Abstract

BIFANO, THOMAS GARY. Ductile-Regime Grinding of Brittle Materials.

(Under the direction of THOMAS DOW.)

Traditionally, the grinding of hard materials has resulted in accurate contours but a poor quality, fractured surface. Polishing, on the other hand, provides a good surface finish at the expense of contour accuracy. Through recent advances in precision actuation and control, a new form of machining has emerged in which a stiff, accurate grinding machine is controlled in real-time to provide extremely small material removal rates, similar to those encountered in polishing. Under these conditions, the material removal mechanism changes from brittle fracture to plastic deformation, resulting in a much improved surface finish. Research in the Precision Engineering Center at NCSU has characterized the mechanical conditions and material properties that influence the brittle-to-ductile transition. The micro-plasticity of otherwise brittle materials has been modeled in terms of the mechanical properties of the material surface, and quantitative techniques have been developed for the measurement of surface ductility. An experimental apparatus has been designed and built to plunge grind hard materials. Using this device, transitions from brittle to ductile material removal have been demonstrated for a broad range of materials, including single crystals, amorphous glasses, and advanced ceramics. The dependence of grinding ductility on the infeed rate has been established, and

a series of exploratory experiments have further delineated the mechanical and material parameters that control the ductility of the material removal regime.

by

Thomas Gary Bifano

A dissertation submitted to the Graduate Faculty of
North Carolina State University
in partial fulfillment of the
requirements for the Degree of
Doctor of Philosophy

Department of Mechanical and Aerospace Engineering

Raleigh

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Acknowledgements

I would like to thank Professor Tom Dow for his invaluable contribution to this work. His friendship, dedication, enthusiasm, and intellectual curiosity have made a deep impression on me, both professionally and personally. As I launch into an academic teaching career, I set as my loftiest goal to be an advisor of his quality.

I am also grateful to Professor Ron Scattergood, Professor John Bailey, and Professor Dick Keltie for their guidance and advice on this work.

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For their assistance and comradeship, I would like to thank the entire Precision Engineering "team," especially Peter Blake, Peter Falter, Dan Luttrell, Mike Loewenthal, and Jennifer Rhatigan.

My parents, Leonard and Rose, and my sister, Rosanne, and her family have given a tremendous amount of support to me during my extended career as a student, for which I am forever thankful. Any success that I have is in large measure due to their influence.

Lastly, I would like to thank my partner, Mary Kate Weaver for her love and encouragement, which has made it all worthwhile.

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fixed diamond abrasive grinding for "stock removal" of material, and polishing as a "finishing" operation. These sequential processes, however, are often unacceptable for precision fabrication of ceramics, where contour tolerances of $\sim 0.1\mu\text{m}$ and surface finishes measured in angstroms are needed. Magnetic heads for computers,

1 Introduction

Progress in fabrication techniques for the production of high precision components is essential for many areas of high technology. Optical systems, bearings, computer memory components, and microelectronic devices all share a direct dependence on the field of precision fabrication for their manufacture. Advances in the contour accuracy and surface finish of these machined components translates directly into improved performance for the systems in which they are used.

For many advanced technology systems, demands for higher fabrication precision are complicated by the use of brittle materials. The past decade has seen a tremendous resurgence in the use of ceramics in structural applications. The excellent thermal, chemical, and wear resistance of these materials can finally be realized, because of recent improvements in the overall strength and uniformity of advanced ceramics.

The estimated use of ceramics in U.S. industries will constitute a \$3.5 billion industry by the end of the decade [1]. For many of these components, traditional fabrication techniques will be acceptable. These conventional techniques include fixed diamond abrasive grinding for "stock removal" of material, and polishing as a "finishing" operation. These sequential processes, however, are often unacceptable for *precision* fabrication of ceramics, where contour tolerances of $\sim 0.1\mu m$ and surface finishes measured in angstroms are needed. Magnetic heads for computers,

automobile diesel injectors, semiconductor components, and optical lens molds are typical examples of components that require precision fabrication of brittle materials. Advances in fabrication technology are essential to the economic production of these parts.

Brittle materials can be divided into three groups: amorphous glasses, hard crystals, and advanced ceramics. Advanced ceramics are a modern development. They are made from fine, pure particles that are formed, consolidated and thermally treated under precisely controlled conditions. Because of their unique properties and microstructure, these advanced ceramics have been described as an "enabling technology" [2], because the use of these materials enables development of high technology devices and systems that simply could not be otherwise produced. The same statement could be made about the use of certain crystalline materials (e.g. semiconductors) and advanced high temperature glasses.

