeated machine slide-way movements during operation.

10.11 Column Deflections and Thermal Effects

Bending Deflections

Column bending errors arise for two quite different reasons, although both effects are major. Bending arises due to the grinding force and also due to machine and process heating. A column may be tall and slender as in a

radial drill but for stiffness, it should be short and stubby. A column is usually stiff in pure compression but is much more compliant in bending. If the column is hollow and has large openings for other equipment such as drives and spindle motors, bending deflections are even more apparent.

Thermal Deflections

The effect of a wheel head spindle is to create heat leading to bending deflections as illustrated in Fig. 10.16. Bending may also be caused by warm grinding fluid splashing against the front of the column. A modern machine, as illustrated in Fig. 10.1(b), shields the column from the fluid using a splash guard.

Figure 10.17 shows substantial thermal deflections measured during warm-up on a cylindrical grinding machine as seen looking down in a plan view. The deflections are measured between the wheel and the workpiece.

Bending deflections due to force are minimised by making the column stiffer. Thermal deflections are minimised through the use of splash guards

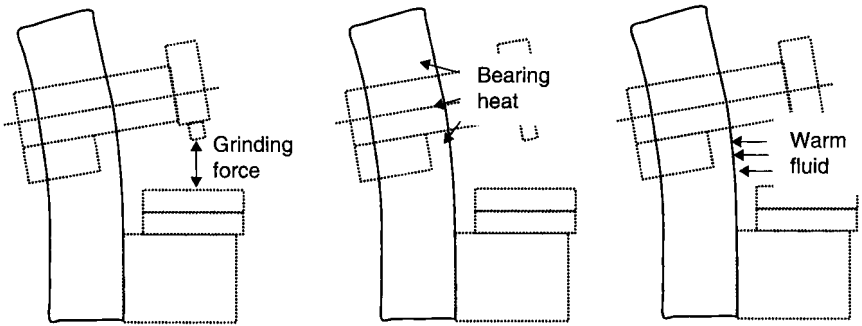


Figure 10.16 Column bending due to grinding force and thermal deflections due to bearing heat and grinding fluid.

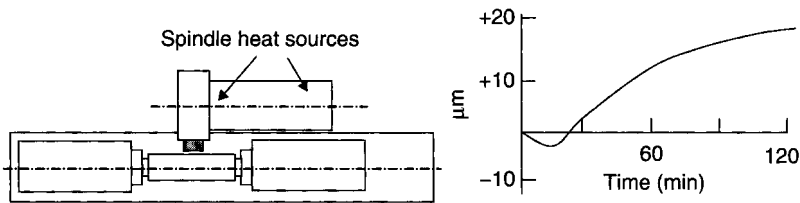


Figure 10.17 Thermal deflections of a cylindrical grinding machine illustrated in a plan view.

to prevent coolant splashing against the column and also by controlling lubricant and coolant temperatures.

The effect of bearing heat can be minimised by controlling bearing temperatures and through equalisation of temperature rise at the front and back of the column. It is better still to thermally insulate the column from the wheel head. For high-accuracy machines, such measures are essential. For ultra-precision machines, a shower curtain of falling oil at controlled constant temperature may be poured down the column to provide thermal insulation against draughts.

Reduction and compensation of thermal errors in machine tools for the achievement of high accuracy requires attention to one or more of the following techniques (Weck et al. 1995)

- isolation of the heat source
- cooling of the heat source
- temperature measurement and cooling to keep temperature constant
- in-process measurement of deflections and position compensation

10.12 Joints, Slide-Ways, and Feed Drives

Feed-Drive Elements

Figure 10.18 shows the basic elements of a work table supported on a slide-way together with the feed mechanism.

The basic elements include

- a slide-way to support the work table and allow motion in the feed direction
- a source of feed motion such as a hand-wheel, a servo-motor, a pneumatic feed cylinder, or a hydraulic feed cylinder
- a transmission such as a lead screw, a lead-screw nut, and thrust bearing(s)
- a position measurement mechanism such as an optical scale.

Positioning Accuracy

Positioning accuracy depends on the position resolution, position measurement, and position control of the feed actuator. Position accuracy

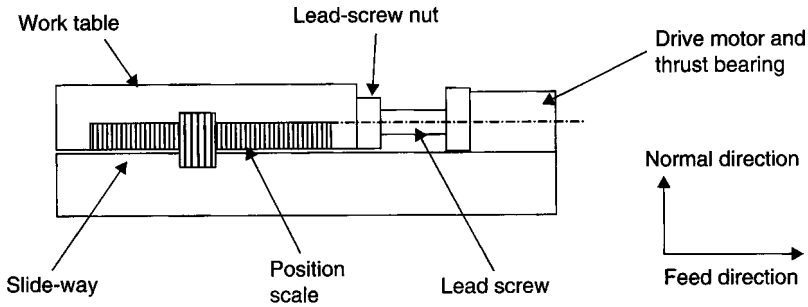


Figure 10.18 Basic elements of a work table, slide-way, and feed mechanism.

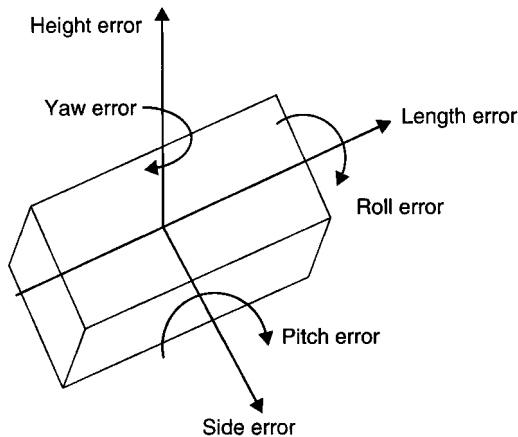


Figure 10.19 Position errors of a work table.

also depends on the guidance of the mechanical elements of the feed drive. The feed drive and slide-way assembly has to provide position accuracy in six ways as illustrated in Fig. 10.19.

Movement Directions

A free body has six degrees of freedom of movement. Each degree of freedom can be eliminated by providing a machine constraint but then the constraint may cause an error. Ideally, a slide-way provides constraint in five directions and only allows movement in one direction. The basic movement controlled by a linear slide-way is the “length.” However, as a work head or tool head moves, the slide-way may allow or cause the head to tilt in any of the three directions shown. These directions are known as

roll, pitch, and yaw and are defined as shown with reference to the basic motion. The slide-way may also give rise to unwanted side and height motions as shown. To summarise, there are errors in three linear directions and in three rotational directions. The rotations give rise to increasing linear errors with increasing radius from the centre of rotation.

Errors arise in a variety of ways due to inaccuracy or deflections in

- slide-ways
- feed drives
- position measurement
- machine deflections due to shifting position of the workpiece mass
- machine deflections due to changes in grinding force
- machine deflections due to thermal expansions

For the highest accuracy, every error must be brought under control. These include thermal errors due to elements such as the thrust bearing flange(s) and other elements easily overlooked (Weck et al. 1995). One of the most important aspects is the accuracy with which the machine elements are manufactured. For nano accuracy it is necessary to pursue every aspect using physical principles that have the capability to achieve accurate movements at sub-micron levels. These may require the use of piezo-electric materials or other thermal, mechanical, or electro-magnetic devices of an unusual kind. Another route to achieving high accuracy of movement is the automatic correction of motion errors using calibration or other in-process measurements of deflections. For the very highest precision, it may be necessary to combine both approaches.

One of the basic sources of error is backlash in the feed-drive mechanism. Backlash is illustrated in Fig. 10.20. Backlash is a common problem

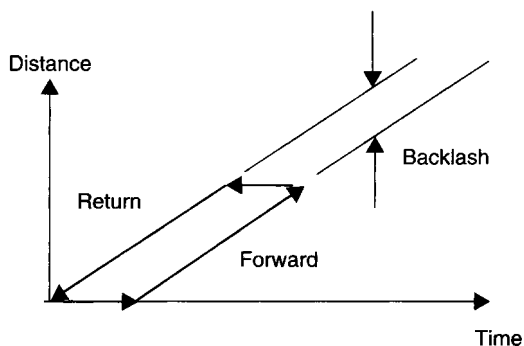


Figure 10.20 Backlash error.

in lead screws and other forms of feed mechanism. It is caused by looseness in the mechanism. Ball screws often overcome the problem by employing pre-loading to eliminate play in the mechanism.

Backlash can also be caused by looseness in thrust bearings and in slide-ways. Another form of error arises due to non-linear deflection across joints.

Joint Deflections

Size accuracy is often controlled by deflection normal to the feed direction. The work table is therefore required to have high stiffness and accuracy in the normal direction across the joint. Sliding, rolling, and bolted joints all reduce stiffness compared with a solid (Connolly and Thornley 1967). This effect is illustrated in Fig. 10.21.

The two surfaces of a joint have to “bed in” before stiffness approaches the stiffness of a solid. Contact under light loads takes place on just a few asperities. These asperities are subject to plastic shear. At very high loadings, the contact area increases and the joint is increasingly supported by elastic deflections. This is the reverse of experience with a solid where initial deflection is elastic and deflection becomes plastic as the solid starts to fail.

The bedding-in process across a joint causes a non-linear load deflection characteristic where the initial stiffness is low and then increases with further loading. An unwanted effect is difference in loading and unloading

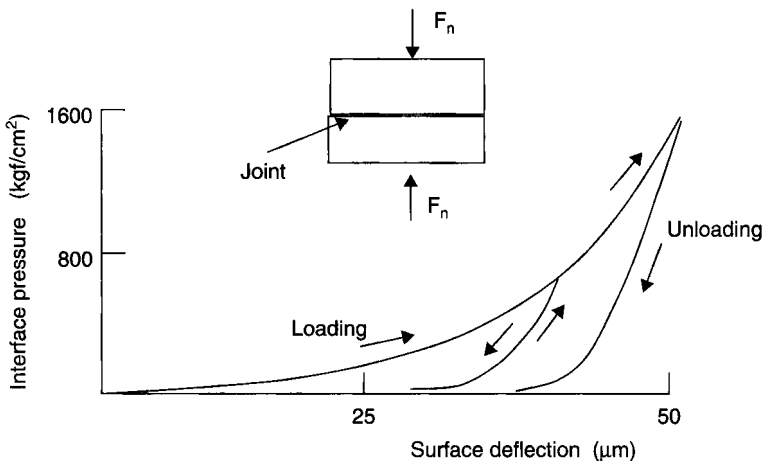


Figure 10.21 Load deflection characteristics across fixed and sliding joints.

characteristics. The bedding-in that takes place in the loading phase is irreversible so that the unloading curve follows a line of higher stiffness. However, when the position of a sliding joint is moved, the bedding-in has to be repeated. The loss of stiffness is further exaggerated due to lubricant between the joint faces.

An advantage of a joint, if the stiffness effects can be tolerated, is that a joint introduces vibration damping into the structure due to plastic shear deflections.

Slide-Ways

Modern computer-numerical control (CNC) machines are designed for speed and accuracy. The slide-way has to allow movement in the feed direction with relatively low friction and absence of stick-slip. As with spindle bearings, the range of possibilities include plain bearing slide-ways, rolling element slide-ways, hydrostatic slide-ways, and air-bearing slide-ways. Many of the considerations are the same as for spindle bearings. There are several configurations of slide-ways commonly employed. These include vee and flat slides, double vee slides, square and flat, double square and round slides.

Plain Bearing Slide-Ways

Plain bearing slide-ways are mostly used for manual feed drives and hydraulic feed drives. However, plain bearing slide-ways can be subject to stick-slip depending on the friction characteristics of the materials used, the stiffness of the drive mechanism and the driven mass. Stick-slip usually occurs at low speeds if the friction reduces as the speed increases. At first the friction resists motion and deflection builds up in the feed mechanism. When the feed force builds up sufficiently to overcome the friction, the table moves forward with increased speed. The increased speed causes reduced friction and the table shoots forward overtaking the feed drive. The table then stops again until the feed motion builds up again and the cycle is repeated. Stick-slip makes it extremely difficult to achieve high orders of position accuracy. Stick-slip can sometimes be overcome by choice of suitable materials and use of non-stick coatings.

It is important when using plain bearing slide-ways that the clearance is correctly adjusted. Too little clearance causes tightness and makes accurate positioning impossible. Too much clearance allows backlash and side-ways movement that also makes accuracy impossible.

Rolling Element Slide-Ways

Rolling element slide-ways offer lower resistance to motion in the feed direction than plain slide-ways. They also offer lower damping both in the feed direction and in the normal direction. Insufficient damping can lead to vibration problems. This is sometimes overcome by inserting plain rubbing strips into the slide-way surface made from a composite material that offers a degree of friction and hence damping. Caged needle bearings may be employed and may be considered to offer the smoothest motion. Re-circulating rolling element bearings are common and allow a substantial table movement avoiding the problem of the rolling elements jamming up at one end of the slide motion. Re-circulating rolling element slide-ways are widely used for precision CNC machines although such bearings tend to be subject to wear of the return tubes that are used to re-circulate the balls. Wear debris produced can enter the bearing and shorten bearing life. For the same reasons as with plain slide-ways, it is important that clearance is correctly adjusted.

Hydrostatic Slide-Ways

Hydrostatic bearings tend to be reserved for machines that require the highest accuracy (Rowe 1974). Hydrostatic bearings have the advantage of very low friction at low speeds and allow a work table to be positioned with extremely high accuracy. There is also the advantage of very high damping and stiffness in the normal direction to the feed motion. The further advantage is the absence of wear.

Design. The design follows similar reasoning to the design of spindle bearings described above. A restrictor must be employed for each recess; otherwise, the bearing will have zero film stiffness. The mean load is usually designed for a recess pressure $P_r = \beta \cdot P_s$ where $\beta = 0.5$ and P_s is the supply pressure. This allows loads to be safely increased or reduced by two-thirds of the mean load.

The basic design of hydrostatic pads is straightforward for thin land bearings of either circular or rectangular shape where the ratio for land width/minimum pad diameter lies in the range 0.1–0.2.

$$W = P_r \cdot A_e \quad \text{Hydrostatic load} \quad (10.10)$$

where A_e is the effective area of the pad $(A_{\text{total}} + A_{\text{recess}})/2$.

$$Q = P_r \cdot h^3 \cdot \bar{B} / \eta \quad \text{Hydrostatic flow rate} \quad (10.11)$$

where h is the film thickness, $\bar{B} \equiv \frac{L_e}{12.a}$ is the pad flow factor, a is the land width, and the effective circumference of the flow path is approximately equal to the mean circumference $L_e = (L_{\text{total}} + L_{\text{recess}})/2$.

Example 10.10 for a hydrostatic plane pad. Estimate the load and flow rate for a recessed circular hydrostatic pad where the recess pressure is 20 bar, the pad diameter is 50 mm, the recess diameter is 40 mm, the film thickness at the mean load is 25 μm , and the oil viscosity is 35 cP. The mean load is to be designed for $\beta = 0.5$.

$$\begin{aligned}\text{Effective area } A_e &\approx \pi \cdot (0.050^2 + 0.040^2) / 2 \times 4 \\ &= 0.00161 \text{ m}^2 \text{ or } 1610 \text{ mm}^2\end{aligned}$$

$$\text{Mean load } W = 0.5 \times 2 \times 10^6 \times 0.00161 = 1610 \text{ N or } 362 \text{ lbf}$$

$$\text{Max load } W_{\text{max}} \approx 1610 \times 5/3 = 2683 \text{ N or } 603 \text{ lbf}$$

$$\text{Min load } W \approx 1610 \times 1/3 = 537 \text{ N or } 121 \text{ lbf}$$

$$\text{Mean flow width } L_e \approx \pi(0.050 + 0.040)/2 = 0.141 \text{ m}$$

$$\text{Flow factor } \bar{B} = 0.141/12 \times 0.005 = 2.35$$

$$\begin{aligned}\text{Flow rate } Q &= 0.5 \times 2 \times 10^6 \times 25^3 \times 10^{-18} \times 2.35/0.035 \\ &= 1.05 \times 10^{-6} \text{ m}^3/\text{s or } 0.063 \text{ l/min.}\end{aligned}$$

Hydrostatic bearings will often be chosen where it is desired to produce a few very high quality machines. For large-scale production of machines, cost is a disadvantage.

Air-Bearing Slide-Ways

Externally pressurised air bearings tend to be used for smaller machines where low damping is less of a problem and where there is less danger of air-bearing instability. Friction is very low and air bearings allow a table to be positioned very precisely.

Feed-Drive Mechanisms

Feed-drive mechanisms mainly consist of the following types:

- plain lead screw either manually or hydraulically operated
- hydraulic cylinder drives with servo-valve control

- ball screw and servo-motor control
- linear motors with servo control
- micro and nano drives of various types

Low cost grinding machines traditionally had plain lead screws. Machines for production were more likely to have hydraulic cylinder drives. In recent years, machines are increasingly computer controlled and operate with ball screws and servo-drives.

For very high table speeds up to 1 m/s or higher, linear motor drives have been introduced. These tend to be supplied as a package including a drive control. At such high speeds, careful attention has to be given to acceleration and deceleration times and distances. Acceleration and deceleration require high power in the control system to cope with the substantially increased acceleration forces. Acceleration forces cause significant machine deflections.

For nano accuracy, special requirements come into play and novel mechanisms are used for feed drives. These may be pneumatic, hydraulic, thermal, or electromagnetic. The field is still in its infancy and a standard practice has not yet been established.

While it is relatively easy to produce a drive that will feed in sub-micron increments, it is much more difficult to maintain these accuracies over a production run. Most grinding machines have difficulty in achieving micron accuracy over a production run. For production runs, it is usually necessary to rely on in-process or post-process monitoring with automatic position compensation.

Feed-Drive Controls

The feed-drive control in a modern machine comprises a servo loop. Typical elements of a closed-loop system are illustrated in simplified form in Fig. 10.22. The machine CNC directs input to a servo-driver of a servo motor. Depending on the logic of the CNC and of the servo-driver, current is provided to the servo motor to produce the required position, velocity, and acceleration of the work table. Feedback is required and may be provided in the form of position and velocity measurements. The feedback is compared with the input commands to ensure control of the output position, velocity, and acceleration parameters. For accuracy, particularly when following curved forms, it is necessary to limit the velocities and accelerations. It has to be remembered that acceleration causes additional forces on the feed mechanism that result in machine deflections.

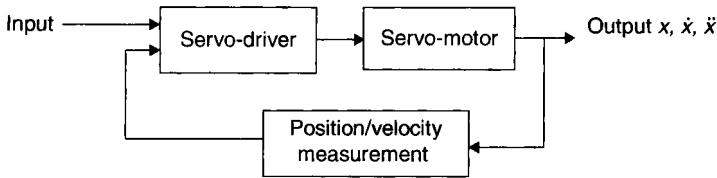


Figure 10.22 Feed-drive control.

10.13 Trends in Grinding Machine Development

High Wheel Speed Grinders

High wheel speeds have advantages in many applications. Case studies in Chapter 9 showed that increased capital cost is more than offset by improved productivity. Trends towards higher wheel speed are described in Chapter 6. Chapter 6 lists machine requirements for high wheel speeds. Further information is provided in the chapters on grinding wheels, dressing, costs, and kinematics. The advantages range from improved grinding quality to improved removal rates.

High wheel speed grinders require high-powered spindle motors, high stiffness and stability, low vibrations, stronger wheel guarding, dynamic wheel balancing, high wheel bursting speed, high dressing speed, and high-velocity fluid delivery. Limiting conditions for thermal damage are described in Chapter 7 and prediction of grinding temperatures in Chapter 18.

Deep-Form Grinders

Productivity is substantially increased by grinding deep forms in one pass from the solid. This modern trend reduces the number of manufacturing operations required to produce formed parts and is particularly beneficial for hard materials as used in the aerospace industry where creep-feed grinding first became popular.

Deep grinding has developed under the title High Efficiency Deep Grinding (HEDG) and is used in the automotive industry for crank and cam shaft grinding. Creep grinding and HEDG are essentially deep grinding developments based on high wheel speeds. HEDG is also used for form grinding of cutting tools such as drills.

Speed-Stroke Grinders

Fast traverse speeds allow smaller cut depths and cooler grinding. Speed-stroke grinders take advantage of this fact to achieve improved surface quality. At the same time reciprocating grinders waste time when the stroke has to be reversed at the end of each pass. Speed-stroke grinders require much higher table feed rates than are possible with conventional feed drives. Linear motors allow speeds up to and exceeding 1 m/s. The key feature for success in speed-stroke grinding is the ability to achieve rapid accelerations and decelerations at the beginning and end of each pass. This reduces the wasted time that occurs with conventional reciprocating grinders.

Multi-Part Grinders

Grinding becomes extremely efficient when many parts are ground in one operation. For example, a through-feed centreless grinder can grind many needles with continuous flow of the parts past the wheel. In fact one of the earliest examples of multi-part grinding was the circumferential feed of needles around the periphery of a grinding wheel guided by a shoe.

Another example of multi-part grinding is the use of vertical spindle surface grinders to grind flat parts. Vertical surface grinders are extremely efficient because a large area of abrasive can be brought into play at one time on numerous parts. The process is illustrated in Fig. 10.23.

Abrasive inserts are usually attached to the face of the grinding wheel. This arrangement allows a very large surface area to be achieved. High surface speeds of the abrasive allow efficiency and surface quality levels to be maintained. The contact length between the wheel and the workpiece is

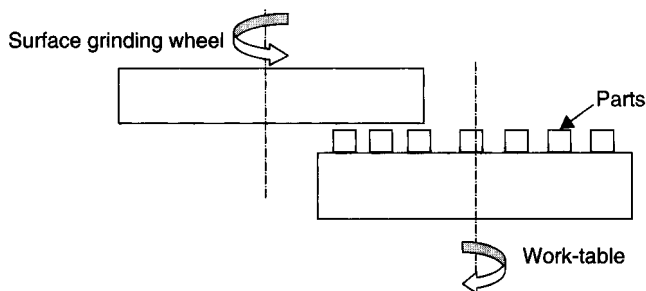


Figure 10.23 Multi-part surface grinding using a vertical spindle machine.

large which limits the removal rate possible by each abrasive grain. However, the extremely large number of abrasive grains cutting simultaneously allows high removal rates to be achieved.

Multi-Tool Grinders

Multi-purpose grinders perform a range of grinding operations that may include, for example, face grinding and external diameter grinding. Multi-spindle machines are required with multiple grinding wheels. Examples given by Hitchiner include a three-spindle turret with grinding wheels on a Voumard grinder and a four-spindle turret on a Tripet grinder (Marinescu et al. 2007, chapter 15). Other examples include simultaneous grinding of internal diameters and external angle-head grinding of a face and angled diameter on a flange.

Flexible Multi-Part Grinding

The ultimate challenge is to integrate grinding heads into a general machining centre. A Campbell grinder introduced turning for a gear face into a machine and achieved a substantial reduction of overall cycle time (Marinescu et al. 2007, chapter 15).

The manufacturing engineer has three basic aims: better accuracy, higher production rates at lower cost, and more flexible reliable manufacture. These three trends are illustrated in Fig. 10.24.

The demands for high accuracy and for high production rates both lead towards larger and costlier machines. To justify the extra cost, there must be sustained production runs of parts for which the machine has been designed such as drills, spindles, bearings, crankshafts, camshafts, gear shafts, balls, and so on. Lead times can be reduced, handling cost reduced, and efficiency increased if several operations can be combined on one

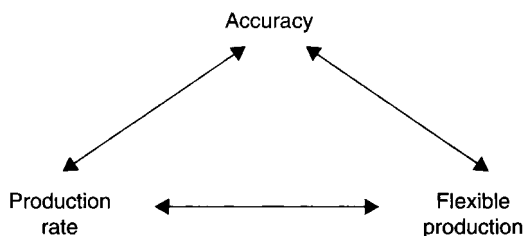


Figure 10.24 Competing trends that influence grinding machine developments.

machine or if operations can be eliminated. Reducing the number of processes and operations remains an ongoing challenge to the manufacturing engineer.

10.14 Ultra-Precision Grinders

Applications

Ultra-precision grinders are mainly required for grinding hard and brittle materials such as hard ceramics although ultra-precision grinders may also be used for conventional hardened steels. Hard ceramics provide a particular challenge as outlined below.

Hard ceramics are employed in applications such as semi-conductor wafers, electro-optical elements, high intensity lighting envelopes, fibre optic cladding, laser scanner face plates, and catalytic carriers (Dudley 1990). The range of ceramic materials needing to be machined is large and includes silicon, sapphire, quartz, silicon carbide, silicon nitride, and cubic boron nitride. Often, it is required to achieve optical properties requiring accuracies much finer than achieved by conventional grinding processes using conventional machines. Ultra-precision grinders are required to avoid material damage.

Basic Principles

Hard and brittle ceramics are damaged by conventional grinding techniques because small conventional cutting depths lead to cracking, chipping, and edge deterioration. The problem can be overcome by plastic regime grinding. The principles of plastic flow in ductile materials are explored in Chapter 17 and in further detail in Marinescu et al. (2004, chapter 5).

The crack failure problem can be overcome by limiting abrasive grain penetration to a very small depth (Inasaki 1987; Malkin and Hwang 1996; Komanduri et al. 1997). An explanation is illustrated in Fig. 10.25 based on indentation mechanics. At small depths of penetration, an indenter causes plastic flow. There is a small hydrostatic region of high constant compressive stress. In this region, material does not flow, whereas outside there is a small region where material flows plastically (Johnson 1970). With increasing indentation and plastic flow, cracks start to grow; median cracks at first and then also lateral cracks that move sideways and up to the surface leading to surface pitting.

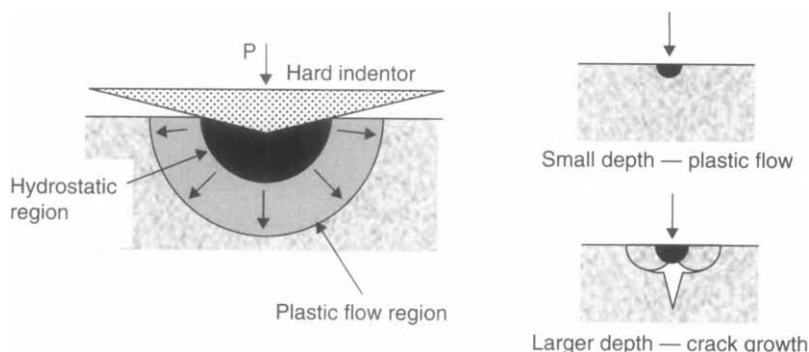


Figure 10.25 Principle of plastic flow and crack growth in brittle materials.

In grinding, conventional grain depths with brittle materials lead to similar cracking behaviour. This is because the plastic region is very small for brittle materials. The result with conventional grinding is poor surface texture and parts that break easily. This led to research to determine permissible cutting depths to avoid such problems. It was shown that plastic grinding behaviour can be achieved at very small cutting depths employing very fine abrasive. This is the basis for ultra-precision grinding.

Some examples below illustrate approaches to ultra-precision grinding based on employing fine abrasives and methods of limiting depth of grain penetration.

Ultra-Precision Centreless Grinder (Rowe 1979; Spraggett 1979; Rowe et al. 1987)

An early machine designed for ultra-precision grinding is outlined in Section 10.4. The feed drive was a hydrostatically lubricated 50:1 wedge cam (Fig. 10.26). The feed-drive feed position resolution achieved was 25 nm. Hydrostatic pads provide high damping and high stiffness of 210 MN/m or 1,200,000 lbf/in.

Ultra-Precision Surface and Centreless Grinders (Yoshioka et al. 1985)

Conventional precision grinders hold size tolerances of approximately 5 μm and have a slide positioning resolution of 0.25 μm (Hahn 1983). The machines designed by Yoshioka et al. for grinding ceramics achieved

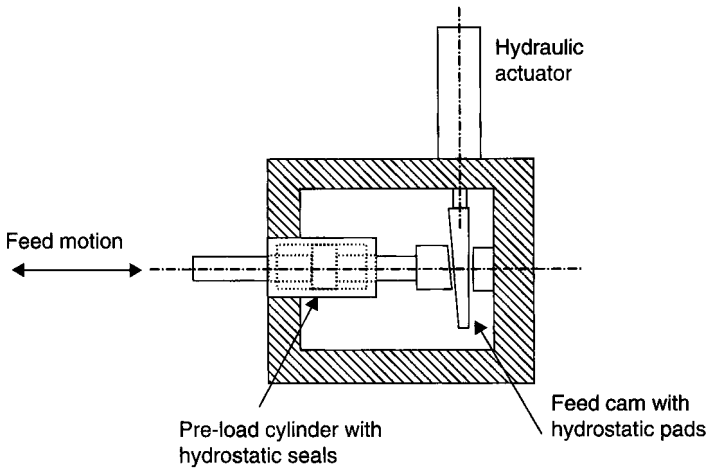


Figure 10.26 Principle of a feed drive employing a hydrostatic 50:1 wedge cam pre-loaded with a hydrostatic cylinder.

size control of approximately $0.2\ \mu\text{m}$ and $0.1\text{--}0.15\ \mu\text{m}$ shape accuracy. Surface roughness achieved when grinding quartz crystal was $2\ \text{nm}$. This represents more than an order of magnitude improvement compared with conventional grinding and takes the process into the nano grinding regime.

Two machines were designed: a vertical spindle surface grinder and a centreless grinder. To achieve this improvement, the following measures were necessary:

- Plastic regime grinding with depth of cut from $0.05\ \mu\text{m}$ for steel
- Plastic regime grinding with depth of cut from $0.005\ \mu\text{m}$ for glass
- Removal rates from $0.03\ \text{mm}^3/\text{mm.s}$ for steel and $0.001\ \text{mm}^3/\text{mm.s}$ for ceramics
- Spindle accuracy $< 0.1\ \mu\text{m}$
- Slide straightness $< 1\ \mu\text{rad}$
- Slide positioning resolution $< 0.1\ \mu\text{m}$
- Repeatability of wheel/workpiece support system $< 0.1\ \mu\text{m}$
- Stiffness of support system $> 100\ \text{MN/m}$
- Height distribution of cutting points on wheel $< 0.2\ \mu\text{m}$
- Shape accuracy of trued wheel, $0.2\ \mu\text{m}$
- Plastic regime wear of the grinding wheels.

Features of the two grinders included the following:

1. A composite hydrodynamic bearing and hydrostatic bearing slide-way arrangement, Fig. 10.27.
2. Fixed spindles with grinding wheels rotating on hydrostatic bearings, Fig. 10.28.
3. A feed drive with load compensation was employed to reduce positioning errors, Fig. 10.29.
4. Micro-true grinding wheels. For centreless grinding, the regulating wheel surface was plastic-regime trued using a vitrified alumina grinding wheel to produce an extremely flat surface and mirror finished by constant-pressure surfacing with a tungsten carbide pad.

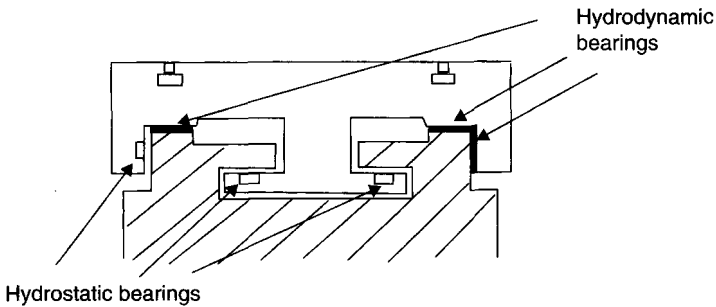


Figure 10.27 Composite hydrodynamic/hydrostatic bearing slide-way.

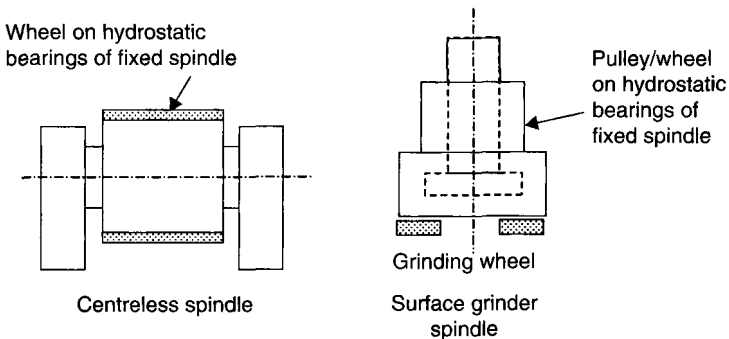


Figure 10.28 Fixed wheel spindles with hydrostatic bearings.

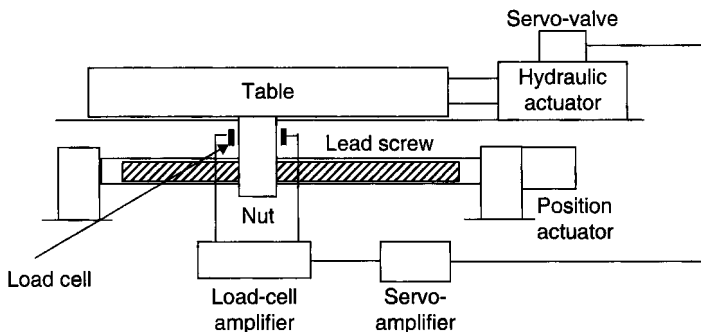


Figure 10.29 Load compensation reduces positioning errors.

Size control when centreless grinding alumina ceramics was $0.2\ \mu\text{m}$. Straightness was $0.1\ \mu\text{m}$ in 30 mm. For a range of ceramics, roundness was in the range $0.15\text{--}0.2\ \mu\text{m}$ and mirror finish was achieved.

The vertical surface grinding machine employed metal-bonded diamond wheels and resin-bonded diamond cup wheels. In both cases, run-out of less than $1\ \mu\text{m}$ was achieved allowing a polished finish to $2\ \text{nm R}_{\text{max}}$ for quartz. Silicon wafers were mirror-finished to $0.8\ \mu\text{m}$ flatness in 100 mm.

Ultra-Precision Grinding Using Ultra-Fine Abrasive Pellets (Ikeno et al. 1990)

Ikeno et al. (1990) describe nano grinding of silicon wafers using a conventional cup wheel grinding machine. Abrasive pellets using ultra-fine silica powders were manufactured by a special process. The problem of producing pellets conventionally is that the ultra-fine powders coagulate due to cohesion of the grains. Effectively, the result is a larger grain abrasive.

Ultra-fine abrasive pellets were produced using a process of electrophoretic deposition to achieve a high bonding strength and a uniform distribution of grains.

It was found that silicon wafers could not be ground using constant feed rate as this produced too great a grain depth. The problem was overcome by placing the wafer on a rubber mat so that grinding was approximately at constant pressure. Mirror finish of less than $10\ \text{nm}$ was obtained. The low bonding strength of the pellets allowed high removal rates to be achieved although grinding ratios were close to or less than 1.

Ultra-Precision Grinding with ELID (Ohmori and Nakagawa 1990)

Ohmori and Nakagawa developed a technique to achieve mirror finish surfaces employing ELID. The new technique of ELID was employed with cast-iron fibre-bonded wheels with micro-grain diamond abrasive. The ELID technique has already been introduced in Chapter 4. An ELID grinding machine may otherwise be a conventional grinding machine. ELID aims to replace lapping and polishing techniques, the benefit of the new grinding technique being a much higher removal rate and high grinding ratio.

Roughness values were of the order of 50 nm, achieved using #4000 CIFB-D wheels. It was reported that ELID grinding allowed stable grinding forces to be maintained whereas conventional grinding with such fine grain wheels leads to increasing forces. Forces were said to be $\frac{1}{2}$ to $\frac{1}{10}$ of conventional forces. Also surface roughness values were reported to be $\frac{1}{3}$ to $\frac{1}{10}$ of conventional values.

Much finer grain wheels may also be employed for lower roughness values. Ohmori and Nakagawa use the description “micro-grain” for wheels having a mesh number up to #10,000. For higher mesh numbers, the wheels are described as “sub-micro-grain.” It was reported that use of sub-micro-grain wheels for in-feed grinding under constant pressure feeding allowed lower roughness to be achieved. This process was termed nano grinding.

Other recommendations included the use of hydrostatic bearings in the grinding machine for accuracy and a feed resolution less than the average diameter of the grains.

Mass Production Double-Sided Face Grinder (KMT Precision Grinding AB, 1998)

The DG500 double-sided face grinder is illustrated schematically in Fig. 10.30. This machine is widely employed in the bearing and automotive industries for grinding the faces of rings as used in rolling element bearings. The machine is highly symmetric and employs a closed-loop, box-shaped, cast-iron machine structure. This box structure is itself mounted onto a simple machine tool base. Combined linear and grinding spindle units are mounted on either side of the machines' upper box structure. The machine design ensures that the force loop is extremely stiff with regard to “tilt” of one side of the machine with respect to the other. Clearly,

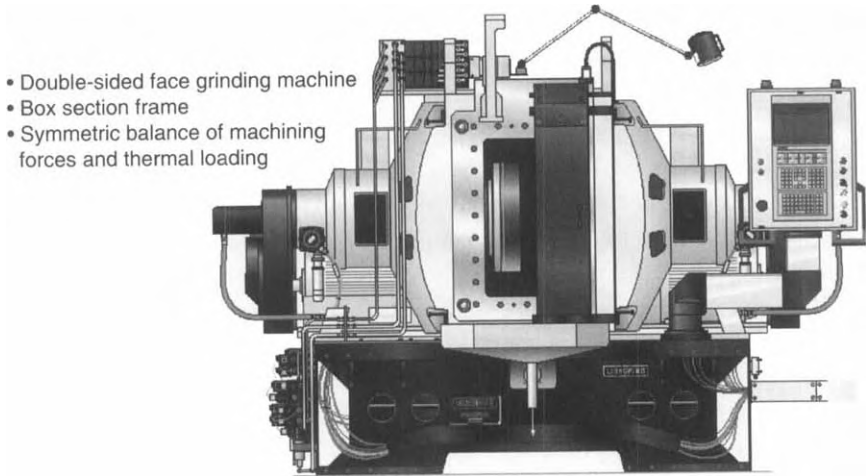


Figure 10.30 Schematic of symmetric double-face grinder (Courtesy: KMT Precision Grinding AB).

in a double-sided face grinder, “tilt” stiffness is an important requirement to ensure parallelism of ground surfaces when grinding at high production rates.

Magnetic Fluid Grinding of Ceramic Balls (Childs et al. 1994)

Magnetic fluid grinding is a process developed in Japan. The process finish grinds silicon nitride balls some 50 times faster than lapping. Magnets are used to apply a constant pressure on a float plate. The float plate loads the ceramic balls against a rotating shaft as shown in Fig. 10.31.

The balls are immersed in a magnetic fluid loaded with diamond abrasive. Large removal rates are attributed to high sliding speeds between the shaft and the balls of several metres per second. The bearing balls typically 6–15 mm diameter are rounded to 0.1 μm . Surface roughness achieved is 5–10 nm Ra.

Ultra-Precision Grinding Machine: “Nano-Centre” (McKeown et al. 1990)

This machine is a three-axis diamond turning and grinding machine. It uses interferometry to achieve position resolution of 1.25 nm and can finish complex shape surfaces for workpieces up to 600 mm in diameter.

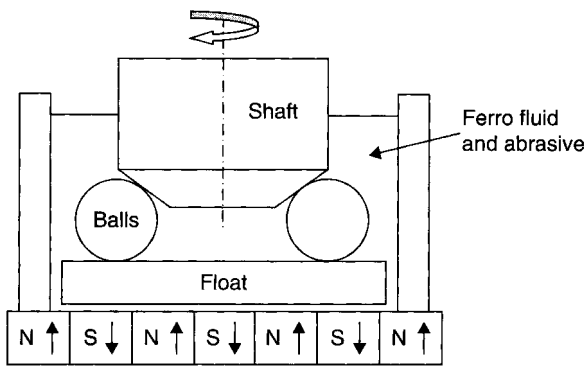


Figure 10.31 Magnetic fluid grinding of ceramic balls.

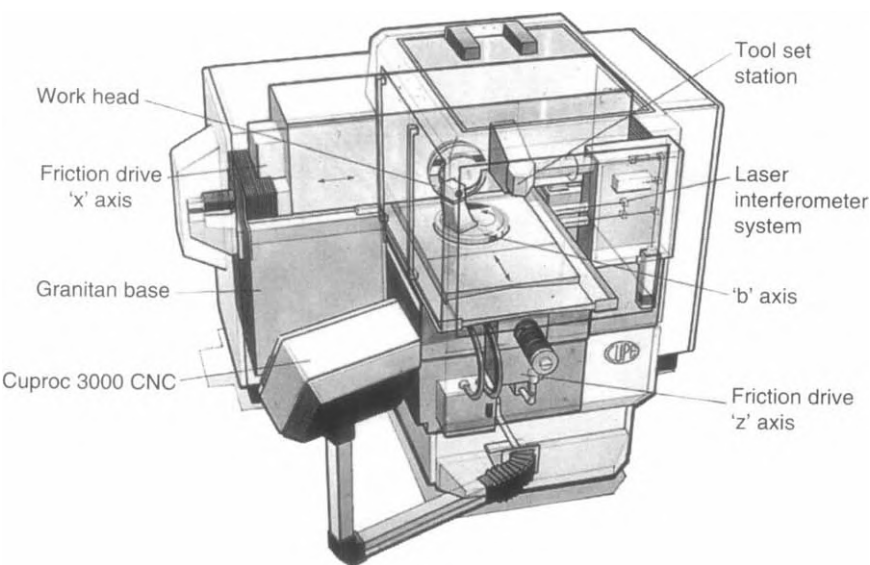


Figure 10.32 Ultra-precision grinding machine: “Nano-centre” (McKeown et al. 1990; Courtesy: Cranfield University).

The machine features friction drives and stiff ceramic guide-way-based hydrostatic bearings. The machine base is constructed from granite-epoxy for dimensional stability, high damping, and high stiffness. Ultra-precision truing and ELID dressing are employed. The machine is operated in a temperature controlled environment and has five specific temperature control systems with a 0.001°C resolution. The machine employs an oil shower over the entire machine to ensure long-term dimensional stability.

Magnetic Memory Disk Combination Grinder (Cranfield Precision Engineering Ltd, 1993)

This inner and outer diameter combination grinder employs smooth running air-bearing technologies for grinding wheels and work head spindle. Air-bearing technology ensures brittle glass or ceramic memory disk workpieces are edge ground with minimal levels of edge damage (chipping). Since this mass production machine demands motions having highly repetitive movement, the linear motions of the grinding spindles are provided through flexures and not from conventional bearing technologies. Size control of the workpieces is maintained through a post-process, non-contact, laser-based measurement system. The post-process measurement data is used to update grinding wheel wear and also to compensate for any long-term thermal distortion of the machine itself. Minimal thermal control is therefore needed on this machine.

Ultrasonic-Assisted Grinding (Uhlmann 1998; Uhlmann et al. 2000)

Ultrasonic-assisted grinding is a technique that can ease the problem of grinding ceramics and other hard brittle materials. The advantages of applying ultrasonic vibration in grinding are mainly reduced grinding forces and increased removal rates. These benefits occur as a result of micro-splitting of the abrasive grains so as to maintain grain sharpness. In ultrasonic grinding, the sizes of the particles removed from the workpiece are smaller than in the case where the vibration is switched off. The chips are thicker but shorter. Roughness is very slightly increased as is wheel wear and residual stress. Also, bending strength is slightly reduced. These effects are small but benefits include reduced grinding temperatures, absence of high sub-surface damage, steady grinding forces, and much greater removal rates. These benefits are claimed to make it possible to achieve highly accurate complex forms.

Kinematic variants of ultrasonic-assisted grinding with axial tool vibration are illustrated in Fig. 10.33.

Where the grinding wheel vibrates, the size of the wheel is limited by the requirement to achieve ultrasonic vibrations of the tool. The grinding wheel forms part of a tuned mass where the frequency has to be adjusted to achieve resonance. Typically, the grinding wheel rotates at up to 6000 rev/min and the ultrasonic vibration has a frequency of the order of 20 kHz.

Further variants are possible where vibrations are applied to the workpiece. In this case the workpiece dimensions must be small enough to

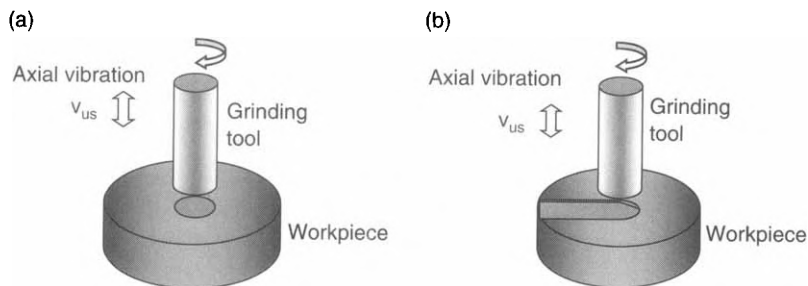


Figure 10.33 Ultrasonic-assisted grinding with tool vibration: (a) Plunge face grinding and (b) Cross-peripheral grinding.

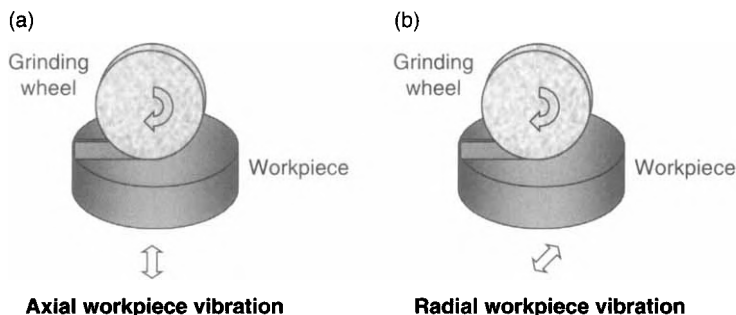


Figure 10.34 Ultrasonic-assisted grinding with workpiece vibration.

make this possible. The vibration can be applied axially to the workpiece surface or radially as shown in Fig. 10.34.

Precision Big OptiX (BoX™) Grinding and Measuring Machine (Shore et al. 2005)

This machine is a dedicated large mirror/lens grinding system. The layout illustrated schematically in Fig. 10.35 is highly symmetric and employs two integrated hydrostatic-based linear motions which are both driven via dual linear motors. The third axis, fixed into the machine base, is an oil hydrostatic bearing rotary table. The machine employs a post-process measuring system whereby measured surface data are referenced to an optical straight-edge mounted on a non-stressed metrology frame. High dynamic stiffness is achieved by minimising moving masses and cantilevers while maximising bearing stiffness and servo-control performance. Long-term thermal stability is achieved by active temperature

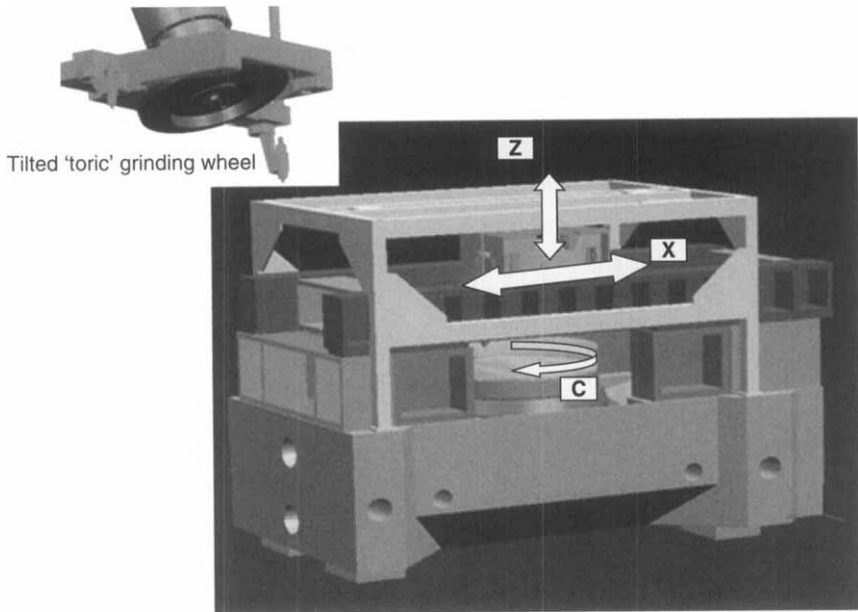


Figure 10.35 BoX™ Ultra Precision Grinder and Profilometer (Shore et al. 2005, with permission).

control of bearing oils to the grinding spindle, linear, and rotary axes. In addition cooling to linear motors and grinding coolant are provided. The machine induces low levels of sub-surface damage into ground surfaces through excellent control of the grinding point at frequencies up to three times that of the operational frequency of the grinding spindle. The BoX™ grinding accuracy is better than $1\text{ }\mu\text{m}$ form accuracy on a 1 m sized part. Surface roughness is 100 nm RMS with sub-surface damage less than $5\text{ }\mu\text{m}$.

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11 Process Control

Although basic machine design is important, here we consider factors that limit process control and methods for improving process control. It helps to be clear about factors that affect accuracy, repeatability, and speed of machining. From this understanding the engineer can select the most appropriate techniques for process control. Two aspects of process control are discussed below.

Basic process variability. Grinding processes are subject to variations in size, shape, surface roughness, forces, deflections, and temperatures. Variations occur owing to varying wheel condition including wheel wear, wheel shape, vibrations, and wheel roughness. Fundamentally, this variability depends on the type of grinding wheel employed, on the process feeds and speeds, and on other factors such as the quality of the grinding machine and the nature of the dressing process.

Control capability. Control capability is the ability of the machine to control feed position, feed rate, and dwell period and to compensate for wheel wear, dresser wear, and temperature rise. Some machines are better at control capability than others and some are better at compensating for basic process variability. Within the basic capability of the machine there is scope for the engineer to adopt strategies for improving control capability. Some strategies are relatively simple, such as the introduction of automatic gauging, whereas others require more sophistication such as the introduction of intelligent control of feed-change points.

11.1 Process Variability

After grinding a batch of parts, measurement shows that some parts are slightly smaller or bigger in size than others. Variations also occur in shape, surface roughness, surface hardness, and surface stresses. The variations are related to grinding forces, mechanical and thermal deflections, wheel wear, and contact temperatures. The process depends on the type of grinding wheel, work material, and grinding conditions. It also depends on the variations that take place during grinding. These are variations in wheel size and sharpness, dresser size and sharpness, and wheel shape and wheel roughness.

Variations due to Wheel Wear

Typical effects of wheel wear are illustrated in Fig. 11.1. Quality progressively deteriorates in the course of grinding as seen in the figure. Variations

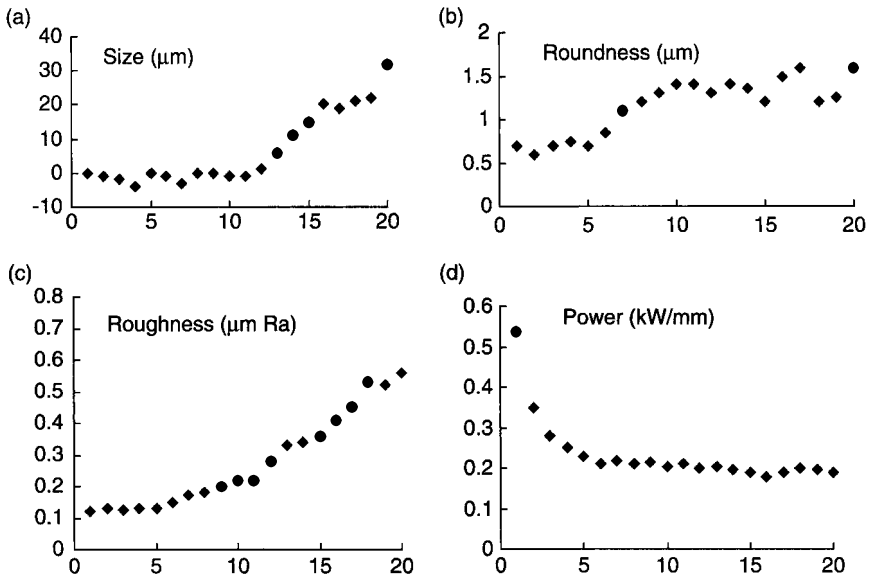


Figure 11.1 Variation of (a) size, (b) roundness, (c) roughness, and (d) grinding power measured at regular intervals for a series of parts. Cylindrical CBN plunge grinding with B91 wheel, AISI 52100 work material, 60 m/s wheel speed and 10 mm³/mm s removal rate. Water-based coolant was supplied at 36 l/min flow rate and 30 bar supply pressure. Total volume of material removed in grinding was 3500 mm³/mm of wheel width.

in size, roundness, and roughness all increase as the number of parts increase. Initially sizes remain reasonably constant within a 5 μm error band and then sizes increase steadily. The overall error band approaches 35 μm. A process control strategy must decide how best to set up the process to meet the designers' part tolerances. Before discussing control, it would be interesting to see why the sizes follow a pattern.