Impeding the use of these brittle materials in precision engineering applications is the fact that they can not currently be machined in a way that ensures high precision in both form and surface finish. When these materials are ground, the material removal proceeds through a process of controlled, localized surface fracture. The rough surface left by such a grinding operation is unacceptable for many applications. Therefore, a post-grinding polishing operation is typically employed to improve the surface finish.

Unlike grinding, polishing proceeds by a predominantly ductile, or plastic mate-

rial removal process. This plasticity is responsible for the uniform, smooth surfaces generated by polishing. Unfortunately, in the case of polishing, this improvement in surface finish is accompanied by a decrease in the contour accuracy of the work-piece. Also, polishing is a more expensive process than grinding, because it demands skilled labor and is difficult to automate.

The mechanics of grinding and polishing are fundamentally similar: both rely on randomly oriented diamond particles for material removal. The basic difference between the processes is that grinding uses a fixed abrasive, while polishing uses a loose abrasive. The difference in material removal mechanisms for grinding and polishing (i.e. brittle vs. ductile) is intriguing, especially in view of the similarities between the two processes. The similarity between grinding and polishing brings up an important question: can the material removal mechanism be changed from one of fracture to one of plastic deformation in the grinding of brittle materials? This possibility of a brittle-ductile transition in material removal is the central concern of microgrinding research, and is the subject of this dissertation.

1.1 The Fundamental Hypothesis of Ductile-Regime Grinding

The expectation of a transition from brittle to ductile material removal in grinding proceeds from the following widely established premises:

- All materials will deform plastically if the scale of the deformation is small enough.

- Conventional grinding machines cannot produce plastic material removal on hard materials, primarily because the abrasive grain load is large enough to fracture the workpiece material.
- Polishing can produce ductile material removal on hard materials, because of its inherently low abrasive grain loads.

Consideration of these facts has led to the following hypothesis: by controlling the forces on individual diamond abrasive grains, and keeping these forces below a threshold value, the scale of material removal in grinding can be reduced to a level small enough to allow a plastic material removal process, thus combining the dimensional control inherent in grinding processes with the superfinished surfaces typical of polishing.

The transition from brittle to ductile material removal at smaller cutting depths can be argued purely from considerations of material removal *energy*. Specifically, for lower machining depths-of-cut, it can be shown that plasticity is a more energetically favorable material removal process than fracture.

The material property characterizing resistance to plastic flow is the yield stress, σ_y . The energy (E_p) required to plastically deform a specified volume of material (V_p) can be written as:

$$E_p = \sigma_y V_p$$

The material property characterizing resistance to fracture is the Griffith crack propagation parameter, G . The energy (E_f) required for fracture is a function of

the area (A_f) of new surface created by crack propagation. Thus:

$$E_f = GA_f$$

For a machining depth d , it is reasonable to assume that the order of magnitude of both V_p and A_f are determined by d . That is:

$$V_p \sim d^3$$

$$A_f \sim d^2$$

The ratio of material removal energies, then, is given by

$$\frac{\text{Plastic Flow Energy}}{\text{Fracture Energy}} \sim \frac{E_p}{E_f} \propto d$$

Consequently, as the scale of machining decreases, plastic flow becomes an energetically more favorable material removal mechanism. The specific depth at which a brittle-ductile transition occurs is a function of the intrinsic material properties governing plasticity and fracture.

Another way of viewing the ductile-regime grinding problem is that there is a challenge, first described by Miyashita (Figure 1.1) to fill a gap in the achievable material removal rate in abrasive machining [3]. In this figure, the material removal rates for grinding and polishing are compared, and there is a gap in which neither technique has successfully been utilized. This region of material removal has been termed the microgrinding gap. One of the primary goals of this program is to develop the technology necessary to fill the microgrinding gap. The importance of

this material removal rate gap is that it represents the threshold between ductile and brittle grinding regimes for a wide range of glasses, ceramics, and semiconductors.

The key to the ductility of the material removal process in polishing is the small forces with which the loose polishing grains abrade the material surface. It is, however, an inherently *inaccurate* process, producing fine finishes at the expense of fine tolerances. Obtaining precise dimensional control requires an exceptionally rigid machine so that infeeding of the abrasive tool translates directly into material removal from the workpiece, not flexure of the machine. Grinding is the only abrasive machining process that can provide this necessary machine rigidity.

To simultaneously machine to fine tolerances *and* obtain fine surface finishes requires a grinding machine capable of operation in the ductile material removal regime. Recalling the brittle-ductile transition hypothesis described above, it is clear that a low grain depth of cut or equivalently a low cutting force is necessary for ductile material removal. A precision ductile regime grinder, then, must provide a union of low cutting forces with high machine stiffness. To achieve both of these conditions simultaneously, a third restriction must be instituted into the machine's design; namely, precise control of the workpiece infeed with respect to the abrasive tool. This constraint is significant: in addition to the need for an actuator with submicron resolution, the combined effects of thermal machine drift, vibration, and spindle runout must be reduced to acceptable levels or compensated.

1.2 Goals of this Research

The primary objective of this research is to study the parameters controlling grinding ductility for brittle materials. This goal can be broken into five tasks:

ACHIEVABLE MATERIAL REMOVAL RATES IN ABRASIVE MACHINING

- Analysis and modelling of the material properties and machining variables that influence the ductility of the material removal process

- Quantitative, post-process evaluation of grinding ductility and residual surface stress states

MATERIAL REMOVAL RATE (MM³/MM SEC)

- Evaluation of potential techniques for in-process control of grinding ductility

Development of a grinding apparatus for ductile-regime grinding demands identification of the machining variables that are relevant to ductile regime grinding. While

Figure 1.1. Microgrinding regime gap in currently attainable material removal rates

provide a suitable foundation for establishing the design criteria. Specifically, the design of a ductile-regime grinder must include considerations of stiffness, environmental isolation, precision actuation, and real-time feedback control. For

1.2 Goals of this Research

The primary objective of this research is to study the parameters controlling grinding ductility for brittle materials. This goal can be broken into five tasks:

- Design of a grinding apparatus capable of ductile-regime grinding on brittle materials
- Analysis and modelling of the material properties and machining variables that influence the ductility of the material removal process
- Quantitative, post-process evaluation of grinding ductility and residual surface stress states
- Experimental evaluation of the influence of the material properties and machining variables that influence grinding ductility, including verification of both the ductile-regime grinding hypothesis and the proposed analytical model
- Evaluation of potential techniques for in-process control of grinding ductility

Development of a grinding apparatus for ductile-regime grinding demands identification of the machining variables that are relevant to ductile regime grinding. While such information cannot be known *a priori*, the precepts of precision engineering design provide a suitable foundation for establishing the design criteria. Specifically, the design of a ductile-regime grinder must include considerations of stiffness, environmental isolation, precision actuation, and real-time feedback control. For

use of the apparatus as a microgrinding test bed, simplicity and ease of parameter variation are also essential design requirements.

To establish relationships between material properties, machine parameters, and grinding ductility, the micromechanics of the material removal process must be modelled. In particular, the material properties that control plasticity, fracture, and elastic deformation have to be evaluated in relation to the stresses and forces that are generated by a diamond abrasive grain. Implicit in this analysis is that the properties of the material surface must be measured, and the stresses beneath the diamond grain must be estimated; relatively difficult tasks in and of themselves. Fortunately, much can be borrowed from previous analytical studies of indentation on brittle materials. Indeed, even the properties of the material surface can be estimated by indentation. The parallels between multi-point grinding and single point indentation can be exploited to develop a model for grinding ductility.

While no quantitative measure of grinding ductility currently exists, direct evaluation of the ground surface using scanning electron microscopy provides a technique for estimating the relative amounts of fracture and plastic flow that occur during machining. Raman Spectroscopy, on the other hand, permits a detailed view of the residual stresses in the near surface region of crystalline materials, and can be used to evaluate the effects of grinding ductility on the material surface stresses.

The experimental aspect of this research is focused primarily on exploring the characteristics of the brittle-ductile transition: its dependence on grinding infeed

rate, cutting speed, lubricating fluid, grinding wheel type, machine stiffness, material toughness, material hardness, and material elasticity. By controlling these parameters individually and in selected groups, the fundamental contributions of each parameter to the grinding ductility can be evaluated. In addition, these experiments represent the first extensive study of ductile-regime grinding, and will therefore provide a valuable database for future research.

The existence of a transition from brittle to ductile grinding is made more valuable if it can be measured and controlled in-process. Because of fundamental differences between plastic flow and brittle fracture, it might be expected that a real-time measurement system for monitoring grinding ductility is possible. Two quantities that are sensitive to changes in material removal mechanisms are acoustic emissions and specific grinding energy. Both of these variables can be explored for their feasibility as in-process measures of grinding ductility.