There are two causes of size change. First, as the wheel wears, it gets smaller. Second, the sharpness changes so that forces either increase or reduce. The process trends can be explained from the effect on grinding power. Initially after dressing, power is high but rapidly drops as grinding proceeds. This reduces forces and deflections so that sizes reduce. At the same time, however, wheel wear means a smaller wheel which increases size. Initially, the two opposing trends balance. Later, as power stabilises, the wheel wear trend dominates.

Limits and Tolerances

The conclusion from Fig. 11.1 is that a tolerance of 5 μm cannot be achieved. Of course, if the part tolerance is 10 μm, the process may be

acceptable for a limited period and for a volume removal of $1800 \text{ mm}^3/\text{mm}$ wheel width. If the specified tolerance is $50 \text{ }\mu\text{m}$ the process is acceptable for much longer and for larger volumes of material removal.

Similar consideration can be given to roughness and roundness limits. If the roughness limit is $0.2 \text{ }\mu\text{m Ra}$, the process is broadly acceptable for a volume removal of $900 \text{ mm}^3/\text{mm}$ of wheel width. If the roundness limit is $2 \text{ }\mu\text{m}$, the process is acceptable for a full $3500 \text{ mm}^3/\text{mm}$ removal.

The engineer employs statistical process control to be precise in specifying acceptable and cost economic process conditions.

Size Variations due to Dresser Wear

Each time the wheel is dressed the wheel size is reduced. This means an adjustment is required to the grinding depth of cut to allow for the reduced wheel size after dressing. The adjustment can be made by applying a “position offset” equal to the dressing depth after each dressing pass.

Another method commonly adopted is to use the dressing tool as a datum for re-setting a size. By mounting the dressing tool on the work table, the grinding wheel is automatically advanced towards the work table with each dressing cut. This provides an automatic size adjustment for change in wheel diameter due to dressing. The procedure works very well except that the dresser wear allows errors to build up in the process. In effect, the system acts as though the grinding wheel is smaller than it is because the dresser removes less from the wheel than it should due to wear. The effect of dresser wear is illustrated by the results in Fig. 11.2. The parts progressively become smaller matching the dressing tool wear.

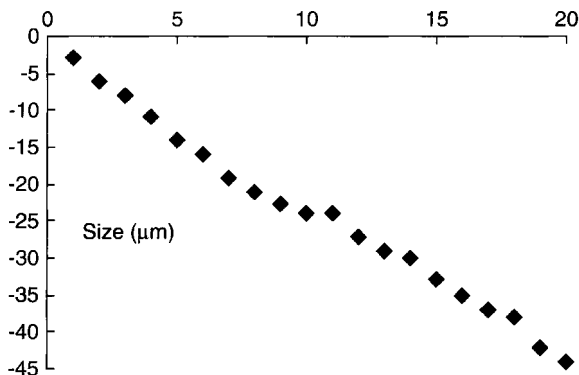


Figure 11.2 Variations in size due to dresser wear with re-dress after each part. Cylindrical plunge grinding. Wheel A465K5V30W: Work material hardened cast steel: Single-point diamond dresser.

The conclusion from Fig. 11.2 must be that the process cannot achieve a tolerance of less than 45 μm over 20 parts when employing re-dress after every part. Replacing a single-point dressing tool with a multi-point tool reduces the errors, but a better method of holding a precise tolerance is to introduce in-process size gauging and compensation.

Process Stabilisation

Where batches are repeated, it is worth seeking to stabilise the forces by optimising the grinding conditions. Process stabilisation may mean that the volume removed can be doubled before it is necessary to re-dress the grinding wheel. Chapter 9 showed that increasing re-dress life reduces costs and improves output. Three ways of increasing re-dress life include

- adjusting dressing conditions—Chapter 4.
- increasing wheel speed—Chapter 9.
- using size gauging and error compensation.

The effects of dressing conditions and grinding conditions such as wheel speed have previously been discussed. Next we consider the application of size gauging and error compensation.

In-Process Gauging

The previous section serves to demonstrate that it is difficult to satisfy small tolerances without error compensation. The most commonly employed form of error compensation is achieved in cylindrical grinding through the use of automatic diameter gauging. A common type of diameter gauge is illustrated in Fig. 11.3. The diameter probe is hydraulically or electrically advanced onto the workpiece during the grinding cycle. Two sensitive fingers contact on either side of the diameter. Movements of the fingers are measured by the diameter gauging unit and are used to provide control signals to the computer numeric control (CNC) unit. Retraction of the wheel is triggered when a finish size has been reached.

The benefits of automatic size gauging are enormous. Some results comparing different grinding cycles with and without automatic gauging are given in Fig. 11.4.

It can be seen that sizes are maintained within approximately 1 μm . Similar benefits can be achieved in surface grinding using a gauge to monitor size after each grinding pass. It is found that size variations in precision

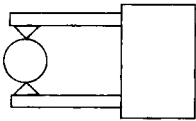


Figure 11.3 Diameter gauging for cylindrical grinding.

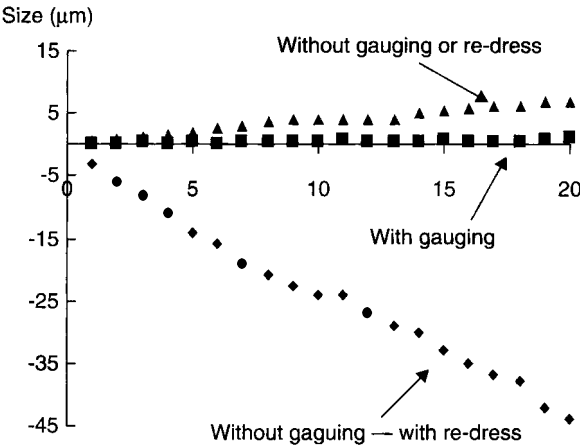


Figure 11.4 Size variations of parts with gauging and size compensation. Cylindrical plunge grinding using an alumina wheel A465K5V30W at 33 m/s. Material hardened cast steel 19 mm diameter by 50 mm wide.

grinding with high-quality gauging approach the accuracy that can be easily measured in a machine shop environment. This can be checked by measuring the same workpiece repeatedly in a standards room over a period of time with one or more operators making the measurement.

Size control also depends on the grinding cycle employed. A typical precision grinding cycle may employ three feed rates. A three-feed rate cycle is shown in Fig. 11.5. The programmed positions are shown as X_p . The actual positions X_r , lag behind the programmed positions due to machine deflections as described in Chapter 2.

The highest feed rate is employed first to remove most of the material. A lower feed rate is then employed to allow the final size to be approached as deflections are reduced. A final very fine feed rate may be employed to achieve close size control and to improve roundness and surface quality. Or in many cases, a spark-out or dwell phase is employed instead. However, a very fine feed has the advantage of ensuring that the desired size is finally reached. With a fixed dwell period, the final size may never be reached and retraction is signalled due to a “time-out” instruction.

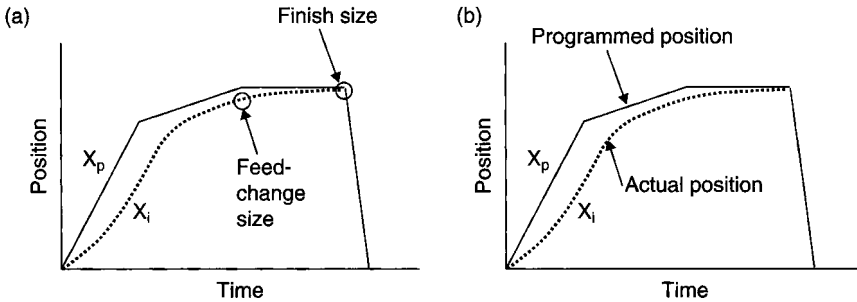


Figure 11.5 Three feed-rate cycles (a) with and (b) without size gauge control.

The control system is programmed to change the feed rates at positions chosen by the programmer. Without size gauging, the programmed positions for the feed change take place at the same feed position for every part. This is not when the part has reached a specified size as size at the instant of feed change depends on the degree of deflection in the system.

When size gauging control is employed, the gauge system triggers the final feed change based on measured size of the part. The signal for feed retraction is given when the size gauge detects that final size has been reached. This difference in the way feed changes are triggered is the essential feature that allows improved accuracy control to be achieved. For the highest accuracy control it is also necessary to ensure that removal rate has dropped to a sufficiently low level before retraction is triggered. If material is still being removed rapidly at this point there is overshoot with the consequence that parts are undersize (Thomas et al. 1995). Roundness also suffers if there is a sudden change in depth of cut as retraction takes place.

Improvements in productivity and accuracy of a process can be achieved by introducing intelligent control techniques as described below.

In summary, accuracy, quality, and removal rate depend not only on the grinding wheel and the work material but also on the grinding machine as described in the previous chapter and also on the machine control system, including the nature of error compensation.

11.2 Classes of Machine Control

Process control these days ranges over the following categories:

- manual control
- switching control

- CNC
- intelligent control

Measurement is critical for all types of control.

Manual Control

An example of manual control is where a human operator provides a feed motion. This may be as simple as providing an increment of cross-feed or down-feed in surface grinding or as complex as following a contour when grinding a form tool on an optical profile grinder. The feature that distinguishes manual control is that a human being has control of measurement, decision making, and actuation. An important decision is deciding when the workpiece has reached its finish size. This implies the use of some measurement device such as a micrometer or a reading on a lead-screw scale. Early grinding machines were all manually operated. Many machines are still manually controlled but almost all machines incorporate elements of automation such as automatic traverse reversal for traverse grinding.

Switching Control

Perhaps the simplest example of switching control is a machine that incorporates switches to reverse the feed motion in traverse grinding. The element of programming in this case is limited to adjusting the position where the switching takes place. This may be achieved for example by moving a toggle switch relative to the machine table. As with manual control, there is an element of measurement in switching control. For example, in traverse grinding, measurement consists of automatically detecting that the reversal position of the toggle switch has been reached.

Switching may be achieved using programmable logic controls (PLCs) and by low-cost computers together with some form of position measurement encoder or linear scale that is used to trigger switching.

The critical step for more advanced automatic controls is the application of servo-drives and ball screws for feed motion. Such programmable controls allow easy adjustment of switching positions in more complex feed cycles. Provision can be incorporated for automatic retraction when a measuring device indicates attainment of finish size.

Computer Numerical Control (CNC)

Modern CNCs vary from low-cost computers that basically provide switching control using servo-drives to full three-axis contouring control that allows complex shapes to be generated. A cylindrical grinder is shown being set up for a CNC operation in Fig. 11.6. The example shown is an easy set-up machine produced by Jones & Shipman Precision Ltd. Other CNCs in the product range provide far more advanced control features.

The process engineer produces a process sheet that specifies the stages in the production of a part together with critical dimensions and the type of feed cycle to be employed. These instructions are fed into the CNC using a code format acceptable to the CNC. After validation of the code, and machine set-up, the CNC operates automatically to produce a part or batch of parts.

Intelligent Control

Intelligent control is where the CNC can change an input process control parameter it has been given to improve process operation. An example of intelligent control is where the feed position for final size is changed in response to a sensor measurement such as a diameter gauge. Intelligent control implies the measurement of a parameter and the inference of a need to change another parameter. The size example is only a simple one. However, in practice, intelligent software can make a large difference to the accuracy and productivity of a machine.

Accuracy and productivity depend on the selection of feeds and speeds. The values that can be selected depend on the work material and dimensions and on other grinding conditions such as wheel specification. This is illustrated by a typical limit chart for a plunge cylindrical centreless grinding process, Fig. 11.7. There are constraints on grinding conditions that may be selected. Constraints are imposed by acceptable surface roughness and re-dress life. This sets a constraint on the maximum power target. A thermal damage limit sets a minimum work speed that is acceptable and will also tend to limit the maximum in-feed rate. The maximum work speed is often constrained by chatter. An approximately optimum set of grinding conditions for this grinding operation is shown as a target where operation takes place at the target power and well removed from the chatter limit and the thermal damage limit.

Intelligent control aims to achieve grinding conditions near optimum.



Figure 11.6 A Jones & Shipman—Holroyd Suprema Easy CNC cylindrical grinding machine during set-up (with permission).

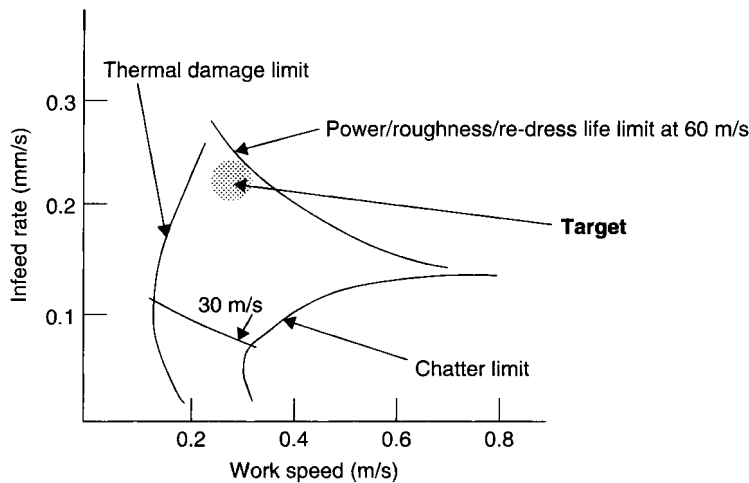


Figure 11.7 Limit chart showing target grinding conditions for high removal rate and acceptable quality. Material AISI 1055. Wheel WA60MVR. Work diameter 50 mm and grinding width 65 mm.

11.3 Intelligent Control of Grinding

Grinding is widely used for finishing parts to achieve close tolerances and low values of surface roughness. However, variations occur as grinding forces change and consequently machining conditions may need to change. Ideally it is required to continually optimise feed cycles to maintain accuracy while also maintaining short cycle times and set-up times. Pre-production trials allow acceptable grinding conditions to be determined but in practice a process is usually operated conservatively to allow for process variability and to ensure consistent quality of a batch of finished parts.

Intelligent control monitors sensors installed on the grinding machine. Sensor information is analysed and grinding conditions continually modified to maintain quality. A schematic arrangement of an intelligent control system is shown in Fig. 11.7 (Liverton et al. 1993). In an earlier version, intelligent routines were located in a personal computer interfaced to the CNC. In later versions, intelligent routines were incorporated directly into an open CNC system (Statham 2000). Intelligent routines introduce a higher level of control. Figure 11.7 illustrates the need to be able to interrupt the lower level CNC control routines.

In Fig. 11.8, two additional sensors are introduced for adaptive control. The first is a diameter gauge and the second is a power sensor. Force sensors can be used instead of a power sensor and may actually give better results. However, power sensing is usually more convenient. A power sensor may already be available from the motor control system.

Other sensors may also be employed for process control. Specialist equipment manufacturers supply equipment for automatic wheel balancing.

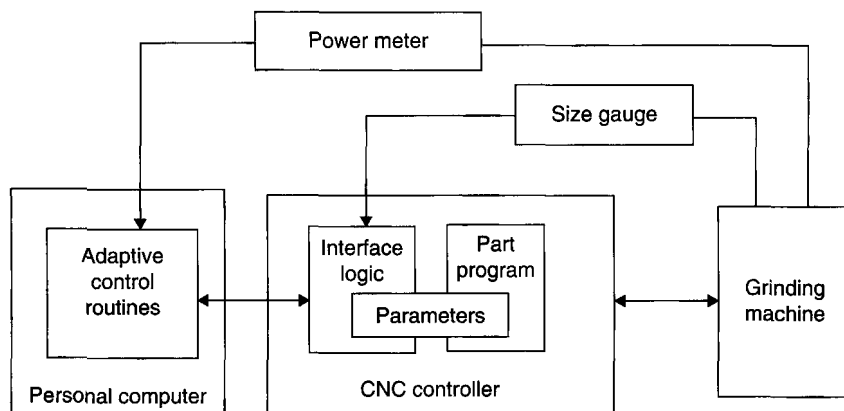


Figure 11.8 Use of a personal computer for adaptive control of power and size.

Other sensors available include acoustic emission (AE) sensors for gap elimination which are available in the same way as diameter gauging from specialist manufacturers for use with grinding machines. It is also possible to incorporate vibration sensors or temperature sensors in the machine.

Many CNCs have provision to accept diameter gauging inputs to adapt feed rate change points. It is now increasingly common for CNC grinding machines to feature power sensors. Some manufacturers simply display a power signal so that an operator can see when the process is drifting out of control. Adaptive power control monitors maximum power in a grinding cycle and takes decisions to maintain process accuracy at higher removal rates. For example, the decision may be the duration of the dwell period to be employed for spark-out.

Adaptive Strategy

A strategy for maintaining grinding conditions is illustrated in Fig. 11.9.

At the start of a new batch, initial grinding parameters are selected automatically where there is previous relevant information stored in the database. Where new wheels and workpiece materials are to be ground for which there is no relevant information, initial conditions are selected by the user. The grinding power and time constant for the first workpiece to be ground is recorded and used as the basis for adaptive control.

Adaptive Feed Rate Control

The in-feed rate required to give the target power is calculated from the measured power in the previous cycle and the calculated value is used when grinding the next workpiece. Grinding power is thus maintained close to the target level throughout the batch by constantly updating the required feed rate. At the end of the batch the database is updated by storing the new grinding conditions together with the other workpiece and wheel details. Kinematic parameters of mean chip thickness and length are also stored and are used to suggest machining conditions for new workpiece parts based on kinematic similarity to grinding conditions which are known to be optimal.

Typical grinding results in Fig. 11.10 show the benefit of an adaptive system for reducing cycle time. One reason for the improved productivity is that a user normally starts grinding using cautious values to ensure good quality. The adaptive system allows production rate to be automatically increased and maintained at the higher level as confidence grows. Another

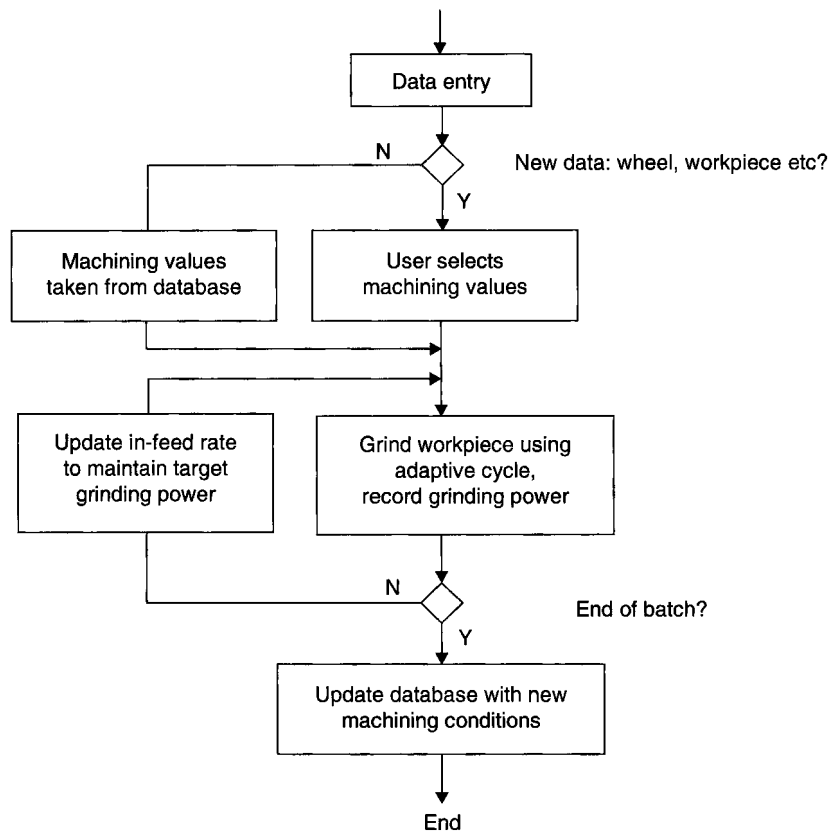


Figure 11.9 Basic adaptive strategy and intelligent database.

reason for the improved cycle times is that grinding wheels tend to suffer an initially high rate of wear just after dressing and there is either an associated drop in grinding power or an increase in grinding power. Either way, corrective action is required. As grinding forces reduce in Fig. 11.10, in-feed rate can be increased to an acceptable level while power is maintained constant.

Adaptive Dwell Control

Material removal lags behind programmed values owing to deflections of the system. A measure of the lag is given by the time constant of the grinding system. This means that accuracy achieved depends on the dwell employed for spark-out. If the dwell is too short, deflections will not be relaxed and roundness errors will result. If the dwell is too long the wheel

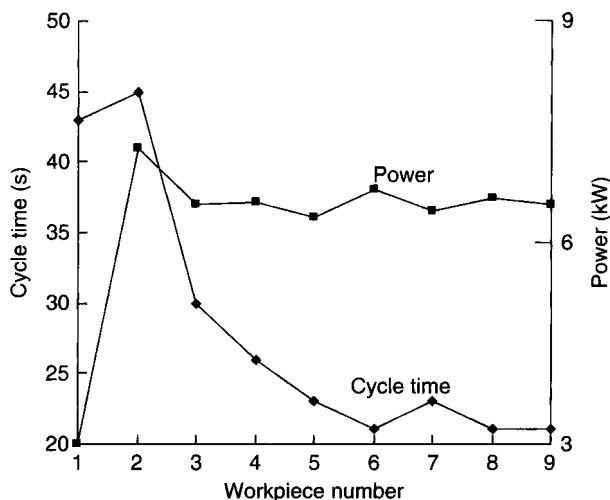


Figure 11.10 Effect of updating power level on cycle time. Wheel A465K5V30W, diameter 450 mm, speed 33 m/s. Workpiece EN9, diameter 46 mm, width 50 mm.

tends to become glazed and the cycle time is unnecessarily increased. The ideal length of the dwell period depends on the wheel sharpness and the system time constant. An adaptive system controls the length of the dwell automatically to maintain the required accuracy while also minimising wheel-wear and optimising production output.

The benefits of an adaptive system for target power and dwell can be seen in Fig. 11.11. The non-adaptive system operated for best accuracy with a cycle time of 60 s allowed the size errors to be kept within an error band of 1 μm . Increasing feed rate and reducing dwell reduced cycle time to 30 s. However, the size errors were increased. Using the adaptive system with a 2.5 kW power target, the 1 μm size error band was achieved with a mean cycle time of 37 s. Increasing the target power to 5 kW doubled the size error band to 2 μm but achieved a mean cycle time of 22 s (Rowe et al. 1994).

The introduction of adaptive dwell requires automatic detection of grinding time constant. This is not as difficult as it might sound. Ways of detecting time constant are explained as follows.

Role of Time Constant

The lag between programmed in-feed and material removal is proportional to the steady-state deflection of the system including the effect of

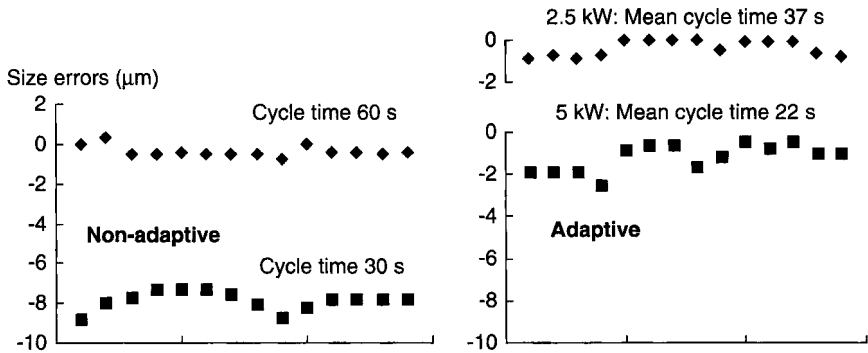


Figure 11.11 Size and cycle time comparison for adaptive and non-adaptive systems. Time constant 3 s approx. Wheel A465K5V30W. Workpiece EN9. Grinding width 50 mm. Workpiece diameter 46 mm. Wheel speed 33 m/s.

workpiece bending. The deflection can be related to the time constant τ of a first-order system. Using a power sensor, an adaptive system will be capable of determining the time constant from the power variations during the cycle. With knowledge of the time constant τ , size reduction of the workpiece during the dwell period is given by (Liverton et al. 1993)

$$X_i = X_1 + x_1(1 - e^{-(t-T_1)/\tau_1}) \quad \text{Size reduction during dwell} \quad (11.1)$$

where X_i is the actual size position at time $t - T_1$ from the start of dwell, X_1 is the actual size position at the start of dwell, and x_1 is the deflection at the start of dwell. Even with a stiff machine, time constant can vary typically between 1 and 10 s. With very flexible workpieces, even longer time constants are possible. The time constant reflects not only the workpiece stiffness and machine stiffness but also the flexibility of the grinding wheel and the sharpness of the grinding wheel. A sharp wheel has a shorter time constant than a blunt wheel. A time constant is shown in Fig. 11.12 with typical variations during grinding.

Time constant can be determined automatically during a grinding cycle either during in-feed or during dwell.

Time Constant during In-Feed

Time constant during in-feed can be determined by integrating the power signal from the commencement of grinding. The procedure is illustrated in Fig. 11.13.

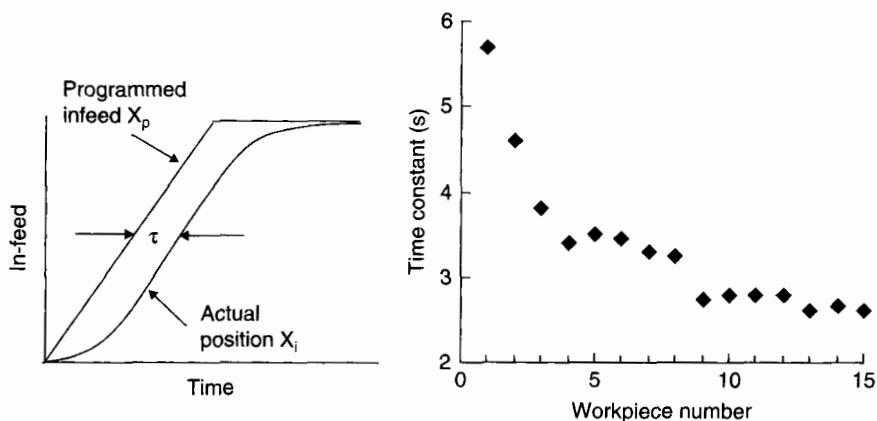


Figure 11.12 Time constant changes due to deflections and variations of wheel sharpness.

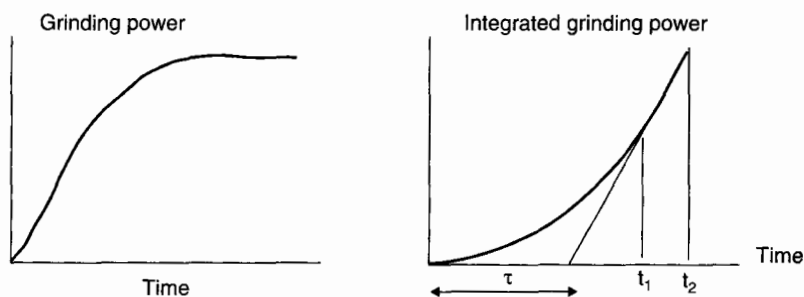


Figure 11.13 Determination of time constant during in-feed.

Comparing values of the power integral at times t_1 and t_2 yields the time constant (Liverton et al. 1993)

$$\tau = \frac{t_1 \int_0^{t_1} p dt - t_2 \int_0^{t_2} p dt}{\int_0^{t_2} p dt - \int_0^{t_1} p dt} \quad \text{Time constant during in-feed} \quad (11.2)$$

Time Constant during Dwell

One problem with determination of time constant during in-feed is that time constant of the grinding system may change between the in-feed part of the cycle and the dwell period for spark-out. For high accuracy it is best to determine time constant during dwell (Allanson 1995). The basis is shown in Fig. 11.14.

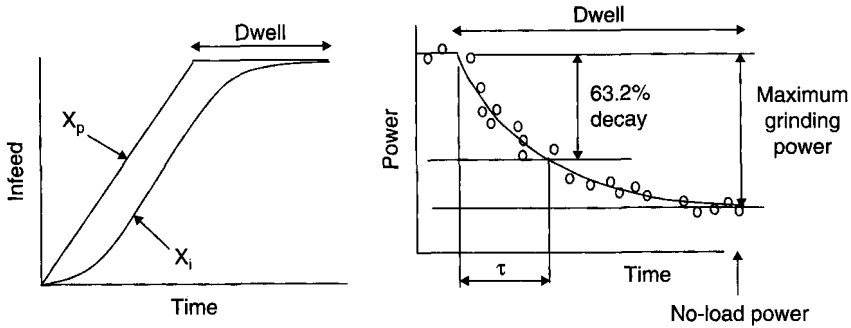


Figure 11.14 Grinding power decays exponentially during dwell at a rate that depends on the time constant τ .

The most accurate way is to determine time constant automatically during the dwell period using a weighted least mean squares estimate (Allanson et al. 1997). Taking time samples t_i and power samples P_i from the commencement of the dwell period, the time constant is given by

$$\tau = \frac{\sum w_i \cdot t_i^2}{\sum w_i \cdot t_i \cdot \log_e (P_i / P_{\text{start}})} \quad \text{Time constant} \quad (11.3)$$

where w_i are values of weights selected according to the band in which power level falls. While the values of w_i could all be made equal to 1, the calculation is made much more reliable if the weights are adjusted according to the normalised power bands. The normalised power level P_i is the instantaneous power level divided by the mean maximum power signal. The power level is divided into five bands according to the value expected after 0.5, 1, 1.5, 2, and 2.5 time constants. The mid-point of each band is employed and the weighting factor is P_i^2 .

This procedure using weighted least mean squares reduces the effect of noise in the power signal. The procedure is reliable and most important of all allows time constant to be accurately predicted early within the dwell period within approximately 0.5 of a time constant. Using this method the user can specify a dwell of x time constants where x could be equal to 2 for lower precision or as large as 6 where precision is proving harder to maintain. The beauty of the system is that it provides a means for automatically maintaining accuracy and speed of production at the optimum level.

Adaptive Control of Multi-Plunge Grinding

The size control system was also employed for rough grinding long slender shafts. Prior to this system being employed it had proved difficult

to achieve straightness in either a multi-plunge process or in a traverse grinding process, the reason being that deflection of the parts caused the resulting shape after grinding to suffer from barrelling. In traverse rough grinding, the barrelling was in the range 150–180 μm . Using adaptive multi-plunge grinding, the barrelling was reduced to approximately 25 μm and a 50% reduction in cycle time. The magnitude of the challenge in grinding these long slender shafts can be seen from the variation of time constant from 3 s when grinding near the tailstock to 62 s when grinding at the mid-point, Fig. 11.15.

A variable dwell period was programmed according to the time constant at each position along the shaft for the rough grinding operations. Longer dwell periods were employed towards the centre of the shaft and shorter dwell near the ends of the shaft. After rough grinding using the varying dwell periods, shaft straightness was greatly improved compared to traverse grinding or compared to multi-plunge grinding with constant dwell. This speeded up rough grinding but also speeded up finish grinding as the initial straightness of the shafts was much improved.

11.4 Knowledge-Based Intelligent Control Systems

This section discusses what is possible and presents a structure in which a variety of developments can be incorporated. Many such developments have been used and tested. The potential for intelligent control is much greater than currently employed and is only limited by the resources of manufacturers of grinding machine and manufacturers of ancillary grinding

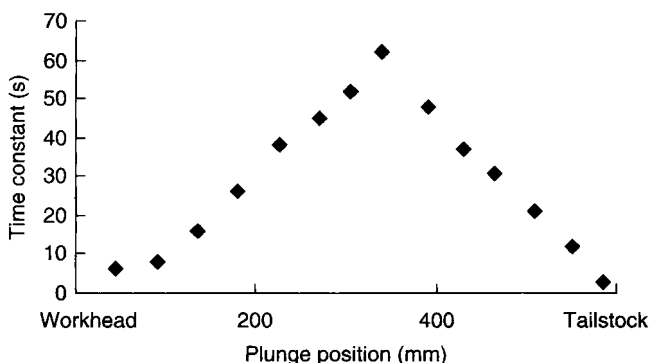


Figure 11.15 Time constants estimated during multi-plunge grinding of long slender shafts.

machine control equipment to develop reliable systems. With time, it is likely that further intelligent features will be incorporated within grinding control systems.

A General Framework for Intelligent Control

Many techniques have been proposed for intelligent control systems for grinding (Rowe et al. 1994). Techniques range from advisory systems for selecting grinding conditions before a grinding operation starts to adaptive control techniques for process optimisation as a process proceeds. Some of these techniques have been adopted for particular grinding control problems such as nano grinding for mirror finish as discussed in the previous chapter. The full range of intelligent techniques is extensive and beyond the scope of this book. The following is a brief introduction to the potential of a knowledge-based intelligent system for grinding. Elements that may be included in a system are illustrated in Fig. 11.16. The figure is not to be taken as a description of a particular system; it is a structure which encompasses a range of developments. The particular developments are the techniques employed by many different system developers. As yet, no single comprehensive and integrated system provides all the features of such a system although extensive descriptions of such a system can be found (Morgan et al. 2007). However, these features have been described in the literature by Billatos and Tseng (1992), Kelly et al. (1989), Allanson et al. (1992), Xiao et al. (1992), and Toenshoff et al. (1993). The expert system concepts draw particularly on developments described by Inoue et al. (1987), Sakakura and Inasaki (1992), Koenig (1991), Trmal et al. (1992), and Toenshoff et al. (1993).

Advisory Systems and Intelligent Databases

In Fig. 11.16, the query represents the input of product dimensions, tolerances, and material. Ideally and where data are available for identical parts, the advisory system provides machining data drawn from a database including feeds, speeds, dressing conditions, dressing frequency, and grinding wheel selection. Where data are available, it is simply necessary to call up the relevant details from the database. Where almost similar parts can be identified, an intelligent system can make inferences based on kinematic similarity and suggest suitable machining conditions. It is this feature that makes the database intelligent.

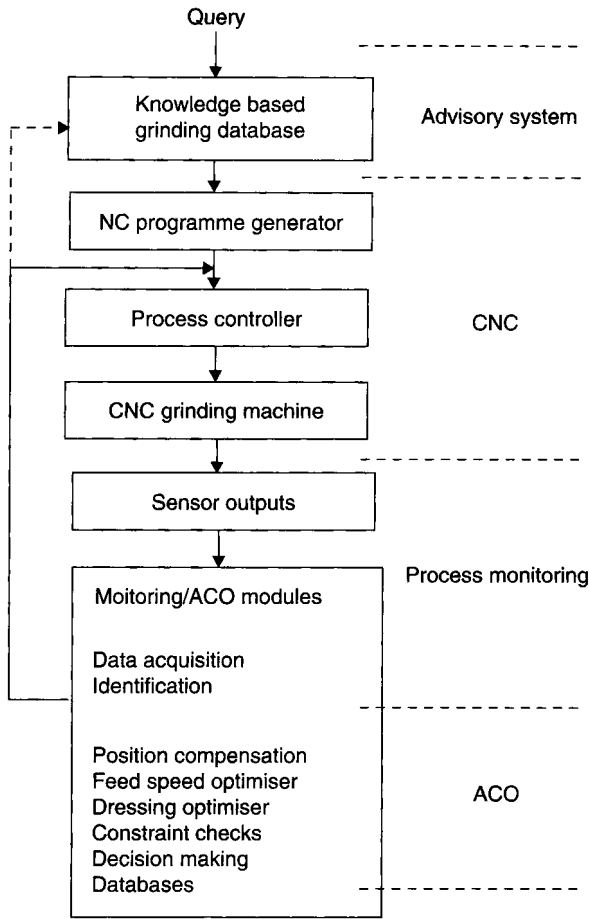


Figure 11.16 Levels of interaction in an intelligent grinding system.

The CNC

The machining data together with the product data are used by the programme generator to produce a part programme. The CNC then controls the machining operation.

Monitoring and Adaptive Control Optimisation (ACO) Modules

Sensors are only useful if action is taken as a consequence of the sensor information. In a manual system, action may be taken by operator intervention to re-dress the grinding wheel, for example. The monitoring and

ACO modules analyse data from sensors or from operator inputs and by reference to rules and process modules takes appropriate decisions to optimise performance. Feedback from the ACO modules is essential to keep the database up to date. The use of ACO modules to optimise feed rate and dwell have already been discussed in the previous sections. Further details can be found in the literature (Marinescu et al. 2007, Chapter 11).

In an intelligent system, the sensor information leads to an automatic action. Sensors, where these are available, feed appropriate information to the monitoring and ACO modules. Specialised equipment is readily available for use with grinding machines for the following examples:

- gauging for size control and feed rate changes
- AE for gap elimination and for touch dressing
- power sensors for wheel sharpness and thermal damage
- accelerometers for vibration and wheel balancing equipment

Temperature Sensing

Temperature sensing is often used to control oil temperatures and coolant temperatures or even for temperature control of machine elements, but temperature control systems usually operate separately from the CNC system.

The use of gauging modules for size control has already been discussed.

Gap Elimination

Another readily available technology is AE sensing for gap elimination. AE sensors provide a fast and sensitive signal when contact is made with the grinding wheel. Gap elimination allows a rapid feed of the grinding wheel to be employed until contact is made with the workpiece. With gap elimination, the AE signal is employed to change the feed rate. Without gap elimination it would be necessary to stop the rapid feed a safe distance before contact. Gap elimination reduces cycle time by eliminating the extra stand-off distance to be travelled at a reduced grinding feed rate.

Touch Dressing

Touch dressing discussed in Chapter 4 is a technique where contact between the grinding wheel and the dressing tool is detected using an AE

sensor. Touch dressing is used to achieve a very small dressing depth and is important in the optimisation of grinding with vitrified CBN wheels.

Power Sensing

Power sensors are used to monitor the condition of the grinding wheel and thus prevent thermal damage and infringement of constraints such as maximum power level.

Operator Inputs

Additional information may be input by the machine operator that serves the same purpose as sensor input. For example, an operator may measure workpiece size, roughness, or roundness after parts have been ground and input values into the system.

Thermal Damage

The process can be monitored by the ACO modules for potential thermal damage. The usual method is to monitor maximum power level during the main in-feed stage of the cycle. Using a thermal model, as described in Chapter 18, it is possible to determine whether the grinding conditions are likely to cause thermal damage (Rowe et al. 1994). Where thermal damage is indicated, the system will limit in-feed rate and give a warning to the operator. The operator can allow operation to continue with reduced feed rate or decide to initiate other corrective action such as wheel dressing or a change of work speed. Several companies use power monitoring as an indicator of satisfactory grinding conditions. The use of thermal models for adaptive control is still in the early stages.

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12 Vibration Problem Solving

12.1 Introduction

The first priority in overcoming vibration problems is to recognise the source and nature of the vibration. Vibrations in grinding lead to surface waviness and loss of workpiece accuracy. Wheel wear is increased and workpiece roughness deteriorates. Three types of vibration that cause damage are

- impulsive vibrations
- forced vibrations
- self-excited vibrations

The differences as they may be experienced in practice are illustrated in Fig. 12.1.

Impulsive Vibrations

Impulsive vibrations are a special case of forced vibration where the forcing is a sharp disturbance or shock loading that arises either from outside the machine or from within. For example, shock loading occurs when a work table is fed up to a stop and suddenly halted. This situation often arises in plunge feed operations where the in-feed is fed rapidly to a stop and then held there for spark-out. Other sources of apparently random vibrations can be flapping belts, damaged lead screws, gears, and slide-ways. All sources of impulsive vibrations should be avoided or eliminated in the

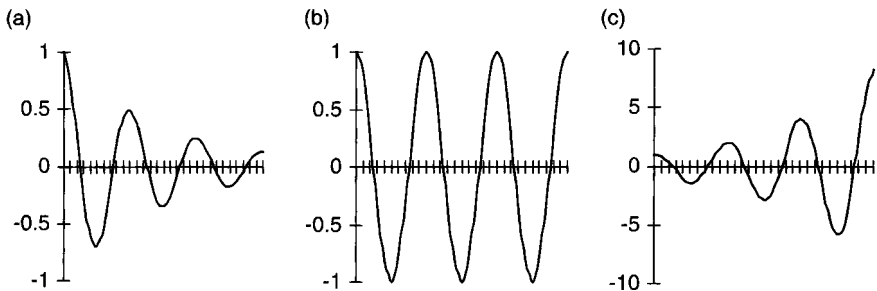


Figure 12.1 (a) Impulsive vibrations decay, (b) forced vibrations are sustained, and (c) self-excited vibrations grow.

machine design or in the machine operation. For example, a feed drive should not be operated hard up to a stop and feed reversal should be smoothly decelerated and accelerated to avoid jerk.

Random vibrations can also arise externally from a passing heavy machine or truck or from goods dropped nearby on the floor. Machine mountings should be designed to minimise transmission of vibrations.

Forced Vibrations

Figure 12.1 shows an example of a steady, sustained vibration. Sources of steady vibration include wheel unbalance, rotary dresser unbalance, spindle unbalance, motor unbalance, and pulley wheel unbalance. The source of the problem is usually identified from the vibration frequency. The vibration frequency can be identified from the wavelength of the waves on the workpiece surface and the work speed. Also, if the rotational speed of the grinding wheel is exactly 10 times the rotational speed of the workpiece, 10 waves may be found on the workpiece. In this case, it is clear that the vibration is related to spindle run-out. The use of an integer speed ratio, such as in this case, where speeds are 10:1 should be avoided as waviness is difficult to avoid. It is also important that all rotating parts be carefully balanced before commencing grinding operations.

Automatic wheel balancing is essential for the best quality work (Trmal and Kaliszer 1976). There are a number of commercial systems available that can be incorporated either into the machine or into the grinding wheel spindle assembly.

Wheel unbalance leads to spindle run-out. It is therefore necessary to balance the wheel before truing and again after truing. A final truing pass should always be made after balancing to achieve minimum run-out.

Self-Excited Vibrations

Self-excited vibrations occur in most machining operations as a consequence of machine vibration interacting with depth of cut (Tobias 1965). Grinding is particularly sensitive to vibrations owing to the high standard of surface accuracy expected.

As shown in Figs 12.1 and 12.2, self-excited vibrations build up with time. This is therefore the most serious cause of inaccuracy in grinding. Self-excited vibrations can build up in two different ways as either work-regenerative chatter or as wheel-regenerative chatter; the difference is shown in Fig. 12.3 (Marinescu et al. 2007, Chapter 8). Work-regenerative

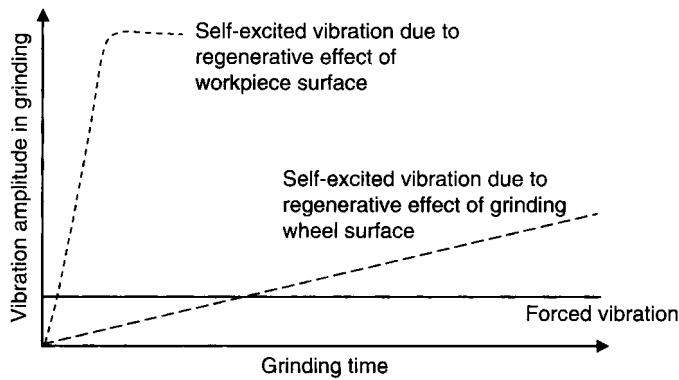


Figure 12.2 Growth of self-excited vibrations and forced vibration.

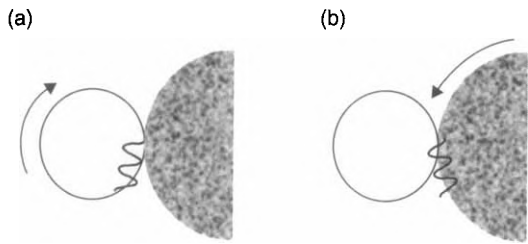


Figure 12.3 (a) Work-regenerative and (b) wheel-regenerative vibrations.

vibration develops primarily on the workpiece surface whereas wheel-regenerative vibration develops primarily on the wheel surface. Wheel-regenerative vibration is evidenced by banding and waviness on the grinding wheel surface.

Work-regenerative vibrations. Vibration starts from a small amplitude and builds up afresh on each workpiece surface as grinding proceeds. Waves generated on the workpiece surface result in a change of depth of cut after one revolution of the workpiece. This causes a new wave to be created. The phase shift between the old and the new surface waves creates a varying depth of cut. The process becomes unstable when the phase is right and the grinding force is too large. The vibration usually develops very quickly but the amplitude of vibration is ultimately limited by non-linearity such as loss of contact between the workpiece and the grinding wheel or by interference due to the shape of the grinding wheel. Work-regenerative vibrations build up more rapidly at high work speeds, particularly in cylindrical grinding processes as illustrated in Chapter 2 (Figs 2.13 and 2.14). A low work speed chatter is less likely to be sustained as a greater number of waves have to be formed around the circumference for a particular chatter frequency. Large numbers of waves tend to be filtered

out by the contact length as shown below. The use of a free-cutting, soft grade grinding wheel helps to offset work-regenerative vibration because the grinding force is reduced. The role of grinding force is further explored in the following sections.

Wheel-regenerative vibrations. Wheel-regenerative vibration is similar to work-regenerative vibration except that the waves in this case build up mainly on the grinding wheel. Wheel-regenerative vibrations build up progressively from one part to another. The amplitude continually builds up on the surface of the grinding wheel and becomes larger for each workpiece produced. Eventually the wheel surface is so badly affected that the wheel has to be re-dressed to restore accuracy and acceptable surface roughness. A hard grade of grinding wheel that gives a high G ratio helps to offset wheel-regenerative vibration because the grinding wheel is more resistant to wheel wear.

Generally, most grinding processes are unstable in terms of grinding wheel regenerative chatter (Inasaki et al. 1974). It follows that the speed of the vibration development is a matter of concern with respect to this type of chatter.

Other important factors to be taken into account in terms of grinding chatter are the elastic deformation of the grinding wheel (Brown et al. 1971) and the geometrical interference between the grinding wheel and the workpiece (Rowe and Barash 1964).

Vibration behaviour in cylindrical, internal, and surface grinding processes differ in significant ways (Inasaki 1977). In the case of internal grinding, the chatter frequency is, in most cases, related to the natural frequency of the grinding wheel spindle system because the dynamic stiffness of internal grinding spindles is often lower than that of the workpiece system. This is not the case, however, for cylindrical grinding. In this latter case, the dynamic stiffness of the workpiece system is usually lower than that of the grinding wheel spindle system. In addition, chatter vibration caused by the regenerative effect on the workpiece surface develops in a less constrained way in surface grinding. This is because the phase shift between the new wave and the old wave on the surface is not constrained because of the uncertainty in the workpiece reciprocating motions.

12.2 Dynamic Relationships for Grinding

Block Diagram

Figure 12.4 shows the relationships between vibrations, depth of cut, grinding force, machine system stiffness, and workpiece shape. The rela-

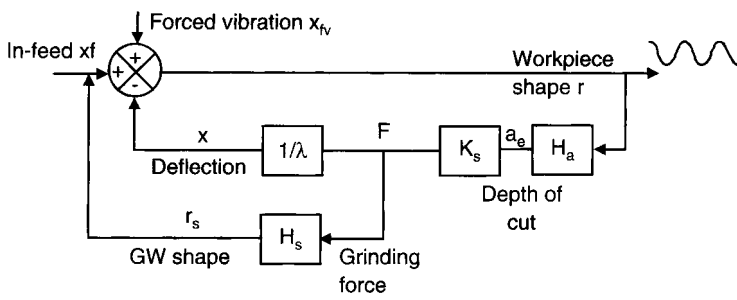


Figure 12.4 Relationships between vibrations and workpiece shape.

tionships are explained below with the aid of equations. For the sake of familiarity, symbols used are the same for vibrations as used for steady-state quantities such as a_e and F_n .

Basic Equations

Workpiece reduction-in-radius r , is the summation of forced vibration x_{fv} , in-feed disturbance x_f , grinding wheel shape irregularity r_s , and machine deflection x . Vibration movements vary with time; with the usual convention that deflection represents a reduction in depth of cut, the summation can be written as

$$r(t) = x_f(t) + x_{fv}(t) - x(t) \quad \text{waviness} \tag{12.1}$$

For free vibrations when there is no external vibration forcing, $r(t) = -x(t)$. The depth of cut is the difference between the new wave being cut and the old wave on the workpiece being removed as shown in Fig. 12.5. The wave being removed from the surface is the wave that was formed on the

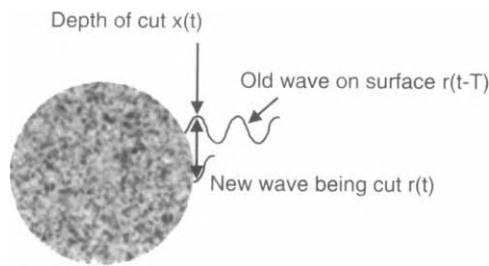


Figure 12.5 The variation in depth of cut when replacing a surface wave with a new wave.

surface 1 workpiece revolution earlier at a time $t - T$, where T is the time for 1 work revolution.

If the machine were perfectly rigid, there would be no deflections and a new wave would not be cut on the surface. However, with ongoing steady vibration, a new wave is formed equal in amplitude to the old wave on the surface but not usually in phase with the old wave. With two waves exactly in phase, there is no variation in depth of cut and no vibration force to maintain deflections.

Waviness is related to the sinusoidal depth of cut as shown in Fig. 12.5. Maximum depth of cut occurs when the old wave and the new wave being cut are in anti-phase as shown.

The depth of cut a_c is therefore given by

$$a_c(t) = r(t) - r(t - T) \quad \text{Time-varying depth of cut} \quad (12.2)$$

The depth of cut gives rise to a grinding force depending on the grinding force stiffness K_s . For simplicity, the grinding force stiffness is assumed to be constant here. The grinding force stiffness relates the component of the grinding force acting normal to the cut surface to the depth of cut.

$$F(t) = K_s \cdot a_c(t) \quad \text{Grinding force} \quad (12.3)$$

The grinding force gives direct rise to deflections normal to the cut surface. The deflection that results from the grinding force depends on the stiffness of the machine including the grinding wheel and the workpiece. For simplicity, these are all lumped together in one frequency-dependant stiffness parameter $\lambda(j\omega)$, where the Greek symbol lambda is for stiffness and omega indicates that the stiffness varies with frequency. The complex operator j indicates that the deflection lags the force by a phase angle:

$$x(t) = \frac{1}{\lambda(j\omega)} \cdot F(t) \quad \text{Deflection} \quad (12.4)$$

Deflections are also influenced by grinding wheel wear r_s . With each revolution of the grinding wheel the waviness r_s is increased by additional wear given by the wheel wear coefficient C_{sw} and the grinding force. The period for 1 grinding wheel revolution is T_s .

$$r_s(t) = C_{sw} \cdot F(t) + r_s(t - T_s) \quad \text{Grinding wheel wear} \quad (12.5)$$

Because the grinding wheel performs many more revolutions than the workpiece, there is always a susceptibility to wheel-regenerative vibration

even where the build up is slow. Variation of wheel speed can break down waves that form although new frequencies will start to build up.

Basic Solutions

Solutions to the dynamic equations have the form $x(t) = A.e^{pt}$. The roots have the complex form $p = \sigma + j\omega$, where σ (Greek symbol sigma) represents the growth rate at the vibration frequency ω . Limiting stability is when $\sigma = 0$. If σ is negative, the system is stable, and if σ is positive, the system is unstable.

Solutions to the above dynamic equations strongly depend on the grinding stiffness K_s and the frequency-dependent machine system stiffness $\lambda(j\omega)$.

Free Vibration

A free vibration is where there is no externally applied vibration forcing. The solution for a free vibration is usually roughly sinusoidal. Of course, vibrations can occur across a whole spectrum of frequencies. There will usually be one or more frequencies where there is growing amplitude. At the limit of stability, a free vibration has constant amplitude. It neither grows nor decays.

Forced Vibration

Vibration forcing of a stable system produces steady vibration amplitude. However, if the system is forced with a frequency at the limit of stability, vibration amplitude will grow to infinity or until limited by non-linearity. We may therefore consider limiting stability to be unstable in practice.

Transfer Functions

A transfer function is the ratio of output to input. The transfer function represents the amplification and phase between input and output. It is usual to express block relationships in Laplace transfer function form for ease of handling. The equations for free vibrations become

$$H_a(s) = a_c(s)/r(s) = 1 - e^{-sT} \quad \text{Depth of cut function} \quad (12.6)$$

$$K_s = F(s)/a_e(s) \quad \text{Grinding force function} \quad (12.7)$$

$$1/\lambda(s) = x(s)/F(s) \quad \text{Compliance function} \quad (12.8)$$

$$H_s(s) = r_s(s)/F(s) = C_{sw}/(1 - e^{-sT_s}) \quad \text{Wheel wear function} \quad (12.9)$$

Work-regenerative stability depends on the main feedback loop in Fig. 12.4. The open-loop transfer function is $x(s)/r(s) = H_a(s) \cdot K_s/\lambda(s)$. At the stability limit, $x(s) = -r(s)$.

$$1 + (1 - e^{-sT}) \cdot K_s/\lambda(s) = 0 \quad \text{Work-regenerative limit} \quad (12.10)$$

The open-loop wheel-regenerative transfer function is $r_s(s)/r(s) = H_a(s) \cdot K_s \cdot H_s(s)$. At the stability limit for wheel-regenerative vibration, $r_s(s) = -r(s)$.

$$1 + (1 - e^{-sT}) \cdot K_s \cdot C_{sw}/(1 - e^{-sT_s}) = 0 \quad \text{Wheel-regenerative limit} \quad (12.11)$$

12.3 Grinding Wheel Contact Length Filtering

It was found that amplitudes of workpiece waviness tend to be much smaller at high frequencies than at low frequencies. One reason for this is that the shape of the grinding wheel will not allow large amplitudes to be created on the workpiece at high frequencies. This was realised in the analysis of centreless grinding and is illustrated in Fig. 12.6 (Rowe and Barash 1964; Marinescu et al. 2007).

The shape of the grinding wheel can be accommodated within the work-piece surface wave if the wavelength is sufficiently large. The wavelength is v_w/f , where v_w is the work speed and f is the frequency of the vibration. The vibration can be accommodated if the wavelength is greater than approximately twice the grinding contact length l_c . This is the condition

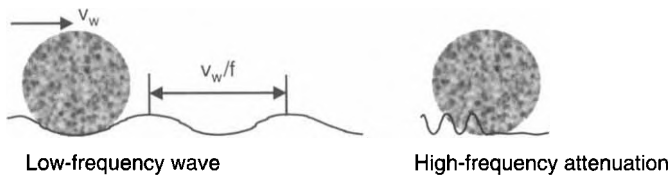


Figure 12.6 Amplitude of high-frequency waves attenuated by the grinding wheel shape.

corresponding to a low frequency wave. The shape of the grinding wheel cannot be accommodated if the wavelength is too short as with high frequency waves. High frequency results in the waviness being removed in grinding. At the critical condition for attenuation, vibration amplitude a gives a contact length $l_c = \sqrt{a \cdot d_e}$ equal to half the wavelength v_w/f .

$$a_{\text{crit}} = \frac{v_w^2}{4 \cdot f^2 \cdot d_e} \quad \text{Critical amplitude} \quad (12.12)$$

Larger amplitudes are attenuated. Equation 12.12 also reveals that larger vibrations are more likely at high work speeds and low frequencies.

This benefit of grinding wheel interference explains why wheel-regenerative vibrations can often be tolerated for considerable periods at low workpiece speeds. This is because frequencies of vibration from the grinding wheel are usually very high compared to the workpiece speed.

Example 12.1 Determine the frequency, wavelength, and maximum amplitude of waviness due to wheel unbalance at a work speed of 0.1 m/s using a grinding wheel of 100 mm equivalent diameter at a wheel speed of 30 m/s. Compare the values at 0.1 m/s with the values at 0.4 m/s work speed.

$$\text{Frequency} = \text{wheel speed} = \frac{30}{0.1 \times \pi} = 95.5 \text{ c/s}$$

At 0.1 m/s:

$$\text{Wavelength} = \frac{0.1}{95.5} = 0.00105 \text{ m or } 1.05 \text{ mm}$$

$$\text{Max amplitude} = \frac{0.1^2}{4 \times 95.5^2 \times 0.1} = 2.74 \times 10^{-6} \text{ m or } 2.74 \text{ } \mu\text{m}$$

At 0.4 m/s:

$$\text{Wavelength} = \frac{0.4}{95.5} = 0.00419 \text{ m or } 4.2 \text{ mm}$$

$$\text{Max amplitude} = \frac{0.4^2}{4 \times 95.5^2 \times 0.1} = 43.9 \times 10^{-6} \text{ m or } 43.9 \text{ } \mu\text{m}$$

Higher amplitudes than these values will be attenuated due to grinding wheel interference. This effect is sometimes termed contact filtering.

12.4 Machine Stiffness Characteristics

Excitation Tests

The stiffness characteristics of a grinding machine vary with frequency. This can be shown by exciting a machine structure using well-known methods. Typical methods include the use of electromagnetic or hydraulic vibrators to excite the machine over a range of frequencies. Vibration measurement transducers are used to check amplitudes at different locations on the machine. Another technique is to use a force-instrumented hammer to excite the machine impulsively. The impulsive technique is usually less accurate and more difficult to apply than harmonic excitation.

The vibration shape at resonance can be found by positioning a vibration measurement transducer at various points on the machine and noting the amplitude at each point. Plotting the amplitudes on a diagram of the machine shape builds up a picture of the machine vibration. It is also helpful to measure the phase between the excitation force and the response. From the various amplitudes, the shape of each vibration mode can be plotted.

A machine has many resonances although some are more important than others. It is important to be aware that the excitation position and the response measurement position have a substantial effect on the resonant frequencies measured. In Fig. 12.7 the excitation was applied horizontally to the grinding wheel head and the responses were measured at a nearby wheel head position. In this case the machine was free-standing on four

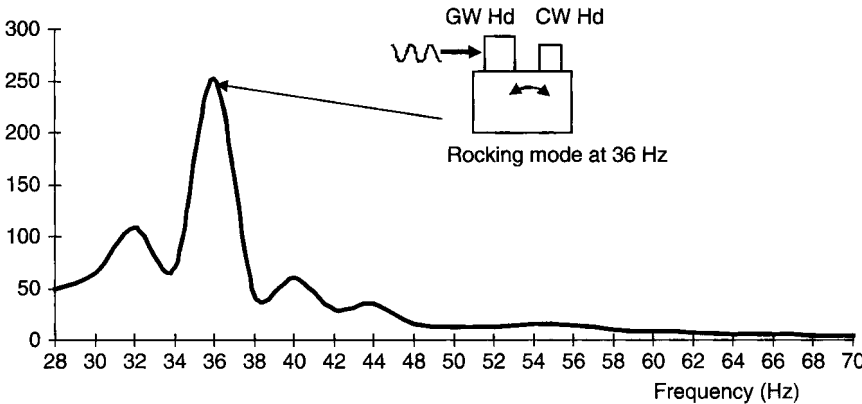


Figure 12.7 Vibration amplitude (μ in.) for a free-standing centreless grinding machine excited horizontally with a force of 5 lbf at the grinding wheel head.

levelling pads. A dominant sideways rocking mode resonance at 36 Hz was found. There is a lesser resonance for sideways excitation caused by forward and backward rocking at 32 Hz. Rocking modes such as these are found for most machines.

The rocking modes in this example had a relatively small influence on the accuracy of the process compared to the dominance of the vibration amplitudes. This is because at the frequency of vibration, the whole machine rocked almost as a solid body with only small relative vibration between the two wheel heads. It is the relative vibration between the two heads that is critical for workpiece shape (Rowe 1964).

The rocking modes can be eliminated by fixing the machine rigidly to the foundations as in Fig. 12.8. In this figure it becomes apparent that there are three relative vibration resonances. The most important resonance is the tuning fork vibration where the two wheel heads vibrate in anti-phase. It is this tuning fork vibration shown at 82 Hz that is most susceptible to relative excitation by the grinding force and is most strongly associated with work-regenerative chatter.

A more informative way of viewing frequency responses is by means of a polar plot of amplitude and phase as in Fig. 12.9. At low frequency the deflection is in phase with the applied excitation force. At resonance the deflection lags the applied force. Figure 12.9 shows relative vibration responses between the control wheel and the work plate position when the above centreless machine was vibrated at the control wheel. The first resonance was at 82 Hz but was found to be reduced to 78 Hz when excitation was applied between the two grinding wheels.

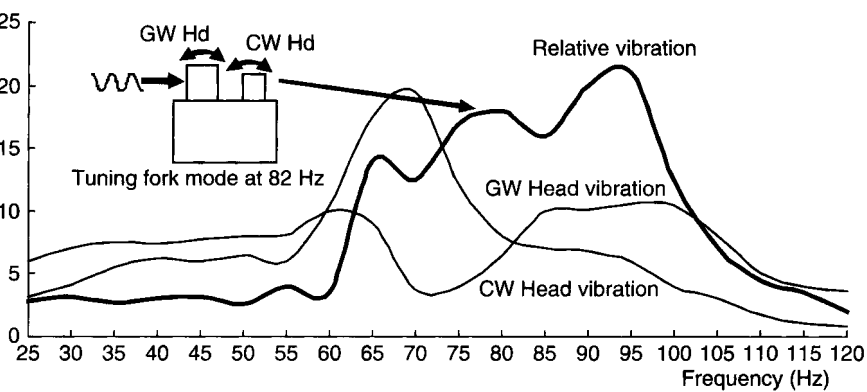


Figure 12.8 Vibration amplitudes (μ in.) of a rigidly mounted centreless grinding machine excited horizontally with a force of 5 lbf at the grinding wheel head.

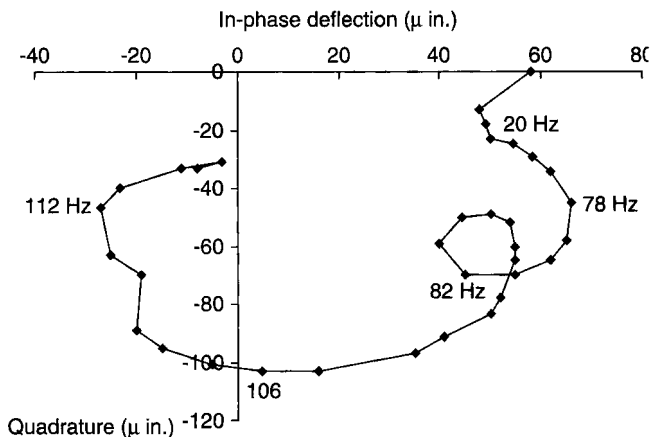


Figure 12.9 Polar plot of relative vibrations between the control wheel head and the work plate position of a centreless grinding machine when excited with a force of 5.46 lbf.

Additional vibration responses are found at higher frequency modes. These become important when grinding large workpieces at low work speeds. For example, the first grinding wheel spindle mode was found at 240 Hz.

Light-Running Tests

Light-running tests show the effect of the various motors, pumps, belts, pulleys, dressing wheels, and grinding wheels. The machine should be mounted on foundations in such a way so as to minimise vibrations, and the rotating parts must be carefully balanced. The light-running amplitudes can be measured by positioning transducers at various positions as in excitation tests. Typical light-running vibrations for relative vibrations between the two wheel heads of the same centreless grinding machine as above are shown in Fig. 12.10. The grinding wheel motor running at 24.2 Hz has a strong effect on the light-running vibrations. The effect is demonstrated not only at the corresponding frequency but also at harmonics of this frequency at 48, 68, and 97 Hz. The spindle speed was 30 Hz, but as the spindle was carefully balanced there is no direct evidence of this frequency. Impulsive vibrations due to belts cause vibration over a range of frequencies but particularly at resonant frequencies identified from excitation tests.

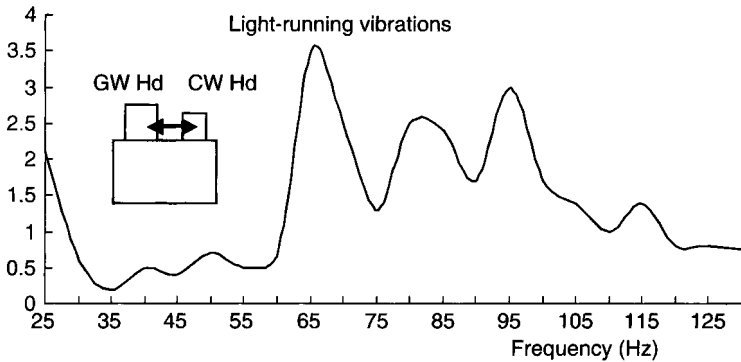


Figure 12.10 Light-running relative vibration amplitudes (μ in.) between the two wheel heads of a well-balanced centreless grinding machine.

12.5 Stiffness, Damping, and Resonance Parameters

Most machine tools are lightly damped. Machine stiffness and damping vary according to the vibration mode. The impulsive response shown in Fig. 12.1 is typical for a lightly damped machine. For a spring-mass-damper system, motion is described by $m \cdot \ddot{x} + c \cdot \dot{x} + \lambda \cdot x = F$, where x is deflection and F is the applied excitation force. The equation of motion states that the applied force is reacted by the elastic, damping, and inertia forces. For a simple spring-mass-damper structure, the elastic factor is the static stiffness λ , the damping factor is c , and the inertia factor is the mass m . With a harmonic force $F \cdot \cos \omega t$ applied at constant frequency ω , the equation of motion is solved in the usual manner. The deflection amplitude per unit force is the inverse of the frequency-dependent dynamic stiffness $\lambda(j\omega)$.

$$\frac{1}{\lambda(j\omega)} = \frac{1}{\lambda - m\omega^2 + j\omega c} \quad \text{Spring-mass-damper deflection} \quad (12.13)$$

The machine resonates when $\lambda = m \cdot \omega$. The natural frequency is given by $\omega_n = \sqrt{\lambda/m}$. At resonance, $x/x_0 = 1/c\omega_n$ with a phase lag of 90° indicated by the j operator. This resonant amplitude is $Q \cdot x_0$, that is to say the amplitude is Q times larger than the zero frequency deflection x_0 , where $Q = \lambda/c\omega_n$. For example, if $Q = 10$, the deflection x at the natural frequency is 10 times larger than x_0 . For this reason, Q is known as the dynamic magnifier. A value $Q = 10$ represents a lightly damped structure. With re-arrangement Eqn (12.13) becomes

$$\frac{x}{x_0} = \frac{\lambda}{\lambda(j\omega)} = \frac{1}{1 - \frac{\omega^2}{\omega_n^2} + j \frac{\omega}{Q \cdot \omega_n}} \quad \text{Dimension-less deflection} \quad (12.14)$$

Example 12.2 Calculate the dynamic deflection for a spring-mass-damper system at a frequency ratio of 1.05 for the following conditions: Dynamic magnifier $Q = 10$ and static deflection $x_0 = F/\lambda = 1 \mu\text{m}$.

The in-phase component of the deflection is given by the real part of Eqn (12.14):

$$\text{Re} \left[\frac{x}{x_0} \right] = \frac{1 - \omega^2/\omega_n^2}{\left[1 - \frac{\omega^2}{\omega_n^2} \right]^2 + \left[\frac{\omega}{Q \cdot \omega_n} \right]^2} = \frac{-0.1025}{(-0.1025)^2 + 0.105^2} = -4.761$$

The quadrature component of the deflection is given by the imaginary part:

$$\text{Im} \left[\frac{x}{x_0} \right] = \frac{-\omega/(\omega_n Q)}{\left[1 - \frac{\omega^2}{\omega_n^2} \right]^2 + \left[\frac{\omega}{Q \cdot \omega_n} \right]^2} = \frac{-0.105}{(-0.1025)^2 + 0.105^2} = -4.877$$

The dimensionless resultant vibration amplitude is

$$\left| \frac{x}{x_0} \right| = \sqrt{(-4.761)^2 + (-4.877)^2} = 6.816$$

The amplitude is therefore $1 \times 6.816 = 6.816 \mu\text{m}$.

The phase is given by

$$\tan(\pi - \phi) = \frac{\text{Im} [x/x_0]}{\text{Re} [x/x_0]} = \frac{-4.877}{-4.761} = 1.024$$

Evaluating gives $\phi = \pi - 0.797 = 2.345 \text{ rad}$ or 134.3° .

Values for the single-degree-of-freedom system from Eqn (12.14) are shown in Fig. 12.11 for the case $Q = 10$. Measured frequency responses for real machines usually exhibit several loops, one for each mode of vibration as shown previously in Fig. 12.9.

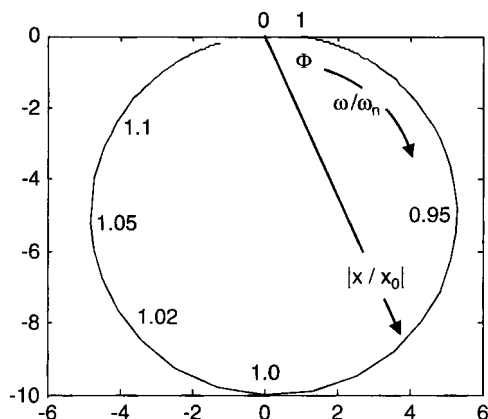


Figure 12.11 Amplitude and phase plot of a mass-spring-damper with dynamic magnifier $Q = 10$.

12.6 Chatter Conditions

Introduction

This section analyses conditions that give rise to chatter and describes techniques for measuring susceptibility to vibration. It is followed by several techniques for avoiding or overcoming chatter problems.

Work-regenerative chatter conditions are determined from measured machine vibration responses (Gurney and Tobias 1961; Tlustý 1965). The graphical approach given as follows demonstrates features required for stability. It is shown that reducing grinding width makes the process more stable. It is also shown that making a machine more flexible can actually improve stability if flexibility is introduced in the correct way.

The sensitivity of a machine to chatter can be assessed from frequency responses. Frequency responses must be measured for the directions that relate grinding force and deflections. This means measuring force in the direction of the grinding force and deflections in the direction that affects depth of cut. The inverse directions for force and deflection can be used if this is more convenient based on Maxwell's Principle of Reciprocity. Using Maxwell's principle, force is applied in the direction of the depth of cut and deflection measured in the direction of the grinding force.

A more convenient but rather more approximate method is to both apply the force and measure deflections in the direction of the depth of cut. The accuracy of this approximation will depend on the sensitivity of the machine deflections to force components in the orthogonal direction.

Graphical Stability Determination

Taking measured vibration responses, the graphical construction is made as in Fig. 12.12. The frequency responses are divided by static deflection for convenience as in Fig. 12.11. The static deflection at zero frequency is equal to F/λ , where F is the magnitude of the excitation force that produces the relative deflections $x(t)$ between the workpiece and the grinding wheel.

At the stability threshold, the deflection $x(t)$ and the waviness $r(t)$ are equal and opposite in sign. The amplitude of $x(t)$ is constant and therefore equal to the amplitude $x(t - T)$. The force F represented by the vector OP in Fig. 12.12 is equal to $K_s(x(t) - x(t - T))$ represented by the vector OP' . The necessary phase relationships are satisfied by the vector triangle $OR'P'$.

The machine deflection $x(t)/x_0$ is represented by the vector OR . It follows that

$$\frac{K_s}{\lambda} = \frac{OR'}{OR} = \frac{-1}{2 \cdot \text{Re}[x(t)/x_0]}$$

Stability condition

(12.15)

where $\text{Re}[x(t)/x_0]$ is the negative real part of the response locus and can be simply measured with a rule.

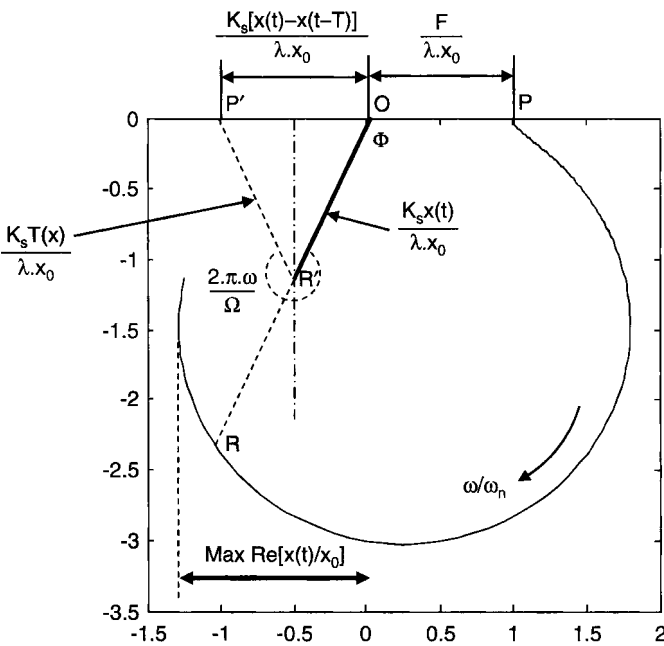


Figure 12.12 Graphical determination of threshold stability conditions from measured responses.

This important result shows that the overall resistance of a machine to chatter can be measured in terms of the maximum negative real part of the measured frequency response shown in the figure as $\text{Max Re}(x(t)/x_0)$.

Using Measured Frequency Responses

Using measured frequency responses it is straightforward to determine threshold values of K_s/λ . This value represents the grinding force stiffness divided by the machine static stiffness. The machine stiffness must include the effect of all the elements in the grinding force loop including the grinding wheel contact stiffness. For the example shown in Fig. 12.12, it can be seen that the threshold value of K_s/λ is approximately equal to 0.48. This is a very high stability threshold arising because the dynamic magnifier is low, $Q = 3$. A more usual value $Q = 10$ leads to much lower threshold forces where the minimum value of K_s/λ is equal to 0.105 or a little larger than $1/Q$. The minimum value is found where the frequency responses have the maximum negative in-phase component. In Fig. 12.11 this condition corresponds to a frequency 1.05 times the natural frequency. For this basic type of chatter the chatter frequency is always slightly higher than the associated natural frequency and requires the machine response to have a negative in-phase component.

The phase relationship between $x(t)$ and $x(t - T)$ can be found for a work speed Ω expressed in radians per second. The phase is given by $\omega T = \theta + 2\pi n = 2\pi\omega/\Omega$, where n represents any integer number of waves. It can be seen that the phase angle between the new surface wave and the old surface wave is given by $\theta = 3\pi - 2\phi$. It follows that a work speed Ω is related to a chatter frequency ω by

$$\frac{\Omega}{\omega_n} = \frac{\omega/\omega_n}{1.5 + n - \phi/\pi} \quad \text{Chatter work speed} \quad (12.16)$$

where ω_n is the natural frequency, n is the complete number of waves around the workpiece periphery, and ϕ is the measured phase lag at the chatter frequency ω . Figure 12.13 shows K_s/λ against work speed for $Q = 10$. The number of chatter waves that can be accommodated around the workpiece reduces at high work speeds. Careful adjustment of the speed finds regions where the threshold value of K_s/λ is substantially increased. This means that the grinding force can be larger before chatter occurs.

It should be remembered that K_s increases with grinding width. A grinding width twice as large creates double the grinding force. Therefore, one way to reduce the risk of chatter is to reduce the grinding width.

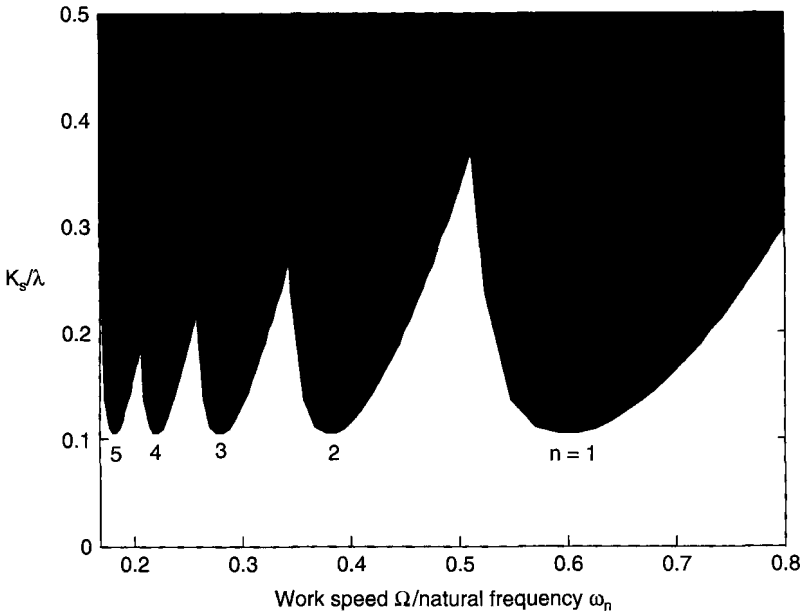


Figure 12.13 Chatter regions for a machine where $Q = 10$.

In plunge grinding with a dynamic magnifier $Q = 10$, the process is completely unstable at low work speeds for values of K_s/λ greater than 0.105.

Reducing Overlap in Traverse Grinding

In traverse grinding, the new cut width does not completely overlap the old cut width. Using an overlap factor μ , the vector OP' in Fig. 12.12 must be replaced by $\frac{K_s}{\lambda} [x(t) - \mu x(t - T)]$. The new vector triangle $OR'P'$ is shown in Fig. 12.14.

The threshold condition for traverse grinding with overlap becomes

$$\frac{K_s}{\lambda} = \frac{-1}{1 + \mu \cos \psi / \cos(\pi - \phi)} \cdot \frac{1}{\text{Re} \left[\frac{x}{x_0} \right]} \quad \text{Overlap threshold} \quad (12.17)$$

The work speed is given by

$$\frac{\Omega}{\omega_n} = \frac{\omega/\omega_n}{1 + n + (\psi - \phi)/2\pi} \quad \text{Overlap work speed} \quad (2.18)$$

and

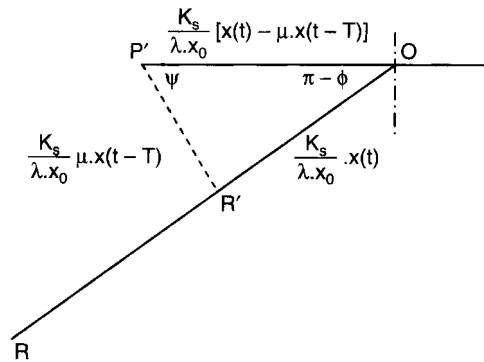


Figure 12.14 Vector diagram for threshold stability with overlap.

$$\psi = \sin^{-1} \left(\frac{\sin(\pi - \phi)}{\mu} \right) \quad \text{Overlap angle} \quad (12.19)$$

It is found that reducing overlap greatly improves stability. This is because the grinding force is reduced as the grinding width is reduced. The effect of adding wheel flexibility is explained in the next paragraph. It is found that the diagram can only be constructed with $\mu < 1$ if values of ψ are possible. The minimum value of K_s/λ is increased from 0.105 to 0.133 by reducing the overlap factor from 1 to $\mu = 0.8$. It is also found that the size of the stable areas is increased.

Of course, contact stiffness of the grinding wheel also reduces as grinding width reduces. This further improves stability for the same reason as adding flexibility at the wheel contact. The effects of contact stiffness and of adding wheel flexibility are discussed in the sub-sections below (Fig. 12.15).

Reducing Grinding Wheel Contact Stiffness

The measured frequency responses should include the effect of deflections of the grinding wheel surface as these are significant and important for the stability of grinding. Although it is not very convenient to apply force measurement directly to the wheel surface or to measure the deflections of the wheel surface directly, flexibility of the wheel surface has a significant stabilising effect. Experiments conducted where the wheel deflections are measured show that these deflections are substantial. If the response curve is derived for a solid wheel of equivalent mass, it is necessary to modify the response curve to allow for the contact stiffness of a real grinding wheel. This is discussed in the next sub-section.

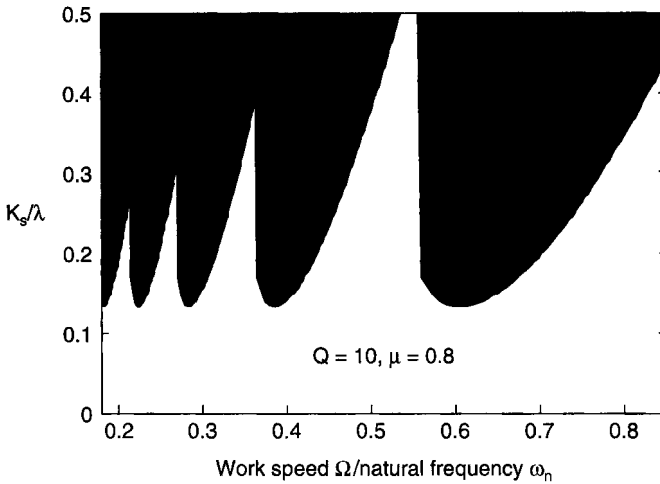


Figure 12.15 Stability threshold with an overlap factor $\mu = 0.8$ and dynamic magnifier $Q = 10$.

The grinding wheel contact stiffness is found to be non-linear as would be expected from contact mechanics (see Chapter 15). To allow for non-linearity, contact stiffness K_c is defined as the gradient of the normal force/deflection curve.

$$K_c = \frac{dF_n}{dx} \quad \text{Wheel contact stiffness} \quad (12.20)$$

An example of contact stiffness for an alumina vitrified grinding wheel is shown in Fig. 12.16.

Adding Flexibility to the Machine System

The effect of grinding wheel flexibility is equivalent to an additional static compliance in the system as illustrated for a spring-mass-damper system in Fig. 12.17. The static compliance in this example does not affect the shape of the response curve but simply shifts the curve to the right. This has the effect of stabilising the process. This argument demonstrates an experimentally validated finding that added grinding wheel flexibility improves stability when the flexibility does not reduce the resonant frequency. The effect of adding flexibility is summarised as follows.

The stability of the grinding process is only improved if elasticity is added at a position where it does not change the resonant frequency. This

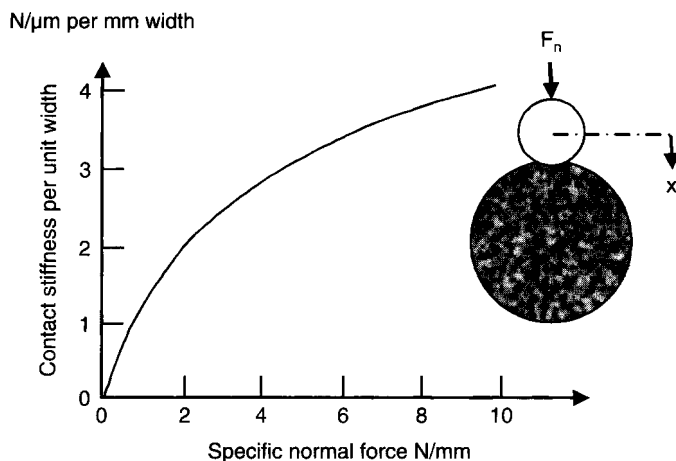


Figure 12.16 Contact stiffness of a vitrified alumina grinding wheel: 2A60K6V, $d_s = 300$ mm, $d_w = 214$ mm (based on Marinescu et al. 2007, Chapter 8; Inasaki 1977).

has been unconsciously achieved in many cases by designing a more elastic grinding wheel although it came to light as a mechanism for stabilising centreless grinding in a work by Rowe (1964). The author compared regulating wheels of radically different stiffness. However, the principle was demonstrated as a way to improve the performance of a hard wheel by Sexton and Stone (1981; Sexton et al. 1982). It was discovered that increasing the elasticity of the grinding wheel or regulating wheel in most cases will not reduce the structural stiffness supporting the main structural mass. The effect is to add a static in-phase deflection to the remaining structural deflection. This causes the frequency response locus to be shifted to the right. This in turn reduces the maximum negative in-phase component of the frequency responses. According to the stability condition, Eqn (12.15), this increases the grinding force that can be applied before the process becomes unstable.

Varying Work Speed or Wheel Speed

The sensitivity to chatter in grinding depends on the particular speed conditions employed. This indicates a possibility of reducing chatter growth by varying work or wheel speed during the grinding process.

Perhaps the simplest technique to operate is to change the grinding speeds to more stable conditions for the finish grinding operation. It has also been shown that it is possible to reduce wheel-regenerative vibration

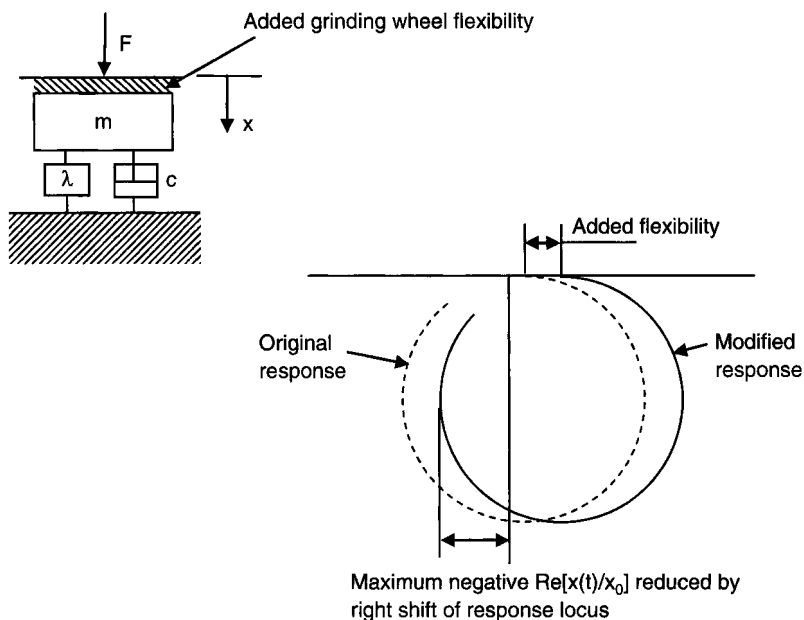


Figure 12.17 Principle of stabilisation of the grinding process by adding grinding wheel flexibility without changing the resonant frequency using a spring-mass-damper model to illustrate a grinding wheel support structure.

by progressively varying wheel speed by $\pm 10\%$ while grinding (Hoshi et al. 1986; Marinescu et al. 2007).

Adding Vibration Damping

Vibration damping greatly reduces resonant amplitudes as demonstrated in Sections 12.4 and 12.5. The best time to incorporate damping is when the machine is designed. However, sometimes damping can be added later. For example, it is possible to add a vibration damper to a troublesome wheel head. Another possibility is to add an auxiliary tuned mass to a troublesome spindle to reduce the effect of a particular resonance. Such devices can be simple and very effective (Den Hartog 1956).

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13 Centreless Grinding

13.1 Introduction

Application

Centreless grinding is a fast and efficient process for precision batch and mass production grinding. Wide wheels allow substantial removal rates. It has another major advantage in that centres are not required as in centre grinding. Location of the workpiece is provided from the newly ground surface itself. This eliminates a production operation. It also avoids shape errors associated with the centre holes.

Many materials and parts of various shapes and sizes are produced by centreless grinding particularly for bearing and automotive industries. Workpiece surfaces can be either internal or external cylindrical shapes. The process is often automated for production with minimal supervision through the day and night. Wide wheels mean that wheel wear is low and high accuracy is maintained for long periods particularly when using in-process gauging. As wheel wear is low and grinding wheels are often large, wheels for centreless grinding are usually manufactured from conventional alumina and silicon carbide. Super-abrasives are increasingly being employed for very high wheel speeds and even higher production rates.

This chapter reviews the different centreless processes and then gives detailed insights into external plunge grinding and the effects of machine design. Several techniques are reviewed for investigating the possibility of lobing becoming a problem and advice is given on how to overcome these if experienced.

Research by the Author

External centreless grinding was extensively investigated by the author between 1961 and 2007. Relevant references are listed at the end of this chapter particularly on the effects of machine design and set-up on accuracy and production rate. Early publications in the 1960s formed the basic methodology employed for subsequent research on roundness. Studies by the author included the first computer simulation (Rowe and Barash 1964), dynamic analysis (Rowe and Koenigsberger 1965), experimental roundness results, simulations of complete feed cycles, contact interference conditions

(Rowe et al. 1965), and geometric stability charts for set-up conditions and speed selection (Richards et al. 1971; Rowe and Richards 1972). Machine compliance effects on size, roundness, and roughness were determined (Rowe 1973). High-speed grinding was developed and led to optimisation strategies (Rowe et al. 1985). Limit charts formed the basis for adaptive process control (Rowe et al. 1987a). Improvements through radical machine design were explored (Rowe et al. 1987b). A major research review summarised international developments in the field (Rowe et al. 1989).

Research by Other Authors

Centreless grinding is a fascinating subject because of the unique rounding action. It is not possible to review every article on the subject. In addition to contributions by the author significant research includes the following. A strong rounding action was shown to be provided by the set-up geometry (Dall 1946; Yonetsu 1959). There is also the possibility of geometric instability (Gurney 1964; Rowe 1964; Becker 1965). The effect of machine vibration behaviour was analysed and related to the type of vibration development (Miyashita 1965; Furukawa et al. 1971; Gimenez and Nieto 1995). However, with optimum machine design and correct set-up, it was shown that very high roundness accuracy can be achieved (Yoshioka et al. 1985). Above and below centre grinding is possible and has been extensively investigated (Johnson 1989). Some personal research was reviewed by Gallego (2007). A recent article gave a genuinely new approach to process simulation solving the equilibrium forces at each contact point (Jameson et al. 2008).

Most work has focussed on plunge-feed grinding. Through-feed grinding is an important process and has been separately investigated (Meis 1980; Koenig and Henn 1982; Goodall 1990).

13.2 Centreless Grinding Processes

External Centreless Grinding

A control wheel and a work plate support the workpiece as in Fig. 13.1. The workpiece is pushed against the grinding wheel by the control wheel otherwise known as a regulating wheel.

The control wheel controls work speed by friction. In a normal operation, the workpiece has the same surface speed as the control wheel.

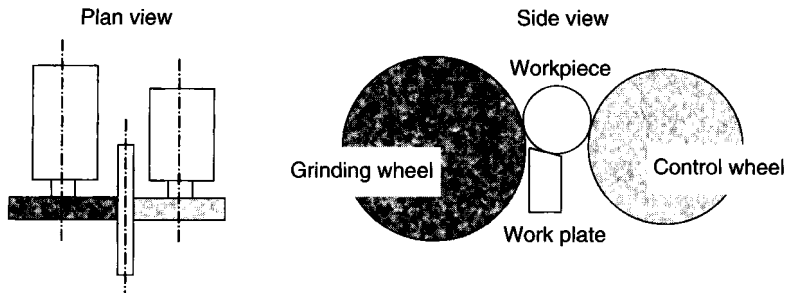


Figure 13.1 The external centreless grinding process.

The control wheel is usually a rubber-bonded wheel designed to achieve a reasonably high coefficient of friction. The function of the control wheel is to slow the workpiece down rather than drive it. If the friction is insufficient the workpiece can spin out of control (Hashimoto et al. 1998; Gallego 2007; Marinescu et al. 2007, chapter 19). The problem is overcome by cleaning and re-dressing the control wheel. The problems of spinning out of control tend to be experienced less with large work plate angle γ and small tangent angle β .

A problem that can arise in centreless grinding is when the workpiece fails to turn on initial contact with the grinding wheel. This is a fairly unusual problem but can occur when the workpiece is exceptionally heavy. If a problem should arise it is important to stop the process quickly before any damage can occur. However, the problem is usually foreseen and overcome in the process planning stage. The problem can be overcome by providing an additional drive mechanism for very large and bulky workpieces. It may also be possible to pre-load the workpieces against the control wheel to ensure work rotation before contact with the grinding wheel.

A rubber-bonded wheel has the additional advantage that flexibility is introduced into the system which helps to maintain a stable process. The principle of introducing flexibility through a soft grinding wheel was analysed in Chapter 12.

Plunge grinding and through-feed grinding may be performed in external centreless grinding and are described below.

The workpiece centre is usually set higher than the grinding wheel, and the control wheel centres to speed up the rounding process. However, the process is most stable when the workpiece is set at zero height above centre.

Internal Centreless Grinding

For internal grinding, a control wheel and a support roll support the workpiece as in Fig. 13.2. An additional pressure roll is used to firmly locate the workpiece. The external surface needs to be finished before the internal surface as the external surface becomes a datum surface for the internal surface. Errors on the external surface are reflected in the shape of the internal surface.

External Shoe Grinding

Shoe centreless grinding is used for plunge-grinding workpieces of short length to diameter ratio. External shoe centreless grinding is illustrated in Fig. 13.3. The workpiece is supported on two stationary shoes

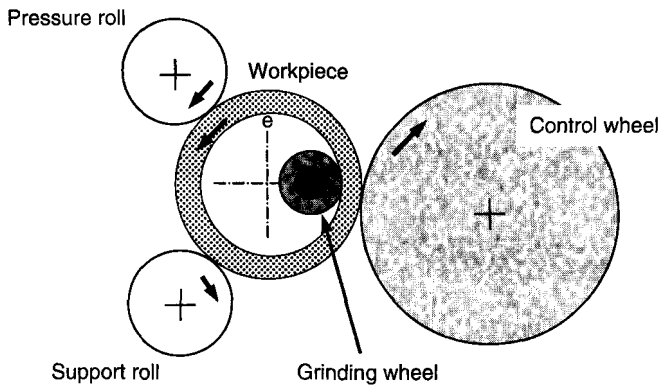


Figure 13.2 Internal centreless grinding.

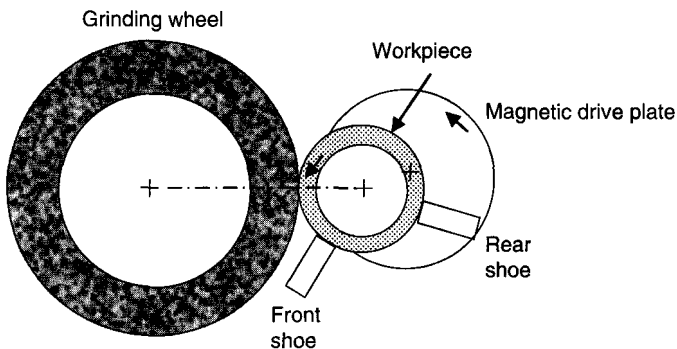


Figure 13.3 External shoe grinding.

and located axially by a magnetic drive plate. The work speed is controlled by the speed of the magnetic drive plate.

Internal Shoe Grinding

As in the external shoe grinding process, the workpiece is supported on two shoes and located against a magnetic drive plate. The internal process is illustrated in Fig. 13.4.

13.3 Set-Up Geometry and Removal Parameters

Contact Geometry

The basic principle of the various centreless grinding processes is the same but there are significant differences in the set-up geometry. The work height and the contact angles are shown in Fig. 13.5 for an external centreless process.

Work Plate Angle

The contact angles are selected to obtain a positive rounding action during the grinding process. The work plate is usually inclined at an angle $\gamma = 30^\circ$. Some work plate angles are more favourable than others for rounding and 15° may be considered, for example. A method is given in Section 13.10 for evaluating contact angles for achieving roundness (see Fig. 13.20.)

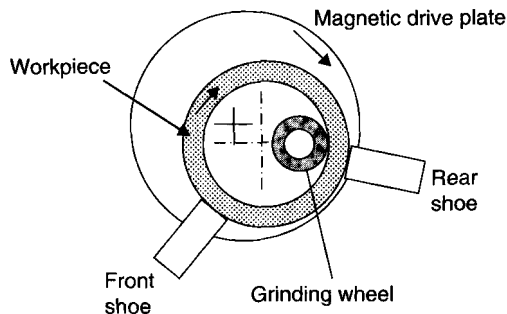


Figure 13.4 Internal shoe grinding.

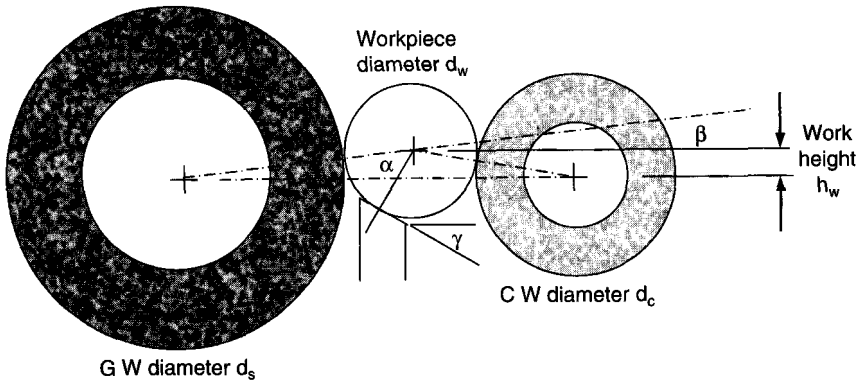


Figure 13.5 Geometric configuration.

The method is straightforward and can be achieved manually using a calculator to calculate stability for whole numbers of waves.

Work Height

Work height h_w is usually set above centre for rapid rounding. Work height is set to give the best angle β for rounding.

Tangent Angle

The angle β is known as the tangent angle. The range $\beta = 6-8^\circ$ is usually considered to be optimal for finish grinding. It has been suggested that performing grinding operations at two different heights reduces roundness errors (Harrison and Pearce 2004). For example, rough grinding may be performed on a machine set to $\beta = 4^\circ$. Alternatively, the reverse procedure may be employed where a larger angle is used for rough grinding to achieve rapid rounding and a smaller more stable angle used for finish grinding.

The work height is determined from

$$h_w = \frac{\beta/2}{\frac{1}{d_s + d_w} + \frac{1}{d_c + d_w}} \quad \text{Work height} \quad (13.1)$$

where β is expressed in radians.

Example 13.1 Determine the height of the workpiece above centre to achieve a tangent angle of 7° where the wheel diameter is 300 mm,

the control wheel diameter is 175 mm, and the workpiece diameter is 25 mm.

$$h_w = \frac{7 \times \pi / (2 \times 180)}{\frac{1}{300 + 25} + \frac{1}{175 + 25}} = 7.56 \text{ mm or } 0.3024 \text{ in.}$$

The angle α is given by $\alpha = \pi/2 - \gamma - \beta_s$ where $\sin \beta_s = 2 \cdot h_w / (d_s + d_w)$.

Experimental Rounding Investigation

The rounding characteristics of the process can be demonstrated very powerfully by grinding workpieces with a large controlled error. This was first shown by the author using workpieces where a flat 46 μm deep had been previously ground along the length (Rowe et al. 1965). The experiments have to be undertaken with precautions to ensure that the flat on the workpiece is not adjacent to the grinding point or to one of the other contact points at the commencement of grinding. This would incur the risk that the workpiece would fail to turn. However, with this simple precaution the technique is a very effective way to compare the rounding action for different set-up geometries. Some experimental results are shown in Fig. 13.6 for two different work plates. It can be seen that a 30° work plate gave a stronger and more stable rounding action than a 20° work plate.

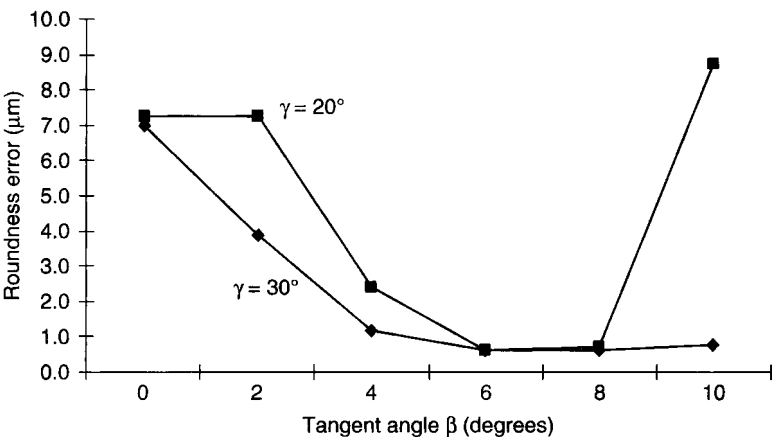


Figure 13.6 Experimental roundness errors grinding a workpiece with a large initial flat error 46 μm deep.

Removal Parameters for Plunge Grinding

In centreless grinding, material is removed from the diameter rather than from the radius as in centre grinding. This introduces a factor of 2 into the depth of cut that does not appear in the centre-grinding expression.

$$a_e = \frac{1}{2} \cdot \pi \cdot d_w \cdot \frac{v_f}{v_w} \quad \text{Depth of cut} \quad (13.2)$$

$$Q_w = a_e \cdot b \cdot v_w = \pi \cdot d_w \cdot b \cdot \frac{v_f}{2} \quad \text{Removal rate} \quad (13.3)$$

where b is the grinding width, v_f is the plunge feed rate, and v_w is the work speed. Other removal parameters are the same as for other grinding operations.

Example 13.2 Determine feed rate and depth of cut required to achieve a specific removal rate of 10 mm³/s per mm grinding width when centreless plunge grinding an easy-to-grind material of 50 mm diameter at a grinding wheel speed of 60 m/s and a work speed of 0.3 m/s.

$$Q'_w = \pi \cdot d_w \cdot \frac{v_f}{2} = 10 = \pi \times 50 \times v_f / 2$$

$$v_f = 2 \times 10 / (\pi \times 50) = 0.1273 \text{ mm/s or } 0.3055 \text{ in./min}$$

$$a_e = \frac{1}{2} \cdot \pi \times 50 \times \frac{0.1273}{300} = 0.03333 \text{ mm or } 0.00133 \text{ in}$$

13.4 Work Feed

Plunge Feed

Plunge feed is illustrated for grinding a stepped shaft in Fig. 13.7. The plunge feed can be implemented either through a grinding wheel-head feed slide or through a control wheel-head feed slide. The feed cycle may consist of a roughing feed, a fine feed, and a spark-out dwell followed by a rapid retraction as in centre grinding. In some cases several feed rates may be employed. The fine feed and dwell are essential for close tolerances and best roundness.

The set-up for plunge grinding shows the use of an end stop for axial location of the workpieces. The workpieces are maintained in position against the end stop by employing a small tilt angle of the control wheel.

Through Feed

A set up for through feed is shown in Fig. 13.8.

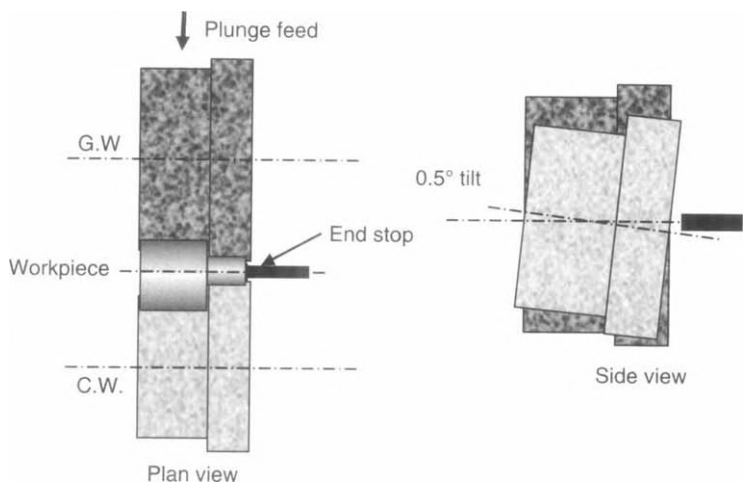


Figure 13.7 Grinding a stepped shaft.

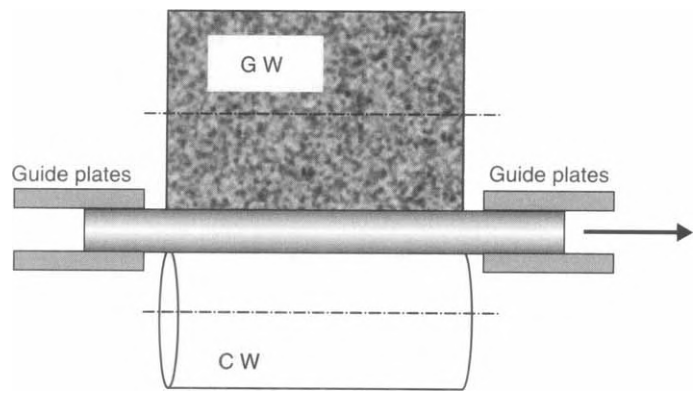


Figure 13.8 Through-feed grinding of long and short shafts.

Tilt Angle

The workpiece is fed axially between the grinding wheel and the control wheel. The axis of the control wheel is inclined at a small angle Φ to the horizontal so that there is an axial through-feed component of the control wheel motion. The through-feed rate v_f depends on the angle Φ and the surface speed v_{cw} of the control wheel according to

$$v_f = v_{cw} \cdot \sin \Phi \quad \text{Through-feed rate} \quad (13.4)$$

Example 13.3 Determine the tilt angle required to achieve a through-feed rate of 10 mm/s when the surface speed of the control wheel is 0.3 m/s.

$$\Phi = \sin^{-1}(0.01/0.3) = 0.0333 \text{ rad or } 1.91^\circ$$

The work speed v_w is approximately equal to the surface speed of the control wheel v_{cw} and is given by $v_w = v_{cw} \cdot \cos \Phi$.

Further information on through-feed grinding is available from research at the University of Aachen (Meis 1980; Koenig and Henn 1982). Further experimental investigation of through-feed grinding was conducted at the Indian Institute of Technology (Pande and Lanka 1989) and at Liverpool Polytechnic (Goodall 1990).

13.5 Wheel Dressing

Grinding Wheel Dressing

The principles of grinding wheel dressing follow the same lines as for other grinding processes previously described in Chapter 4. While single-point dressing tools may be considered best for form dressing, the large size of centreless grinding wheels can lead to rapid tool wear. For this reason the full range of dressing tools should be considered and the type of dressing tool selected according to the operational needs. Multi-point tools give a more consistent dressing performance over a longer life than single-point dressing tools and do not require frequent adjustments to achieve a sharp cutting point.

Grinding wheels may be dressed using a cam plate follower to achieve the desired form. An alternative is to employ a CNC controlled dressing motion.

Control Wheel Dressing

Control wheel dressing is rather more complex in centreless grinding due to the tilt angle Φ employed for the feed motion described above. This is because the axial cross-section of the regulating wheel is a hyperboloid rather than a rectangle as would be the case with zero angle of inclination. In consequence, the dressing line must be set up accurately to ensure line contact for the workpiece. Failure to set the dressing line correctly leads to workpiece taper particularly in plunge grinding.

The dressing position adopted depends on the grinding machine manufacturer as the dressing position has implications for the design and construction of the machine. The simplest arrangement to understand is where the dressing tool is traversed along a horizontal path at Position A in Fig. 13.9.

Position A

Using Position A for dressing, the dressing tool is fed along a horizontal path directly opposite and at the same height as the contact of the workpiece with the control wheel. This ensures that the plan view of the control wheel is correct to achieve a straight line of support to the workpiece at the correct height h_d for dressing and for the work contact. The dressing height is related to the work height h_w by

$$h_d = h_w \cdot \frac{d_c}{d_c + d_w} \quad \text{Dressing height} \quad (13.5)$$

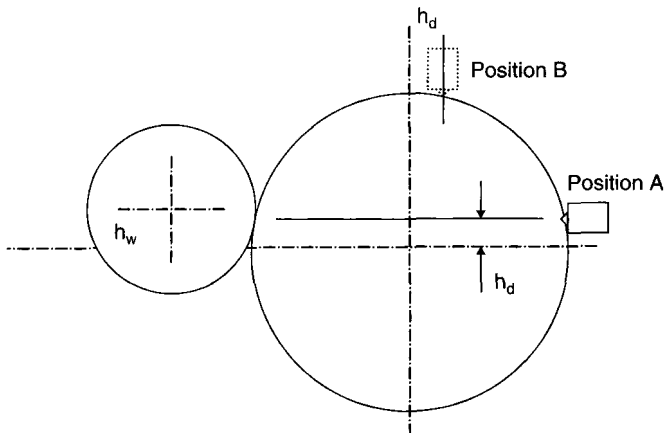


Figure 13.9 Alternative positions for dressing the control wheel.

Example 13.4 Determine the dressing height for Position A where the work height is 7.56 mm, the control wheel diameter is 175, mm and the work diameter is 25 mm.

$$h_d = 7.56 \times \frac{175}{175 + 25} = 6.615 \text{ mm or } 0.265 \text{ in.}$$

Position B

When dressing is carried out at Position B, the dressing slide must be rotated relative to the plane of the line of contact. The rotation is made in a plane parallel to the tangent to the control wheel at the point of contact with the dressing tool. The adjustment depends on the workpiece diameter. The dressing angle for Position B as given by Jessup (King and Hahn 1986) is

$$\Phi_d = \frac{\Phi}{\sqrt{1 + d_w/d_c}} \quad \text{Position B dressing} \quad (13.6)$$

Example 13.5 The control wheel axis is inclined 2° in the vertical plane to provide a through-feed motion to the workpiece. Determine the angle to which the dressing slide must be adjusted in the horizontal plane for dressing at Position B. The work diameter is 25 mm and the control wheel diameter is 175 mm.

$$\Phi_d = \frac{2}{\sqrt{1 + 25/175}} = 1.871^\circ$$

Control Wheel Run-Out

It has been shown that accuracy in centreless grinding is strongly dependent on control wheel run-out (Hashimoto et al. 1983). This remarkable discovery was made by dressing the control wheel with the grinding wheel. It was found that much greater accuracy was achieved than could previously be obtained. Roundness errors were reduced on average from 1.7 to 0.2 μm with high precision dressing, and surface roughness was reduced from 0.32 to 0.12 μm Ra. The new dressing method also improved wear resistance of the control wheel. This result has been confirmed by experiments conducted by the author.

13.6 Machine Design, Roundness Errors, and Productivity

Machine Design

Machine design has a strong effect on both accuracy and removal rates achievable in grinding. Some of the effects have already been described in Chapter 10 on “Machine developments.” The benefits of a stiff machine include the faster achievement of size and roundness tolerances. Figure 13.10 additionally demonstrates the benefits of machine stiffness coupled with the benefits of high-precision hydrostatic bearings, slide-ways, and feed drives. The very stiff machine is seen to be better for low roundness errors and an improved working range of operating speeds. The improved range of working speeds means that higher wheel speeds and feed rates can be employed. This means that higher removal rates can be employed.

One reason for the improvements shown is that the very stiff machine had a dominant natural frequency of 500 Hz compared with 78 Hz for the standard machine. Also the very stiff machine was designed to achieve high damping so that vibration levels were reduced. The design of the machine is described in more detail in Chapter 10 and also in other references (Rowe et al. 1987a; Marinescu et al. 2007, Chapter 19).

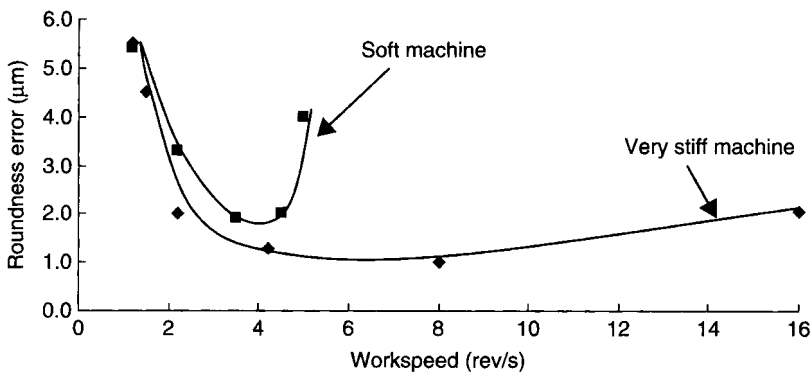


Figure 13.10 Effect of machine stiffness on roundness and operating speeds. Grinding conditions: Tangent angle $\beta = 8^\circ$, work plate $\gamma = 30^\circ$, material EN30B, hardness Rc 48, $d_w = 25.4 \text{ mm} \times 50 \text{ mm}$ long, GW A60LV, $d_s = 305 \text{ mm}$, CW A80RR, $d_c = 178 \text{ mm}$, $v_s = 28.7 \text{ m/s}$, $N_s = 29.67 \text{ rev/s}$, Coolant emulsion 2.5%, stock removal 0.254 mm on diameter by plunge feed, spark-out 8 s.

In Chapter 12, the benefits of increased wheel flexibility were argued. This is not contradictory to the need for a stiff machine as explained in Chapter 12.

Work Speed and In-Feed Rate

Generally observed effects of work speed and in-feed rate in grinding are described in Chapter 2. Figures 2.13 and 2.14 show the limits of grinding achieved for a range of work speeds and in-feed rates. Intermediate work speeds are generally preferred to very low work speeds or very high work speeds. This is because low work speeds increase the dangers of thermal damage and high work speeds increase the risk of chatter.

The previous findings are confirmed by Fig. 13.10 but the high stiffness machine has the advantage of a wider operating envelope for work speed. Roundness accuracy is similar at low work speeds for the two machines. This is partly because there are insufficient revolutions at low speed for rounding. There is another reason why low work speeds are less favourable. Low work speeds lead to increased temperatures and increase the risk of thermal damage (Rowe et al. 1987a). Severe thermal damage leads to an increased risk of vibration associated with the thermal damage.

Vibration problems are also experienced at high work speeds due to chatter. The stiff machine had better chatter resistance so that good accuracy was achieved at much higher speeds. This illustrates that roundness is not only a question of rounding geometry but also of the dynamic performance of the grinding machine. As production rate is related to work speed and wheel speed, there is an implication that a dynamically stiff machine allows much higher levels of productivity.

13.7 Convenient Waviness

Taking the precautions described in Section 13.3 it was shown that rounding tendencies could be effectively investigated using workpieces with a large initial error. Some comparative roundness values are given in Fig. 13.6.

It was discovered from experiments that while a grinding geometry might be stable, it was difficult to eliminate a roundness error that is convenient to a particular set-up (Rowe and Barash 1964). This gives rise to the term “convenient waviness.” Two examples are illustrated in Fig. 13.11.

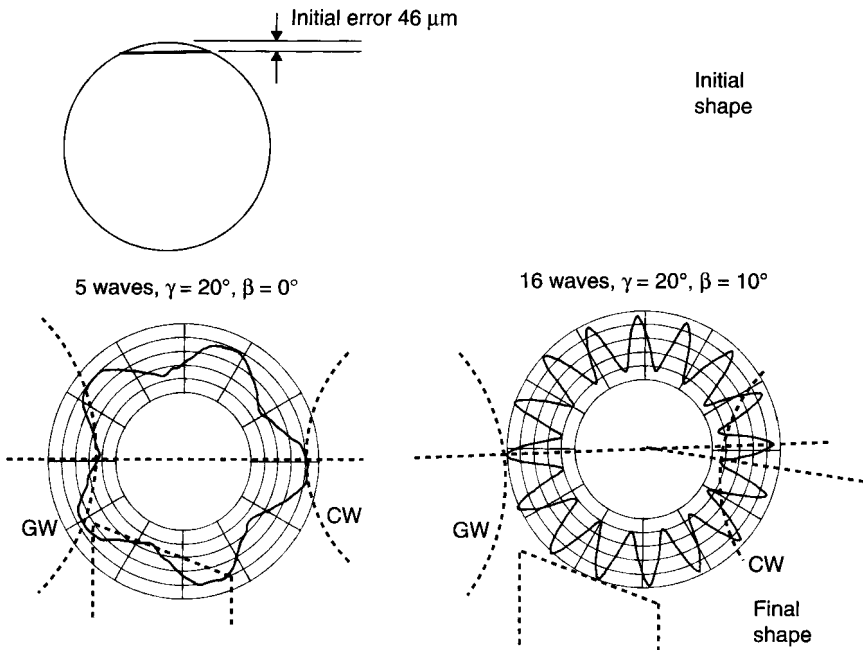


Figure 13.11 Examples from Fig. 13.6 of convenient waviness arising from the set-up geometry.

In the five-wave grinding example, the set-up geometry has no rounding effect because the five waves represent a constant diameter shape. Constant diameter shapes are quickly rounded up when the tangent angle is increased to 8° , but at 10° with a 20° work plate, it was discovered that a 16-wave shape was convenient.

To explain convenient waviness, we need to consider the corrective effects at the work plate contact and at the control wheel contacts in turn. For convenience, angles are given in degrees.

Work Plate Corrective Action

With reference to Fig. 13.5, $\alpha = 90 - 20 - 4 = 66^\circ$. Taking the 16-wave shape as an example, the angular pitch of the waviness is $p = 360/16 = 22.5^\circ$. The number of waves between the grinding contact and the work plate contact is therefore $66/22.5 = 2.93$. This is almost a whole number. In other words, if there is a peak at the grinding point there will also be a peak at the work plate.

The movement at the grinding contact due to an error at the work plate is

$$K_1 = \frac{\sin \beta}{\sin(\alpha + \beta)} \cdot \delta_1 = \frac{\sin 10^\circ}{\sin 76^\circ} \times \delta_1 = 0.1736 \times \delta_1 \quad (13.7)$$

Work plate action

It can be seen that a peak at the work plate will reinforce the magnitude of a peak at the grinding point. Therefore, a whole number value of α/p reinforces a convenient waviness and is to be avoided.

Control Wheel Corrective Action

The movement at the grinding contact due to an error at the control wheel is

$$-K_2 = \frac{-\sin \alpha}{\sin(\alpha + \beta)} \cdot \delta_2 = -\frac{\sin 66^\circ}{\sin 76^\circ} \times \delta_2 = -0.9145 \times \delta_2 \quad (13.8)$$

Convenient waviness action

The number of waves between the grinding contact and the control wheel contact is $(180 - \beta)/p = 170/22.5 = 7.56$. This is approximately 7.5. In other words, when there is a peak at the grinding contact there is a trough at the control wheel contact. The error at the control wheel contact is therefore in a negative sense to the error at the grinding contact. The control wheel action from Eqn (13.8) produces a positive feedback to the grinding error and reinforces the waviness. It may be concluded that a half-integer remainder for the number of waves between the grinding contact and the control contact reinforces a convenient waviness and is to be avoided.

To summarise, convenient waviness arises when the following two conditions are approximately fulfilled:

$$\alpha/p = \text{a whole number}$$

$$(\pi - \beta)/p = \text{a whole number} + 0.5$$

where the pitch $p = 2\pi/n$ for n waves on the workpiece.

13.8 Simulation of the Rounding Action

Simulation was introduced in the early 1960s because it allowed non-linear effects of geometry and time delays on the rounding action to be investigated (Rowe and Barash 1964; Rowe et al. 1965). With modern

computers the technique is relatively simple. Basically the circumference of the workpiece is divided into 360 segments. The radius value at each segment is calculated in turn. It is possible to define an initial workpiece shape and take account of different feed cycles. The process is summarised below.

Importantly, the technique allows deflections of the machine system to be taken into account. This is essential for realistic results because deflections are always substantial in comparison with depth of cut.

Basic Simulation Equation

The reduction in radius at the grinding point is termed $r(\theta)$, where θ defines the workpiece rotation and starts from zero at the commencement of grinding. The rotation θ steadily increases with duration of grinding. The reduction in radius is

$$r(\theta) = X(\theta) - x(\theta) + K_1 \cdot r(\theta - \alpha) - K_2 \cdot r(\theta - \pi + \beta) \quad (13.9)$$

Radial movements

where $X(\theta)$ is the infeed at the grinding contact, $x(\theta)$ is the machine deflection, K_1 and K_2 are the movements defined in the previous section, $r(\theta - \alpha)$ is the radius error at the work plate, and $r(\theta - \pi + \beta)$ is the radius error at the control wheel contact.

The deflections for an elastic machine structure are approximately proportional to the depth of cut. This allows the simulation to be simplified by assuming a value of the machining stiffness factor K .

$$K = \frac{\text{true depth of cut}}{\text{set depth of cut}} = \frac{a_e}{a_c} \quad \text{Machining stiffness factor} \quad (13.10)$$

where the true depth of cut is given by $a_e = r(\theta) - r(\theta - 2 \cdot \pi)$ and the set depth of cut is given by Eqn (13.9) when the deflections are set equal to zero. Eliminating $x(\theta)$ from Eqns (13.9) and (13.10) yields the basic equation used for simulation of the rounding action;

$$r(\theta) = K \cdot [X(\theta) + K_1 \cdot r(\theta - \alpha) - K_2 \cdot r(\theta - \pi + \beta) - r(\theta - 2\pi)] + r(\theta - 2\pi) \quad \text{Basic rounding equation} \quad (13.11)$$

It is possible to measure K as described in Chapter 2. Basically, if the grinding force stiffness is high and the machine loop stiffness is low, then K is low. K is always greater than 0 and always less than 1. A large grinding width causes a low value of K . A low value of K of the order of

0.1 means the rounding action is slow. The value of K can also be deduced by comparing shapes from simulation with measured shapes. The two methods give closely similar results. Further discussion of the effect of workpiece deflections is given in the sections on the work-regenerative effect below.

Interference and Loss of Contact Constraints

It was soon realised that measured workpiece shapes were smoother than early shapes predicted from simulation. This problem was overcome and better accuracy was achieved by taking account of the interference effect of neighbouring points on the point being calculated. It was also necessary to eliminate the theoretical possibility of replacing material on the workpiece. This meant it was necessary to check at each point of calculation that these constraints were not offended. The metal replacement restriction simply requires negative removal predictions to be ignored. The interference restrictions have to be applied at all three points of contact. The principle of grinding wheel interference is illustrated in Fig. 13.12. A large error on the workpiece near to the grinding point causes interference I_ψ at an angle Ψ from the grinding point. This means that grinding not only

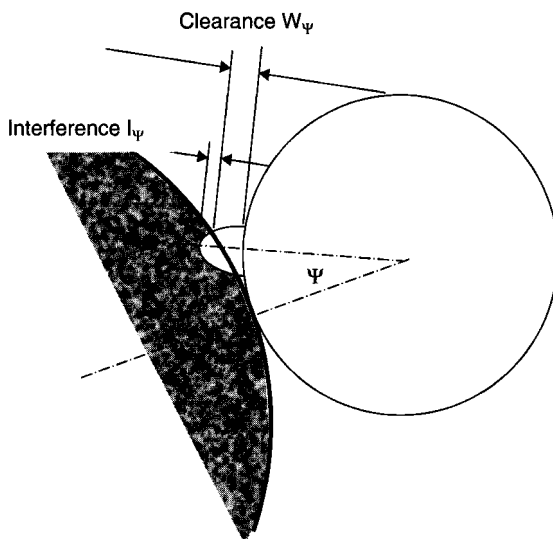


Figure 13.12 Grinding wheel interference I_ψ at an angle Ψ from the main grinding contact point.

takes place at the main grinding point but also at the point where the interference occurs. The interference value is given by

$$I_{\Psi} = r(\theta - \Psi) + W_{\Psi} - r(\theta - 2\pi - \Psi) \quad \text{Interference} \quad (13.12)$$

where $W_{\Psi} = \left[\frac{1}{\cos \Psi} - 1 \right] \cdot \frac{d_e}{2}$ is the clearance at the nearby point due to the curvature of the grinding wheel and the workpiece given by the effective diameter d_e . The amount of material removed where the grinding wheel interference occurs is $K \cdot I_{\Psi}$.

Interference also occurs in the work plate area and in the control wheel contact area. Interference in these areas means the values $r(\theta - \alpha)$ and $r(\theta - \pi + \beta)$ must be replaced by larger values taking the interference and curvature calculations into account.

A simulated workpiece shape is compared with a measured shape from a grinding experiment in Fig. 13.13. It can be seen that the simulation gives a remarkable indication of the rounding action in centreless grinding. However, there are differences.

The main difference is that the experimental shape is smoother in nature than the simulated shape. This difference is largely related to the different deflection behaviour of the system at higher frequencies. It will be recalled from Chapter 12 that deflections are larger in the region of a system resonance. These deflections can either attenuate or amplify the rounding action in this region.

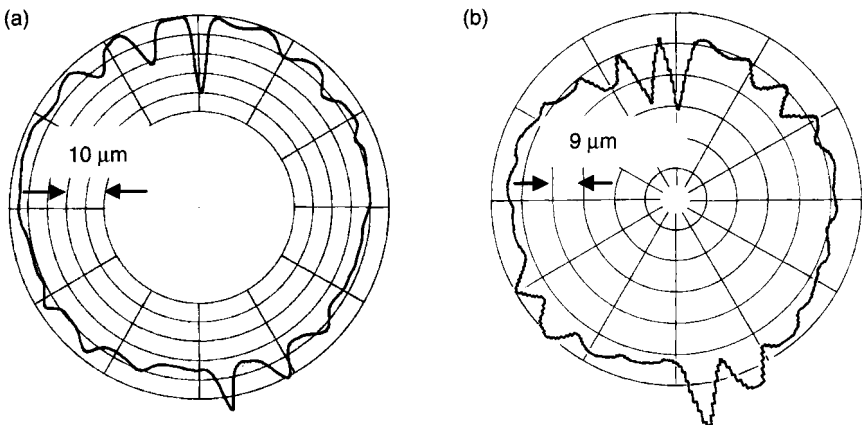


Figure 13.13 (a) Experimental and (b) simulated shapes: $\gamma = 20^\circ$, $\beta = 8^\circ$, $K = 0.08$. Initial shape: workpiece with flat $57.4 \mu\text{m}$ deep. Stock removal: $56 \mu\text{m}$ on diameter.

The conclusion is that the simulation accurately reflects the rounding action in the absence of attenuation or amplification from the machine system dynamics. Since dynamic deflections are specific to the machine and simulation results are specific to the set-up geometry, we have a useful way to differentiate the two effects.

Simulation Results

Examples of roundness results by simulation are shown in Fig. 13.14. The resulting workpiece shapes were subjected to Fourier analysis to show the amplitudes of the different number of waves that make up the final shape. Looking at a measured shape, it is not obvious that so many different harmonics contribute to the final shape. A shape may appear to be predominately five lobes. However, analysis into harmonics for such a case at zero tangent angle β shows that all the odd harmonics contribute to the shape but with reducing amplitude at higher frequency.

It can be seen that when the tangent angle β is greater than 6° , the set-up geometry is susceptible to higher order even number of waves. In practice, the susceptibility to higher order waves becomes more dependent on work speed and dynamic deflections.

The aim of this chapter is to understand how to avoid roundness problems without requiring the reader to necessarily understand all the mathematics.

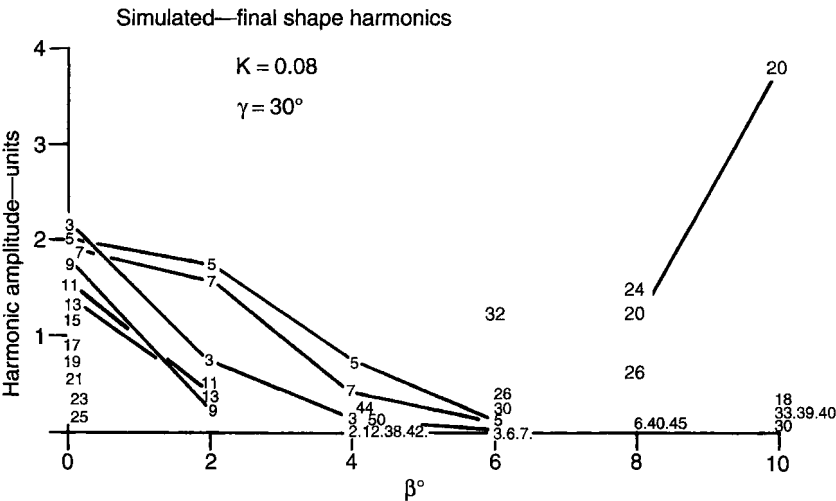


Figure 13.14 Final workpiece shapes by simulation analysed into number of wave harmonics. Initial shape workpiece with a flat and a 300 work plate.

However, for completeness and for the sake of those who wish to explore the subject in greater depth, the following basic relationships are presented. From these relationships, a simple technique is demonstrated for prediction of roundness instability. Rules are extracted for avoiding lobing.

13.9 The Shape Formation System

Grinding vibrations were analysed in Chapter 12 and advice given for avoiding vibration problems. In this chapter, consideration is given to the unique shape formation process in centreless grinding. As in Chapter 12, dynamic relationships are stated for radius reduction r , depth of cut a_e , grinding force stiffness K_s , machine loop stiffness λ , and grinding force F_n . Since the dynamic analysis considers errors from the steady state, feed rate is ignored. A sinusoidal waviness is assumed and the analysis determines whether the waviness will grow or whether the waviness will diminish. Equation (13.9) for the radius error $r(t)$ simplifies to

$$r(t) = -x(t) + K_1 \cdot r(t - T_1) - K_2 \cdot r(t - T_2) \quad \text{Waviness} \quad (13.13)$$

$$F_n = K_s \cdot [r(t) - r(t - T)] \quad \text{Grinding force} \quad (13.14)$$

$$x(t) = F_n / \lambda(\omega) \quad \text{Deflections} \quad (13.15)$$

where grinding time $t = \theta/\Omega$. The time delays T_1 , T_2 , and T correspond to rotation through the angles α , $\pi - \beta$, and 2π . The value of T is given by $T = 2\pi/\Omega$, where Ω is the work speed in radians per second. The other delays are $T_1 = \alpha/\Omega$ and $T_2 = (\pi - \beta)/\Omega$. Vibration frequency and waviness n are related by $n = \omega/\Omega$.

Shape formation is a closed-loop system (Fig. 13.15).

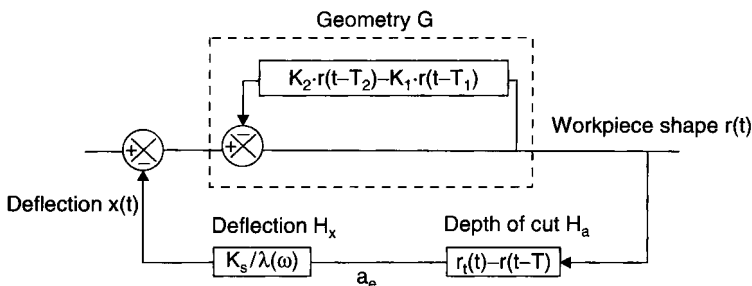


Figure 13.15 Shape formation is a closed-loop dynamic system.

The differences between centreless grinding and centre grinding are the two feedback terms in the geometry function G . The geometric loop gives rise to the possibility of geometric instability.

A convenient way to solve the relationships is by expression in Laplace form, then replacing the Laplace operator s by $j\omega$. Substituting for the term $\omega T = 2\pi\omega/\Omega = 2\pi n$ allows frequency to be expressed directly as n waves around the workpiece circumference. Similarly, $\omega T_1 = n \cdot \alpha$ and $\omega T_2 = n \cdot (\pi - \beta)$. The substitution $n = \omega/\Omega$ leads to the following transfer functions.

$$G = \frac{1}{1 - K_1 e^{-j\alpha n} + K_2 e^{-j(\pi - \beta)n}} \quad \text{Geometric function} \quad (13.16)$$

$$H_a = 1 - e^{-j.2\pi n} \quad \text{Depth of cut function} \quad (13.17)$$

$$H_x = \frac{K_s}{\lambda(j\omega)} \quad \text{Deflection function} \quad (13.18)$$

The significance and stability of each term can be made clearer by graphical representation as shown in the next section.

13.10 Stability of the Rounding Process

Stability of a Closed Loop System

The stability of a closed loop system is controlled by feedback. A proportion of the output subtracted from the input stabilises the output. This is illustrated for a general feedback loop in Fig. 13.16.

When the feedback is larger than the output and changed to a negative sense, the net feedback adds to the input leading to unstable growth. The stability condition is summarised by the Nyquist encirclement criterion. When the open-loop feedback gain crosses the real axis with increasing frequency in a clockwise direction, stability requires the gain H in Fig. 13.16 to be greater than -1 . When $H = -1$, a sinusoidal input forces an infinite response. This condition is termed marginal stability as the output only grows if there is a sinusoidal forcing input. When $H < -1$, the output grows even without an input. The more negative the value of H when it crosses the real axis, the faster the growth rate.

The application of this principle allows instability and marginal instability to be demonstrated graphically as in the following examples.

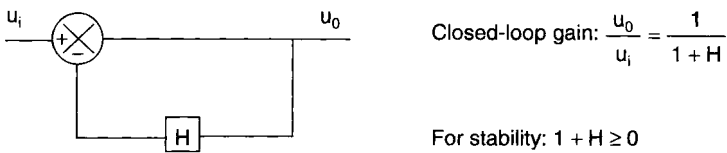


Figure 13.16 Requirement for closed loop stability.

Geometric Instability

The geometric rounding function $1/G$ from Eqn (13.16) is a measure of how quickly waviness will be eliminated. The real part of the function is the rate of decay of the amplitude when positive and the rate of growth when negative. The phase angle is a measure of the wave precession around the workpiece at each revolution. A sinusoidal solution is found when the growth rate is zero and the phase angle is zero. A sinusoidal wave then exists and has constant amplitude. For this condition, there is no rounding action. In Fig. 13.17 a solution is found when $n \approx 15.9$ which is close to the whole number 16 but requires a slow precession of the wave. Precession involves depth of cut variations.

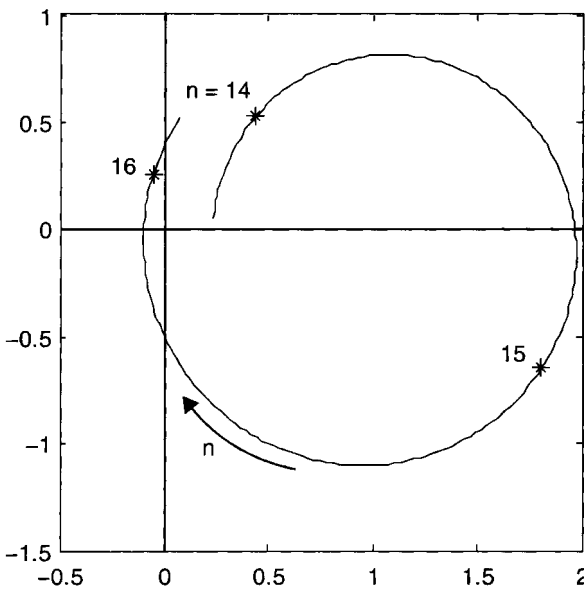


Figure 13.17 Geometric instability is indicated by a negative real value of the rounding parameter $1/G$ at $n = 16$ for $\gamma = 20^\circ$ and $\beta = 10^\circ$.

The function $1/G$ when plotted is approximately circular. A portion of the geometric rounding function is shown in Fig. 13.17 for waves between 13.8 and 16.1. Instability is indicated by the clockwise encirclement of the origin for 16 waves.

The rounding function is negative for 16 waves indicating an unstable condition whereas the rounding function is stable for 14 waves. However, the rounding function is strongly positive for 15 waves meaning that this shape will be quickly rounded up.

It will be recalled that the lack of rounding action was described in Section 13.7 under the heading of convenient waviness. The three terms in the expression for $1/G$ in Eqn (13.16) can be taken to represent the pre-existing wave amplitude at the grinding point and the corrective movements at the work plate and the control wheel. These can be illustrated graphically as shown in the following example.

Example 13.6 Draw a vector diagram to illustrate the wave amplitude and the corrective movements for 16 waves at $\beta = 10^\circ$ and $\alpha = 66^\circ$ using the expression for $1/G$ from Eqn (13.16) (Fig. 13.18).

$$\alpha \cdot n = 66 \times 16 = 1056 \equiv -24^\circ$$

$$(\pi - \beta) \cdot n = 170 \times 16 = 2720 \equiv 200^\circ$$

$$K_1 = 0.1736 \text{ and } K_2 = 0.9145 \text{ (see Section 13.7)}$$

Vectors are

Grinding point 1

Work plate action

$$-0.1736(\cos(-24) - j \cdot \sin(-24)) = -0.1586 - 0.0706j.$$

Control wheel action

$$0.9145 \cdot (\cos(200) - j \cdot \sin(200)) = -0.8593 + 0.3128j$$

The corrective action is negative. The corrective action when subtracted from the previous wave causes the amplitude to increase.

Geometric Stability Parameter “A”

Stability relies on the ‘real’ or in-phase part of the rounding function $1/G$. Considering only the real part has the advantage of providing a stability plot where a large number of frequencies can be viewed on one

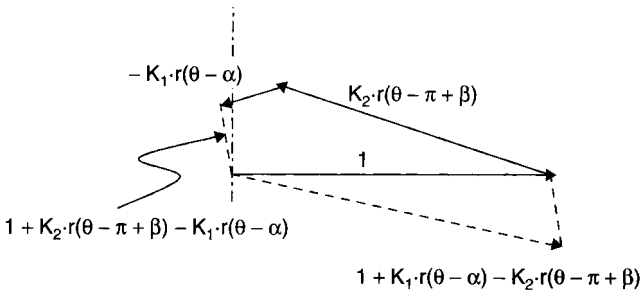


Figure 13.18 Work plate action and control wheel action for 16 waves, $\alpha = 66^\circ$, $\beta = 10^\circ$.

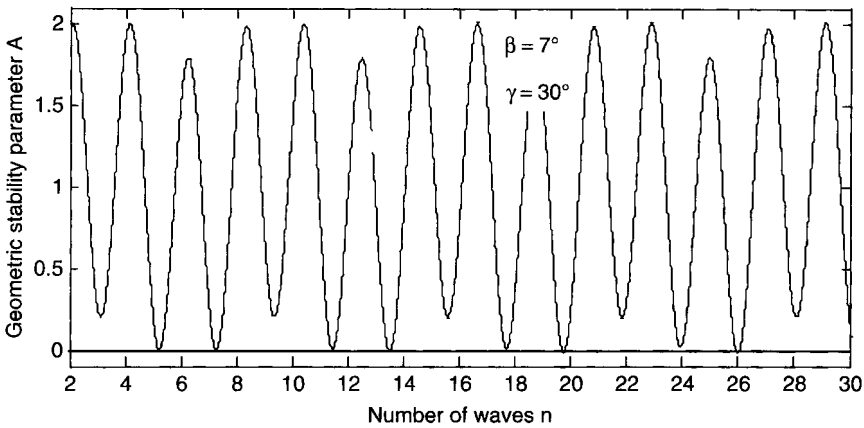


Figure 13.19 Geometric stability parameter A for $\beta = 7^\circ$ and $\gamma = 30^\circ$.

diagram. The real part is termed the geometric stability parameter A and is given by

$$A = 1 + K_2 \cos n \cdot (\pi - \beta) - K_1 \cos n \cdot \alpha$$

Geometric stability parameter (13.19)

A geometric stability chart is given in Fig. 13.19 based on Eqn (13.19). There are several numbers of waves that show marginal geometric stability. The near zero value at $n = 26$ is for a whole number of waves so this shape would be hard to eradicate. A waviness at $n = 24$ is stable but would be slow to round up. Other conditions of marginal stability are indicated for non-integer numbers of waves. In practice, non-integer waviness is not usually a problem as it requires the waviness to continually precess around the workpiece. This implies substantial depth of cut variations whereas a

whole number of waves requires only small depth of cut variations in order to build up.

Integer Wave Stability

For geometric stability, prime consideration is given to the rounding effect of whole numbers of waves. It is a simple matter to calculate the value of the geometric stability parameter A for whole numbers. This yields a chart that is straightforward to interpret as in Fig. 13.20. With tangent angle 8° and work plate angle 30° , the geometry is stable for all numbers of waves up to 32. Odd orders of waviness $n = 3, 5, 7, 9$, and 11 show a reasonably strong rounding action.

Even order waves 20, 24, and 26 are positive but have marginal stability meaning a weak rounding action. It is important not to force one of these marginal waviness values by choosing a work speed that creates a coincidence with a machine vibration.

Regions of Instability

A negative value of the geometric stability parameter A is a measure of the growth rate for a value of waviness. Plotting regions where the geometry gives negative values gives a comparison for a wide range of geometries. An example is shown in Fig. 13.21 for up to 30 waves. Each unstable region represents a different waviness.

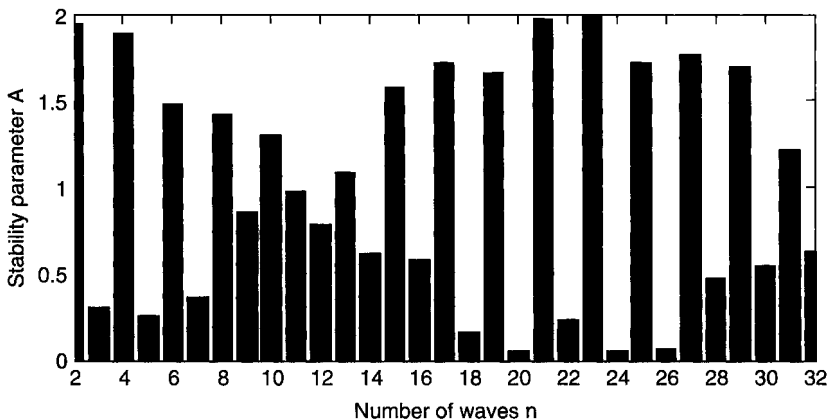


Figure 13.20 Geometric stability, $\beta = 8^\circ$ and $\gamma = 30^\circ$.

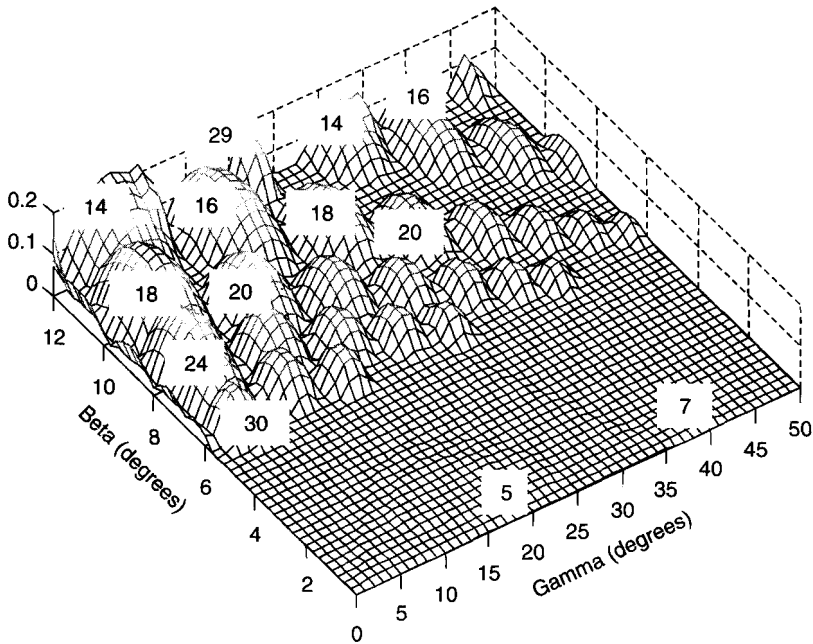


Figure 13.21 Regions of geometric instability and number of waves up to 30 waves. Peaks represent growth rate.

The regions shown are mainly for even number of waves. As the tangent angle becomes larger, higher odd lobing appears. For example, with a 20° work plate angle and a 12° tangent angle, both 16 waves and 31 waves are unstable. At small values of tangent angle, odd lobing is either marginally stable or marginally unstable. Higher frequency waves greater than 30 may also be unstable but are not shown. High frequencies are less of a problem for reasons explained previously in this chapter and in the previous chapter. In between unstable regions there are stable regions. The regions for a 30° plate angle and a tangent angle 5° , 6° , or 8° are examples. At a 7° tangent angle, 26 waves are marginally unstable.

In practice, geometrical instability usually grows slowly and often is not apparent unless there is significant initial roundness error or a source of disturbance. In some circumstances, deflections slow down the growth rate, particularly deflections due to contact flexibility of the control and grinding wheels. It is only when a vibration excites a low frequency instability that fast growth rates are experienced. A forced vibration coinciding with a marginally stable geometry will also cause a roundness problem. This can be avoided by either eliminating the vibration or by adjusting the

work speed to a more suitable value. Deflection effects are analysed in the next section.

13.11 Effect of Deflections

Static deflections tend to stabilise a process as described in the previous chapter. Dynamic deflections associated with a resonance can make a process more unstable. These effects are analysed in the following paragraphs.

The open-loop gain in Fig. 13.15 including effects of geometry, depth of cut, and deflections is given from Eqns (13.16–13.18) by

$$\frac{K_s}{\lambda(jn)} \cdot (1 - e^{-j \cdot 2\pi n}) \cdot \frac{1}{1 + K_2 \cdot e^{-jn(\pi-\beta)} - K_1 \cdot e^{-jn\alpha}} \quad \text{Open-loop gain} \quad (13.20)$$

Static Deflections

At low frequencies, deflection is approximately in phase with the depth of cut. Deflection is therefore given by $x = \frac{K_s}{\lambda} \cdot a_c$, where λ is the static loop stiffness of the machine and grinding wheels. At the limit of stability the open-loop gain is equal to -1 . From Eqn (13.20), the limiting stability condition is therefore

$$-\frac{K_s}{\lambda} \cdot (1 - e^{-j \cdot 2\pi n}) = 1 + K_2 e^{-jn(\pi-\beta)} - K_1 e^{-jn\alpha} \quad \text{Stability condition} \quad (13.21)$$

Or in shorter form, Eqn (13.21) states that

$$-x = A$$

It has already been shown that a negative value of the geometric rounding parameter A represents a geometric instability. Limiting stability is when $A = 0$ and the wave neither grows nor decays. A negative value of the parameter A means there is a depth of cut variation. This depth of cut adds to the existing wave making the wave increase. However, depth of cut is minimised with sufficient in-phase deflection. Sufficient deflection reduces the growth rate until limiting stability is approached. Growth rate is reduced when K_s/λ is large and $1 - e^{j2\pi n}$ is non-zero. Unfortunately, as limiting stability is approached, the depth of cut represented by $1 - e^{j2\pi n}$ approaches zero for a whole number of waves. This means that stability cannot be achieved by introducing deflection for a whole number of waves. This is confirmed by the effect of deflection on open-loop gain shown

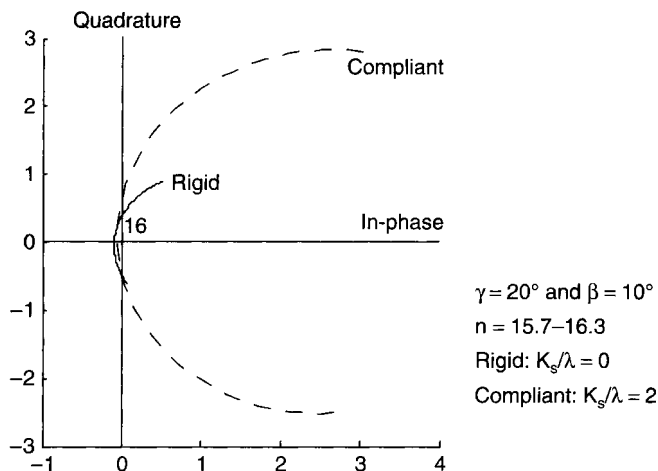


Figure 13.22 Effect of static deflections on geometric stability.

in Fig. 13.22. Stability is compared for a rigid system and a compliant system for the unstable geometry previously considered at $\gamma = 20^\circ$, $\beta = 10^\circ$. At $n = 16$ waves, the rounding function for the rigid and compliant systems are identical. The instability at $n = 15.9$ has been very slightly reduced but not eliminated.

Dynamic Deflections

The effect of dynamic deflections is very different from the effect of static deflections. The effect of measured responses when plotting the open-loop gain can illustrate these differences. However, the effect can be more simply demonstrated by calculating the effect of a structure represented by a single spring-mass damper. For this case, the machine response function is $\lambda(j\omega)/\lambda = 1 - \omega^2/\omega_0^2 + j\omega/Q\omega_0$, where the natural frequency is $\omega_0 = \sqrt{\lambda/m}$ and the dynamic magnifier is $Q = \lambda/c\omega_0$ as described in the previous chapter. The stability limit for this case is then given by

$$\frac{(K_s/\lambda) \cdot (1 - e^{-j2\pi n})}{1 - \omega^2/\omega_0^2 + j\omega/Q\omega_0} + 1 + K_2 e^{-jn(\pi-\beta)} - K_1 \cdot e^{-jn\alpha} = 0 \quad \text{Stability limit} \quad (13.22)$$

The effect of a natural frequency ω_0 on the stability of a particular number of waves, n at a particular work speed Ω can be investigated since $n = \omega/\Omega$. Frequency is written in terms of n for a particular work speed

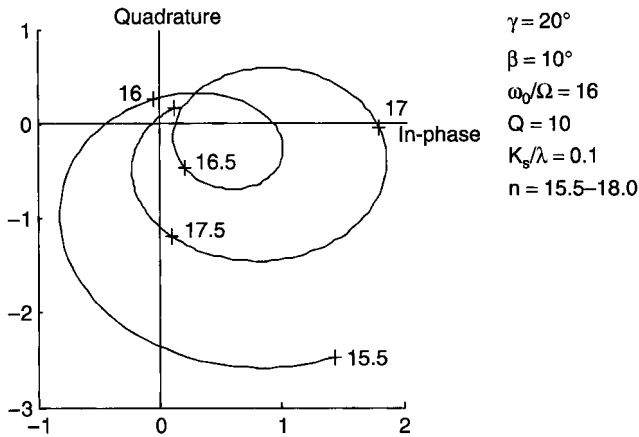


Figure 13.23 Effect of a natural frequency coinciding with a geometric instability.

ratio ω_0/Ω . An example is shown in Fig. 13.23 showing the effect of a natural frequency ω_0 coinciding with $n = 16$ waves at $\gamma = 20^\circ$, $\beta = 10^\circ$.

Two interesting differences arise due to the proximity of a resonance. The instability at just below $n = 16$ remains and becomes more unstable. The previously stable condition at $n = 18$ has now become unstable. This shows that a resonance leads to increasing instability just below the natural frequency and also at a frequency higher than the natural frequency. The clear conclusion is that a resonance should not be allowed to lie close to a marginal geometric stability condition.

In this case, $\omega_0/\Omega = 16$ with a natural frequency of 80 Hz corresponds to a 5 rev/s work rotational speed. Reducing the work speed to 2 rev/s makes the natural frequency correspond to $n = 40$. This will usually be high enough for small work diameters to eliminate a rounding problem.

13.12 Avoiding Dynamic Problems

It should be remembered that a geometric instability is a very gentle process compared to the violence of a dynamic machining chatter. That is why centreless grinding is widely used in the high precision bearing industry. Geometric stability is of particular interest because grinding is a high accuracy process and there is an interest in achieving an ideal rounding process. As previously mentioned, very high orders of accuracy can be achieved by paying attention to machine design, contact flexibility, correct geometry, and selection of work speed. Often a roundness problem is related to a forced vibration source within or external to the grinding

machine. The following is a brief review of rules for avoiding critical frequencies and achievement of best accuracy.

- Avoid a geometry that is unstable for low order waviness such as $n = 5$ or 10
- If waviness is experienced count the number of waves on the workpiece and determine the frequency by multiplying by the work speed. If possible eliminate the vibration at source. Use dynamic wheel balancing and balance transmission shafts
- Seek the source of vibration and determine whether the geometry is marginally stable for the particular frequency
- Avoid a work speed that creates a coincidence between a natural frequency and a marginally stable or unstable geometry
- Select a precision grinding machine with a high first natural frequency in the tuning fork mode
- Select a machine with high damping
- Avoid rapid changes of feed velocity and avoid impact with feed stops
- Avoid a wheel speed that is a direct multiple of the work speed
- Avoid multiple ratios for all vibration sources with work speed or wheel speed
- For the smallest roundness errors use a compliant control wheel on a stiff machine.

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14 Material Removal by Grains

14.1 Introduction

Grains as Cutting Tools

A grinding wheel removes material in a similar way to a micro-milling cutter. In micro-milling, the cutting tools are identical in shape. The situation is quite different in grinding. The cutting tools in a grinding wheel are the grains, and material removal depends on their shape and position. The shapes and positions are both random distributions that change with wear. Grinding forces, accuracy, and wheel wear all depend on the amount of material removed by individual grains. Blunt worn grains are much less efficient than sharp grains. Material removal by grains provides a basis for understanding forces, power, roughness, and temperature with different grinding geometries and abrasives.

Grinding Chips

Figure 14.1 shows examples of chips produced in grinding. It is clear that the chips were formed in ductile flow processes. Figure 14.1(a) shows a long thin chip rather similar to a chip formed in turning operations. The chip is wide in relation to its thickness. Figure 14.1(b) shows not only similar chips but also shows a spherical chip clearly formed at high

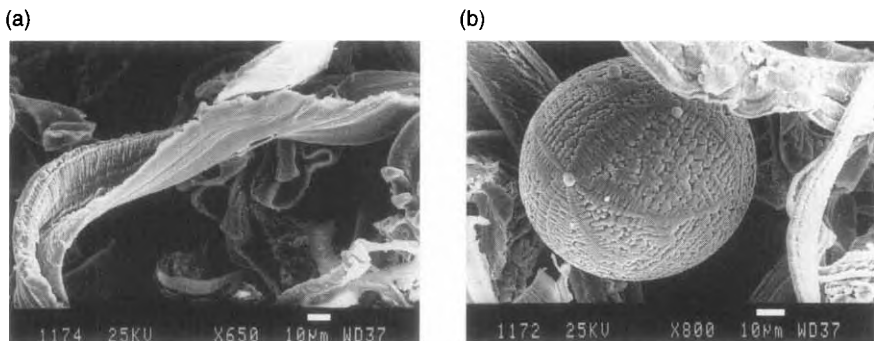


Figure 14.1 Examples of grinding chips: (a) ductile chip and (b) spherical chip formed at high temperature.

temperature as there is evidence of re-casting in the growth of structure similar to dendrites. Examination of numerous examples of chips reveals that the spherical shapes are usual and the spheres are in fact hollow. In the discussion below, we look more closely at the effects of speeds and feeds on chip sizes, surface roughness, and wear behaviour of the abrasive grains.

The appearance of spherical particles was discussed by Dowson et al. (1991) as part of a study of chip morphologies in grinding processes. It was stated that partially formed spheres looked as though they were formed from platelets released from the surface in fatigue wear. It was found that the number of spherical particles in the swarf correlated with contact temperature. Grinding 52100 steel, there were fewer using a diamond wheel, rather more with a CBN wheel, and most with an aluminium oxide wheel. The exact mechanism for the formation of spheres was not found, but similar shapes have been found in a number of wear processes. However, it was observed that wheel dulling increased the number of spheres produced. It was also found that wet grinding reduced the number of spheres compared with dry grinding.

14.2 Equivalent Chip Thickness

Equivalent chip thickness is a kinematic parameter that combines real depth of cut, work speed, and wheel speed. It can be considered as a modified depth of cut. The real depth of cut is the thickness of the layer of material removed in one pass or revolution of the workpiece. The equivalent chip thickness is the thickness of the layer of material removed in one pass or revolution of the grinding wheel (Fig. 14.2). It may be visualised as the thickness of the stream of material emitted from the contact zone by the grinding wheel as illustrated in Fig. 14.2. The removal rate per unit width of contact is $Q'_w = a_e \cdot v_w = h_{eq} \cdot v_s$ so that

$$h_{eq} = a_e \cdot \frac{v_w}{v_s} \quad \text{equivalent chip thickness} \quad (14.1)$$

Example 14.1 Depth of cut is 0.02 mm (or 0.00079 in.), work speed is 0.3 m/s (or 709 in./min), and wheel speed is 40 m/s (or 7874 ft/min). Calculate equivalent chip thickness.

$$h_{eq} = 0.02 \times 300/40000 = 0.00015 \text{ mm (or 0.000006 in.)}$$

The physical interpretation of equivalent chip thickness is the thickness of the material removed at wheel speed. It is how the material would appear

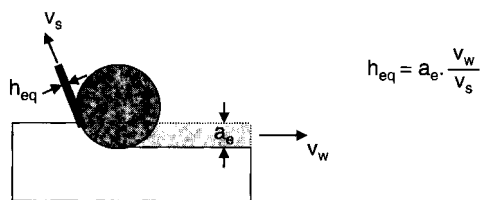


Figure 14.2 Equivalent chip thickness is the thickness of the layer removed at grinding wheel speed.

if it emerged as a uniformly thin solid sheet. Obviously, this is much thinner than the thickness of chips removed by randomly spaced grains.

Empirical Relationships for Grinding Data

Equivalent chip thickness is widely used to relate empirical data for a particular abrasive and conformity of grinding contact. Force, power, and roughness may be expressed empirically in terms of equivalent chip thickness using relationships of the type

$$F_t = F_0 \cdot h_{eq}^f \quad \text{empirical force equation} \quad (14.2)$$

$$R_a = R_0 \cdot h_{eq}^r \quad \text{empirical roughness equation} \quad (14.3)$$

Examples of grinding forces and roughness values expressed in empirical form are given in Fig. 14.3. These results are based on a cooperative CIRP research (Snoeys et al. 1974).

Plotting experimental values on a log-log basis produces a single straight line for a range of wheel speeds and work speeds. The slope of the line gives the value of the index. For example, in Eqn (14.3) for grinding force, $f = 0.78$.

Limitations of h_{eq}

This simple approach to presentation of grinding data has much to commend it. However, results only apply for one grinding wheel and work-piece material under a limited range of grinding conditions. Equivalent chip thickness takes no account of the number of active cutting edges on the grinding wheel or the conformity of the contact. For this we need to examine the cutting edge contacts and the geometry of the grinding contact.

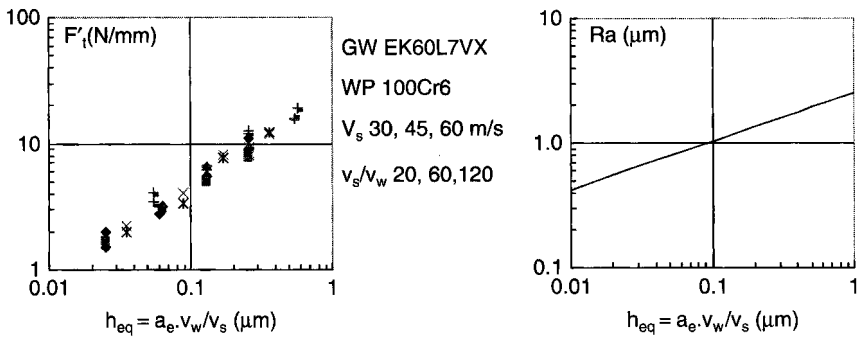


Figure 14.3 Experimental values of tangential force and surface roughness related to equivalent chip thickness based on cooperative CIRP research (Snoeys et al. 1974).

14.3 Cutting Edge Contacts

Random Cutting Action

Chapter 3 introduced the random nature of the grains in the wheel surface. Randomly positioned grains cut a series of grooves in the workpiece as in Fig. 14.3. Each groove is of different length, width, and depth depending on the position of the grain in the surface of the wheel. Because some grains only make a small groove, a succeeding grain makes a larger groove (Fig. 14.4).

The grains are rather like a crowd of people of varying heights, separated by varying distances. Figure 14.5 illustrates a typical surface trace taken with a stylus instrument across a section of a replica of a wheel profile.

The spaces between cutting edges are measured along the surface and the depths measured below the surface. The lengths and depths grouped by size and the percentage of cutting edges in each group are shown in Fig. 14.6. The grinding wheel in this example was a 60 grit mesh size corresponding to an average grain diameter of at least 0.25 mm from Eqn (3.2). The average cutting edge spacing measured was 1.33 mm, approximately five times the grain diameter. The average cutting edge depth measured was 4.28 μm below the datum level with depths ranging from -6 to $+18 \mu\text{m}$.

Representation by Poisson Distribution

The actual distribution of grain positions in a grinding wheel can be represented by the terms in a Poisson distribution (Brough et al. 1983).

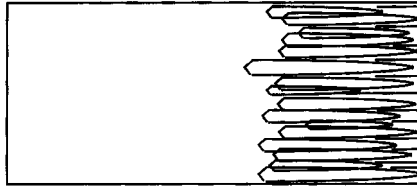


Figure 14.4 Material is removed by grooves of varying depths and random positions corresponding to the random distribution of grains in the abrasive.

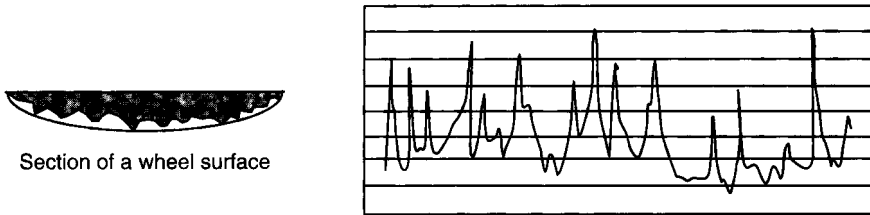


Figure 14.5 A typical stylus trace from a section of the wheel surface.

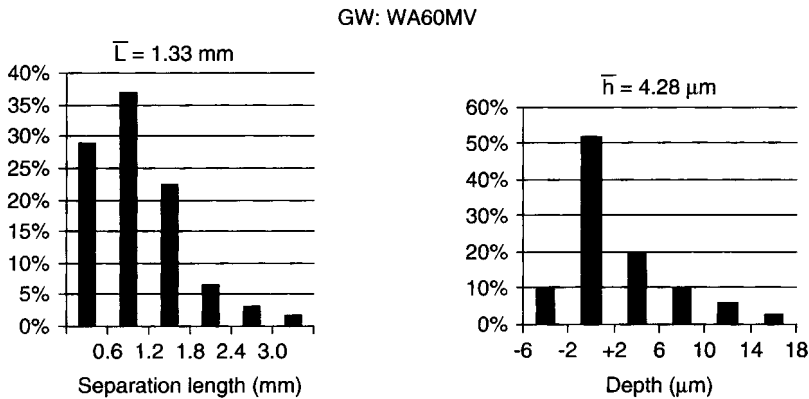


Figure 14.6 Measured positions of cutting edges in a grinding wheel surface.

Measuring the spacing in the grinding direction over a given range of depths, the spacing frequencies were found to correlate with the terms of the following distribution:

$$1 = e^{-m} \left(1 + m + \frac{m^2}{2!} + \frac{m^3}{3!} + \dots \right) \quad \text{Poisson distribution} \quad (14.4)$$

where m is the average spacing obtained from a series of measurements. Similarly, the depth frequencies were reasonably predicted knowing the average depth value. Expressing the measured spaces and depths by a Poisson distribution can be useful for modelling a wheel surface.

Cutting Edge Density

Figures 14.5 and 14.6 are based on two-dimensional measurement. More useful information can be obtained from three-dimensional measurement (Cai 2001). Cai used several different methods to measure the cutting edge density of grinding wheel surfaces. The cutting edge density is defined as the number of cutting edges per unit area of a grinding wheel surface. The first stage is to obtain a three-dimensional image of the surface by direct measurement of the surface or by measurement of a moulded replica. A series of parallel traces are made with a stylus or using optical instrumentation. The data can then be analyzed using three-dimensional analysis software to determine the variation in the cutting edge density with depth below the surface. It is also possible to count the number of active cutting edges that have experienced wear in a grinding process using a microscope. The results by counting wear points and by image process analysis of wheel scans gave agreement within 10%. The error using Microset replica materials was within 20% (Cai and Rowe 2004).

Figure 14.7 shows the magnified cutting surface of a grinding wheel. The surface was measured by taking parallel traces with a measuring stylus and using three-dimensional software for analysis.

Effect of Wear

Figure 14.8 shows the number of cutting edges per unit area of CBN grinding wheels that have actually engaged in grinding. This number is known as the active cutting edge density. The active cutting edge density depends on the grinding conditions and on the dressing conditions as described in the previous chapter. Wear of the grinding wheels caused cutting edge density to reduce as is commonly experienced. This is because some active cutting edges break out of the surface under the action of the grinding force on the individual grains. The cutting edge density reduces more quickly on the high-porosity wheel because the strength of the bond is reduced and the forces on each grain are increased. As a consequence, wheel life was shorter employing the high-porosity wheel.

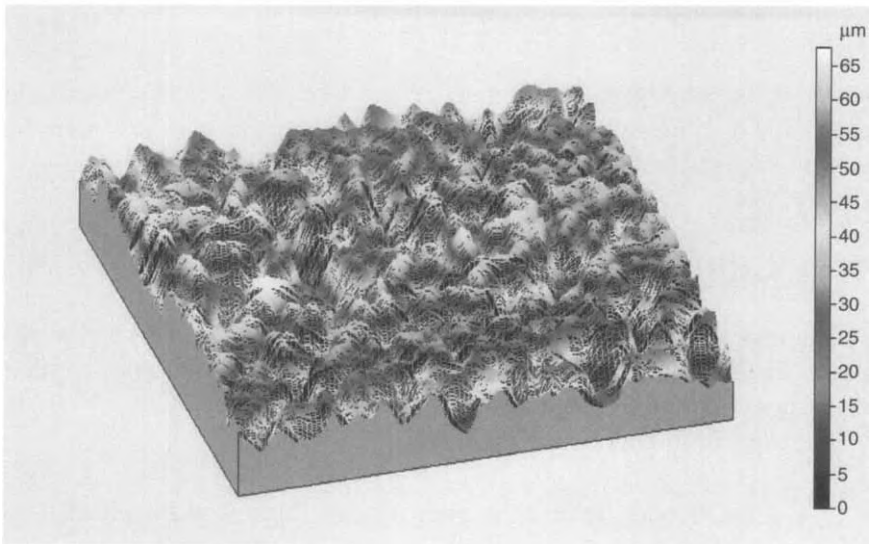


Figure 14.7 Three-dimensional topography of a CBN grinding wheel obtained by stylus measurement (Cai and Rowe 2004).

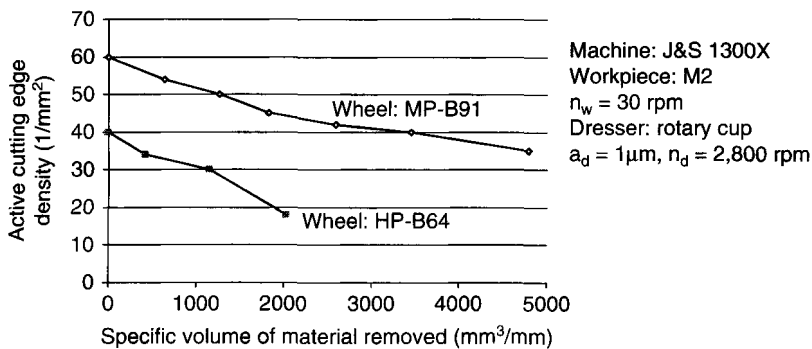


Figure 14.8 Number of cutting edges per unit wheel area C reducing with grinding wear for vitrified fine-grain CBN grinding wheels. MP-B91 is a medium porosity wheel and HP-B64 is a high-porosity wheel having a smaller grit size (Cai and Rowe 2004).

Cutting Edge Shape

Cutting edge shape also changes with grinding wheel wear. This can be demonstrated by evaluating wear flat area on the tips of the active grains using microscopy. A dullness parameter may be defined as

$$\gamma = d_0/d_g \quad \text{cutting edge dullness} \quad (14.5)$$

where d_0 is the equivalent diameter of the wear flat area on the cutting edges and d_g is the average grain diameter. As the cutting edges wear, the cutting edge dullness increases as shown in Fig. 14.9.

14.4 Cutting Edge Contact Times

There are three greatly different contact times between grains and workpiece. Each is significant in process analysis particularly in consideration of energy concentration and local temperatures.

The three different contact times are

- grain contact time as a grain passes through the contact length
- wheel contact time for many grains as the wheel passes a section
- grain contact time with a point on the workpiece

The contact time experienced by a grain in each interaction is given by the contact length l_c divided by v_s .

$$t_g = l_c/v_s \quad \text{grain contact time} \quad (14.6)$$

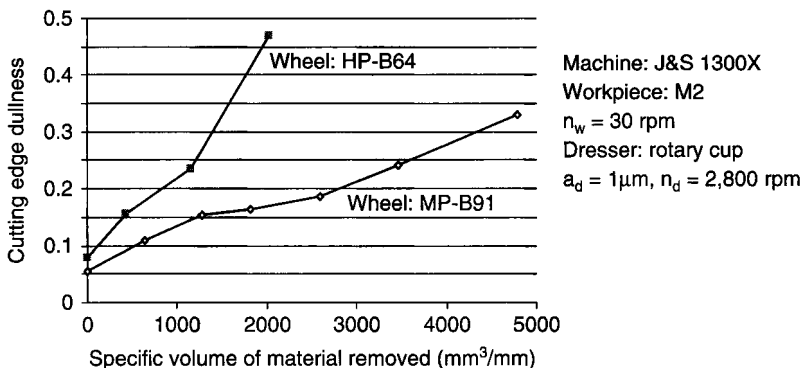


Figure 14.9 Dullness γ increasing with grinding wheel wear for vitrified fine-grain CBN grinding wheels. MP-B91 is a medium porosity wheel and HP-B64 is a high-porosity wheel having a smaller grit size (Cai and Rowe 2004).

Example 14.2 Contact length is 1 mm (or 0.039 in.) and grinding wheel speed is 30 m/s (or 5900 ft/min). Calculate the time an individual grain is in contact with the workpiece.

$$t_g = 0.001/30 = 0.000033 \text{ s or } 33 \mu\text{s}$$

The short duration of the grain contact means that the grain becomes very hot. It is very important that the abrasive grains retain hardness at high temperature. It is also important that the grain should not dissolve in the work material at these high temperatures.

The grain contact time as given above is very much shorter than the wheel contact time experienced as the grinding wheel passes a section of the workpiece. The grinding wheel contact time is given by the contact length divided by the work speed v_w :

$$t_s = l_c/v_w \quad \text{wheel contact time} \quad (14.7)$$

Example 14.3 Contact length is 1 mm (0.039 in.) and work speed is 200 mm/s (or 472 in./min). Calculate the time the wheel contacts a section of the workpiece.

$$t_s = 1/200 = 0.005 \text{ s or } 5 \text{ ms}$$

An individual grain passes a point on the workpiece in an extremely short time. The time a point on the workpiece experiences contact with a grain is the width of the grain contact divided by the grain speed. This contact between a grain and a point on the workpiece is known as a flash contact.

$$t_p = l_{gp}/v_s \quad \text{flash contact time} \quad (14.8)$$

Example 14.4 The active grain width is 0.1 mm (or 0.0039 in.) and the wheel speed is 30 m/s (or 5900 ft/min), the point contact time experienced by the workpiece is $0.0001/30 = 0.0000033 \text{ s or } 3.3 \mu\text{s}$.

The point contact time gives rise to extremely brief input of energy into the workpiece and an almost adiabatic increase in temperature. This spike temperature or “flash temperature” as it is often known takes the point on the cut surface into the region of the melting temperature of the material.

14.5 The “Uncut Chip”

Effects of the Uncut Chip Dimensions

The uncut chip is a term used to describe the shape and size of the cut taken by an average abrasive grain. Uncut chip dimensions are the result of the combination of speed, feed, depth of cut, and grain spacing on the wheel. The uncut chip is a measure used to express the combined effect of changes in grinding behaviour resulting from the speeds, feeds, etc. (Alden 1914; Guest 1915). The uncut chip thickness can be viewed as the grain penetration into the workpiece and in that sense is the grain depth of cut. Grain depth of cut not only controls surface texture of the ground workpiece but also the wear of the abrasive grain. If the grain depth of cut is too large, the grinding wheel wears rapidly. If it is too small, the wheel glazes. The optimum lies between these two extremes.

Use of Kinematic Models

Analysis of grain depth of cut based on speeds and feed is known as kinematics. Kinematic models allow grinding conditions to be approximately reproduced in different applications (Tönshoff et al. 1992). A grinding wheel and workpiece material combination may be optimum for one geometry and set of kinematic conditions but less so for another. Trying to match a set of chip dimensions for a known wheel and material combination helps to match a known grinding behaviour. A study of cutting motions helps us to explain how grinding behaviour changes for different speeds and feeds. We can even attempt to optimise grinding conditions based on optimised kinematic conditions determined for a different geometry.

Basic Kinematic Model

Work material displaced by the grain is termed the uncut chip even if a chip is not actually formed. In reality, the uncut chip represents the shape of the material that lies in the path of a grain before the grain deforms the surface. Figure 14.10 illustrates idealised uncut chip dimensions including thickness h_{cu} , width b_{cu} , and length l_c .

Figure 14.10 shows paths taken by successive abrasive grains cutting through the surface of a workpiece. In reality, successive grains do not precisely follow one behind another.

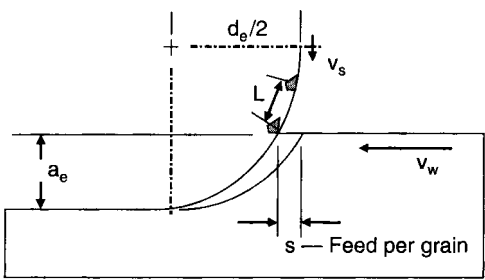


Figure 14.10 Paths followed by successive abrasive grains.

Basic Chip Shape Models

Depending on wheel direction, grains enter the workpiece surface either at the thick end of the chips or at the thin end. By measuring grooves cut by abrasive grains, it was found that chip width was typically 8–15 times larger than chip thickness (Backer et al. 1952). Of course, that order of magnitude would be expected given the approximately spherical shape of the grains that cut the chips. Figure 14.11, shows uncut chip cross-sections corresponding to different assumptions for grain shape. A rectangular grain is assumed to give a rectangular uncut chip section. This might be roughly appropriate for a trapezoidal grain with a large wear flat. Backer et al. (1952) assumed very reasonably that grain width increases with grain depth and modelled the grain as a triangle. Modelling grains as spheres is a first-order representation of different random shapes (Chen and Rowe, 1996). These three grain shapes lead to different relationships between speeds, depth of cut, and chip thickness as shown below.

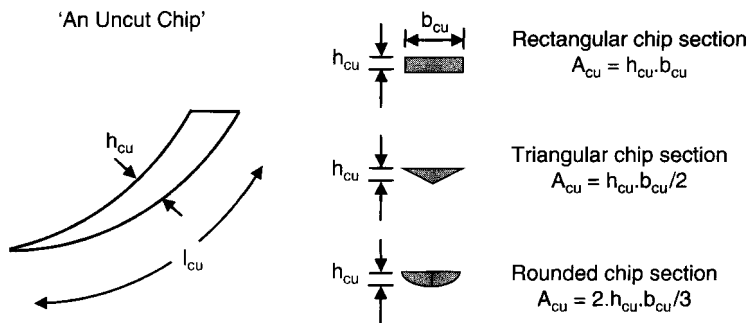


Figure 14.11 An uncut chip based on grain shape, trajectory, and penetration depth.

14.6 Chip Length

Average chip length is found from Eqn (3.8) where the contact length ratio R_L usually lies in the range 1–3. For rigid wheels and workpieces, $R_L = 1$. For vitrified wheels, a higher value of 1.5–3 is more appropriate.

$$l_c = R_L \cdot l_g = R_L \cdot \sqrt{a_e \cdot d_e} \quad \text{chip length} \quad (14.9)$$

Example 14.5 Depth of cut is 0.02 mm and the work speed is 0.2 m/s. Estimate chip length assuming (a) a rigid wheel $R_L = 1$ and (b) a moderately flexible wheel $R_L = 2$.

$$\text{Rigid wheel: } l_c = 1 \times \sqrt{0.02 \times 200} = 2 \text{ mm (or 0.079 in.)}$$

$$\text{Flexible wheel: } l_c = 2 \times \sqrt{0.02 \times 200} = 4 \text{ mm (or 0.16 in.)}$$

14.7 Chip Volume Based on Removal Rate

Average chip sizes can be simply analysed based on volume consistency with removal rate. Removal rate divided by the number of active grits gives the mean chip volume. Volume removal rate per unit width is $Q'_w = a_e \cdot v_w$. The volume removed during a period of time must be equal to the total volume of the chips so that $Q'_w = C \cdot v_s \cdot V_{cu}$, where C is the number of active grains per unit wheel area as illustrated in Fig. 5.3 and V_{cu} is the average chip volume. It follows that

$$V_{cu} = \frac{a_e \cdot v_w}{C \cdot v_s} \quad \text{mean chip volume} \quad (14.10)$$

Example 14.6 Estimate mean chip volume from Eqn (14.10) assuming the following grinding conditions and wheel measurements:

Wheel diameter d_e : 200 mm (or 7.87 in.)

Active grain density C : 0.25 per mm^2 (or 161/ in^2).

Ratio of chip width to thickness r : 10

Mean grain diameter d_g : 1.5 mm (or 0.059 in.)

Work speed v_w : 0.3 m/s (or 709 in./min)

Wheel speed v_s : 40 m/s (or 7874 ft/min)

Depth of cut a_e : 0.02 mm (or 0.00079 in.)

Mean chip volume is

$$V_{cu} = \frac{0.02 \times 300}{0.25 \times 40000} = 0.0006 \text{ mm}^3 \text{ (or } 36 \times 10^{-9} \text{ in}^3\text{)}$$

Chip volume is related to grinding forces and to specific energy as defined from the relationships given in Chapter 2,

$$\frac{F'_t}{e_c} = \frac{Q'_w}{v_s} = h_{eq} = C \cdot V_{cu} \quad \text{related to force and energy} \quad (14.11)$$

Either increasing h_{eq} or reducing C usually reduces specific energy required. In other words, increasing chip volume reduces specific energy. The process requires less energy and removal rate can be increased. It is not always the case as there are other effects that can come into play. Chip shape may also be important. For example, very long thin chips tend to consume more energy than short fat chips. Abrasive pore size is also a consideration. Another constraint is the maximum chip volume that can be accommodated by the pores without impeding chip flow.

14.8 Chip Cross-Section Area

There are other consequences of increasing chip volume. Force on the abrasive grain is increased and this in turn increases the rate of grain fracture. Also, as the material is removed in larger chip volumes, surface roughness is increased. These effects become apparent from consideration of the chip cross-section area and chip thickness.

Dividing chip volume by chip length gives the average cross-section area of the chips.

$$A_{cu} = V_{cu}/l_c = \frac{a_e \cdot v_w}{C \cdot v_s \cdot l_c} \quad \text{chip cross-section area} \quad (14.12)$$

Example 14.7 Estimate mean chip cross-section area from the data in Example 14.6 for a rigid wheel.

$$A_{cu} = \frac{0.02 \times 300}{0.25 \times 40000 \times 2} = 0.0003 \text{ mm}^2 \text{ (or } 0.46 \times 10^{-6} \text{ in}^2 \text{)}$$

A_{cu} affects grain cutting force directly unlike chip length. It is therefore expected that chip section area will correlate more generally with grinding forces than chip volume and this is usually found in practice. For example, reducing a_e or increasing v_s reduces grinding forces as is seen from Fig. 14.3. The reduction in grinding forces results in an improvement in grinding accuracy. Increasing v_w with constant a_e increases grinding forces and increases surface roughness.

Equation (14.12) says that doubling the expression on the RHS will double the mean chip cross-section area and greatly increase the average force on individual abrasive grains.

The chip cross-section area A_{cu} is proportional to width b_{cu} and thickness h_{cu} of the chip as illustrated in Fig. 6.10. That is,

$$A_{cu} = k \cdot b_{cu} \cdot h_{cu} \quad \text{area shape factor} \quad (14.13)$$

where the area shape factor $k = 1$ for a rectangular grain, $k = 1/2$ for a triangular grain, and $k \approx 2/3$ for a spherical grain.

14.9 Chip Thickness

General Expression for Chip Thickness

A general equation for chip thickness results from Eqns (14.12) and (14.13).

$$h_{cu} = \frac{a_e \cdot v_w}{C \cdot v_s} \cdot \frac{1}{k \cdot b_{cu} \cdot l_c} \quad \text{mean chip thickness—general} \quad (14.14)$$

However, it is necessary to use the correct value of k and b_{cu} to determine chip thickness for a particular chip shape. Examples below illustrate this point.

Example 14.8 Rectangular chip. Estimate mean chip thickness from the data in Example 14.6 assuming static grain spacing roughly equal to 1.5 mm (or 0.059 in.) and a rectangular chip. Use either Eqn (14.13) or Eqn (14.14). Using Eqn (14.14),

$$k = 1$$

$$b_{cu} = 1.5 \text{ mm}$$

$$h_{cu} = \frac{0.02 \times 300}{0.25 \times 40000 \times 1 \times 1.5 \times 2} = 0.0002 \text{ mm (or } 7.9 \times 10^{-6} \text{ in.)}$$

The large value of chip width used for this calculation leads to an extremely thin uncut chip only slightly larger than the value of h_{eq} in Example 14.1. The chip width to thickness ratio is 10,000 and is not representative of actual grinding measurements (Backer et al. 1952).

Chip Width to Chip Thickness Ratio—Triangular Chip

Chip width is not easily determined in a practical situation. This problem was overcome by Backer et al. (1952) who determined chip width as a function of chip thickness on the basis of grooves cut by abrasive grains. It was assumed that $b_{cu} = r \cdot h_{cu}$, where r is a ratio typically of the order 8–15 according to Backer. It means that chip width is usually about 10 times greater than chip thickness. The exact value of the ratio r is less important than the order of magnitude that gives sensible chip thickness values that vary in a reasonable way with other process variables.

Triangular Chip Thickness

Substituting for b_{cu} , k , and l_c in Eqn (14.14),

$$h_{cu} = \left[\frac{2 \cdot a_e \cdot v_w}{C \cdot r \cdot v_s \cdot l_c} \right]^{1/2} = \left[\frac{2 \cdot v_w}{C \cdot r \cdot v_s \cdot R_L} \cdot \sqrt{\frac{a_e}{d_e}} \right]$$

mean chip thickness—triangular (14.15)

It can be shown that $h_{cu \cdot \max} = \sqrt{3} \cdot h_{cu}$ for a triangular chip. This follows because the volume of a triangular chip is $V_{cu} = b_{cu \cdot \max} \cdot h_{cu \cdot \max} \cdot l_c / 6 = b_{cu} \cdot h_{cu} \cdot l_c / 2$. It therefore follows that $b_{cu \cdot \max} \cdot h_{cu \cdot \max} = 3 \cdot b_{cu} \cdot h_{cu}$ and hence $h_{cu \cdot \max} = \sqrt{3} \cdot h_{cu}$. Therefore,

$$h_{cu \cdot \max} = \left[\frac{6 \cdot a_e \cdot v_w}{C \cdot r \cdot v_s \cdot l_c} \right]^{1/2} = \left[\frac{6 \cdot v_w}{C \cdot r \cdot v_s \cdot R_L} \cdot \sqrt{\frac{a_e}{d_e}} \right]^{1/2}$$

max chip thickness—triangular (14.16)

Example 14.9 Estimate mean and maximum values of chip thickness and chip width for a triangular cutting edge from the data in Example 14.6.

$$h_{cu} = \left[\frac{2 \times 300}{0.25 \times 10 \times 40000 \times 1} \sqrt{\frac{0.02}{200}} \right]^{1/2} = 0.0077 \text{ mm (or 0.0003 in.)}$$

$$b_{cu} = 10 \cdot h_{cu} = 10 \times 0.0077 = 0.077 \text{ mm (or 0.003 in.)}$$

$$h_{cu \cdot \max} = \sqrt{3} \times 0.0077 = 0.013 \text{ mm (or 0.00052 in.)}$$

$$b_{cu \cdot \max} = 10 \cdot h_{cu \cdot \max} = 10 \times 0.013 = 0.13 \text{ mm (or 0.0052 in.)}$$

The mean chip thickness by this estimation is some 38 times larger than the previous estimate based on typical grain spacing. Although Backer et al. (1952) assumed a triangular chip cross-section, Eqn (14.15) can also be applied for other cross-sections such as the rectangular chip (Malkin 1989). Employing the same approach for a rectangular chip, with $r = 10$ and $k = 1$, yields a value of $\bar{h}_{cu} = 0.0055$ mm (or 0.00022 in.).

Chip width has been eliminated as a variable in Eqn (14.15) which is very helpful, given the uncertainty of chip width and gives a more realistic view of the effect of grinding conditions on mean chip thickness.

Spherical/Round Chip Thickness

A spherical grain shape as shown in Fig. 14.11 leads to an expression for chip width $b_{cu} = 2 \cdot d_g^{1/2} \cdot h_{cu}^{1/2}$. Applying this relationship in Eqn (14.14) leads to

$$h_{cu} = \left[\frac{3 \cdot a_e \cdot v_w}{4 \cdot C \cdot \sqrt{d_g} \cdot v_s \cdot l_c} \right]^{2/3} = \left[\frac{3 \cdot v_w}{4 \cdot C \cdot \sqrt{d_g} \cdot v_s \cdot R_L} \cdot \sqrt{\frac{a_e}{d_e}} \right]^{2/3}$$

mean chip thickness—round (14.17)

Maximum chip thickness is $h_{cu,max} = 1.84 \cdot h_{cu}$ for a round chip produced by a spherical grain shape. This can be shown by integrating the expression for section area along the length of the chip to find the volume. In this case, it is found that $A_{cu,max} = \frac{5}{2} \cdot A_{cu}$ so that $h_{cu,max}^{3/2} = \frac{5}{2} \cdot h_{cu}^{3/2}$ and $h_{cu,max} = 1.84 \cdot h_{cu}$. Therefore,

$$h_{cu,max} = 1.84 \left[\frac{3 \cdot a_e \cdot v_w}{4 \cdot C \cdot \sqrt{d_g} \cdot v_s \cdot l_c} \right]^{2/3} = \left[\frac{15 \cdot v_w}{8 \cdot C \cdot \sqrt{d_g} \cdot v_s \cdot R_L} \cdot \sqrt{\frac{a_e}{d_e}} \right]^{2/3}$$

max chip thickness—round (14.18)

Example 14.10 Estimate mean and maximum values of chip thickness and chip width for a spherical grain from the data in Example 6.6 assuming a rigid wheel $R_L = 1$.

$$h_{cu} = \left[\frac{3 \times 300}{4 \times 0.25 \times \sqrt{1.5 \times 40000} \times 1} \sqrt{\frac{0.02}{200}} \right]^{2/3}$$

$= 0.0032$ mm (or 0.00013 in.)

$$b_{cu} = 2\sqrt{d_g \cdot h_{cu}} = 2 \times \sqrt{1.5 \times 0.0032} = 0.14 \text{ mm (or 0.0055 in.)}$$

$$h_{cu \cdot \max} = \left[\frac{15 \times 300}{8 \times 0.25 \times \sqrt{1.5 \times 40000} \times 1} \sqrt{\frac{0.02}{200}} \right]^{2/3}$$

$$= 0.0059 \text{ mm (or 0.00023 in.)}$$

$$b_{cu \cdot \max} = 2\sqrt{d_g \cdot h_{cu \cdot \max}} = 2 \times \sqrt{1.5 \times 0.0059} = 0.19 \text{ mm (or 0.0075 in.)}$$

Typically, the spherical grain shape assumption gives rise to a wider and thinner uncut chip than simply assuming chip width is 10 times larger than chip thickness. Assuming a round cutting edge means it is unnecessary to have *a priori* knowledge of chip width.

Comparison of Mean Chip Thickness Values

The mean values from the above examples for the different chip shapes are compared in Fig. 14.12.

Maximum Chip Thickness

Maximum chip thickness can also be calculated geometrically from Fig. 14.17 (see appendix). Although this is more complicated, it provides an alternative formula that is useful for considering the effects of grain spacing. A geometric analysis is given in the appendix in Section 14.11 at the end of this chapter. It leads to a general expression for maximum chip thickness in terms of grain spacing L .

$$h_{cu \cdot \max} = 2.L \cdot \frac{a_e \cdot v_w}{v_s \cdot l_c} \quad \text{max geometric chip thickness} \quad (14.19)$$

Chip Thickness Conclusions

We can now draw some general conclusions about the effects of kinematics on wheel behaviour in the same way as Alden and Guest almost 100 years ago. Reducing work speed or increasing wheel speed with constant depth of cut reduces uncut chip thickness and cross-sectional area of the chips. This reduces stress on the abrasive grains and reduces grain fracture so that the grains have a greater tendency to become dull with time. The same is true for reducing depth of cut or increasing the number

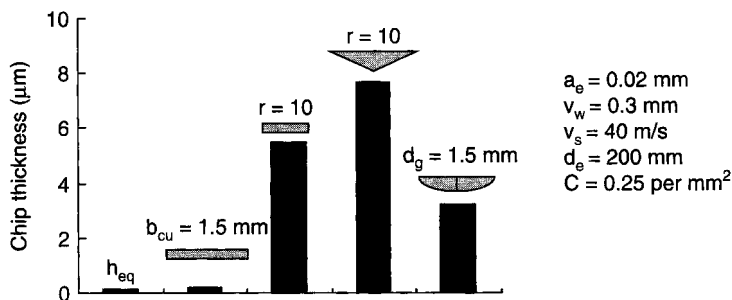


Figure 14.12 Comparison of h_{eq} and mean chip thickness for different chip shapes and widths.

of active cutting edges per unit area. Similarly, when conformity and contact length are increased, chip thickness is reduced.

A grinding wheel tends to glaze with insufficient stress on the abrasive grains and action must be taken to increase the stress. For example, depth of cut can be increased. Alternatively, work speed can be increased while maintaining depth of cut constant. Other possibilities are to employ an abrasive with larger grain spacing or to use a softer grade of wheel.

The general conclusions reached above are valid for each of the assumptions and have stood the test of time. The more difficult aspects to predict are the second-order effects that depend on the effect of changes in wheel topography due to a change in kinematic conditions. However, with careful examination of the wheel surface it is possible to explain most aspects of grinding behaviour.

Grain Density Variations

Study of grinding wheels and grains involved in active grinding showed that many more grains are actively involved than would be expected based on the static grain distribution in the wheel and use of Eqn (14.16) (Cai and Rowe 2004). This finding suggests that maximum chip thickness can be very much larger than predicted. Two explanations contribute to the difference between prediction and observation of worn grains. These relate first to the variability of grain spacing and second to the compression of the abrasive structure.

From Fig. 14.6, it can be seen that grain spacing will sometimes be double the mean grain spacing. This results in double the maximum chip thickness as is clear from Eqn (14.19). Similarly, grain depth in the wheel surface will sometimes be double the mean grain depth. This means that a

grain may take a very small cut or no cut at all. The next grain to take a cut has to remove a larger depth of material. Consideration of these situations explains why maximum chip thickness will sometimes be several times larger than given by Eqn (14.16).

There is another possible explanation for the much larger number of worn grains than would be expected from the static grain distribution and the predicted chip thickness. Since we know there is a substantial elasticity of resin-bonded and vitrified wheels, the explanation for the inconsistency most probably lies with the compression of the grains into closer proximity with the cutting surface. It was found from experiments that deflections of the wheel under the influence of the grinding forces brought many more grains into active contact as illustrated in Fig. 5.4. This allows many more grains to become actively involved in grinding with a small grain depth of cut. This is consistent with observations that more elastic wheels produce a lower surface roughness.

It is important to remember that C is not actually constant. The number of active cutting edges increases with grain depth of cut (Cai 2001). At the outermost radius of the wheel very few active cutting edges are encountered. Accordingly, finish grinding with almost zero depth of cut brings very few grains into active cutting. As the depth of cut is increased, more grains below the wheel surface are brought into engagement and the value of C increases. As C increases, grain spacing is reduced. For this reason, uncut chip thickness cannot be more accurately estimated than allowed by the knowledge of *active* grain density and spacing and the compression of the wheel.

14.10 Surface Roughness

Surface roughness increases with equivalent chip thickness h_{eq} as demonstrated in Fig. 14.13. In practice, grain spacing in the abrasive also has a strong effect on surface roughness and h_{eq} takes no account of this. Grain spacing depends primarily on grit size, as indicated by Eqn (5.2). This means grit size affects surface roughness.

The effect of grain spacing on kinematic roughness is illustrated in Fig. 14.13.

As each grain cuts a groove in the workpiece, a series of scallops is created. With uniform grain spacing, the theoretical peak-to-valley roughness, R_t along the lay, that is, in the direction of the motion of the grains, is given by the height of the scallops. According to the principle of intersecting chords,

$$R_t = \frac{1}{d_e} \cdot \left(\frac{s}{2} \right)^2 = \frac{1}{d_e} \cdot \left(\frac{L}{2} \frac{v_w}{v_s} \right)^2 \quad \text{roughness along the lay} \quad (14.20)$$

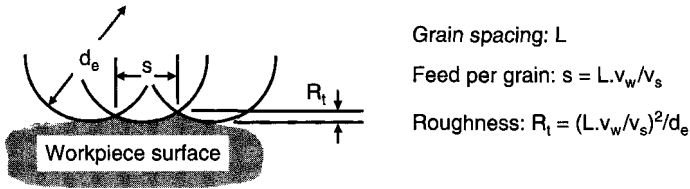


Figure 14.13 Kinematic surface roughness along the lay related to grain spacing.

Along the lay, the determinants of kinematic surface roughness are grain spacing on the wheel which is closely related to grain size, work speed, wheel speed, and equivalent diameter. In practice, other physical effects also play an important role in determining surface roughness, including ploughing and adhesion.

Depth of cut only affects roughness indirectly. For example, this may be by causing grains to fracture or to pull out thereby increasing the spacing L and hence R_t .

Surface roughness measured across the lay will be different from along the lay. Assuming regular grain spacing and that grains make an approximately triangular groove, roughness can be related to average chip thickness as in Fig. 14.14.

Kinematic roughness across the lay is equal to the maximum uncut chip thickness:

$$R_t = h_{cu\text{-max}} = \sqrt{3} \cdot \frac{L}{v_s \cdot R_L} \cdot \sqrt{\frac{a_e}{d_e}} \quad \text{roughness across the lay (14.21)}$$

Wheel manufacturers make recommendations for grit size based on the surface roughness required. Recommendations for conventional abrasives are usually given in terms of mesh size as described in Chapter 3. Converting grit size into average grain diameter using Eqn (5.2), typical recommendations are illustrated in Fig. 14.15. This figure shows that roughness increases with grain diameter as expected from Eqns (14.20) and (14.21).

Results from Saljé confirm that workpiece surface roughness when using a particular wheel and workpiece combination depends primarily on

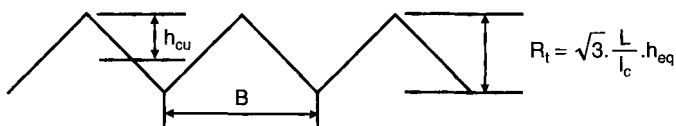


Figure 14.14 Kinematic roughness across the lay related to grain spacing.

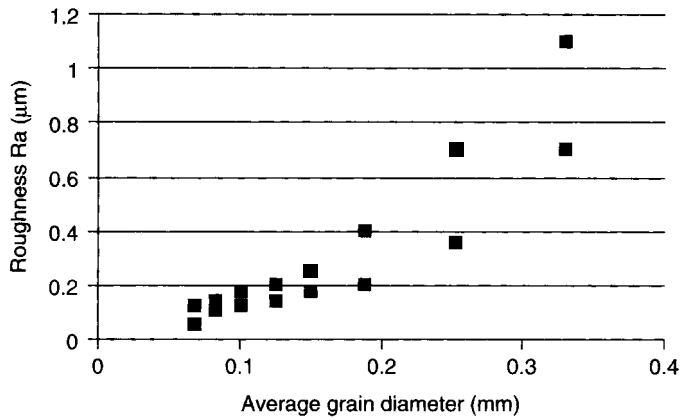


Figure 14.15 Surface roughness range and average grain diameter based on wheel manufacturers' typical recommendations.

the speed ratio and secondarily on square root of depth of cut (Tönshoff et al. 1992). Typical values are shown in Fig. 14.16.

Workpiece roughness depends on the final depth of cut. Within a spark-out period where the feed is ceased to allow deflections to relax, depth of cut reduces and roughness is reduced.

There are a number of factors that increase or reduce roughness in grinding. Some of these factors include

- (i) irregular grain spacing
- (ii) irregular grain depths
- (iii) wheel dressing
- (iv) wheel wear
- (v) wheel loading

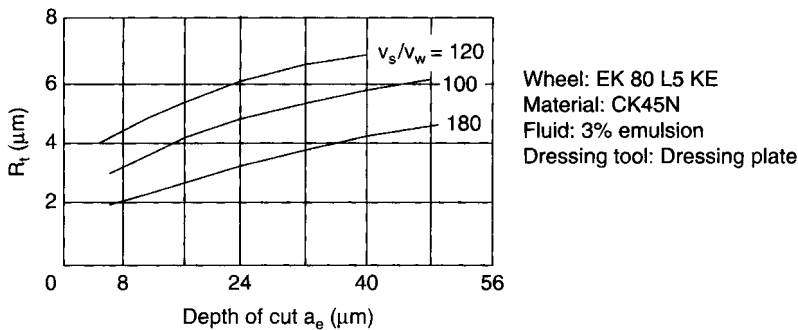


Figure 14.16 Dependence of surface roughness on speeds and depth of cut.

- (vi) ploughing
- (vii) elasticity of the wheel
- (viii) dwell/spark out
- (ix) adhesion
- (x) use of grinding fluid

Some of these effects are kinematic and others are tribological.

14.11 Appendix: Maximum Chip Thickness— Derivation from Geometry

The grinding wheel diameter is taken to be the equivalent diameter d_e so that the analysis applies equally well for internal or external grinding. The justification for use of equivalent diameter was given in Chapter 3. The grinding contact angle is θ_s . The first grain contacts the workpiece at B for down-cut grinding or at A for up-cut grinding and sweeps out the path AB. The second grain makes contact at B' for down cut or makes contact at A' for up cut. The second grain sweeps out the path A'B' after the workpiece has moved a distance s . Strictly, the path AB is not the same as the grinding wheel shape. For a circular grinding wheel the path AB is a trochoid. However, it is normal to assume a circular shape which is a good approximation and greatly simplifies the analysis (Fig. 14.17). The maximum chip thickness $h_{cu,max}$ is the distance BC between the two grain paths. From triangle BCB', $h_{cu,max} = s \cdot \sin(\theta_s - \theta'_s)$. From triangle D'O'B,

$$\sin(\theta_s - \theta'_s) = \frac{D'B}{O'B} = \frac{D'B' - s}{d_s/2 - h_{cu,max}}$$

Since $D'B' = \sqrt{a_e d_e - a_e^2}$ and $h_{cu,max}$ is always very small compared to the term $d_e/2$, maximum uncut chip thickness is given by

$$h_{cu,max} = 2s \sqrt{\frac{a_e}{d_e} - \frac{a_e^2}{d_e^2} - \frac{2s^2}{d_e}}. \text{ Neglecting small terms } h_{cu,max} = 2s \sqrt{\frac{a_e}{d_e}} \text{ and}$$

$$\text{since } s = L \cdot \frac{v_w}{v_s},$$

$$h_{cu,max} = 2 \cdot L \cdot \frac{v_w}{v_s} \cdot \sqrt{\frac{a_e}{d_e}} = 2 \cdot L \cdot \frac{a_e \cdot v_w}{v_s \cdot l_c}$$

$$\text{max. chip thickness—geometric} \quad (14.22)$$

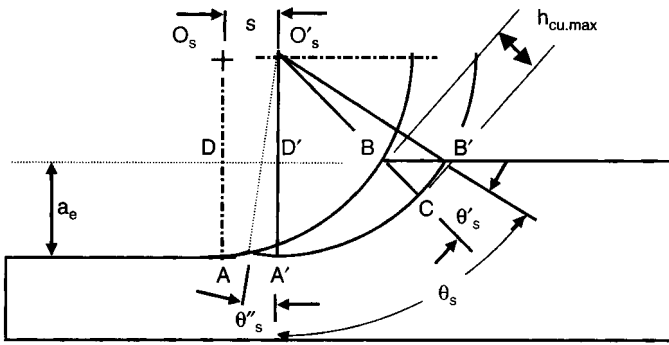


Figure 14.17 Geometry of the uncut chip.

In Fig. 5.3, $C = 1/L.B$, where L represents the mean spacing of the active grains in the grinding direction and B the mean grain spacing in the lateral direction. In other words, on average, an area $L.B$ of the grinding wheel surface contains only one active cutting edge. The relationship between L , B , and $\bar{b}_{cu}$ depends on the chip shape. It is essential to ensure consistency between grain spacing and the implied volume of the material removed. This can be ensured by equating mean chip thickness from removal rate with mean chip thickness from geometry. That is to say, Eqn (14.22) must give the same answers as given by Eqns (14.14), (14.16), and (14.18). The following relationships are obtained.

$$L = \frac{1}{C.b_{cu}} \quad \text{constant width—rectangular} \quad (14.23)$$

$$L = \frac{2}{C.b_{cu}} \quad \text{constant width—triangular} \quad (14.24)$$

$$L = \frac{\sqrt{3}}{2.C.b_{cu}} \quad \text{increasing width—rectangular} \quad (14.25)$$

$$L = \frac{\sqrt{3}}{C.b_{cu}} \quad \text{increasing width—triangular} \quad (14.26)$$

$$L = \frac{1.84 \times 3}{4.C.b_{cu}} \quad \text{increasing width—round} \quad (14.27)$$

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15 Real Contact

15.1 Real and Apparent Contact Area

The importance of the contact region between wheel and workpiece has until recent years been poorly understood. Grinding behaviour strongly depends on the real contact area which is a small proportion of the apparent contact area. That real contact area is a very small proportion of the apparent contact area is well known. Less well known is the effect of real contact behaviour on grinding forces and onset of thermal damage (Malkin 1989). This chapter analyses the real contact and shows the large disparities from simplified assumptions (Fig. 15.1).

The apparent area of contact, A_c between wheel and workpiece is given by

$$A_c = b \cdot l_c \quad \text{apparent area of contact} \quad (15.1)$$

Example 15.1 The grinding width is 15 mm and the contact length is 1.5 mm. Determine the apparent area of contact between the grinding wheel and the workpiece.

$$A_c = 15 \times 1.5 = 22.5 \text{ mm}^2 \text{ (or } 0.0349 \text{ in.}^2\text{)}$$

The real contact area is the summation of all the contacts on the tips of the grains (Fig. 15.2). The real contact area at the tip of an active grain is larger than the wear flat area although the difference is difficult to assess. Measurement of real contact area requires knowledge of the grain depth of cut. The problem of determining grain depth of cut was discussed in the previous chapter. Wear flat area can be determined by one of the methods described and is usually expressed as a percentage of the apparent area of contact A_c .

$$A\% = \frac{100}{A_c} \cdot [\delta A_1 + \delta A_2 + \dots \delta A_n] \quad \text{real contact area} \quad (15.2)$$

Example 15.2 If the contact area is 2% of the apparent contact area, determine the real contact area for the previous example.

$$A = 0.02 \times 22.5 = 0.45 \text{ mm}^2 \text{ (or } 0.0007 \text{ in.}^2\text{)}$$

At the contacts with the grain, the stresses are sufficient to plastically deform and shear the workpiece material. Clearly, the stresses are therefore sufficient to cause significant elastic deflections of the grains within the relatively elastic abrasive structure.

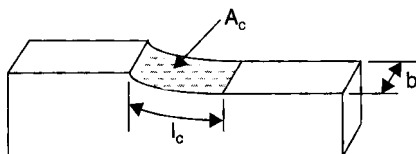


Figure 15.1 Apparent area of contact.

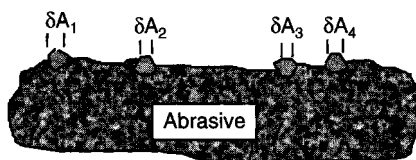


Figure 15.2 Real contact area is the sum of the contact areas at all active grains.

15.2 Real Contact Length

Estimates of contact length have varied by up to 600%. Contact length governs many aspects of grinding behaviour (Saljé and Möhlen 1986):

- energy intensity into the workpiece, $q = P/b.l_c$
- wear length and contact time of the abrasive grains
- number of abrasive grains in contact, chip thickness, and grain forces
- wheel wear and nature of wheel wear
- surface roughness
- contact temperatures

Early workers assumed contact length was equal to the geometric value. This was later shown to be an over-simplification in the majority of conventional grinding processes using vitrified or resin-bond wheels. It was shown by Brown et al. (1971), Verkerk (1975), Gu and Wager (1988), Zhou and Van Luttervelt (1992), and others that the real contact length l_c is substantially larger than the geometric contact length, l_g . The increased area of contact is mainly due to deflection of the grinding wheel grains under the action of the normal force. This effect is strong for vitrified and resin-bonded wheels where the elastic modulus of the porous abrasive grain-bond mixture is much lower than the elastic modulus of most workpiece materials. The deflection of individual grains is also increased due to high stresses at the discrete points of contact.

Figure 15.3 illustrates the difference between geometric contact length l_g due to depth of cut a_e and contact length l_f due to deflection δ under the action of a normal force F_n . The expressions given can be directly obtained from the principle of intersecting chords of a circle. These expressions are accurate as long as the depth of cut and the deflection are both much smaller than the equivalent diameter which is generally true in grinding. The expressions are

$$l_g = \sqrt{a_e \cdot d_e} \quad \text{depth-of-cut contact length} \quad (15.3)$$

$$l_f = 2 \cdot \sqrt{\delta \cdot d_e} \quad \text{deflection contact length} \quad (15.4)$$

Example 15.3 Calculate the values of l_g and l_f for an equivalent diameter of 200 mm, a 0.3 mm depth of cut, and a normal deflection of 0.15 mm between the wheel and the workpiece due to the normal grinding force. (Note: The equivalent diameter for deflections is always larger than the equivalent diameter for depth of cut. See below.)

$$l_g = \sqrt{0.3 \times 200} = 7.75 \text{ mm (or 0.305 in.)}$$

$$l_f = 2 \times \sqrt{0.15 \times 200} = 10.95 \text{ mm (or 0.431 in.)}$$

In both cases, the expressions are defined in terms of equivalent wheel diameter d_e representing conformity between a cylinder and a plane. Since a force is required to remove a depth of cut, both equations apply simultaneously. The effects of l_g and l_f are illustrated in Fig. 15.4. Here effective diameter for deflection d_{ef} is derived from the effective wheel diameter for

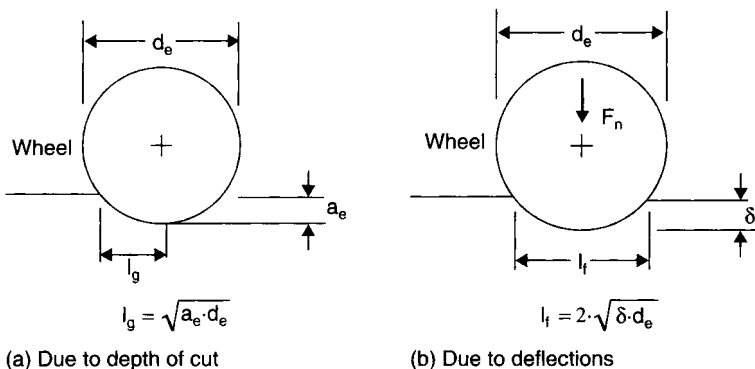


Figure 15.3 Contact length due to (a) depth of cut and (b) deflections.

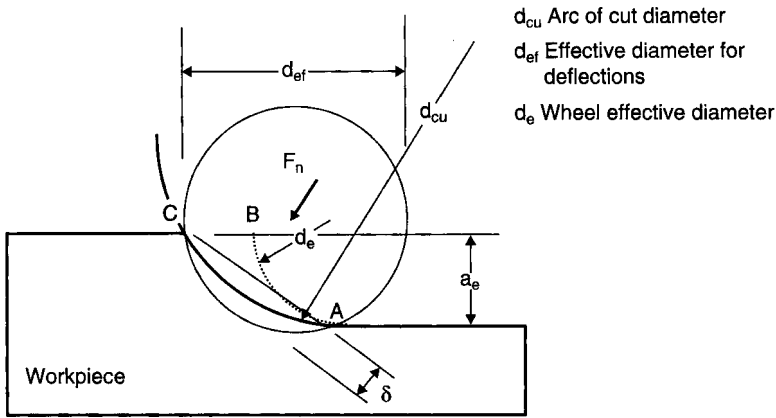


Figure 15.4 Effective diameter d_{ef} for calculation of deflections is based on a wheel of diameter d_e pressed against the unloaded arc of cut d_{cu} .

depth of cut, d_e pressed against the curved surface of the workpiece having a contact diameter, d_{cu} .

The effective wheel diameter d_e in grinding is

$$\frac{1}{d_e} = \frac{1}{d_s} + \frac{1}{d_w} \quad \text{equivalent diameter in grinding} \quad (15.5)$$

The effective diameter, d_{ef} for calculating deflection has to take into account the conformity with the arc AC being cut. The effective diameter for deflections must therefore be based on the combined effect of the effective wheel diameter d_e and the workpiece contact diameter d_{cu} . The equivalence is further illustrated in Fig. 15.5.

$$\frac{1}{d_{ef}} = \frac{1}{d_e} - \frac{1}{d_{cu}} \quad \text{effective diameter for deflection} \quad (15.6)$$

The minus sign is because the two contact curves are conformal. Under the combined effect of the normal force and depth of cut, the arc of contact l_c is shown as AC in Fig. 15.4. The contact length is related to the diameter of the unloaded cut by

$$d_{cu} = \frac{l_c^2}{a_e} \quad \text{unloaded cut diameter} \quad (15.7)$$

The contact length, l_c , under the normal force, is also related to the equivalent diameter, d_{ef} , so that

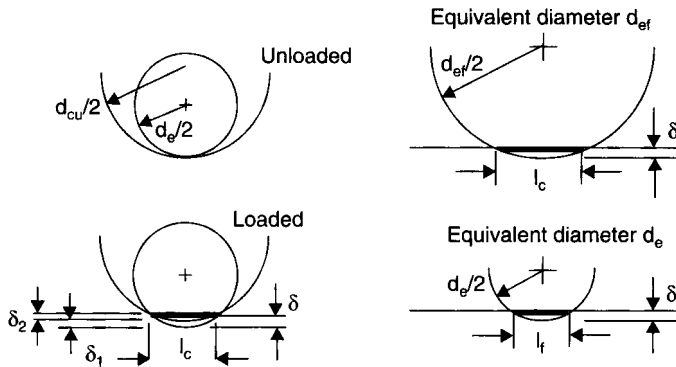


Figure 15.5 Deflections between a smooth grinding wheel and a workpiece arc.

$$d_{ef} = \frac{l_c^2}{4\delta} \quad \text{equivalent diameter for deflections} \quad (15.8)$$

By definition,

$$d_e = \frac{l_g^2}{a_e} \quad \text{equivalent diameter for depth of cut} \quad (15.9)$$

Substituting for these three diameters in Eqn (15.6), we obtain $l_c^2 = l_g^2 + \frac{4d}{a_e} \cdot l_g^2$. From the definitions of l_f and l_g given in Eqns (15.3) and (15.4), $\frac{4\delta}{a_e} = \frac{l_f^2}{l_g^2}$, so that

$$l_c^2 = l_g^2 + l_f^2 \quad \text{combined contact length} \quad (15.10)$$

Example 15.4 What is the total contact length for Example 15.3?

$$l_c = \sqrt{7.75^2 + 10.95^2} = 13.4 \text{ mm (or 0.528 in.)}$$

It should be noted that d_e was employed in Eqn (15.4) instead of d_{ef} . It follows that Eqn (15.10) is valid when l_f is defined in terms of d_e . This means l_g and l_f can be determined quite independently to find the combined length using Eqn (15.10). This is consistent with Qi et al. (1997) although the derivation is slightly different in approach.

Simplification is possible under either of two extreme conditions. In very shallow cut grinding, a_e is very small and l_g will be small. If the normal

force is sufficiently large, due to a blunt wheel the contact length due to deflections will be much more significant. In this case, $l_c = l_f$ when $\delta \gg a_c$.

In deep grinding with a sharp wheel, the deflections are small compared with the depth of cut, so that $l_c = l_g$ when $a_c \gg \delta$.

15.3 Smooth Wheel Analysis

Elastic deflections between smooth bodies was analysed by Hertz in 1882 for spheres in contact. Assuming two parallel cylindrical surfaces, the solution may be extended to a smooth grinding wheel in contact with the workpiece arc of contact as illustrated in Fig. 15.5. The use of effective diameter from Eqn (15.6) is the equivalent of pressing a roller of diameter d_{ef} into a flat surface.

The pressure distribution in the contact area based on Hertz is parabolic as in Fig. 15.6.

$$p = p_{\max} \left[1 - \left(\frac{2x}{l_{fs}} \right)^2 \right]^{\frac{1}{2}} \quad \text{parabolic pressures} \quad (15.11)$$

where $x \leq \pm \frac{l_{fs}}{2}$ and l_{fs} is the contact length for smooth surfaces. The deflections of each surface depend on the pressure and the elastic properties including Young's modulus, E and Poisson's ratio ν . For consistency with the pressure distribution, we must make $\delta_1(x) = 0$, at $x = \pm l_{fs}/2$. Ignoring end effects, deformation of the first cylindrical surface is (Williams 1994)

$$\delta_1(x) = \frac{1 - \nu_1^2}{E_1} \cdot \frac{2 \cdot p_{\max}}{l_{fs}} \cdot \left[\left(\frac{l_{fs}}{2} \right)^2 - x^2 \right] \quad \text{single body deflection} \quad (15.12)$$

A similar deflection applies at the second cylindrical surface. Adding them both, total deflection $\delta(x)$ is

$$\delta(x) = \frac{2 \cdot p_{\max}}{l_{fs} \cdot E^*} \cdot \left[\left(\frac{l_{fs}}{2} \right)^2 - x^2 \right] \quad \text{total deflection} \quad (15.13)$$

where E^* represents the elastic properties of the two surfaces.

$$\frac{1}{E^*} = \frac{1 - \nu_1^2}{E_1} + \frac{1 - \nu_2^2}{E_2}.$$

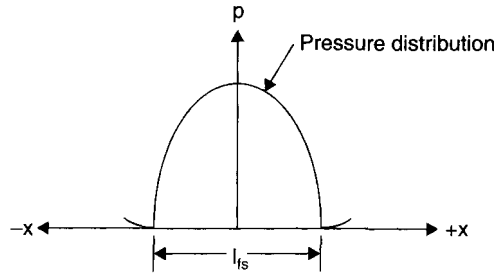


Figure 15.6 Pressure distribution on a roller loaded against a flat surface.

When $x = 0$, $\delta(x) = \delta$ and maximum pressure is $p_{\max} = \frac{2 \cdot E^*}{l_{fs}} \cdot \delta$. Substituting for δ from Eqn (15.4)

$$p_{\max} = \frac{l_{fs} E^*}{2 \cdot d_e} \quad \text{max. pressure} \quad (15.14)$$

Integrating the pressures in Eqn (15.11) to obtain the normal force together with Eqn (15.14) yields,

$$F'_n = \frac{F_n}{b_w} = \frac{\pi \cdot l_{fs}^2 \cdot E^*}{8 \cdot d_e} \quad \text{spec. normal force} \quad (15.15)$$

The contact length due to deflection for smooth bodies is therefore given by

$$l_{fs}^2 = \frac{8 \cdot F'_n \cdot d_e}{\pi \cdot E^*} \quad \text{smooth deflection length} \quad (15.16)$$

The combined contact length for smooth surfaces can now be obtained by the use of Eqn (15.10)

$$l_c^2 = \frac{8 \cdot F'_n \cdot d_e}{\pi \cdot E^*} + a_e \cdot d_e \quad \text{combined smooth length} \quad (15.17)$$

15.4 Rough Wheel Analysis

Smooth bodies are far from smooth when real contact is investigated. Steel surfaces have to be pressed together with substantial plastic flow to approach 100% contact. Real surfaces contact only on asperities. This phenomenon,

often ignored in engineering analysis, greatly reduces stiffness across a contact compared with the stiffness of a solid body. Experimental evidence confirms that grinding contact is far removed from smooth body contact (Rowe et al. 1993; Qi 1995). Grinding wheels are designed to contact a workpiece on widely spaced cutting edges as illustrated in Fig. 15.7.

Figure 15.8 illustrates a modified pressure distribution taking into account asperity contact and a limiting stress at a plastic contact. The pressure at each of these small contact points is limited by the plastic stress of the workpiece material. Penetration and increase of the apparent contact area occurs until the contact points support the normal force.

Some contact points are plastic, others are elastic. The effective pressure distribution remains approximately parabolic in shape, becoming more Gaussian as roughness increases. Most importantly, contact length for rough surfaces, l_{fr} , is substantially greater than contact length for smooth surfaces, l_{fs} . The effective pressure distribution in Fig. 15.8 is shown by the dotted line. The effective pressure distribution is assumed to be parabolic according to Eqn (15.13) although p_{max} is much lower than for smooth contact.

The area of asperity contact is much smaller than the apparent area of contact (Greenwood and Tripp 1967). The ratio of rough and smooth contact lengths R_r is

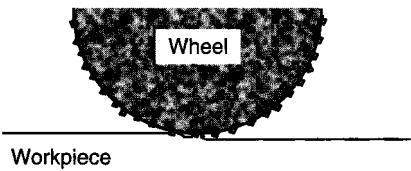


Figure 15.7 A grinding wheel makes rough body contact with a workpiece.

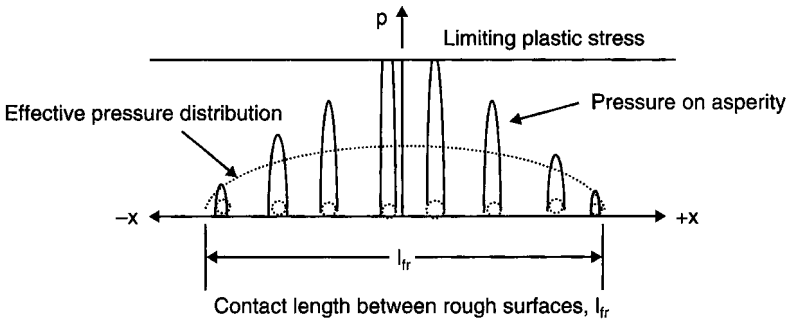


Figure 15.8 Real contact pressures between rough surfaces.

$$R_r = \frac{l_{fr}}{l_{fs}} \quad \text{roughness factor} \quad (15.18)$$

In experiments R_r was found to increase with roughness of the surfaces in contact. In grinding, R_r is usually an order of magnitude larger for precision machined surfaces. Employing the definition of R_r given in Eqn (15.18), we can modify and generalise the expression given in Eqn (15.16) to apply to grinding.

$$l_r^2 = l_{fr}^2 = \frac{8 \cdot R_r^2 \cdot F_n' \cdot d_e}{\pi \cdot E^*} \quad \text{rough deflection length} \quad (15.19)$$

Substituting for l_f and l_g in Eqn (15.10),

$$l_c^2 = \frac{8 \cdot R_r^2 \cdot F_n' \cdot d_e}{\pi \cdot E^*} + a_e \cdot d_e \quad \text{combined contact length} \quad (15.20)$$

The contact length can be estimated approximately based on the grinding power, which is often more readily available. Although the approximate method is less accurate, any method which makes a reasonable estimate of the real contact length is more accurate than simply ignoring the effect of deflections. The contact length can be expressed in terms of power using the approximate force ratio, $\mu = F_t/F_n$. This approach is used for on-line process monitoring for the purpose of avoiding thermal damage. In terms of grinding power, the contact length is

$$l_c^2 = \frac{8 \cdot R_r^2 \cdot P \cdot d_e}{\pi \cdot \mu \cdot E^* \cdot v_s \cdot b_w} + a_e \cdot d_e \quad \text{combined length based on power} \quad (15.21)$$

15.5 Calibration of the Roughness Factor R_r

Comparison with Verkerk

The contact mechanics approach was substantially investigated by Qi (1995). Qi found that the new approach gave greatly improved correlation between measurement and prediction as shown in Fig. 15.9.

Verkerk (1975) was one of the first to measure contact length in grinding. The contact mechanics approach given above was initially tested by comparison with the results of Verkerk illustrated in Fig. 15.9 for flat surface grinding.

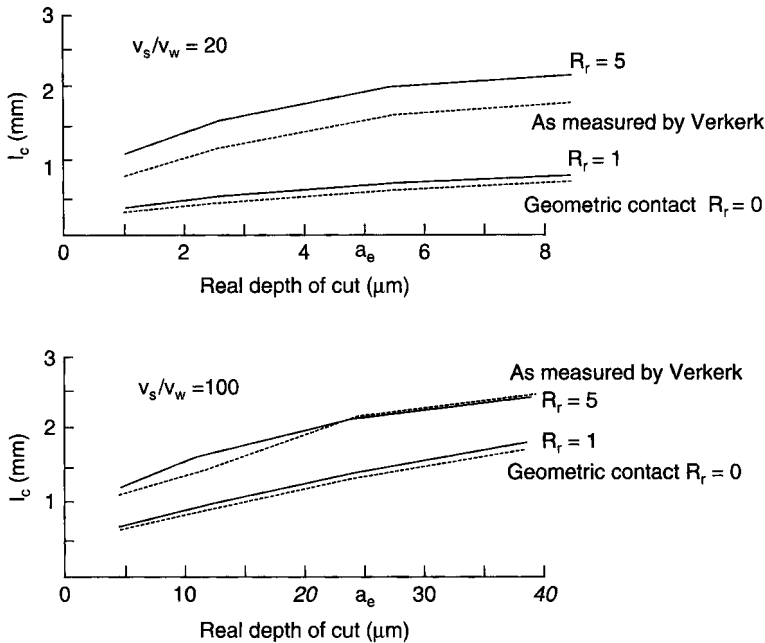


Figure 15.9 Comparison of measured and predicted contact lengths.

Defining Contact Length Empirically

Defining contact length is more difficult than it looks at first sight. Sometimes contact length is defined as the distance moved between the first contact between the workpiece and a grain and the last contact with the same point on the workpiece. However, vibrations between the workpiece and the wheel can artificially increase the measurement by this method. Contact length becomes relatively meaningless if sparse contacts which occur outside the main contact region are included in the estimate. Sparse contacts can be ignored but it raises the question of where to draw a line. Ignoring sparse contacts seems to be a reasonable approach since sparse contacts have little effect on the process. It is considered probable that this was the approach adopted by Verkerk.

In Fig. 15.9, predicted values are based on values of $R_r = 1$ and $R_r = 5$. These values were chosen because $R_r = 1$ corresponds to the smooth body analysis and $R_r = 5$ gave reasonable correlation for these results. Results are also given for contact length based purely on geometric contact length $R_r = 0$.

It can be seen that the values based on smooth body mechanics predicted contact length only slightly greater than l_g .

Values used in the predictions were

- E_w for steel: 213 kN/mm²
- E_s for vitrified alumina: 49.6 kN/mm²
- v_w for steel: 0.29
- v_s for vitrified alumina: 0.22
- d_s : 500 mm
- d_w : 90 mm
- v_s : 30 m/s
- v_w : various
- a_c : various

Verkerk's results gave measured contact lengths that were 1.5–3.5 times larger than the geometric length, l_g . The contact model with $R_r = 5$ gave reasonable agreement with the experiments whereas the smooth contact model made very little difference from l_g .

Qi Measurements

Qi measured contact length by inserting an insulated electrical contact sensor in the workpiece. A voltage was applied to the sensor. When the grinding wheel passes over the sensor, contact is established with the earthed workpiece. The duration and magnitude of the signal gives a measure of the contact. A typical contact signal is shown in Fig. 15.10. In this example, a conservatively estimated contact signal is almost three times longer than would be expected based on the geometric contact length.

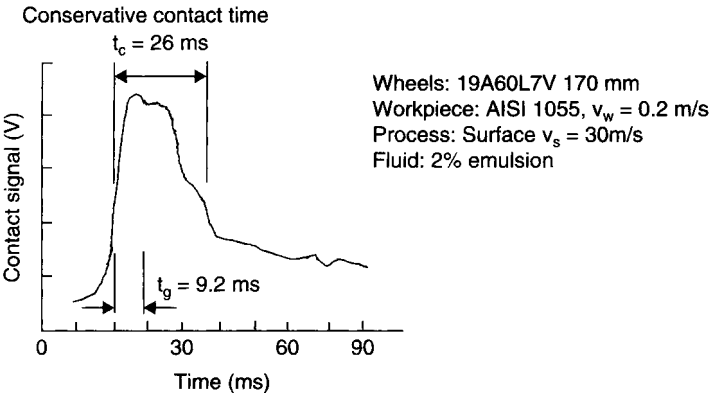


Figure 15.10 Typical contact signal (smoothed).

Contact Length Ratio

It is often convenient to express the contact length as a multiple of the geometric value.

$$R_L = \frac{l_c}{l_g} \quad \text{contact length ratio} \quad (15.22)$$

The grinding force increases almost in direct proportion to the depth of cut. Contact length should therefore increase approximately with the square root of depth of cut. This expectation is confirmed by many results. Figure 15.11 shows typical examples.

R_L tends to increase as depth of cut reduces towards zero due to the proportionately higher ratio of normal force and depth of cut. R_L is increased at higher work speeds. This is a direct result of increased grinding force and deflections for the same depth of cut.

Example 15.5 Evaluation of R_L . A measured contact time using a contact sensor in horizontal surface grinding gave $t_c = 26$ ms. The process conditions were as listed:

Grinding wheel: 19A60L7V

Wheel diameter: $d_s = 170$ mm

Wheel properties: $E_s = 49.6$ kN/mm², $\nu_s = 0.22$

Workpiece material: AISI 1055

Work speed: $v_w = 0.2$ m/s

Grinding width: $b_w = 15$ mm

Workpiece properties: $E_w = 213$ kN/mm², $\nu_w = 0.29$

Depth of cut: $a_e = 0.020$ mm

Normal grinding force: 225 N

The equivalent wheel diameter is given by $\frac{1}{d_e} = \frac{1}{170} + \frac{1}{\infty}$, so that

$d_e = 170$ mm.

Geometric contact length, $l_g = \sqrt{0.020 \times 170} = 1.84$ mm

Geometric contact time of a point on the workpiece,

$$t_g = \frac{l_g}{v_w} = \frac{1.84}{200} = 9.22 \text{ ms}$$

Real contact length, $l_c = t_c \cdot v_w = 0.026 \times 200 = 5.2$ mm

Contact length due to the force, $l_f^2 = l_c^2 - l_g^2 = 5.2^2 - 1.84^2$. $l_f = 4.86$ mm.

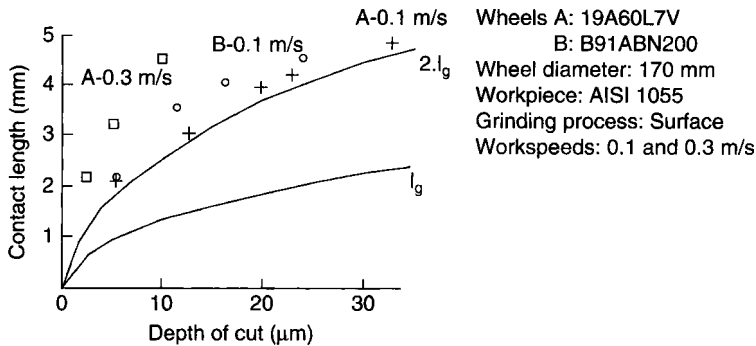


Figure 15.11 Measured contact length and geometric contact length.

The roughness factor from Eqn (15.19) is given by

$$R_r^2 = \frac{l_f^2 \cdot \pi \cdot E^*}{8 \cdot F_n' \cdot d_e} = \frac{4.86^2 \times 3.142 \times 42600}{8 \times 15 \times 170} = 155$$

where, $E^* = 42.57 \text{ kN/mm}^2$ and $F_n' = 225/15 = 15 \text{ N/mm}$.

So that $R_r = 12.5$.

Roughness factors, R_r , determined over many results typically range from 5 to 15. An average value for dry grinding of about 9 and for wet grinding of about 14 is expected for grinding common engineering steels. The value of R_r remains reasonably constant for a particular wheel–workpiece material combination, although R_r is reduced as a wheel loses its sharpness due to wear or wheel loading. This is expected due to the reduced roughness of the wheel.

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16 Specific Energy

16.1 Introduction

This chapter explores the energy required in grinding. The energy reduces as depth of cut and removal rate increase. This phenomenon is known as the size effect and is confirmed by numerous authors. Various explanations have been offered. As pointed out by Malkin (1989), the classic model of chip formation formulated by Merchant (1945) was not easily applied to grinding. The grain shape makes this impossible and unrealistic shear stresses result. An early explanation was attempted by Backer et al. (1952). However, Von Turkovich (1970) argues that the number of dislocations makes former explanations unrealistic. Fortunately, there are other explanations based on developments in the understanding of material flow as discussed below. It is demonstrated that there is more than one cause for the size effect.

16.2 The Size Effect

Measured Specific Energy

Figure 2.8 illustrated the size effect in terms of removal rate as demonstrated from measured grinding forces or power. The results in Fig. 16.1 show that the size effect is directly related to equivalent chip thickness h_{eq} (Rowe and Chen 1997). Both the figures show that energy required per unit volume removed reduces when depth of cut or removal rate is increased. Energy per unit volume of material removed is termed as specific energy.

Relationship to h_{eq}

In general, force per unit width of contact tends to vary with h_{eq} according to $F'_t = F_1 \cdot h_{eq}^f$, where the constants depend mainly on the material and wheel combination. Typically, f has a value 0.7–1.0. Specific energy is equal to power divided by removal rate and is therefore equal to $F'_t \cdot v_s / a_e \cdot v_w$. It follows that specific energy follows a relationship of the form:

$$e_c = F'_t / h_{eq} = F_1 \cdot h_{eq}^{f-1} \quad \text{Specific energy and } h_{eq} \quad (16.1)$$

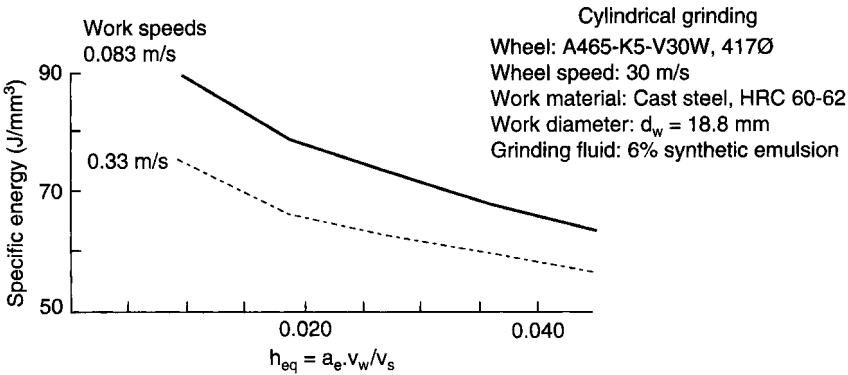


Figure 16.1 Variation of specific energy with depth of cut and removal rate.

Specific energy remains constant, if F_t increases in direct proportion with h_{eq} since $e_c = F'_t / h_{eq}$. Where tangential force increases in direct proportion, no size effect will be evident as illustrated in Fig. 16.2(a). However, in Fig. 16.2(b) and (c), e_c reduces as h_{eq} increases. This is because tangential force increases less than proportionately with h_{eq} . Section 2.6 gave two practical examples.

Physical Reasons

Chen et al. (1989) related specific energy to average cross-sectional area of the uncut chip and demonstrated a strong size effect for grinding ceramics.

The physical reasons for the size effect include

- a threshold force for cutting, Fig. 16.2(b)
- new surface area produced—the sliced bread analogy, Fig. 16.2(c)

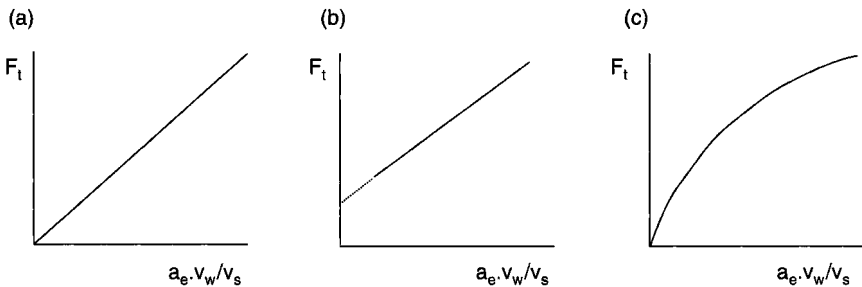


Figure 16.2 Variation of tangential force required for a size effect: (a) No size effect, (b) size effect—Case I, and (c) size effect—Case II.

- grain shape, Fig. 16.2 (b) and (c), also influenced by wear and dressing
- differences between cutting, ploughing, and rubbing, Fig. 16.2(b) and (c).

These reasons are discussed below.

16.3 Threshold Force Effect

An example of grinding with no threshold force is illustrated in Fig. 16.2(a). In practice, there is always a small threshold force as illustrated in Fig. 16.2(b). With blunt grains, the threshold force is larger. This provides the simplest explanation of the size effect. At zero depth of cut, a finite force is required due to friction between the wheel and workpiece even though no material is removed (Hahn 1966). The result is that specific energy is infinite. As depth of cut is increased, specific energy reduces.

16.4 Surface Area Effects

Surface Area Created

The energy consumed in the grinding process is spent on deforming and cutting new surfaces in the workpiece material. The new surface area produced is therefore a measure of the energy required. The new surface area produced is closely related to the surface area of the uncut chips.

Chip Volume and Surface Area

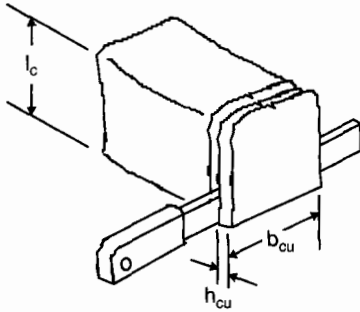
The volume of the chips is increased as depth of cut is increased. Material removed is divided into fewer chips. This requires less energy in the same way that cutting a loaf of bread into fewer slices of larger thickness requires less cuts and therefore less energy (Rowe and Chen 1997). To confirm this, we need to examine the new surface area produced.

Specific Energy and Surface Area

The sliced bread analogy says that the specific energy increases with the surface area created per unit volume removed. This is illustrated in Fig. 16.3.

Mean surface area per unit volume is given approximately by $\frac{S_{cu}}{V_{cu}} \approx \frac{2}{h_{cu}}$.

It follows from Equation 14.15 for the triangular chip,



$$\text{Volume per chip: } V_{cu} = b_{cu} \cdot h_{cu} \cdot l_c$$

$$\text{Cut surface area: } S_{cu} \approx 2 \cdot b_{cu} \cdot l_c$$

$$\text{Cut area/unit volume: } \frac{S_{cu}}{V_{cu}} = \frac{2}{h_{cu}}$$

Figure 16.3 The sliced bread analogy assuming that energy is proportional to surface area.

$$e_c \propto \frac{S_{cu}}{V_{cu}} = \sqrt{2 \cdot \frac{v_s}{v_w} \cdot C \cdot r \cdot R_L} \sqrt{\frac{d_c}{a_e}} \quad \text{surface area/volume} \quad (16.2)$$

where C is the number of active cutting edges per unit area of the abrasive, r is the mean width to depth ratio of the uncut chip cross-section, and $R_L = 1$ for a rigid wheel.

Depth of Cut and Surface Area

A greater surface area produced by grinding requires a greater amount of energy as expressed by Eqn (16.2). A number of conclusions follow. According to Eqn (16.2), new surface area increases with $a_e^{-1/4}$. Halving the depth of cut increases surface area produced by 19%. Specific energy is increased accordingly.

Grain Density and Surface Area

Specific energy will also increase with the number of active grits per unit area C . This is also in agreement with experience.

Work Speed and Surface Area

Increasing work speed at constant depth of cut, surface area varies with $v_w^{-1/2}$. Specific energy reduces as found in practice. This is the logic behind HEDG where high work speeds and large depths of cut are employed.

Increasing work speed at constant removal rate, the effect depends on the variation of a_c and v_w . Since a_c is proportional to $1/v_w$, it is found from Eqn (16.2) that surface area varies with $v_w^{-1/4}$. There is a smaller saving in specific energy required at constant removal rate. The saving is even less taking into account the increased number of cutting edges brought into action. Equation 16.2 gives surface area varying with $v_w^{-1/6}$ when C is assumed to be proportional to chip thickness.

Conclusion—Chip Thickness and Specific Energy

The conclusion is that increasing chip thickness reduces specific energy. This conclusion is invaluable for understanding the implications of changing process parameters.

16.5 Grain Shape and Sharpness Effects

Quantifying Sharpness

Abrasive grains are usually blocky and crystalline in nature as described in Chapter 3. With gentle wear, the grains develop flats at the peaks of the cutting edges as illustrated in Fig. 16.4. Grinding with blunt grains clearly requires more energy and this is found to be the case in practice. Grain geometry is therefore one of the most important aspects of determining grinding forces. One way to represent grain sharpness is to draw a circle through the three extremities of the contact with the work surface as in Fig. 16.4. The depth to diameter ratio t/d_g of the circle is a measure of the sharpness of the contact. Examples are shown for sharp and blunt grains.

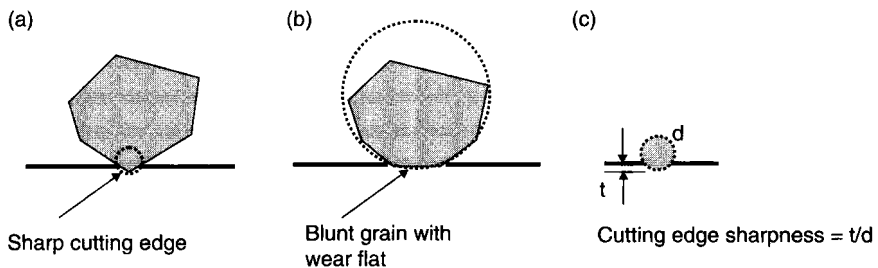


Figure 16.4 (a) A sharp grain easily cuts the workpiece. (b) A blunt grain consumes energy rubbing the surface with little material removed. (c) Cutting edge sharpness represented by depth over contact diameter.

Indentation Model

Shaw (1971) analysed the energy required considering the abrasive grain as a sphere as in Fig. 16.5. The force applied to a grain was assumed to be of a similar nature to the force applied in a hardness test.

As a grain is indented into a workpiece a region becomes plastic and starts to flow sideways and upwards around the grain. As the sphere moves in a horizontal direction, plastically deformed material is forced upwards. If the penetration depth t is sufficient, a chip is formed.

Shaw approximated the specific energy as

$$e_c = \frac{3 \cdot \pi}{4} \cdot \frac{H}{\beta} \cdot \frac{C'}{3} \cdot \left(2 + \mu \sqrt{\frac{d_g}{t}} \right)$$

Indentation model

(16.3)

where H is the hardness of the workpiece, β is an upward flow ratio, C' is a constraint coefficient, μ is a coefficient of friction, d_g is the grain diameter, and t is the indentation depth. Essentially, Eqn (16.3) comprises the indentation energy plus the surface shearing energy. Specific energy is seen to depend on the hardness of the material being cut, ploughing, friction at the interface, grain size, and the grain depth of cut.

As the grain depth t is reduced, specific energy is increased. The ratio t/d_g , is a measure of the grain sharpness, Fig. 16.4. The size effect based on the spherical grain model takes account of the change in the effective sharpness of a grain which is increased as the grain penetrates to a larger depth. Large grain abrasive tools have the advantage of more space for material removal but specific energy is only reduced if the grain penetration h_{cu} is increased relative to the grain size.

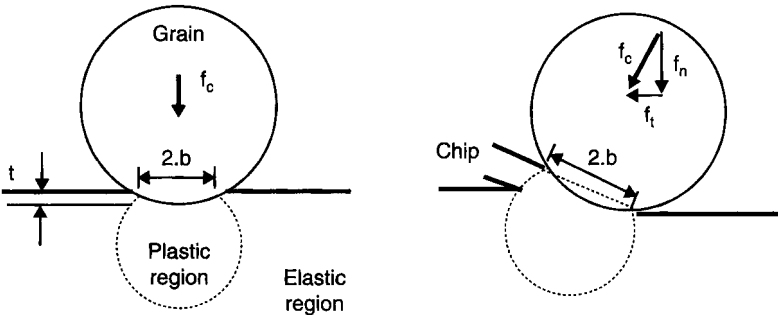


Figure 16.5 Indentation model of abrasion (based on Shaw 1971).

Wear and Dressing Effects on Grain Shape

Wear and dressing both have a substantial effect on energy required in grinding. High removal rates tend to increase grain sharpness by causing fracture. The wear behaviour is also influenced by the dressing process as described in Chapter 4.

16.6 Rubbing, Ploughing, and Cutting

3 Domains of Abrasive Contact

Hahn (1966) proposed three aspects of material deformation as a grain interacts with a workpiece corresponding to rubbing, ploughing, and cutting as illustrated in Fig. 2.2.

Rubbing

In rubbing, material removal is negligible although friction causes energy to be consumed. Elastic and plastic deformations take place as evidenced by polishing of the surface.

Ploughing

Ploughing occurs when the force on the grains is increased. Scratch marks appear and ridges are formed at the sides of the scratches. Plastic deformation is increased but material removal remains negligible.

Cutting

With further increases in force, material is rapidly removed and chips are produced.

Sub-Threshold Condition

According to Hahn, below the threshold force for material removal, specific energy approaches infinity. As depth of cut is increased, rubbing and ploughing energy becomes relatively smaller in comparison with cutting energy. The specific energy drops.

3 Energy Components

Grinding energy has three components corresponding to the three mechanisms proposed by Hahn. According to Kannapan and Malkin (1972), the specific energy requirement comprises chip energy e_{ch} , ploughing energy e_p , and sliding or rubbing energy e_s .

$$e_c = e_{ch} + e_p + e_s \quad \text{Energy components} \quad (16.4)$$

The energy can be partitioned into the three components by a series of experiments.

Sliding or Rubbing Energy

Sliding energy is proportional to the grain wear flat area and therefore requires the difficult measurement of wear flats. The increase in tangential force with percentage wear flat area A is illustrated schematically in Fig. 16.6. Some results for different materials are shown in Fig. 16.7. The forces are seen to increase proportionally with wear flat area up to a discontinuity after which forces increase more steeply and burn is experienced. With a sharp wheel, $A = 0$, the energy consists only of ploughing and chip formation energy.

The sliding energy component is

$$e_s = F_{t,s} \cdot v_s \quad \text{Sliding energy} \quad (16.5)$$

where $F_{t,s}$ is defined in Fig. 16.6.

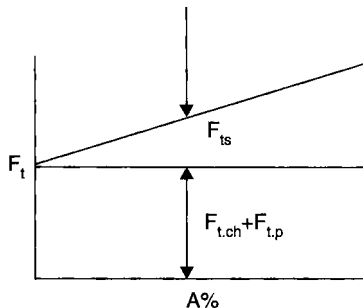


Figure 16.6 The sliding or rubbing component of tangential force increases with wear flat area.

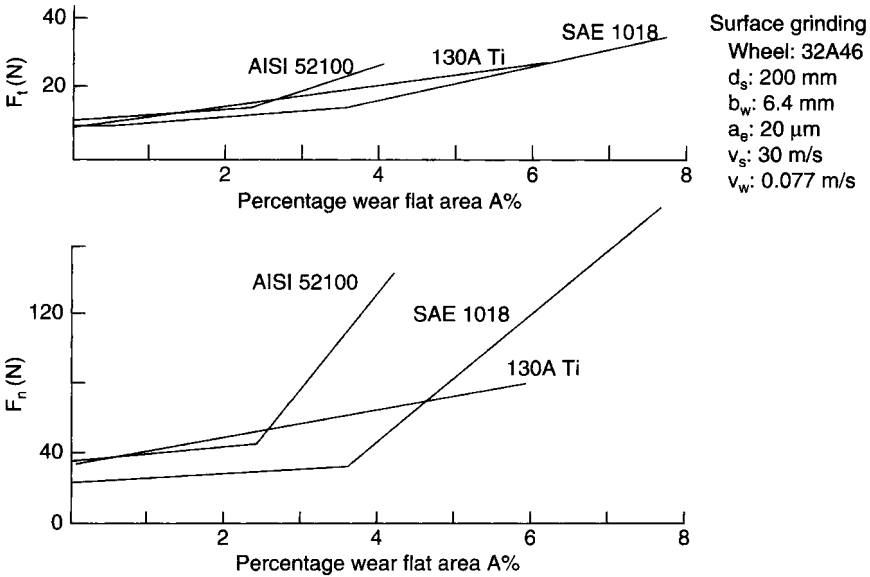


Figure 16.7 Forces increase in proportion to grain wear flat area up to a discontinuity (based on Malkin 1989).

Chip Formation Energy

After subtracting the sliding energy from the total energy, the remaining energy consists, according to Malkin, of ploughing energy and chip formation energy. The chip formation energy is found to be close to the amount of energy required to melt the material removed as chips. The maximum specific heat energy that can be held within the chips is the energy required to raise the temperature of the chips.

$$e_{ch} = \rho.C.T_{ch} \quad \text{Max chip energy} \quad (16.6)$$

where ρ is the material density, C is the average specific heat capacity, and T_{ch} is a temperature below the melting point.

Minimum Energy Asymptote

The ploughing energy becomes a smaller proportion at higher removal rates as shown by results in Fig. 16.8. The chip formation energy remains constant with increasing removal rate and can be identified as the proportion below the dotted line.

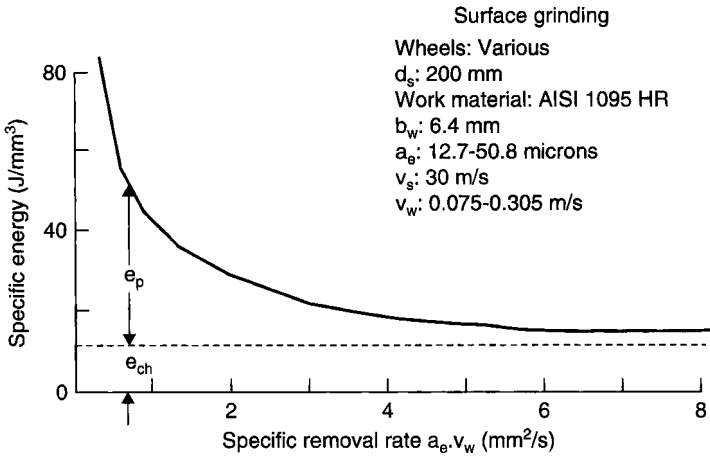


Figure 16.8 Ploughing energy is a smaller proportion at high removal rates (based on Malkin 1989).

More recent experiments have confirmed the proportion of energy which decreases asymptotically at high removal rates with very sharp wheels in the HEDG process (Rowe and Jin 2001; Comley et al. 2004). The asymptotic value of energy is typically of the order of 10 J/mm^3 or even lower.

Example 16.1 Estimate the maximum chip energy for a material with a specific heat capacity of 500 J/kg K and the density is 7850 kg/m^3 . Assume a temperature of 1700°C .

$$e_{\text{ch}} = 7850 \times 500 \times 1700 \times 10^{-9} = 6.67 \text{ J/mm}^3$$

This value is so close to the chip formation energy it suggests that chip temperature tends to increase almost adiabatically until softening occurs. This has the effect of reducing shear stresses.

Malkin (1989) gives the enthalpy increase between ambient temperature and liquid state as 10.5 J/mm^3 for iron and steels. The material does not all melt completely so this value may be a slight over-estimate. Examinations of grinding swarf often show a dendritic structure characteristic of a casting process as shown in Chapter 14, Fig. 14.1. This suggests a temperature close to melting.

Ploughing Energy

Ploughing energy per unit volume reduces with increasing removal rate as illustrated in Fig. 16.8. As depth of cut increases, ploughing energy

becomes a smaller proportion of the total energy. The reduction in ploughing energy is a significant contribution to the size effect.

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17 Mechanics of Abrasion

17.1 Introduction

This chapter introduces the mechanisms of grinding through the study of material deformation. Consideration is given to the effects of friction and grain contact geometry on the forces in grinding. Material removal in grinding has many similarities with the processes of abrasion as studied in the subject of friction and wear. Parameters are described that cause enormous differences in both forces and wear rates that occur in grinding. The importance of the environment and of effective lubrication is emphasised. Consideration is also given to the effects of wear on the grinding wheel and dressing tool. Some key points are as follows:

Rubbing—effect of flat grains

Ploughing—effect of grain angles

Cutting—requirements for chip formation

Adhesion—sticking friction

Interface friction—effect of oxides and lubrication at grain surfaces

Junction growth—effect of material deformation

Tool wear—adhesion, fatigue, crack propagation, corrosion, and loading.

17.2 Primary, Secondary, and Tertiary Shear Zones

Grinding forces depend on the balance of stresses as the abrasive grains shear the workpiece material. In metal cutting, there are three main zones of plastic shearing, all within the workpiece material. An abrasive grain producing a chip is illustrated in Fig. 17.1.

Primary Shear

Primary shear takes place ahead of the grain at the interface between the workpiece and the chip. The approximate plane of the primary shear zone is rather loosely known as the “shear plane,” and the angle of the plane is called the shear angle. Secondary shear takes place at the interface between the chip and the surface of the grain. This interface is often loosely known as the “friction face” of the cutting tool. Tertiary shear takes place at the

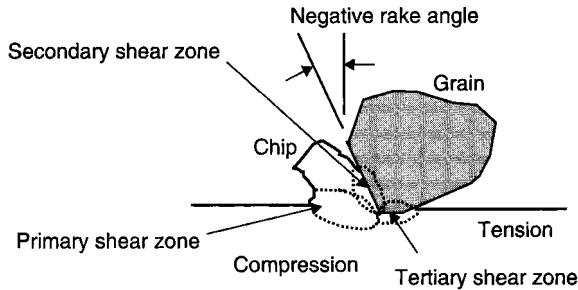


Figure 17.1 Primary, secondary, and tertiary shear zones.

interface between the workpiece and the grain, that is, under and at the sides of the grain. This interface is loosely known as the rubbing surface.

Shear Strain Rates

Shear strain rates in the secondary zone can be extremely high and even higher than in the primary shear zone. Consequences of high strain rates are high localised grain and chip temperatures with implications for reduced yield stress, increased material solution and diffusion, chemical interactions and grain wear.

Transition from Compressive to Tensile Stress

Ahead of the grain, the work material is in compression. After the grain has passed, there is a transition from compressive to tensile stresses.

Redundant Energy

In ploughing, material is pushed sideways to form ridges. Ploughing consumes a lot of energy but does not remove material. Energy that causes material deformation without removing material is termed redundant energy. In grinding at low removal rates, there is a predominance of redundant energy and this is the underlying reason for the size effect.

Blunt Cutting Action

Abrasive grains are blunt compared to conventional cutting tools. Effective rake angles are highly negative which leads to a large compressive plastic zone ahead of and under the grain followed by a shallower tensile zone behind the grain.

It was shown in Chapter 14 that the depths of grain penetration are usually very small. This has implications for the geometry of the grain contact. The grain can be considered as an extremely blunt cutting tool. Many of the grain contacts will not produce a chip but will merely rub against the workpiece. The forces and friction involved in grinding can be explained by considering the different types of contact involved in grinding. The following discussion outlines some basic models of abrasion. A useful test of a model is whether it can explain values of force ratio experienced in rubbing, ploughing, and cutting.

Minimum Energy Principle

A small shear plane angle requires more energy for the same shear stress than the optimum. The energy required in the secondary zone on the friction face increases as the shear plane angle increases. The total grinding energy is the sum of the energies in each shear zone as illustrated for orthogonal cutting in Fig. 17.2.

The principle of minimum energy states that the stress arrangement that requires minimum total energy is the most probable. A corollary is that a physical situation that increases the shear energy required in the secondary zone will increase the total shear energy. The shear plane angle then reduces to try to reduce the increased total energy.

17.3 Rubbing Contact

Basic Adhesion

Rubbing contact for a blunt grain is illustrated in Fig. 17.3. Grain penetration is assumed to be very small. Shear takes place when the shear

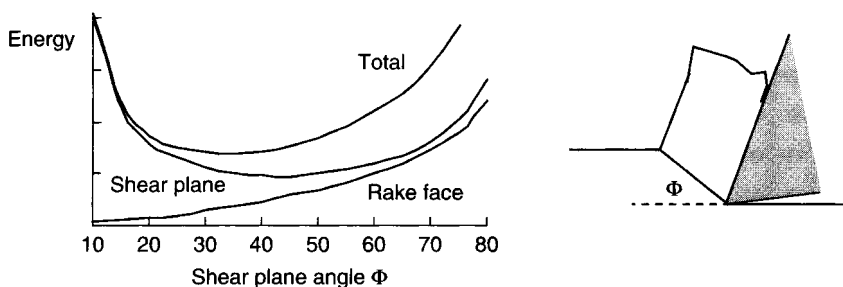


Figure 17.2 Energy required for different shear plane angles (based on Rowe 1979).

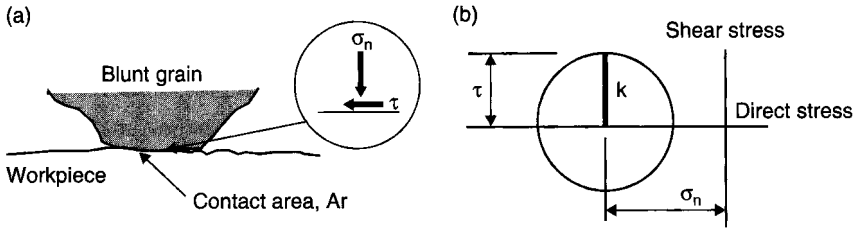


Figure 17.3 Rubbing contact at (a) a blunt asperity and (b) Mohr's circle for average interface stresses in indentation.

stress τ in the shear zone is equal to the shear flow stress k of the softer material. This type of contact is widely termed adhesive contact.

The simplest analysis of adhesive contact was due to Bowden and Tabor (1939). The area of contact at the grain tip is created by indentation of the abrasive grain into the softer surface. The nomenclature for the stresses at the junction is shown by Mohr's circle. The normal stress required for indentation with a blunt grain is highly compressive. The junction area A_r depends on the normal force f_n and the hardness of the softer material, so that $A_r = f_n/H$. The tangential force required to shear a rubbing junction is $f_t = A_r \cdot \tau = A_r \cdot k$. Based on indentation tests, it is found that $H \approx 6k$ (Suh 1986). The coefficient of friction for rubbing contact is therefore

$$\mu = \frac{f_t}{f_n} = \frac{k}{H} \approx 1/6 = 0.167 \quad \text{Bowden and Tabor (1939)} \quad (17.1)$$

Interface Friction

Friction is greatly reduced by small traces of oxides on the surface of the sheared surface. This explains why chemistry plays an important role in friction and wear. It is convenient to define an interface friction factor which is the ratio of the interface shear stress to the material shear flow stress.

$$f = \frac{\tau}{k} \quad \text{interface friction factor} \quad (17.2)$$

Introducing a friction factor $f = 0.7$ in Eqn (17.1) gives

$$\mu = \frac{f_t}{f_n} = \frac{f \cdot k}{H} \approx 0.7/6 = 0.12$$

Clearly, the basic adhesion model has rather limited validity since μ can have much higher values even exceeding 10 for chemically clean surfaces of some materials (Bowden and Tabor 1974).

Junction Growth

To explain much higher values, the concept of junction growth was introduced (Tabor 1959). It was realised that material flow in rubbing has the effect of increasing the area of the junction without needing to increase the normal force. This situation is illustrated in Fig. 17.4. The junction area can be more than doubled due to material flow. This increases the tangential force more than it increases the normal force. The normal stress on the junction is now much less compressive. In the Mohr's circle with junction growth, it is often assumed that the tangential stress is reduced to zero.

Three-Dimensional Stresses with Junction Growth

The stresses in rubbing contact can be described more generally by reference to the three-dimensional Mohr's circle for plane strain (Fig. 17.5).

Under plastic flow conditions, the stresses that act on the plane of the junction are the normal stress σ_n and the shear stress τ . The Mohr's circle for the plane of the junction is shown in full. The stresses are related to the bulk flow stress k of the softer material and the hydrostatic stress σ_{hs} . The hydrostatic stress is the mean of the three principal stresses $\sigma_{hs} = (\sigma_1 + \sigma_2 + \sigma_3)/3$. The principal stresses act normal to the principal planes where the shear stress is zero. In Mohr's circle, the principal stresses are the points of intersection of the three circles on the direct stress axis. The maximum

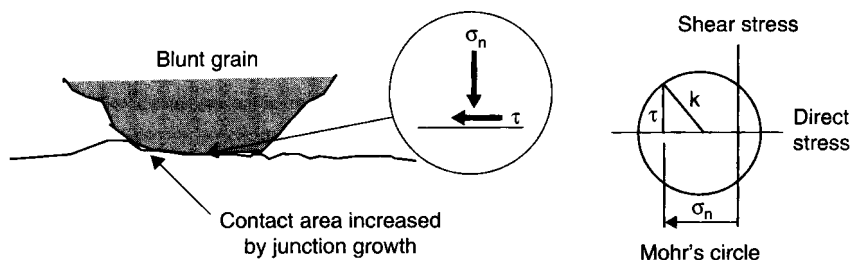


Figure 17.4 Junction growth due to material flow reduces the normal force required to indent the grain.

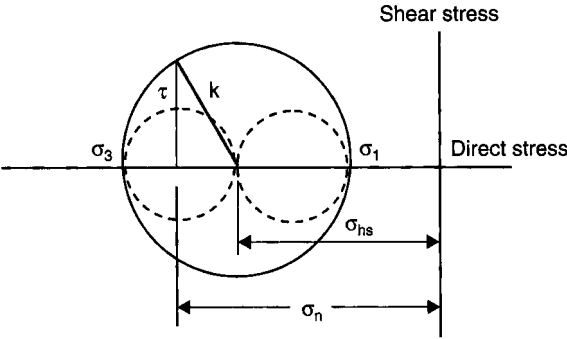


Figure 17.5 Mohr's circle representation of shear and normal stresses at a junction.

compressive direct stress σ_3 is the normal stress on the junction when there is no tangential force on the junction. From indentation tests, the principal stress has a value $\sigma_3 \approx 5.66k$. The hydrostatic stress has no effect on the onset of plastic shear. Plastic shear initiates when,

$$(\sigma_n - \sigma_{hs})^2 + \tau^2 = k^2 \quad \text{grain contact stresses} \tag{17.3}$$

It is convenient to write the hydrostatic stress as $\sigma_{hs} = n \cdot k$. It can be seen from the diagram that for the case where there is no tangential force, $n \cdot k = 5.66 \cdot k - k = 4.66 \cdot k$. That is to say, $n = 4.66$. Expressing the hydrostatic stress in this form allows the value to be adjusted for junction growth. Doubling the junction area allows n to be halved for the same normal stress. That is to say, doubling the junction area reduces n to $n = 2.33$. We can allow the value of n to range between 0 and 5.66 depending on the junction growth and the value of the friction factor.

With $\sigma_{hs} = n \cdot k$, Eqn (17.3) can be solved for normal stress. It is found that $\sigma_n = n \cdot k + \sqrt{k^2 - \tau^2}$. This can be re-arranged by writing $\mu = \tau/\sigma_n$ and $f = \tau/k$ to,

$$\mu = \frac{f/n}{1 + (1/n) \cdot \sqrt{1 - f^2}} \quad \text{junction growth friction} \tag{17.4}$$

Example 17.1 Estimate the friction coefficient for rubbing contact where the junction area is twice the “no-growth” size and the friction factor is equal to 1. Estimate for comparison the value when $f = 0.7$.

$$n = 4.66/2 = 2.33$$

$$f = 1 \text{ gives } \mu = \frac{1/2.33}{1 + (1/2.33)\sqrt{1-1 \times 1}} = 0.43$$

$$f = 0.7 \text{ gives } \mu = \frac{0.7/2.33}{1 + (1/2.33)\sqrt{1-0.7 \times 0.7}} = 0.23$$

These values of μ cover a range of force ratios commonly experienced in grinding with a significant proportion of rubbing contact. Example 17.1 demonstrates a wider range of friction coefficients obtained simply allowing for junction growth and a friction factor. However, much higher values of μ are sometimes found in dry friction (Bowden and Tabor 1974).

The junction growth model explains the much higher values. Junction growth depends on the pair of materials in contact and on the value of f . Under chemically clean conditions in a vacuum, $f = 1$, extremely high values of junction growth may result. Values plotted in Fig. 17.6 show that common friction values are covered with values of $f = 0.7$ and $f = 1$. Values of n less than 0.5 with $f = 1$ give friction coefficients similar to those achieved in chemically clean conditions. Values of n between 1 and 2 are more typical of common experience.

In summary, the junction growth model illustrates good qualitative agreement with variation of interface friction factor and junction growth. It illustrates the great importance of material hardness and low shear strength films in the determination of grinding forces.

17.4 Ploughing Contact

Basic Rabinowicz Model

Rabinowicz (1965) analysed the forces for ploughing by a cone-shaped asperity as illustrated in Fig. 17.7. In the field of friction and wear, this model is often termed abrasive contact as the surface topography is significantly changed by the ploughing action. The rate of wear is significantly greater in ploughing contact than in adhesive rubbing contact.

The cone makes an angle α with the surface as shown and the maximum width of the grain in contact is $2 \cdot r$.

From the definition of hardness, where force is proportional to area, the tangential force is $f_t = r^2 \cdot \tan \alpha \cdot H$ and the normal force is $f_n = \pi \cdot r^2 \cdot H$. The force ratio for abrasive ploughing is

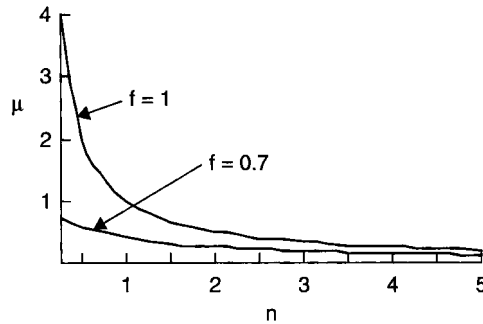


Figure 17.6 Variation of force ratio μ in rubbing contact where $f = \tau/k$ and $n = \sigma_{ns}/k$.

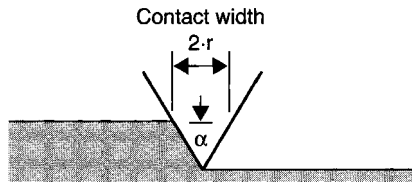


Figure 17.7 Abrasion with a cone-shaped grain.

$$\mu = \frac{f_t}{f_n} = \frac{\tan \alpha}{\pi} \quad \text{Rabinowicz 1} \quad (17.5)$$

The ploughing forces tend to zero as α tends to zero. When two nominally flat surfaces are in sliding contact, asperity contact angles approach zero. As seen in Chapter 14, grain depths of penetration are usually very small. The contact angles for grain contact are therefore expected to approach zero too. This raises serious doubts over the above analysis which produces very low values of force ratio with blunt grains. For a sharp grain, α is larger than for a blunt grain. With $\alpha = 45^\circ$ the force ratio from Eqn (17.5) has a value $\mu = 0.318$.

Modified Rabinowicz Model

A more accurate version of the Rabinowicz model takes account of the fact that contact only takes place between the leading face of the grain and the workpiece. Assuming a normal stress σ_n on the leading face of the grain and a shear stress τ yields $f_t = r_b^2 \cdot \sigma_n \cdot (\tan \alpha + \frac{\tau}{\sigma_n} \cdot \sec \alpha)$ and $f_n = \frac{\pi \cdot r_b^2}{2} \cdot \sigma_n$. The force ratio is therefore

$$\mu = \frac{2}{\pi} \cdot (\tan \alpha + \frac{\tau}{\sigma_n} \cdot \sec \alpha) \quad \text{Rabinowicz 2} \quad (17.6)$$

Example 17.2 Estimate the force ratio for grain contact with a mean contact angle where $\alpha = 45^\circ$ and $\tau/\sigma_n = 0.17$.

$$\mu \approx 2 \cdot (\tan 45^\circ + 0.17/\cos 45^\circ) = 0.64 + 0.15 = 0.79.$$

In summary, the ploughing model illustrates that blunt grains give lower μ than sharp grains. Such a value as given by this example is considered high compared to a more usual value of force ratio experienced in grinding. This suggests that rubbing contact which generally produces lower values of μ tends to predominate in grinding except at high removal rates with sharp wheels.

Cone and Sphere Model

The cone shape assumed by Rabinowicz has the merit of simplicity. In reality, grains have a variety of shapes. A first-order model of grain shape is a sphere. The sphere has the merit that the grain presents a more inclined angle as it penetrates more deeply.

Sin et al. (1979) presented results for a combined cone and sphere model that demonstrate a rather similar effect of variable sharpness with grain penetration. These results are illustrated in Fig. 17.8.

The combined cone and sphere model demonstrates features of grain shape and grain penetration depth. The width of grain contact is r_b and the radius of the grain is r . The effect of deep grain penetration is that r_b/r is large. This gives large values of μ as for a sharp grain. With a blunt grain α is small and the effect is to produce a small value of μ . This model incorporates a size effect and agrees with the well-known observation that increasing grain depth has the effect of increasing the effective grain sharpness.

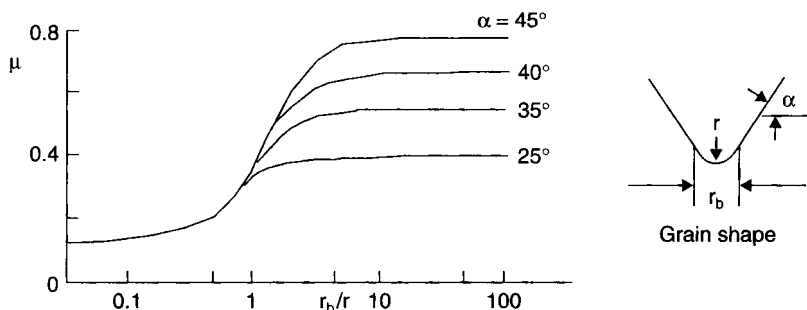


Figure 17.8 Effects of grain shape and grain depth (based on Sin et al. 1979).

17.5 Indentation Analysis

Slip-Line Field Analysis

Slip-line field solutions provide valuable insights into the nature of material flow in cutting processes and the effect of friction at the grain–workpiece interface. Slip-line fields are applied to plane strain problems and can be helpful to a limited extent where there is some variation from plane strain. The following sections will be most helpful to readers familiar with Mohr's circle analysis already introduced in Section 17.3. Readers avoiding the detailed mathematics may still find interest in the development of cutting models for grinding in the following sections.

Pure Indentation

Indentation concepts play an important part in the understanding of abrasive processes. Low-temperature grinding has a similar effect on a surface to ball-peening in that favourable residual compressive stresses are induced. An abrasive grain can be considered approximately as a spherical indenter. A first stage in the study of abrasion is to consider pure indentation in the absence of tangential motion as illustrated in Fig. 17.9. Penetration of the indenter causes shear along the slip lines causing the material to flow sideways and upwards towards the free surface.

Slip lines are lines of maximum shear stress. The slip lines under plastic flow conditions intersect with a free surface at 45° and with each other at 90° . Slip lines meet the surface of the tool at the friction angle γ .

Friction Angle

The friction angle depends on the coefficient of friction μ on the indenter surface where sliding takes place. The normal stress on the surface is σ_n . The shear stress is $\tau = \mu \cdot \sigma_n$. In the Mohr's circle, the angle between the shear stress and the shear flow stress is $2 \cdot \gamma$. In the physical plane, the angle between the shear stress on the surface of the indenter and the plane of maximum shear is given by

$$\gamma = \frac{1}{2} \cdot \cos^{-1} \cdot \frac{\mu \cdot \sigma_n}{k} = \frac{1}{2} \cdot \cos^{-1} f \quad \text{friction angle} \quad (17.7)$$

where f is the interface friction factor as previously defined. Equation (17.7) is widely used in slip-line field analysis to identify principal stresses and their directions.

One slip line meets the surface at the angle given by Eqn (17.7) and another at 90° to this direction. In a case where tangential frictional stress $\tau = \mu \cdot \sigma_n = k$, the condition is known as sticking friction. In sticking friction, one slip line meets the surface tangentially so that $\gamma = 0$ and the complementary slip line meets the surface at 90° .

The Prandtl–Tomlenov solution predicts a dead zone shown as DEF in Fig. 17.9. If friction is high and the material is strain hardening, a dead zone moves with the punch as though part of the tool (Rowe 1979). For low friction materials and constant yield stress, the dead zone disappears. With a well-lubricated low friction surface, $\gamma = 45^\circ$, and the size of the slip line field solution is reduced. Indentation force is reduced and less energy is required. With high friction, more redundant energy is required.

17.6 Indentation with Sliding

Lortz (1979) developed a plane-strain model for abrasion with a spherical grain as illustrated in Fig. 17.10. Lortz assumed that under tangential motion, material builds up ahead of the grain and the stresses are equivalent to indentation at an angle to the surface. Deformation takes place below the surface of the remaining workpiece material. The sub-surface depth w_m depends on the friction angle γ .

17.7 Basic Challen and Oxley Models

Challen and Oxley (1979) proposed three slip-line field models that are relevant for grinding as illustrated in Fig. 17.11. A wave model provides a solution for rubbing with negligible wear; a further wave model is for rubbing with wear and a chip formation model for cutting. It was claimed

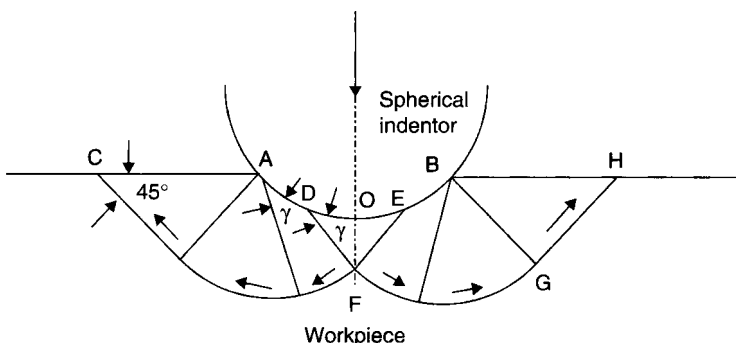


Figure 17.9 Analysis of indentation by slip line fields (Tomlenov 1960).

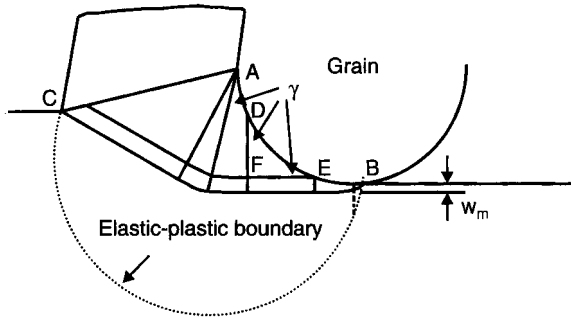


Figure 17.10 Simplified slip-line field proposed for grinding (after Lortz 1979).

that these models were consistent with measurements of friction and wear for three distinct situations, either lubricated or unlubricated.

Wave Rubbing

The wave model for rubbing without wear is designed for small asperity angles. Figure 17.11(a), shows a plastic wave moving along the surface without material removal. From Eqn (17.7), the friction angle at the asperity interface is $\gamma = \alpha + \phi = \frac{1}{2} \cdot \cos^{-1} f$. The slip lines meet a free surface at 45° so that from geometry, it can be shown that $\eta = \sin^{-1} \frac{\sin \alpha}{\sqrt{1-f}}$.

From Mohr's circle, it can be shown that the Hencky equations apply. These are $\sigma_{hs} \pm 2 \cdot k \cdot \psi = \text{a constant}$ along a slip line where the rotation of the slip line is ψ . At the free boundary, the hydrostatic stress $\sigma_{hs} = k$. Working from the value at the free boundary along the slip-line ABCD, it is found that $\sigma_{hs} = k \cdot (1 + \frac{\pi}{2} + 2 \cdot \phi - 2 \cdot \eta)$ at point D. From Mohr's circle, the shear stress $\tau = f \cdot k$ along the line ED and the normal stress $\sigma_n = \sigma_{hs} + k \cdot \sqrt{1-f^2}$. Resolving the stresses normal and parallel to the motion and multiplying by the length of contact ED gives the normal and tangential forces. The resulting force ratio is

$$\mu = \frac{f_t}{f_n} = \frac{A \sin \alpha + \cos(\cos^{-1} f - \alpha)}{A \cos \alpha + \sin(\cos^{-1} f - \alpha)} \quad \text{wave model—no wear} \quad (17.8)$$

$$\text{where } A = 1 + \frac{\pi}{2} + 2 \cdot \phi - 2 \cdot \eta.$$

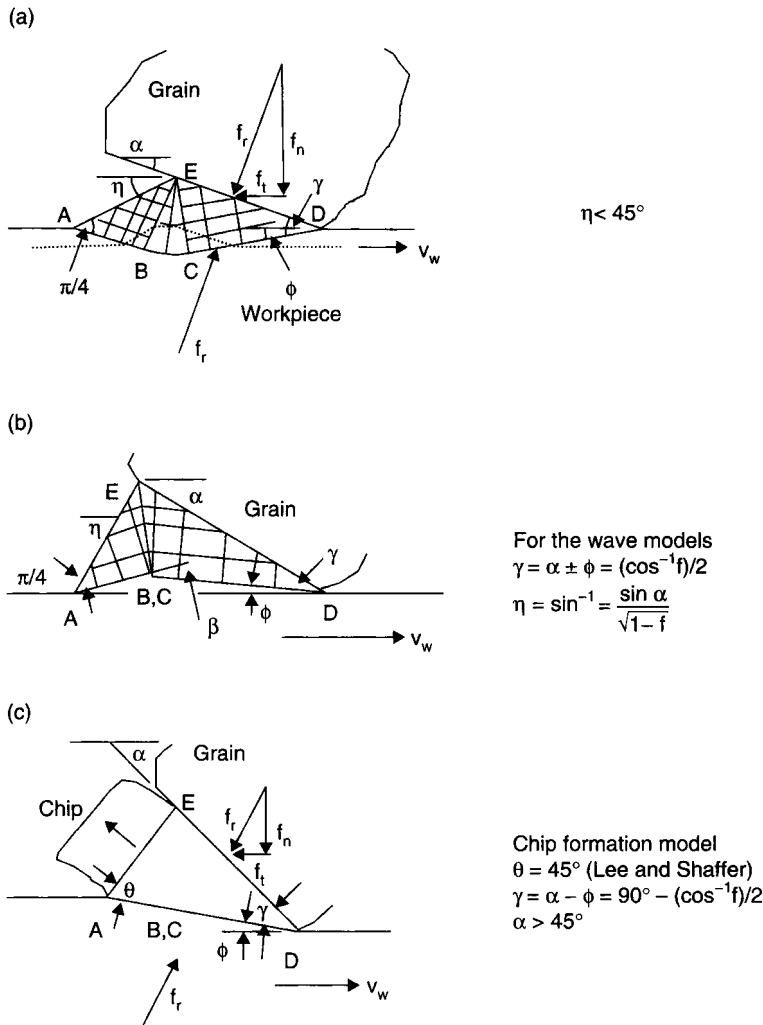


Figure 17.11 (a) Wave model for rubbing without wear, (b) wave model for rubbing with wear, and (c) chip formation model (based on Challen and Oxley 1979).

Grains usually have a strongly negative rake angle compared to conventional cutting tools that usually have a positive rake angle. This is the main reason the specific energy is so high in grinding. It is also the main reason the force ratio is so low. With conventional tools, μ may approach infinity or even be negative. The forces depend strongly on the inclination of the leading face of the grain α and the interface friction factor f . With zero friction, the wave model gives $\mu = \tan \alpha$. The force ratio increases with α . For $f = 0$ and $\alpha = 20^\circ$, $\mu = 0.36$. At the other extreme for $f = 1$ and $\alpha = 0$,

the force ratio $\mu = 1/A$. Assuming $\eta = 45^\circ$, $\mu = 1$. As in previous models, forces and force ratio are reduced with improved lubrication.

High friction corresponds to grinding a “difficult-to-grind” material with poor lubrication. This usually results in “wheel loading.” Temperatures in the contact zone are increased leading to material softening and increased adhesion to the abrasive grains. The result is usually high grinding forces. The resulting surface texture is always very poor.

Wave Wear

Figure 17.11(b) illustrates the wave model allowing for material removal. Challen and Oxley (1979) proposed that the wave builds up, until it is removed by crack formation. Material removal by rapid crack growth requires less energy than material deformation. Material removal requires much lower specific energy than in the no-wear model. This model is not altogether satisfactory because it does not satisfy the velocity continuity requirements for material flow.

Chip Formation

For chip formation, Fig. 17.11(c) shows the well-known Lee and Shaffer model. This relatively simple model is purely concerned with the minimum energy required to produce a chip. No allowance is made for sub-surface deformation in the region of D. The model requires an asperity angle greater than 45° . For smaller angles, a rubbing model should be employed.

It is assumed there is a velocity shock line along the slip-line AD. After crossing the line AD, the material becomes a chip and has an upward velocity component. The slip-line AD meets the friction face DE of the grain at an angle $\gamma = \alpha - \phi$. From Mohr’s stress circle it is found that $\gamma = \alpha - \phi = \frac{\pi}{2} - \frac{1}{2} \cdot \cos^{-1} f$. The stress-free line AE lies at 45° to the slip line and therefore $\theta = 45^\circ$.

Working from the free surface at A towards D, the hydrostatic stress is constant and equal to the shear flow stress, $\sigma_{hs} = k$. The principal stress $\sigma_1 = 2 \cdot k$ and the orthogonal principal stress is zero. The principal plane lies at 45° to the slip line and therefore parallel to AE. The resulting forces are $f'_t = 2 \cdot k \cdot t \cdot \sin(\phi + \pi/4)$ and $f'_n = 2 \cdot k \cdot t \cdot \cos(\phi + \pi/4)$, where t is the grain depth of cut. Since $\phi = \alpha + \pi/2 - (\cos^{-1} f)/2$,

$$\mu = \tan(\alpha - \frac{\pi}{4} + \frac{1}{2} \cdot \cos^{-1} f) \quad \text{chip formation} \tag{17.9}$$

Example 17.3 Estimate the force ratio for an inclination angle of 50° and an interface friction factor f = 0.7.

$$\mu = \tan(50 - 45 + \frac{1}{2} \cdot \cos^{-1} 0.7) = 0.53.$$

Shear plane angle ϕ increases with larger grain inclination, α . This increases the tangential force and reduces the normal force. Similarly, the effect of reducing f increases ϕ so that tangential force is reduced and normal force is increased.

When the friction factor $f = 0.5$, the friction angle is 60°. For an asperity angle $\alpha = 50^\circ$ and $f = 0.5$, the shear plane angle is $50 - 60 = -10^\circ$. This means the plastic zone extends below the depth of penetration of the grain. This leads to compressive residual stresses remaining in the workpiece surface after grinding. Compressive stresses are often offset if high temperatures result from grinding. High temperatures often lead to tensile residual stresses.

Asperity angle is a two-dimensional concept. In practice, material can flow sideways around an asperity. The model cannot take account of three-dimensional flow. Considering oblique flow, it can be reasoned that chip removal can take place with smaller asperity angles than 45°.

Challen and Oxley’s results are illustrated in Fig. 17.12. Improved lubrication reduces the tangential force, and hence the force ratio in rubbing friction, but increases the tangential force and increases the force ratio

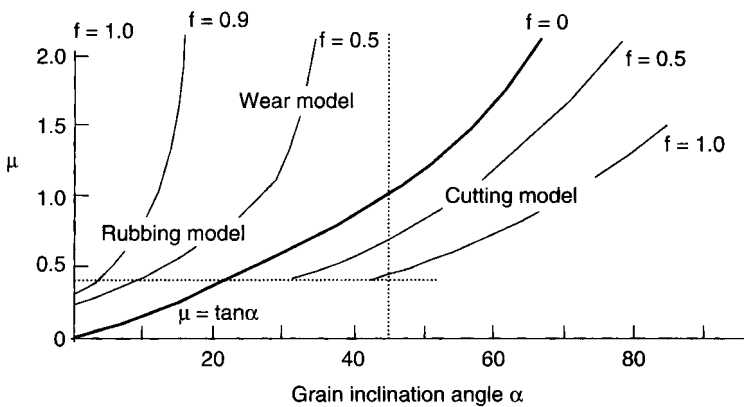


Figure 17.12 Effect of grain inclination angle and interface friction factor on force ratio.

under cutting conditions. In both cases, improved lubrication is a beneficial result leading to reduced redundant energy. The force ratio and energy required are both reduced if the process shifts from rubbing to cutting for any particular values of α and f . Cutting will therefore take priority over rubbing wherever that is physically possible according to the minimum energy principle.

17.8 Oblique Cutting

Williams and Xie (1992) analysed chip removal in oblique cutting based on a pyramid-shaped abrasive grain as illustrated in Fig. 17.13. Chips are formed with very small forward inclination angles if oblique cutting is allowed (Williams 1994). The grain has a forward inclination α , and a dihedral angle 2ϕ at the base of the pyramid. The figure illustrates a moving wave or prow which builds up ahead of the grain. A ridge is thrown up at the sides of the groove. A chip is formed which moves sideways and upwards.

A Gaussian distribution was fitted to the heights of the abrasive peaks for previously published experimental results of abrasion and equations derived for the forces and coefficient of friction.

Badger and Torrance (1998) developed a simulation of the grinding process based on the Williams and Xie model. The equations were applied to measurements of wheel topography. The grinding wheel was characterised by the number of grain asperities per unit area C and the mean inclination α . The force ratio and the specific wear were fitted by empirical expressions:

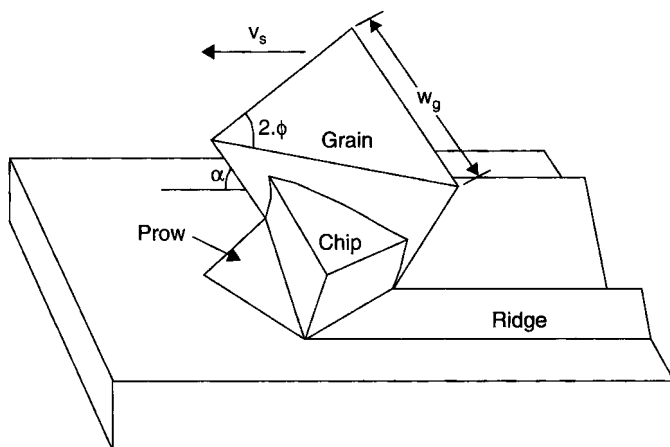


Figure 17.13 Three-dimensional chip formation (Williams and Xie 1992).

$$K \approx 0.003 \frac{\tan^3 \alpha}{f \cdot k \cdot l^{0.5}} \sqrt{\frac{H_b}{H_s}} \quad (17.10)$$

$$\mu \approx \sqrt{\frac{2}{\pi}} \cdot \frac{\tan \alpha}{l^{0.5}} \left[1 - f \cdot \left(1 + \frac{\pi}{4 \cdot \tan^2 \alpha} \right)^{0.5} \right] \quad (17.11)$$

where specific wear is $K = h_{eq}/F_n$. The interface friction factor is $f = \tau/k$. Grain spacing L is expressed as $l = L/0.5 \cdot w_g$, where w_g is the width of the pyramid base, H_b is the bulk hardness of the work material, and H_s is the surface hardness of the work material.

The model allows the surface hardness to be greater than the bulk hardness. For values of $H_s/H_b = 1$, the transition from ploughing to cutting was found to occur at an inclination $\alpha = 6^\circ$ and $\alpha = 12^\circ$ for $H_s/H_b = 1.25$. These values are much lower than indicated from two-dimensional theory. Good correlation was obtained between theory and experiment assuming $f = 0.1$ for grinding with neat oil and $f = 0.4$ using a water-based emulsion. These results indicate that the friction factor for the grain-workpiece interface can be much lower than usually assumed in studies of abrasive action.

17.9 Wear

This section introduces the wear process. It has relevance for wheel life and also for dressing tool life.

Tribo-Chemical Conditions

From practical experience, we know that wear of a grinding wheel is strongly dependent on the chemistry of the interacting materials, the grinding fluid, and the atmosphere. Most importantly, wear depends on the force and hence the temperature at the abrasive grain contact which influences almost all types of wear process. Hahn (1962) makes a distinction between solubility wear of the abrasive grains under very lightly loaded conditions where the grains develop smooth flats and wear due to thermal stress at higher loadings where small particles detach from the grains.

The interaction between a grinding wheel and a workpiece is usually between a rough grinding wheel surface consisting of widely spaced hard grain asperities and a relatively smooth soft workpiece surface. If the surface is not lubricated with an adequate supply of grinding fluid, there is a tendency for the workpiece material to clog the pores of the grinding

wheel. This has a disastrous effect on the grinding process. Forces are increased, temperatures are increased, and the process is out of control. The following discussion is about tool wear under healthy grinding conditions in the absence of wheel loading. The first stage in the study of wear is to consider the effect of adhesion.

Adhesive Wear

Archard (1953) proposed that adhesion at a junction may produce a wear particle. The size of the wear particle was calculated approximately from the area of the junction. Factors were to be found for the probability of a wear particle being produced. Usually, it was expected that the wear particle would be dislodged from the softer surface.

Archard related adhesive wear to normal load, real area of contact, and sliding distance. It was proposed that the wear volume removed is given by $V = K \cdot A_r \cdot L$. The real area of contact in plastic deformation at a junction is given by the normal force divided by the hardness so that $A_r = F_n/H$. The sliding distance is L , and K is the wear coefficient sometimes known as the Archard constant. Archard's law of adhesive wear is therefore

$$V = K \cdot \frac{F_n}{H} \cdot L \quad \text{Archard's law} \quad (17.12)$$

Arnell et al. (1991) suggest three laws of friction:

- worn volume is proportional to sliding distance
- worn volume is proportional to load
- worn volume is inversely proportional to hardness of the softer material.

A further finding is that the constant K is extremely sensitive to the chemistry as well as the mechanics of the interaction and application of lubrication. The value of K typically varies over a range from 10^{-2} to 10^{-8} according to Black et al. (1993). The value of K was originally expressed by Archard in terms of the probability of a junction producing a wear particle (Arnell et al. 1991). There are a number of factors that affect the probability of producing a wear particle. These include the adhesive forces between the two materials as well as the roughness and also the material hardness values. For information on the factors that affect the value of K , the reader should refer to publications in the specialist field.

In the form given in Eqn (17.12), K needs to be expressed per unit sliding length. Archard's law, or Preston's law as it is sometimes termed, is

widely used in the study of wear. It applies best in the steady progressive wear of a grinding wheel under steady grinding conditions.

Wear Life Cycle

The wear process for a grinding wheel is illustrated in Fig. 17.14.

When a wheel commences grinding after being dressed there is an initial period of rapid wear. In this phase, fragile grains and fragile cutting edges are quickly removed from the grinding action. A steady period of wear then ensues until a large number of the grains become severely worn. At this stage, grains pull out and the rate of wear again speeds up. The shape of this diagram is widely used to indicate the end of useful re-dress life.

The radial wheel wear w_r , during the steady wear process, is given by

$$w_r = \frac{V}{A_r} = K \cdot L = K \cdot l_c \cdot N_s \cdot t \quad (17.13)$$

where l_c is the real contact length, N_s is the rotational wheel speed, and t is the total grinding contact time.

Real Contact Length

Real contact length should be used in Eqn (17.13), not geometric contact length l_g . The importance of this is proved by increasing work speed at constant depth of cut. This increases the normal force and increases the real contact length. Such experiments strongly indicate increased radial wheel wear. However, using geometric contact length in Eqn (17.13) predicts wear is unchanged. This is wrong! Real contact length takes account of the grinding forces. This is an important conclusion. Real contact length provides the only way to explain re-dress life.

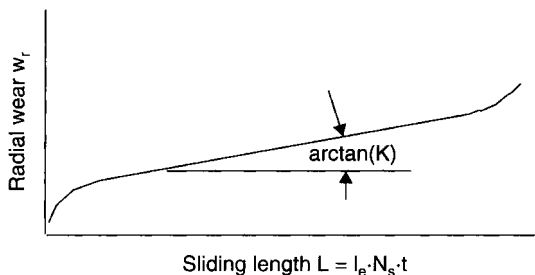


Figure 17.14 Typical progression of wear over a period of time.

Application of Archard's Law

Archard's law also applies to the dressing tool. In fact, it applies very widely in most engineering situations. It forms the basis for wear analysis of both fixed and loose abrasives.

Example 17.4 Estimate the radial wheel wear where the grinding contact length is 4 mm, the grinding wheel speed is 25 rev/s over a period of 4000 s. Assume that the value of K is 0.0000007/m.

$$w_r = 0.0000007 \times (4/1000) \times 25 \times 4000 = 0.00028 \text{ m or } 28 \text{ } \mu\text{m} \\ (\text{or } 0.0110 \text{ in.})$$

Determination of K

It may well be asked where values of K can be found for calculation of wear. The usual way is that users of equipment, where it is important to know wear rate, establish values of K for the particular situation. This may be done from measurements of the equipment under service conditions or by carrying out tests in a laboratory. For example, in the case of a grinding wheel that routinely carries out a particular grinding operation, it is a simple matter to measure wear after a period of grinding, thus allowing K to be determined. This value can then be used to make predictions for more extended periods of operation.

Yield Mode

Material deformation in ductile materials involves the movement of dislocations between atoms that assist the shear process. The variable crystalline nature of many materials means that yield at the microscopic level depends on the variable nature of the material structure. Deformation can lead to the formation of very small cracks at the microscopic level. These defects greatly reduce the stress levels required to deform a material.

In some materials, crack propagation plays a much more dominant role. Materials that are susceptible to macroscopic crack propagation are described as brittle. Crack propagation is increased by repeated stress application leading to fatigue.

Fatigue

Wear occurs in the harder material even when the level of the stresses is below the level expected to produce plastic shear (Arnell et al. 1991). The

probability of a wear particle depends on the stress levels according to the laws governing fatigue. Each time a grain passes through the grinding contact zone, it is loaded to a level that would not necessarily be expected to cause plastic flow. After repeated cycles, a particle detaches from the grain surface. As the force per contact increases, fewer cycles are required and the wear rate increases. The size of the particles produced also increases.

Abrasive Wear

By a similar process to the arguments used to establish Eqn (17.12), an expression can be found for ploughing wear using Eqn (17.5). The result is an expression of the same form as Eqn (17.12). The analysis is usually applied to a softer material, which in grinding is the workpiece. In grinding, removal rate is determined from real depth of cut. The study of wear is of more interest for determining life of a grinding wheel or of a dressing tool.

The wear rate of a grinding wheel or dressing tool increases as grain penetration is increased. For very gentle grinding conditions, wear tends to follow the laws of adhesive wear. As grain penetration is increased, the grain wear progressively changes from the removal of extremely small particles from the grains to removal of larger particles by fracture. Under conditions of fracture wear, the grinding wheel wear rate is greatly accelerated. Much of the study in this book is about the factors that balance wheel wear rates against removal rate. Factors considered include depth of cut, wheel speed, work speed, material hardness, vibration, and the like.

Oxidative Wear

The presence of oxygen in the environment produces oxides on the surface of the workpiece. Even minute quantities of oxygen reduce wear rates. In this sense, oxidative wear can only be considered beneficial in the grinding process. The process is accelerated by high interface temperatures and nascent surfaces (Hutchings 1992). The role of oxygen is usually to provide thin films of low shear stress that lubricate the interface and reduce wear on the hard surface. However, in a situation where oxygen produces hard oxides, wear rates may be increased. Hard oxide particles released into the interface will tend to cause increased wear of both surfaces.

Corrosion

After grinding, machined surfaces of ferrous workpieces need to be washed and protected from corrosion.

Thermal Wear

Wear processes are accelerated by high temperatures. One effect of high temperature is a thermal stress. This is the mechanical stress caused by rapid expansion and contractions. Another effect is that high temperatures accelerate chemical reactions according to Arrhenius' law.

Chemical Wear

Many chemical interactions are accelerated in the grinding process due to the combined effects of temperature and surface deformation (Marinescu et al. 2004). Depending on the materials being ground, the nature of the grinding fluid, the nature of the environment, and the nature of the abrasives, there are a range of chemical effects that speed up wear processes or provide protection against wear. For example, diamond abrasives should not be used to grind ferrous materials because carbon readily diffuses into iron at high temperatures.

Grinding Fluid

Application of grinding fluid almost always reduces tool wear. There are possible exceptions to this rule where the grinding fluid may accelerate wear due to chemical affinity. It is known that water interacts with CBN abrasive in the process of grinding causing increased wear of the abrasive. However, experience shows that the benefits of improved lubrication and cleaning of the wheel surface usually offsets the disadvantages of increased grain wear when using water with CBN. Application of neat oil as the grinding fluid overcomes this particular problem.

Grinding fluids usually contain additives designed to modify friction and wear rates. This is a highly specialized and complex field. The user should seek advice from grinding fluid specialists.

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18 Temperatures in Grinding

18.1 Introduction

Excessive grinding temperatures lead to structural changes in the material and surface damage as described in Chapter 7. This chapter gives methods for determining workpiece temperatures. An analysis of heat transfer in grinding is presented. The analysis points to methods that help avoid excessive temperatures. The treatment employed for prediction of temperatures has been revised and produces better results than previous methods.

18.2 Background

Development of Temperature Analysis

The following describes developments achieved over more than 20 years of research. The approach developed by the author and his colleagues has been influenced by many previous workers whose achievements are acknowledged. The most recent revised approach is the result of many refinements and experiments undertaken to achieve generality for all grinding processes.

A Moving Heat Source

An early assumption was that heat was generated at the shear plane (Outwater and Shaw 1952). However, the main source of heat in grinding was shown to be the grain–workpiece rubbing surface (Hahn 1962). In either case, temperatures must be solved using the theory of moving heat sources.

Four Heat Flows

In grinding, there are four heat flows. Heat flows to the workpiece, to the abrasive grains, to the grinding fluid, and into the chips (Werner et al. 1980). In short, the total heat flow $q = q_w + q_s + q_f + q_{ch}$.

If it assumed that all heat goes into the workpiece; temperatures predicted are much too high. In many cases the workpiece would

completely melt. However, if heat-flows to the wheel, chips, and fluid are subtracted from the total heat, the maximum workpiece temperature can be simply estimated as shown below.

There is a special case where ignoring heat to the wheel, chips, and fluid is reasonable but over-estimates workpiece temperatures. The over-estimate is typically about a third. This special case is for dry, shallow-cut grinding steels and cast irons with conventional abrasives at high values of specific energy.

However, simple techniques have been developed to take account of the four heat flows. The approach becomes very general and applies for all grinding situations including low specific energies, creep-feed grinding, and HEDG. This has been demonstrated by numerous case studies. Super-abrasives, easy-to-grind materials, and deep cuts make the general approach absolutely essential.

Workpiece Conduction

A sliding heat source solution applies for shallow-cut grinding (Carslaw and Jaeger 1946, 1959). An oblique heat source solution applies for both shallow-cut and deep grinding (Rowe 2001). Although the oblique heat source solution is a large improvement on the Jaeger sliding heat source and is accurate for shallow cuts, for deep cuts, it slightly over-estimates maximum contact surface temperatures (Anderson et al. 2008).

A circular arc heat source solution extended the oblique heat source approach (Rowe and Jin 2001). The circular arc heat source data presented in this chapter has been re-computed and provides better accuracy than the oblique heat source. Data is provided for both shallow and deep cuts as in Fig. 18.5.

Fluid Convection

Cooling of a workpiece by a grinding fluid was initially addressed by Des Ruisseaux and Zerkle (1970). For shallow grinding, convective cooling occurs mainly outside the contact region. However, it is cooling within the contact region that prevents thermal damage and therefore it is necessary to make this distinction as in the approach outlined below.

Much greater fluid cooling takes place inside the grinding contact with deep cuts (Shafto 1975). This is due to the large contact length in deep grinding. Usually in creep-feed grinding, most of the heat goes to the fluid. The energy which may be extracted is limited by fluid boiling. This was confirmed for shallow grinding (Howes et al. 1987).

Measurements show that effective cooling techniques can produce very high fluid convection factors within the grinding contact area (Rowe and Jin 2001; Jin and Stephenson 2008).

Chip Energy

The energy carried away by the chips is strictly limited but can be easily estimated. The limit is the energy that causes melting (Malkin and Cook 1971). There is also a small amount of kinetic energy that can easily be shown to be negligible. It is known that chips do not usually melt before being detached. For ferrous materials, the maximum energy carried within the chips is approximately 6 J/mm^3 of material removed.

Heat Partitioning

Heat partition is the process of sharing out the four heat flows to determine the heat into the workpiece.

Work Partition Ratio R_w

R_w defines the net heat entering the workpiece where $R_w = q_w/q$. Typically, R_w may be as low as 5% in deep grinding or as high as 75% in conventional grinding.

Work-Wheel Fraction R_{ws}

Some heat q_{ch} is carried away by the chips. The remaining heat $q - q_{ch}$ is shared between the wheel and the workpiece at the grain contacts. In short, $q - q_{ch} = q_s + q_{wg}$. Initially, heat q_{wg} goes into the workpiece but this heat is larger than the net heat into the workpiece q_w . This is because some heat immediately comes out from the workpiece again into the fluid. Therefore, the net heat flow into the workpiece q_w is less than q_{wg} . In other words, $q_{wg} = q_w + q_f$. The work-wheel fraction is $R_{ws} = q_{wg}/(q_s + q_{wg})$. The work-wheel fraction for conventional abrasives is of the order of 85% and for super-abrasives it is of the order of 50%.

Heat to the Wheel

Heat shared between the workpiece and the wheel yields the heat conducted into the wheel. Two different approaches have been employed: Wheel contact analysis and grain contact analysis.

Wheel Contact Analysis

An early technique was later abandoned for practical reasons. The early technique estimated the work-wheel fraction based on the thermal properties and speeds of the wheel and the workpiece using the expression $\sqrt{\beta_s v_s / \beta_w v_w}$ (Rowe et al. 1988). However, bulk thermal properties for the wheel are required and are not available from published data. This technique was later abandoned in favour of the grain contact analysis.

Grain Contact Analysis

A grain contact model allows the work-wheel fraction to be based on grain properties rather than bulk wheel properties. A good case can be made that grain properties are physically more relevant than bulk wheel properties since heat partition takes place at the grains. Initially, the conical grain model by Lavine (1989) was incorporated within our heat partition approach. At the same time, a plane grain solution was derived for comparison (Rowe et al. 1991).

The plane grain model was found to be more accurate than the conical model (Rowe et al. 1996a, 1996b, 1997). It was realised later that the steady-state version of our plane grain model only differed in minor detail from a very early steady-state assumption (Hahn 1962).

In most cases, a steady-state model is sufficiently accurate. However, the plane grain model can be readily extended to a more accurate transient solution when required (Rowe et al. 1996a).

Real Contact Length

It is found that real contact length is 2–3 times longer than the geometric contact length for *vitrified wheels* (Makino et al. 1966). Experiments from 1988 onwards showed that real contact length must be used rather than geometric contact length in order to match experimental temperature traces (Rowe et al. 1988, 1993, 1995).

Total Grinding Energy

Shear stresses are reduced as melting temperatures are approached. This tends to limit specific energy. Total grinding energy usually exceeds the energy required to completely melt the chips but the maximum grinding temperature cannot be greater than the melting temperature. Grain

temperatures approach but do not exceed workpiece melting temperatures (Ueda et al. 1996).

Energy Monitoring

Grinding energy cannot be accurately predicted. Large variations occur due to variations in wheel sharpness. Grinding energy or grinding forces must be measured. Although temperatures can be measured in a laboratory, the measurement is difficult. It is generally more convenient to estimate temperatures based on measured grinding energy. For this reason, substantial effort has been made to develop reliable temperature measurement methods and reliable temperature calculation methods.

Damage Temperatures

Damage temperatures are discussed in Chapter 7.

Grain Thermal Properties

CBN grains are more conductive than conventional abrasives. Accuracy of grain thermal properties is critical in CBN grinding (Morgan et al. 1998). Some published values produce unacceptable errors. Typical values are given in Table 18.1.

Workpiece Thermal Properties

Thermal properties of ferrous materials vary within a relatively small range. Typical values are listed in Table 18.2.

Table 18.1 Typical Thermal Properties of Abrasive Grains

Abrasive	Conductivity (W/mK)	Density kg/m ³	Specific Heat (J/kgK)	$\beta_s = \sqrt{k_s \cdot \rho_s \cdot c_s}$ (J/m ² s ^{0.5} K)
Diamond	2000	3520	511	60,000
CBN	240 (Pure—1300)	3480	506	20,600 (48,000)
Silicon carbide	100	3210	710	15100
Aluminium oxide	35	3980	765	10,300

Table 18.2 Typical Thermal Properties of Ferrous Materials

Material	Conductivity (W/mK)	Density (kg/m ³)	Specific Heat (J/kgK)	$\beta_w = \sqrt{k_w \cdot \rho_w \cdot c_w}$ (J/m ² s ^{0.5} K)
Cast iron (260)	53.7	7300	511	14150
AISI 1055	42.6	7840	477	12620
Steel				
M2 tool steel	23.5	7860	515	9753
AISI 52100	34.3	7815	506	11650
Bearing Steel				
AISI 1095	41	7870	560	13440

18.3 Heat Input and Heat Dissipation

Heat Input

Grinding power goes into the contact zone as heat. A negligible proportion accelerates the chips and a very small proportion is locked into the deformed material.

Power per unit area is known as heat flux q . The heat is divided by the real contact length and the width of the grinding contact

$$q = P/l_c \cdot b_w \cdot \text{Mean heat flux} \tag{18.1}$$

Example 18.1 Determine the mean heat flux where the grinding power is 2.2 kW, the real contact length is 1.7 mm, and the width of grinding contact is 15 mm.

$$\text{Mean heat flux } q = 2200/(1.7 \times 15) = 86.3 \text{ W/mm}^2.$$

Energy generated is approximately proportional to the rate of material removal. The heat flux is therefore most intense at the leading edge of the contact zone as in Fig. 18.1 (Snoeys et al. 1978).

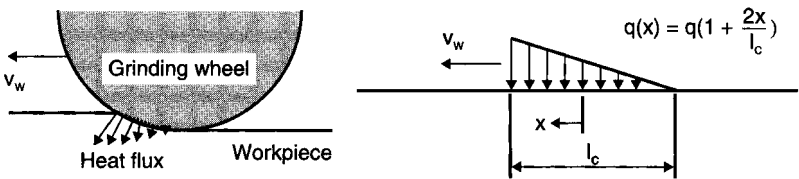


Figure 18.1 Heat input to the grinding contact zone.

Heat Dissipation

The four heat flows are illustrated in Fig. 18.2.

1. Heat is carried away by the chip.
2. Heat is generated at the grain–workpiece interface and is shared between the grinding wheel and the workpiece.
3. Some heat initially goes into the workpiece and flows out again into the grinding fluid, still within the contact length.
4. Some heat remains in the workpiece and governs the workpiece background temperatures.

The average net heat flow into the workpiece is q_w . The net heat flow is the total heat minus the heat carried away by the chips, the abrasive, and the fluid. This may be stated as $q_w = q - q_s - q_f - q_{ch}$.

Flash Heating

Heat enters the grinding contact in short bursts of intensive energy leading to flash temperatures as in Fig. 18.3(b). The flash temperatures occur in the extremely short time it takes for a grain to pass a point on the workpiece. A point on the workpiece has contact with an individual grain for approximately $1 \mu s$. The heat enters the contact in a near-adiabatic process.

Grain Heating

A grain is heated at the grain–workpiece contact for much longer than a point on the workpiece. A grain typically moves across the whole contact

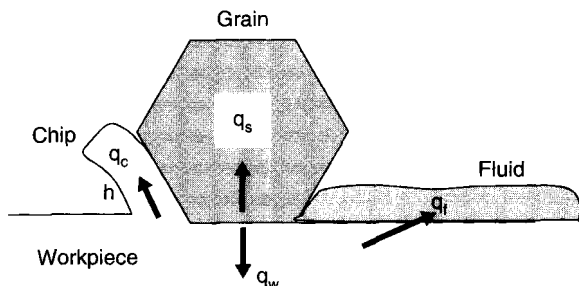


Figure 18.2 Heat flows to the workpiece, the grain, the chip, and the fluid.

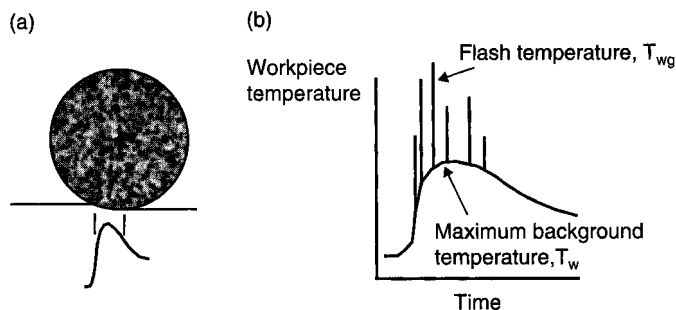


Figure 18.3 Workpiece temperatures: (a) background temperatures and (b) flash temperatures at grain contacts.

length in 100 μs . The grain therefore experiences a heat pulse for a period approximately 100 times longer than a point on the workpiece. It can be shown that this allows the surface of the grain to reach quasi steady-state temperatures. The maximum grain temperature is close to the workpiece melting temperature (Ueda et al. 1996).

Background Heating

Numerous flash contacts gradually heat up the whole workpiece contact area. It is usual therefore to make a distinction between flash temperatures at a grain contact and background temperatures over the whole contact area. Background temperatures are illustrated in Fig. 18.3(a,b). The overall duration of energy pulses in the contact area that provides the background temperatures is of the order of 10,000 μs . This is the time it takes the wheel to move through the contact length. Many energy pulses lead to background temperature rise at depths up to and often exceeding 1 mm.

18.4 Workpiece Surface Temperatures

This section concentrates on maximum temperatures in the workpiece. Further details about the analysis of the full temperature field are given in an appendix at the end of this chapter.

Workpiece Temperature Rise

The temperature rise in the workpiece depends only on net heat flow into the workpiece. The simplest expression for maximum temperature rise is

$$T = C \cdot \frac{q_w}{\beta_w} \cdot \sqrt{\frac{l_c}{v_w}} \quad \text{Max. temperature rise} \quad (18.2)$$

where $\beta_w = \sqrt{k_w \cdot \rho_w \cdot c_w}$ is a thermal property of the workpiece material for transient heating with a moving heat source. It is based on thermal conductivity, density, and specific heat capacity. It is also required to know the real contact length l_c and the work speed v_w . The heat flux q_w is the heat entering the workpiece. The C factor is approximately equal to 1 for most shallow-cut grinding. The maximum value is always less than 1.064 and approaches this value at high work speeds. Values of the C factor for other conditions are given below.

Example 18.2 Determine the maximum temperature rise when shallow grinding steel where the thermal property β_w is $12,620 \text{ J/m}^2\text{Ks}^{0.5}$, heat flux to the workpiece is 41 W/mm^2 , real contact length is 1.7 mm , and work speed is 0.25 m/s . Assume $C = 1$.

The maximum temperature rise from Eqn (18.2) is

$$T = 1 \cdot \frac{41 \times 1000 \times 1000}{12620} \cdot \sqrt{\frac{1.7/1000}{0.25}} = 268 \text{ C}$$

If the ambient temperature is 25°C , the maximum temperature is
 $T_{\max} = 268 + 25 = 293^\circ\text{C}$

Equation (18.2) is accurate but needs a value of q_w . We need an expression that relates q_w to the total heat energy. An expression is found from analysis of heat partitioning as follows.

Heat Partitioning

The heat shared between the wheel and the workpiece is $q - q_{ch}$, see Fig. 18.2. A fraction of this heat R_{ws} enters the workpiece at the grain contacts. This fraction is known as the work-wheel fraction. Some of this heat is quickly lost to the fluid within the contact zone. The heat remaining in the workpiece after allowing for fluid convection is therefore $q_w = R_{ws}(q - q_{ch}) - q_f$. Employing this expression for q_w in Eqn (18.2)

leads to $T = \frac{C}{\beta_w} \cdot \sqrt{\frac{l_c}{v_w}} \cdot \{R_{ws}(q - q_{ch}) - q_f\}$. A fluid convection factor h_f is defined below in Eqn (18.7) and yields the following expression for temperature rise:

$$T = \frac{R_{ws} \cdot (q - q_{ch})}{\frac{\beta_w}{C} \cdot \sqrt{\frac{v_w}{l_c}} + \frac{2}{3} \cdot h_f} \quad \text{Max. temperature rise} \quad (18.3)$$

Equation (18.3) differs in form from previous versions employed as a result of changing the treatment of the fluid convection. In the form presented here, the values of fluid convection factors required for agreement with experiment are lower than in previous publications. Dry grinding predictions are unaffected by the change of treatment.

Sometimes the temperature from Eqn (18.3) clearly exceeds the fluid burn-out temperature. It is then necessary to repeat the calculation setting the fluid convection factor h_f to zero.

Example 18.3 Estimate the maximum temperature when grinding M2 tool steel with an alumina grinding wheel where the total heat flux is 40 W/mm^2 . Assume the following conditions: $R_{ws} = 0.85$, $v_w = 0.15 \text{ m/s}$, $l_c = 2.2 \text{ mm}$, $q_{ch} = 10.3 \text{ W/mm}^2$. The fluid convection coefficient is $120,000 \text{ W/mK}$, $\beta_w = 9753 \text{ J/m}^2\text{K s}^{0.5}$, and $C = 1$.

$$T = \frac{0.85 \times (40 - 10.3) \times 1,000,000}{\frac{9753}{1.0} \times \sqrt{\frac{0.15}{0.0022}} + \frac{2}{3} \times 120,000} = 157^\circ\text{C in wet grinding}$$

This temperature is close to burn-out for a water-based emulsion. Re-calculating with the convection coefficient set to zero gives

$$T = \frac{0.85 \times (40 - 10.3) \times 1,000,000}{\frac{9753}{1.0} \times \sqrt{\frac{0.15}{0.0022}}} = 313^\circ\text{C in dry grinding.}$$

This is a transitional grinding condition. Measured grinding temperatures are likely to fluctuate between the wet and dry values.

Expressions for heat fluxes are given below.

Heat Flux Definition

All heat fluxes are defined in terms of power divided by contact area $l_c \cdot b_w$.

Chip Flux

Heat flux into the chips is determined from the estimated temperature T_{ch} of the chips.

$$q_{ch} = a_e \cdot v_w \cdot \rho_w \cdot c_w \cdot T_{ch} / l_c \quad \text{Heat flux to chips} \quad (18.4)$$

where T_{ch} is less than the melting temperature of the workpiece material. Even an approximate value greatly improves the accuracy of temperature calculations. The melting temperature of hypo-eutectoid steels varies in the approximate range 1470–1530°C. The melting temperature of pig iron is just over 1200°C. A value of 1400°C is often assumed for calculating the chip energy of steels and in most cases gives reasonable correlation with measurements of grinding temperatures. Sometimes values as low as 1000°C are used (Jin and Stephenson 2008).

On this basis, the maximum specific energy of heat carried away by the chips can be easily calculated from

$$e_{ch} = \rho_w \cdot c_w \cdot T_{ch} \quad \text{Chip energy} \quad (18.5)$$

Actual melting of pure iron would require a further energy of approximately 2.14 J/mm³ due to the latent heat of melting. However, it is clear that chips do not completely melt and therefore this term can be ignored. Kinetic energy of the chips for a wheel speed of 100 m/s is approximately 0.04 J/mm³. Kinetic energy is therefore negligible compared to other terms and is also ignored. The following example illustrates the typical energy carried by the chips.

Example 18.4 (i) Estimate the heat flux carried away by the chips for the following grinding conditions when grinding M2 tool steel: Depth of cut $a_e = 20 \mu\text{m}$, work speed $v_w = 0.2 \text{ m/s}$, material density $\rho_w = 7860 \text{ kg/m}^3$, specific heat $c_w = 515 \text{ J/kgK}$, chip temperature $T_{ch} = 1400^\circ\text{C}$, and contact length $l_c = 2.2 \text{ mm}$. (ii) Estimate the specific energy carried away by the chips.

The following result calculated in consistent SI units divided by 1 million converts from W/m² to W/mm².

The heat flux carried away by the chips is

$$q_{ch} = (0.000020 \times 0.2 \times 7860 \times 515 \times 1400 / 0.0022) / 1000000 \\ = 10.3 \text{ W/mm}^2$$

Specific energy in the chips scaled to convenient units is

$$e_{ch} = 7860 \times 515 \times 1400 / 10^9 = 5.67 \text{ J/mm}^3$$

Work-Wheel Fraction

The work-wheel fraction is the proportion of the heat at the work–grain interface that goes into the workpiece. Derivation of the work-wheel

fraction is given in an appendix at the end of this chapter. The grain quickly achieves a quasi-steady state and the following expression applies where k_g is the thermal conductivity of the grain. The constant r_0 is an approximate grain contact radius and $\beta_w = \sqrt{k_w \cdot \rho_w \cdot c_w}$ is the work material thermal property.

$$R_{ws} = \left(1 + \frac{k_g}{\beta_w \cdot \sqrt{r_0 \cdot v_s}} \right)^{-1} \quad \text{Work-wheel fraction} \quad (18.6)$$

Example 18.5 (i) Estimate the work-wheel fraction R_{ws} for grinding M2 tool steel at a wheel speed of 30 m/s with alumina assuming grain thermal conductivity is 35 W/mK and a reasonably sharp grain with $r_0 = 15 \mu\text{m}$. The material density $\rho_w = 7860 \text{ kg/m}^3$, specific heat $c_w = 515 \text{ J/kgK}$. (ii) Compare with CBN abrasive where $k_g = 240 \text{ W/mK}$.

$$\beta_w = \sqrt{23.5 \times 7860 \times 515} = 9753 \text{ J/m}^2\text{K s}^{0.5}$$

$$\text{Alumina abrasive: } R_{ws} = \left(1 + \frac{35}{9753 \times \sqrt{15 \times 10^{-6} \times 30}} \right)^{-1} = 0.855$$

$$\text{CBN abrasive: } R_{ws} = \left(1 + \frac{240}{9753 \times \sqrt{15 \times 10^{-6} \times 30}} \right)^{-1} = 0.463$$

This example shows that CBN grains absorb approximately four times as much heat as alumina grains.

The values in the above example are quite typical. Typically, r_0 is approximately $15 \mu\text{m}$. Sharp grains may have slightly smaller values and blunt grains may have larger values. Approximate values greatly improve accuracy compared to ignoring grain conduction. If no heat goes to the grains $R_{ws} = 1$. This can clearly give large errors if grain conduction is ignored.

Fluid Convection

The fluid convection factor h_f is defined as the average heat flux per degree temperature difference between the workpiece and the fluid in the contact zone.

$$q_f = \frac{2}{3} \cdot h_f \cdot T_{\max} \quad \text{Heat flux to fluid} \quad (18.7)$$

The heat convected to the fluid increases until the fluid completely burns out. It is found from grinding experiments that complete burn out occurs at a maximum temperature approaching 50% greater than the fluid boiling temperature. The average temperature rise over the contact area is approximately two-thirds of the maximum workpiece temperature rise and this factor is included in Eqn (18.7).

Values of fluid convection factor have been estimated from grinding experiments. Average values of $290,000 \text{ W/m}^2\text{K}$ were reported for water-based emulsions (Rowe and Jin 2001) and $23,000 \text{ W/m}^2\text{K}$ for neat oil (Stephenson et al. 2002). More recently, measurements were made for several fluids when grinding 51CrV4 steel with a B252 CBN wheel at 0.4 mm depth of cut. The following results were obtained (Jin and Stephenson 2008):

Water based (1): $283,000 \text{ W/m}^2\text{K}$ at 50 m/s wheel speed
 Water based (2): $393,000 \text{ W/m}^2\text{K}$ at 50 m/s wheel speed
 Water based (3): $229,000 \text{ W/m}^2\text{K}$ at 50 m/s wheel speed
 Mineral oil: $71,400 \text{ W/m}^2\text{K}$ at 50 m/s wheel speed
 Mineral oil: $213,000 \text{ W/m}^2\text{K}$ at 146 m/s wheel speed

Results were all obtained in grinding at temperatures well below fluid boiling. Experiments showed that the fluid convection factors depend on wheel speed as might be expected.

Example 18.6 Estimate the heat flux to the fluid for a water-based emulsion assuming the maximum temperature before complete fluid burn out is 180°C . The maximum temperature rise is 132°C and the fluid convection coefficient is $120,000 \text{ W/m}^2\text{K}$.

$$T_{\max} = 132^\circ\text{C}$$

$$q_f = \frac{2}{3} \times 120000 \times 132 / 1000000 = 10.6 \text{ W/mm}^2$$

$$\text{The average temperature is } 2/3 \times 132 + 25 = 113^\circ\text{C}$$

If the average workpiece temperature exceeds the boiling temperature of the fluid, convection to the fluid is greatly reduced and is then assumed to be negligible.

Predictions of Fluid Convection Coefficient h_f

The simplest and possibly the best model assumes that the entire contact area is covered in fluid travelling at wheel speed. The rapid stirring of the fluid makes this perhaps the best assumption (Rowe et al. 1991; Guo et al.

1999). This is sometimes known as the “fluid wheel” assumption. However, it does imply that fluid is delivered efficiently into the contact area. Sliding heat source theory is used as described in Eqn (18.2). However, in this case, the sliding speed is the wheel speed v_s and the thermal property is given for the fluid. A factor $3/2$ is introduced to relate maximum temperature to average temperature. The fluid convection coefficient h_f is defined as q_f/T_{av} and is given by

$$h_f = \frac{3 \cdot \beta_f}{2} \cdot \sqrt{\frac{v_s}{l_c}} \quad \text{Fluid convection factor} \quad (18.8)$$

Example 18.7 Estimate the fluid convection factors for water taking $k_f = 0.61 \text{ W/m}^2\text{K}$, $\rho_f = 1000 \text{ kg/m}^3$, $c_f = 4200 \text{ J/kgK}$ with $v_s = 30 \text{ m/s}$ and $l_c = 10 \text{ mm}$ and for oil taking $k_f = 0.14 \text{ W/mK}$, $\rho_f = 900 \text{ kg/m}^3$, and $c_f = 2100 \text{ J/kgK}$.

$$\text{For water: } \beta_f = \sqrt{0.61 \times 1000 \times 4200} = 1600 \text{ J/m}^2\text{Ks}^{0.5}$$

$$h_f = \frac{3 \times 1600}{2} \cdot \sqrt{\frac{30}{0.01}} = 131500 \text{ W/m}^2\text{K}$$

$$\text{For oil: } \beta_f = \sqrt{0.14 \times 900 \times 2100} = 514.4 \text{ J/m}^2\text{Ks}^{0.5}$$

$$h_f = \frac{3 \times 514.4}{2} \cdot \sqrt{\frac{30}{0.01}} = 42260 \text{ W/m}^2\text{K}$$

These values are of the right order of magnitude but rather lower than some experimental estimates based on temperature measurements. If the measured surface temperature is lower than the real value, or the measured temperature gradient is low, the convection coefficient will be over-estimated. Another source of error can arise if fluid delivery is ineffective. In this case the convection coefficient will be under-estimated. Using the thermal partition model presented in this chapter reduces the estimate of convection coefficient from measured surface temperatures and therefore gives better correlation with experiments.

Peclet Number and Diffusivity

Temperatures depend on Peclet number L . The Peclet number is a measure of the speed of a heat source. The Peclet number applied to grinding is defined as

$$L = \frac{v_w \cdot l_c}{4 \cdot \alpha_w} \quad \text{Peclet number} \quad (18.9)$$

where v_w is the work speed and l_c is the real contact length. The thermal property $\alpha_w = k_w / \rho_w c_w$ is the diffusivity of the work material. Heat flow is essentially one dimensional at high values of Peclet number. Heat flows directly down into the workpiece with very little sideways flow. Also the heated layer is very thin. Heat flow is two dimensional for low Peclet numbers. Heat diffuses out downwards and sideways into the body of the workpiece and a larger volume of material is thermally affected.

Example 18.8 Determine the Peclet number L for grinding M2 tool steel where the work speed is 0.15 m/s, the real contact length is 2.2 mm, the conductivity is 23.5 W/mK, the density is 7860 kg/m³, and the specific heat is 515 J/kgK.

$$\text{Diffusivity: } \alpha_w = \frac{k_w}{\rho_w \cdot c_w} = \frac{23.5}{7860 \times 515} = 0.0000058 \text{ m}^2/\text{s or } 5.8 \text{ mm}^2/\text{s}$$

$$\text{Peclet number: } L = \frac{0.15 \times 0.0022}{4 \times 0.0000058} = 14.22$$

Contact Angle

Temperatures in grinding depend on the contact angle ϕ as shown in Fig. 18.4. Contact angle $\phi = l_c/d_c$. It is increased both with large depth of cut and with small equivalent diameter as in external cylindrical grinding. For shallow grinding, the contact angle approaches zero: $\phi \rightarrow 0$.

Example 18.9 Calculate contact angle in degrees and in radians for a contact length of 10 mm and an equivalent diameter of 50 mm.

$$\phi = 10/50 = 0.2 \text{ rad}$$

$$\phi = 0.2 \times 180/3.142 = 11.5^\circ$$

C Factors for Maximum Temperatures

Maximum temperatures are given by dimensionless C factors as in Fig. 18.5, from which temperatures in real units are given by

$$T = C \cdot \frac{q_w}{\beta} \cdot \sqrt{\frac{l_c}{v_w}}$$

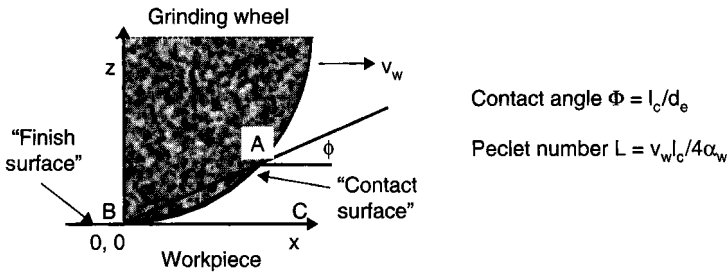


Figure 18.4 Contact angle and Peclet number.

Temperatures are reduced at large contact angles. Temperatures are also reduced at low work speeds. For shallow grinding and $L > 1$, dimensionless temperatures approach but are always less than a maximum of 1.064.

Contact Surface Temperatures and Finished Surface Temperatures

The derivation of temperatures is given in the appendix at the end of this chapter where further charts are presented. The contact surface is the arc AB. The finished surface after grinding lies along the line BC.

Example 18.10 (i) Estimate the C factor for maximum temperature on the contact surface in grinding M2 tool steel where the real contact length is 2.2 mm, the wheel diameter is 200 mm, and the Peclet number is 14.2. (ii) How much is the maximum temperature reduced at the finish surface?

$$\text{Contact angle } \phi = \frac{l_c}{d_e} = \frac{2.2}{200} = 0.011 \text{ rad or } 0.011 \times 180/\pi = 0.63^\circ$$

From Fig. 18.5, the C factor for the contact surface is 1.05. From Fig. 18.5, the C factor for the finish surface is also 1.05. Therefore, the finish surface reaches the same maximum temperature as the contact surface.

In shallow grinding, contact surface temperatures are the same as the finish surface temperatures because the contact angles are so small. Figure 18.5 allows maximum temperatures to be estimated for the complete range from creep-feed grinding and HEDG through to shallow grinding and speed-stroke grinding. This is a remarkable amount of information in one simple diagram.

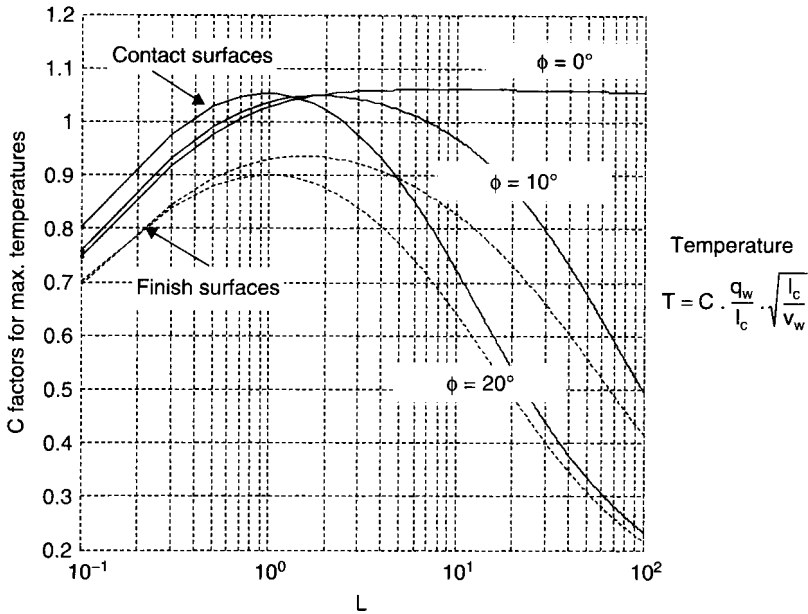


Figure 18.5 C factors for maximum temperatures on the contact surface and on the finish surface for three values of contact angle.

18.5 Workpiece Sub-Surface Temperatures

Accurate Two-Dimensional Method

Accurate sub-surface temperature rise can be obtained by the two-dimensional method using Eqn (18.13). Further details are given in Section 18.9. The same computer program works equally well for shallow-cut grinding and deep grinding. Temperatures are presented in Fig. 18.6 for an example with high work speed. Temperatures are given as the maximum rise at each level below the surface as would be measured by sub-surface sensors. Depths are presented exponentially increasing to reveal the non-linear nature of the curve across the whole range of depths.

In this example, temperatures remain constant for approximately 0.1 mm below the surface. Temperatures then fall rapidly close to zero at a depth greater than 3 mm. The maximum temperature in this example is 132.5°C.

The same trends are found at lower work speeds for the same removal rate. However, maximum temperature is substantially increased and remains constant for a much greater depth. The heat-affected depth is also much increased.

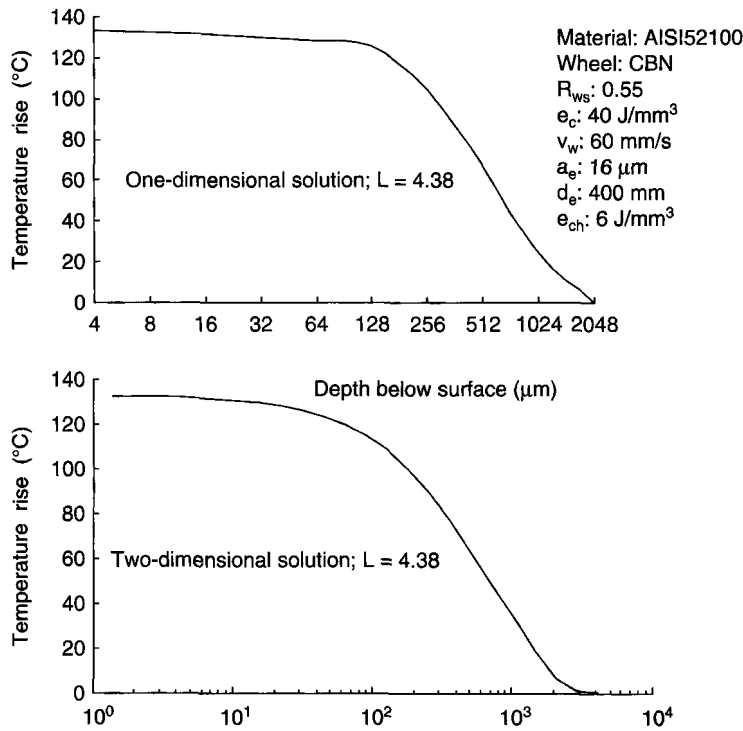


Figure 18.6 Sub-surface temperatures plotted on a non-linear depth scale.

The accurate two-dimensional method must be used at low work speeds and when taking deep cuts.

Approximate One-Dimensional Method

An approximate estimate for shallow grinding temperatures at higher speeds ($L > 1$) is obtained from a one-dimensional method described below. Temperatures from the one-dimensional solution are presented in Fig. 18.6 for the same work speed and are compared with the accurate two-dimensional results. The one-dimensional method gives a maximum surface temperature of 133.1°C, or less than 0.5% over-estimate. It can be seen that the one-dimensional method gives a very good estimate of the maximum contact temperature under limited conditions when correctly applied as described below. However, errors increase at low speeds.

Temperatures for the one-dimensional solution are given for a vertical plot through the maximum temperature at the surface. This is a slightly different condition from the two-dimensional case and explains the slightly

different shape. However, the overall shape is confirmed and also the near-constant temperature just below the surface.

One-Dimensional Solution Technique

The one-dimensional solution is found as follows. At depth z below the surface with a triangular heat flux, temperatures are given by

$$T = \int_0^t \frac{2 \cdot q_w}{\sqrt{\pi \cdot k \cdot \rho \cdot c}} \cdot t^{-1/2} \cdot \left(1 - \frac{t}{t_c}\right) \cdot e^{-z^2/4\alpha t} dt$$

Shallow-cut temperatures (18.10)

This is solved for $0 < t < t_c$, where the contact time $t_c = l_c/v_w$ leading to

$$T = \frac{4 \cdot q_w}{\sqrt{\pi \cdot k \cdot \rho \cdot c}} \cdot t^{1/2} \cdot \left(1 - \frac{2}{3} \cdot \frac{t}{t_c} - \frac{z^2}{6 \cdot \alpha \cdot t_c}\right) \cdot e^{-z^2/(4\alpha t)}$$

$$- \frac{2 \cdot q_w \cdot z}{k} \cdot \left(1 - \frac{t}{t_c} - \frac{z^2}{6 \cdot \alpha \cdot t_c}\right) \cdot \left[1 - \operatorname{erf}\left(\frac{z}{\sqrt{4 \cdot \alpha \cdot t}}\right)\right]$$

one-dimensional temperature solution (18.11)

The maximum is when $z=0$ and $t=0.5t_c$ yielding $T_{\max} = \frac{1.064 \cdot q_w}{\sqrt{k_w \cdot \rho_w \cdot c_w}} \cdot \sqrt{\frac{l_c}{v_w}}$

or $C = 1.064$ compared with $C = 1.061$ from the two-dimensional solution for $L = 4.4$. See also Fig. 18.5 for $L > 1$. The term $\operatorname{erf}()$ is evaluated using a table of error functions.

The one-dimensional solution gives the same value of $C = 1.064$ for the maximum surface temperature at all values of L . However, this gives increasing errors particularly when L is less than 1 as shown by Fig. 18.5 and can be as much as 50% at $L = 0.1$. Large contact angles further increase the errors at high speeds as can be seen from Fig. 18.5.

Linearised Curve Fits and Averaging

It is often assumed that temperature decreases exponentially with depth in order to extrapolate sub-surface temperature measurements to estimate temperature at the surface. Minor errors occur due to non-linear temperatures near the surface. Errors can also occur owing to the physical

dimensions of the temperature sensor. A large measuring volume averages temperatures over a range of depths.

18.6 Temperature Measurement

Grain Temperatures

Grain temperatures have been measured just after the grains leave contact with the workpiece (Ueda et al. 1996). Measurement was made by detecting infrared (IR) radiation using a fibre optic linked to a two-colour pyrometer. It was estimated that the maximum temperature of the grain at the exit from the grinding contact is approximately equal to the melting temperature of the workpiece.

Background Temperature Methods

Temperature measurements require careful experiments. Several techniques have been employed each having advantages and disadvantages. Some workers use a thermocouple below the ground surface. Taking successive cuts reduces the surface level until it cuts through the thermocouple junction. A plot is obtained of temperature against depth below the surface. The technique requires the temperature gradient to be estimated and temperatures extrapolated to the surface. The surface temperature cannot be measured easily if the thermocouple junction is large in comparison with the steep temperature gradient which is also non-linear near the surface. This means that the measured temperature is always averaged over a range of depths. The following is a brief review of surface measurement methods and one sub-surface temperature method.

Surface Temperature Thermocouples

Surface temperatures were measured by Nee and Tay (1981). Insulated thermocouples were housed in a split workpiece. Grinding smears workpiece material to form a thermocouple junction 0.46 mm thick. Junction size was reduced to less than 0.1 mm using thin foil thermocouples (Rowe et al. 1995). A thin thermocouple has greater discrimination of local temperature and faster response. A further reduction in size to approximately 0.05 mm was achieved using the workpiece as one of the electrodes. Early attempts of measuring wet grinding temperatures required several grinding trials for each successful measurement. Problems experienced in wet

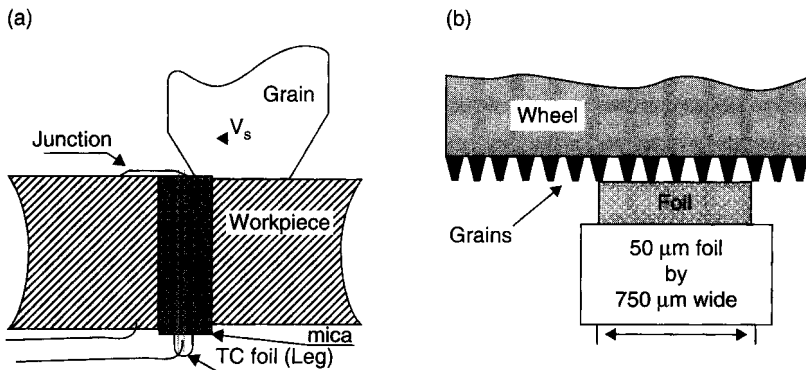


Figure 18.7 Schematic of single-pole thermocouple for measurement of background temperature: (a) formation of junction and (b) wide foil ensures that continuous junction is maintained (Batako et al. 2005).

grinding were electrical noise, failure to form a junction, and corrosion. Reliability was achieved by Batako et al. (2005) using the geometry as illustrated in Fig. 18.7.

Reliability for high-speed measurement requires attention to several aspects.

- Electromagnetic noise must be eliminated. This requires a common earth for the system to avoid earth loops. It is also advisable to shield the apparatus from stray noise. It further helps to work at a time when nearby machinery is switched off.
- High-frequency signal sampling is required to catch the maximum amplitude which is of very short duration. Large errors result from sampling frequencies that are too low.
- If filters are employed, great care must be taken not to distort the signal or introduce phase errors. A zero-phase filter should be employed.
- The foil must be wide enough to ensure that sufficient grains come into contact with the foil and a junction is maintained continuously.

Dry Grinding

With care, reliable direct readings of background temperature can be obtained every time. An example of a temperature trace obtained in dry grinding is shown in Fig. 18.8. This trace is of excellent quality and shows the ability of the measuring system to discriminate temperature variations

over a period of approximately 1 ms. This is of course too long a period to accurately measure flash temperatures. It should be remembered that the physical geometry of the thermocouple is designed to measure temperature over a greater width than one grain contact. The temperature signal represents background temperature plus temperature spikes resulting from grain contacts.

Another technique that was found to give reasonable accuracy for dry grinding was the use of an IR imaging technique (Hwang et al. 2003; Anderson et al. 2008). The IR imaging technique produces a field of measured data. The measurement IR field requires careful calibration to achieve accurate measurement of surface temperatures.

Wet Grinding

Temperature measurement in wet grinding is much more difficult than in dry grinding. Temperature traces in wet grinding tend to be of poorer quality. To obtain good quality traces, care must be taken to maintain a continuous junction. The shape of the trace obtained should conform to the ideal shape based on principles of heat conduction as in Fig. 18.8. If the temperature trace does not conform to this shape, it means that the junction was not maintained and the trial must be discarded. With careful system design and protection of the thermocouple system from corrosive fluid, reliable measurements are obtained and repeatability ensured.

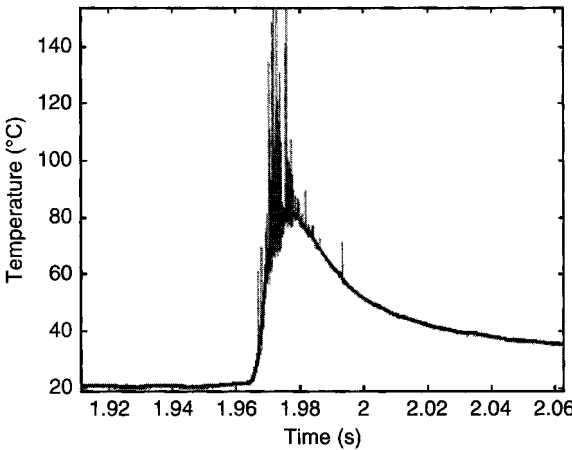


Figure 18.8 Typical temperature trace measured in dry grinding (Batako et al. 2005).

The use of low melting point PVD coatings has particular merit for wet grinding and for very high grinding temperatures (Walton et al. 2006). By using split workpieces, it is possible to coat faces perpendicular to the ground surface with different low melting point coatings. Since each coating melts accurately at a known temperature, it is possible to establish isotherms at different depths by taking the workpieces apart after grinding. A graph can be drawn of temperature against depth below the finished surface with several reasonably accurate temperatures from the isotherms. The surface temperature can be estimated by extrapolation using the Takazawa approximation (Takazawa 1966).

18.7 Measured Temperatures

Effect of Abrasives

In practice, grinding temperatures depend on the grinding forces or in other words on the specific energy. The benefits of high-conductivity abrasives can be seen from Figs. 18.9 and 18.10. In Fig. 18.9, energy using a CBN wheel is high compared with an alumina wheel. This is not usually so. Here, a fine grain 200 grit CBN wheel is compared with a coarser grain

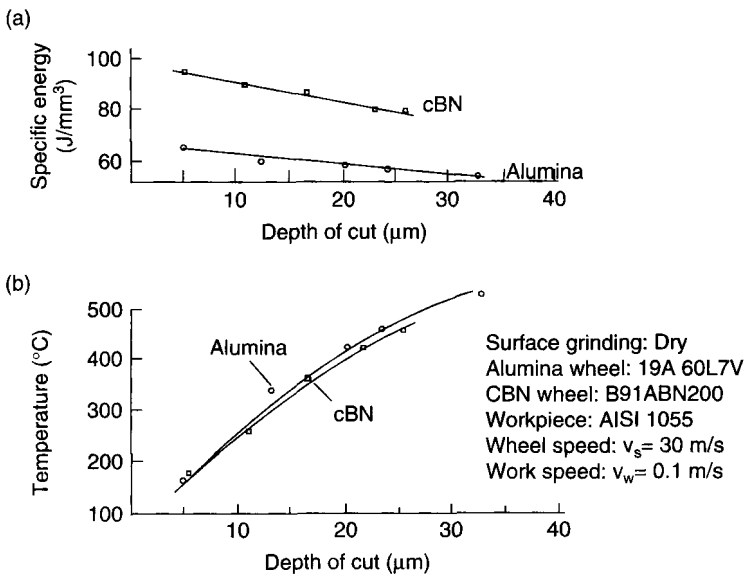


Figure 18.9 (a) Specific energies and (b) temperatures when grinding with a fine grain CBN wheel and a larger grain-size alumina wheel.

60 grit alumina wheel. Due to high thermal conductivity of CBN, measured temperatures are similar in spite of the energy difference. In Fig. 18.10, a more efficient CBN wheel is compared with alumina. In this case temperatures are substantially reduced.

Effect of Depth of Cut

The figures below show that temperatures increase with depth of cut. This is because grinding power increases with greater removal rate.

Effect of Grain Sharpness

Low specific energy and high thermal conductivity are clear advantages of CBN wheels. This is seen in Fig. 18.10, where specific energy for CBN is much lower using a 200 grit wheel than when using a 200 grit alumina wheel. Corresponding to the sharper condition of the CBN wheel, there is an impressive reduction in the workpiece temperature. Many such experiments confirm the validity of these conclusions.

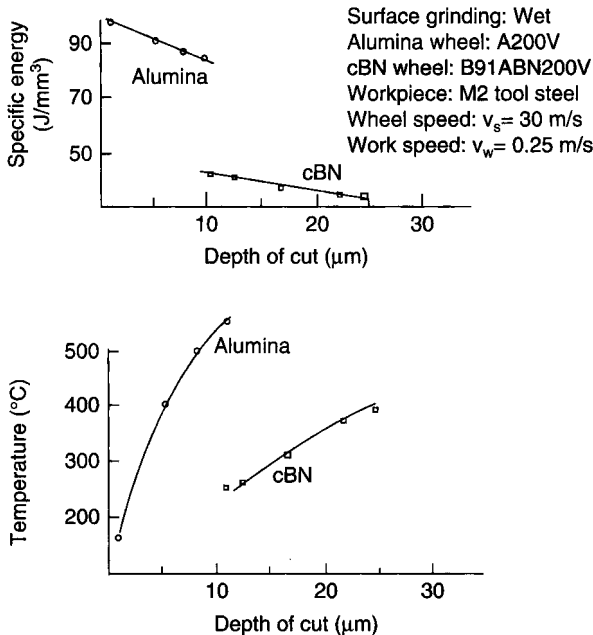


Figure 18.10 (a) Specific energies and (b) temperatures when grinding with CBN and alumina wheels having the same grain size.

Effect of Grinding Fluid

A grinding fluid is very important for the reduction of temperatures. Figure 18.11 shows results in shallow cut grinding of M2 tool steel with an alumina wheel. Temperatures are significantly reduced using 2% oil-in-water emulsion. Contact area is relatively small in shallow grinding and therefore convective cooling is often modest. Another factor is that temperatures often exceed the burn-out temperature for the grinding fluid. This is clearly the case in Fig. 18.11 where temperatures in wet grinding with a water-based fluid exceed 200°C. Burn out of the fluid greatly reduces cooling inside the contact zone.

Fluid lubricates the grinding process even when there is fluid burn out. Specific energy is reduced due to improved lubrication and is sufficient to explain lower workpiece temperatures.

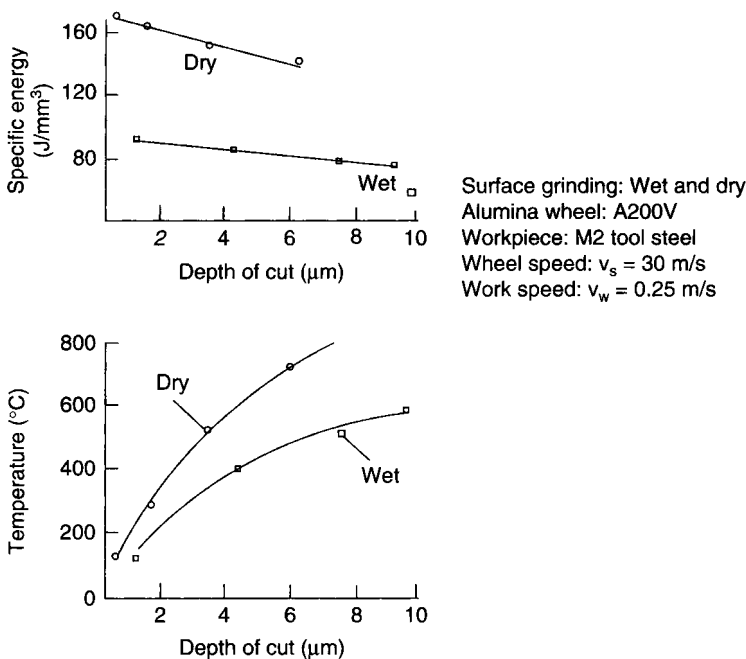


Figure 18.11 (a) Specific energies and (b) temperatures in wet grinding compared with dry grinding.

Shallow Cut or Deep Cut?

A choice can be made: taking shallow cuts at high work speed or taking deep cuts at slower speed at the same removal rate. These alternative strategies were introduced in Chapter 6. Low grinding temperatures can be achieved at either extreme as illustrated in Fig. 18.12 based on Eqn (18.2). Neat oil is assumed as the grinding fluid. Neat oil gives moderate cooling and good lubrication.

Maximum temperatures are compared for different depths of cut in Fig. 18.12. Removal rate is a moderate $1\text{ mm}^3/\text{mm s}$. Constant removal rate is achieved by reducing work speed as the depth of cut is increased. Shallow grinding temperatures are low because work speed is high. Deep grinding temperatures are low because the long arc of contact allows substantial fluid cooling within the contact length.

Wheel glazing can be a problem in creep-feed grinding at large depths of cut. Glazing increases specific energy and increases maximum temperatures if allowed. Glazing can be avoided by more frequent dressing (Andrew et al. 1985).

High Removal Rate Grinding

Maximum temperatures usually increase with removal rate as demonstrated in experimental work presented in Figs. 18.9–18.11. However under the right conditions extremely high removal rates can be achieved without causing thermal damage. This is known as HEDG as introduced in Chapter 6.

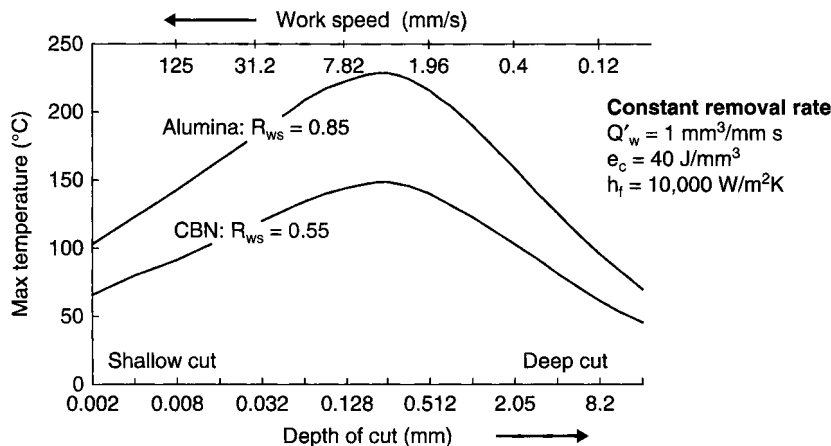


Figure 18.12 Shallow grinding compared with deep grinding temperatures.

The key requirement for HEDG is low specific energy. Low specific energies can be achieved at very high removal rates. As specific energy reduces to, or even below, 10 J/mm^3 for ferrous materials, the total grinding energy is not much greater than the chip energy of approximately 6 J/mm^3 . This means that a small proportion of the grinding energy conducts into the workpiece. An example from Rowe and Jin (2001) is shown in Fig. 18.13 using a conventional surface grinding machine at moderate wheel speed.

The corresponding maximum temperatures are shown in Fig. 18.14. The depths of cut were in the range $0.4\text{--}1.0 \text{ mm}$. These are deep cuts compared with conventional grinding and removal rates reached $Q'_w = 250 \text{ mm}^3/\text{mm s}$ before burn out of the fluid and thermal damage. This compares with removal rates usually less than $10 \text{ mm}^3/\text{mm s}$ for conventional grinding.

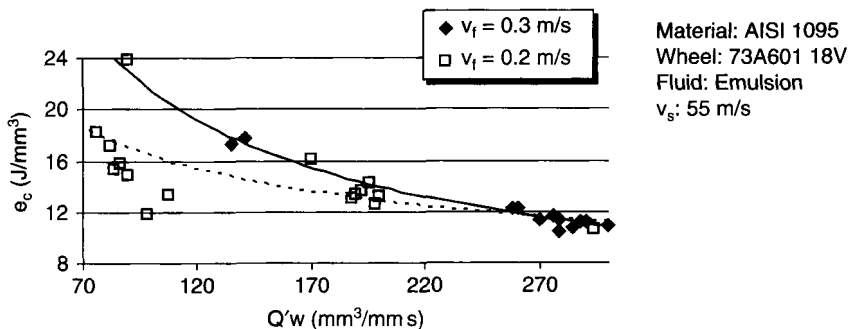


Figure 18.13 Specific energy against removal rate in HEDG.

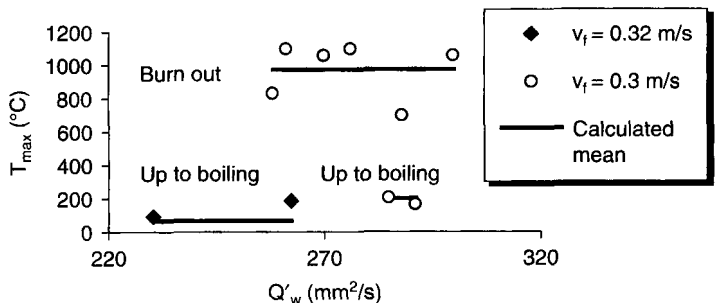


Figure 18.14 Measured and calculated temperatures at the burn transition in HEDG.

Wheel Wear

Wheel wear is high in HEDG using conventional abrasives. Much longer wheel life is achieved using electroplated CBN wheels at wheel speeds greater than 140 m/s where neat oil is used as the grinding fluid (Stephenson et al. 2002). Long wheel life and consistent results were claimed. It was also shown that measured temperatures on the finish surface were substantially lower than at the contact surface in HEDG face grinding.

Work Material

Since maximum temperatures are highly dependent on specific energy in grinding, removal rates are highly dependent on the properties of the material being ground. Some cast irons for example can be ground at extremely high removal rates.

18.8 Appendix A: General Solution for Grinding Temperatures

The following derivation is based on Rowe (2001) and Rowe and Jin (2001). The geometry of the grinding contact is represented in Fig. 18.15.

The contact surface is a circular arc. The heat source is the summation of infinite moving line sources disposed around the contact arc. The contact length l_c is the arc AFB. A line source at $F(x_i, z_i)$ moves at speed v_w parallel to the x-axis at angle ϕ_i to the contact surface. The varying angle ϕ_i is the angle FBC. The maximum value of ϕ_i along the arc AFB is the contact angle ϕ . The arc length, BF, is $l_i = d_e \phi_i$, where d_e is the effective

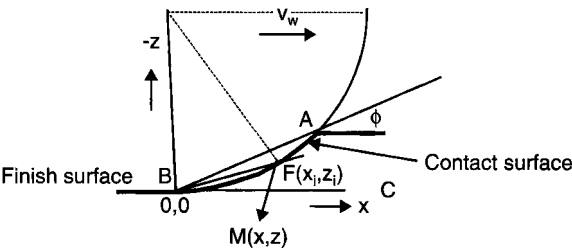


Figure 18.15 Grinding coordinates.

wheel diameter. The temperature rise at a point M (x, z) due to a moving line source dl_i is (Carslaw and Jaeger 1959)

$$dT = \frac{q \cdot dl_i}{\pi \cdot k} \cdot e^{-\frac{(x-l_i \cos \phi_i) v_w}{2\alpha}} \cdot K_0 \left[\frac{v_w \cdot r_i}{2 \cdot \alpha} \right] \quad \text{Moving line source} \quad (18.12)$$

$$\text{where } r_i = \sqrt{(x - l_i \cos \phi_i)^2 + (z - l_i \sin \phi_i)^2}$$

K_0 is the Bessel function of the second kind, order zero, α is the thermal diffusivity, and k is the thermal conductivity. The temperature rise at M(x, z) found by integrating the contributions due to all the moving line sources around the arc is

$$T = \frac{1}{\pi \cdot k} \int_0^{l_c} q \cdot e^{-\frac{v_f}{2\alpha} (x-l_i \cos \phi_i)} K_0 \left[\frac{v_f \cdot r_i}{2 \cdot \alpha} \right] dl_i$$

$$\text{Basic temperature equation} \quad (18.13)$$

The heat flux q has the form $q = \bar{q} \cdot (n+1) \cdot (l_i / l_c)^n$, where $n = 0$ for a uniform heat flux and $n = 1$ for a triangular heat flux. $\bar{q}$ is the mean heat flux on the surface AFB. For ease of computation, the temperatures are expressed in dimension-less form with $X = \frac{v_w \cdot x}{4 \cdot \alpha}$, $Z = \frac{v_w \cdot z}{4 \cdot \alpha}$, and Peclet number $L = \frac{v_w \cdot l_c}{4 \cdot \alpha}$ so that dimension-less temperature $C = \frac{\beta \cdot T}{q_w} \cdot \sqrt{\frac{v_w}{l_c}}$.

The temperatures or C factors at any position under the heat source are solved using a maths package. Examples are given in Fig. 18.16 for $L = 10$ and in Fig. 18.17 for $L = 0.1$. Temperatures are calculated along the contact surface and along what will become the finished surface. Large values of contact angle ϕ are relevant for deep grinding. Deep creep-feed grinding is associated with low values of L . HEDG values of L tend to be higher.

Temperatures on the contact surface and also on the finish surfaces are reduced with increasing contact angle for $L > 1$. Finish surface temperatures are lower than at the contact surface. Under favourable circumstances, thermal damage on the finish surface may therefore be reduced with larger contact angle.

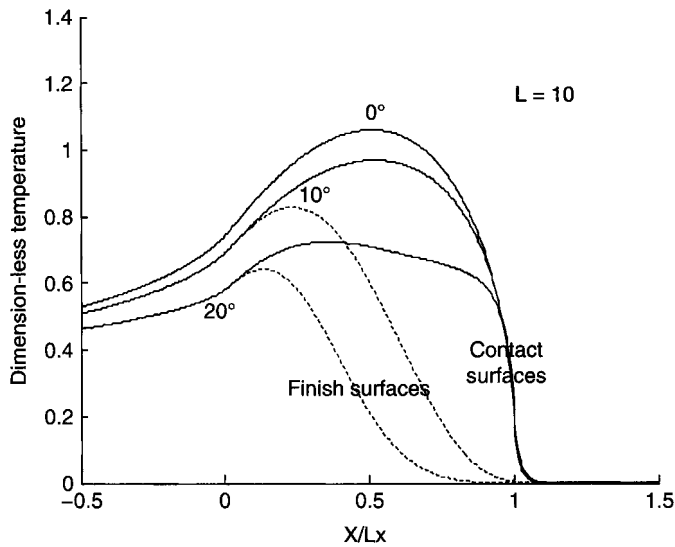


Figure 18.16 Dimension-less temperatures on the contact and finish surfaces for high work speed $L = 10$.

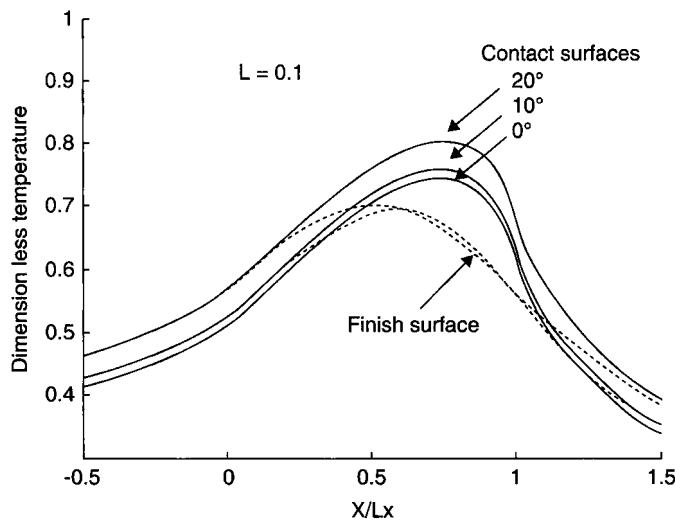


Figure 18.17 Contact and finish surface temperatures for low work speed, $L = 0.1$.

18.9 Appendix B: Derivation of Work-Wheel Fraction

The following outlines a brief derivation. Further details were given by the author previously (Rowe et al. 1995). The work-wheel fraction R_{ws} is defined as

$$R_{ws} = \frac{q_{wg}}{q_{wg} + q_s} \quad \text{Work-wheel fraction} \quad (18.14)$$

The work-wheel partition of heat takes place at the grain contacts and can be replaced by the work-grain partition as follows. The heat flows into a grain and into the workpiece are illustrated in Fig. 18.18.

The heat flux into the grain at the work-grain contact is $q_g = h_g \cdot T_g$, where T_g is the flash contact temperature. The heat flux into the workpiece is $q_{wg} = h_{wg} \cdot T_g$ so that

$$R_{ws} = \frac{q_{wg}}{q_g + q_{wg}} = \frac{h_{wg}}{h_g + h_{wg}} \quad (18.15)$$

Analysis of Conduction into the Workpiece h_{wg}

The grain is a heat source moving over the workpiece at wheel speed. The width of the heat source corresponds to the dimensions of the contact area of the grains. For sharp grains a typical range is $2.r_0 = 20\text{--}100 \mu\text{m}$. A typical value of Peclet number at a wheel speed of 30 m/s for a steel workpiece of thermal diffusivity $9 \times 10^{-6} \text{ m}^2/\text{s}$ is $L = 33$. Since this value is greater than 10, the maximum temperature is given by $T \cdot \pi \cdot k \cdot v / 2 \cdot \alpha \cdot q = 3.54 \sqrt{L}$ and it follows, since the average temperature is approximately two-thirds of the maximum, that $h_{wg} = 0.94 \beta_w \cdot \sqrt{v_s / r_0}$. For a circular contact, Archard (1958) found a factor of 1.02. Clearly, a factor of 1 is a reasonable value where the grains are irregularly shaped.

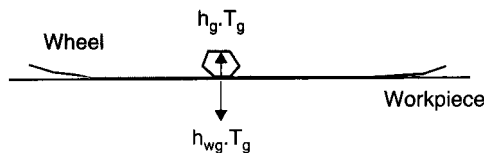


Figure 18.18 Conduction into the grain and into the workpiece at a grain contact.

$$h_{wg} = \beta_w \cdot \sqrt{v_s / r_0} \quad \text{Conduction into workpiece} \quad (18.16)$$

The solution is almost the same for an infinitely wide band or a small circle and is insensitive to shape.

Analysis of Conduction into the Grain h_g

Heat conduction into the grains is a transient two-dimensional problem. The solution for conduction into a circular contact is given by Carslaw and Jaeger (1946):

$$h_g = \frac{k_g}{2 \cdot r_0 \tau} \left[\frac{1}{\sqrt{\pi}} - \text{ierfc} \left(\frac{1}{2 \cdot \tau} \right) \right]$$

where k_g is the conductivity of the abrasive grain, r_0 is the contact half-width, $\text{ierfc}()$ is the integral complementary error function available from tables. Also $\tau = \sqrt{\frac{\alpha_g \cdot x}{r_0^2 \cdot v_s}}$, where x is the distance travelled by the grain from the commencement of contact so that $t = x/v_s$ is the time of contact. Steady state is quickly achieved,

$$h_g = \frac{k_g}{r_0} \quad \text{Steady-state grain conduction} \quad (18.17)$$

Ignoring transient effects,

$$R_{ws} = \left[1 + \frac{k_g}{\beta_w \cdot \sqrt{r_0 \cdot v_s}} \right]^{-1} \quad \text{Steady-state partition} \quad (18.18)$$

There is a small error introduced by assuming steady-state conditions that can be corrected by solving the transient case. For the transient case, it is necessary to integrate the transient solution for partition ratio across the contact length, l_c . Performing the integral, the solution is found in the form:

$$R_{ws} = \left[1 + \frac{k_g}{\beta_w \cdot \sqrt{r_0 \cdot v_s}} \cdot \frac{1}{F} \right]^{-1} \quad \text{Non-steady partition} \quad (18.19)$$

Black (1996) showed that the factor F could be expressed by $F = 1 - e^{-\tau/1.2}$, where τ is as defined above.

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