Collectively, the tasks outlined above represent significant exploration into the domain of ductile-regime grinding. The results can be used to identify important relationships between the grinding machine, the workpiece material, and grinding ductility. In addition, the promise of ductile-regime grinding for producing precision components from brittle materials can be gaged by this multi-parametric study. Finally, these results establish a foundation for further inquiry into this new material removal process.

2 The Microgrinding Process

2.1 Background

The possibility of grinding brittle materials in a ductile manner was proposed as early as 1954, when it was noted that during frictional wear of rock salts, the dominant material removal process was plastic flow and not fracture [4]. By 1975, improvements in precision diamond grinding mechanisms allowed the first reproducible evidence of grinding ductility in brittle glass workpieces [5]. Evidence of the brittle-ductile transition in the grinding of glass appeared as both improvements in surface finish and changes in the specific grinding energy [6].

The first systematic studies of grinding ductility were performed using a single grit grinding apparatus. The material removal regime in these experiments was shown to progress through three stages: plastic grooving, generation of median and lateral cracks, and finally crushing [7]. In this study, it was demonstrated that the progression of material removal mechanisms was directly related to the force on the abrasive grain, with lower forces corresponding to an increase in grinding ductility. Other experiments of single grit abrasion tests on myriad brittle materials including glasses [8] [9] [13], semiconductors [10], and advanced ceramics [11] [7] [12] demonstrated similar transitions in the material removal process with grinding force or depth of cut.

The first grinding apparatus specifically designed to take advantage of the re-

relationship between small grinding infeeds and improved surface finish was built by Yoshioka in 1982 [13]. With this apparatus, ductile-regime grinding was demonstrated for several brittle, crystalline materials. Later improvements in the precision of this grinder were shown to translate directly into improved surface finish on the brittle workpieces [14]. It was from this pioneering precision engineering effort that the concept of the microgrinding gap evolved. While the correlation between higher grinding precision and enhanced grinding ductility was qualitatively demonstrated by these Japanese efforts, quantitative relationships between machine parameters, material properties, and grinding ductility have yet to be established.

Characterization of grinding ductility requires the development of a model and experimental verification of the model. Development of the ductile-regime grinding model makes use of a micromechanical analysis of the grinding process. Experimental verification, on the other hand, necessitates an evaluation of the machine parameters important to grinding ductility. The remainder of this chapter is devoted to these two tasks: deriving a ductile-regime grinding model and delineation of the design criteria for development of a microgrinding apparatus.

2.2 Grinding Process Models

Grinding processes are influenced by two very different physical systems: the mechanics of the machine and the response of the workpiece material to abrasion. Analytical models of the grinding process must focus upon a description of one or both of these systems and their interaction. Historically, analyses of grinding

have been based on models of the machining parameters: metal removal rates, wheel/workpiece wear ratios, grinding efficiency, and specific energies of material removal. Much of this work was given an encompassing framework in a series of papers on the principles of grinding by Hahn and Lindsay [15]. This study and most of the grinding analyses that followed have provided methods for achieving greater productivity and efficiency in precision grinding applications. The approach is empirical: wheel wear, workpiece wear, normal and tangential grinding forces, grinding power, and workpiece surface finish are measured, and all follow linear algebraic relationships over much of the range of grinding conditions encountered in "conventional" precision grinding.

A convenient definition of the conventional precision grinding domain can be made using Figure 2.1, a typical graph of grinding force versus infeed rate showing three distinct regions: rubbing, plowing, and cutting [15]. The utility of the geometrical approach is in the analysis of the cutting region, where fully developed chips form in the plastically deformed zone beneath the abrasive grain, resulting in stock material removal. While the curve in this figure is for a metal, even hard ceramics exhibit these three distinct regimes of abrasion, often with the exception being that fracture becomes the dominant material removal mechanism in the cutting region. In the cutting region the following empirical laws apply:

$$Z_w = \Lambda_w(F_n - F_{no})Z_s = \Lambda_s(F_n)P = F_t V_s \quad (2.1)$$

where

Z_w = Workpiece material removal rate, m^3/sec

Z_g = Grinding wheel material removal rate, m^3/sec

A_w = Work removal parameter, m^3/Ns

F_n = Normal force, N

Figure 2.1. Grinding force versus infeed rate [15].

where:

Z_w = Workpiece material removal rate, m^3/sec

Z_w = Grinding wheel material removal rate, m^3/sec

Δ_w = Work removal parameter, m^3/Ns

F_n = Normal force, N

F_{no} = Threshold normal force, N

F_t = Tangential force, N

P = Power, Nm/s

V_s = Grinding wheel velocity, m/s

Figure 2.2 is an example of how these interrelations can be described for a given material, in this case a steel [15]. The metal removal parameter can be predicted using a series of semi-empirical power-law relations between the machining parameters and the grinding wheel, including as input: abrasive type, bond material, wheel hardness, wheel dressing conditions, wheel speed, work speed, and depth of cut. Notably absent in this approach is any measure of the influence of *material* properties. For these process models, materials are divided into two broad categories - those that are "easy-to-grind" and those that are "difficult-to-grind," the difference being that for difficult-to-grind materials the material removal rates have not yet

Figure 2.2. Multiparametric graph for a given range of grinding conditions while in the “cutting regime” of material removal [15].

been empirically correlated to the machining conditions. In microgrinding there is a considerable influence of the workpiece material properties on the grinding process, so the empirical model described above is not accurate. Also, as the tolerances and surface roughness required of precision ground components are reduced, the domain of grinding moves from the cutting regime to the plowing regime, which cannot be described with simple linear empirical models. The plowing regime, it will be shown, is the important regime for precision grinding of hard materials. For this regime, considerations of efficiency and productivity are often less important than the tolerances and finishes obtained on the workpiece.

To establish analytical models for grinding at the limits of part tolerances and surface finishes, an alternative approach is needed. Specifically, grinding parameters must be modelled with an aim of determining what is happening at the abrasive contact between the grinding wheel grains and the workpiece. This contact is characterized by three physical subsystems: the *micromechanics* of the grain/bond/workpiece interaction, the *material removal mechanism*, and the *thermal condition* of the contact region. A complete analysis of the grinding process, particularly with respect to the ductility of material removal on hard substances, will require analysis incorporating all three of these physical subsystems. Presented below is a description of the most promising models for the micromechanics of grinding and its effects on material deformation regime (ductile versus brittle).

2.3 Micromechanics of Grinding

Each protruding abrasive grain in a diamond grinding wheel will generate an intense local stress field upon contact with the workpiece material. This stress field causes irreversible material deformation, in the form of either plastic flow or crack nucleation and propagation. In addition, the plastic deformation *itself* will generate a residual stress field after the passage of the abrasive grain, which can result in further crack propagation and material removal in brittle ceramics and glasses. In fact, crack propagation from residual stress fields has been shown to be the *primary* mode of brittle material removal in fine abrasive processes [16] [11].

As a first step toward modelling the micromechanics of grinding, the stress field due to the contact between the abrasive grain and the workpiece must be analyzed. Considering first a linear elastic analysis, the problem of an abrasive grain sliding across a workpiece can be modelled as a point load on a semi-infinite half-space. Since the abrasive grain will impart both normal and tangential forces on the material surface, the direction of the point force vector will be inclined with respect to the normal of the half-space surface as shown in Figure 2.3. The elastic stress field resulting from this loading condition were determined by Mindlin [17] and used to evaluate microfracture in the vicinity of scratches in brittle solids by Swain [7]. The stress components in the plane normal to the half-space surface and containing the scratch path ($\theta = 0$) are of the form:

$$\sigma_{ij} = (P/\pi r^2) [\lambda F_{ij}(\phi)_n + \mu F_{ij}(\phi)_t] \quad (2.2)$$

$$p_0 = P / \beta \pi a^2 \quad (2.3)$$

Figure 2.3. Elastic stress field beneath a sliding abrasive point load [7].

$$\frac{\sigma_{ij}}{p_0} = \beta \left(\frac{a}{r} \right)^2 [\lambda F_{ij}(\phi) + \mu F_{ij}(\phi)] \quad (2.4)$$

where:

σ_{ij} = stress components

$F_{ij}(\phi)_n$ = Normal load stress components

$F_{ij}(\phi)_t$ = Tangential load stress components

λ = Normal force

μ = Tangential force

r = Radial distance from force application

For Equation 2.2, there is a singularity at the origin, and the point load will produce infinite stresses in the vicinity of the load. One approach to alleviating this difficulty while maintaining an elastic analysis is to spread the point load out by replacing it with a pressure p_0 acting over an area of radius a . The mean contact pressure can then be written:

$$p_0 = P/\beta\pi a^2 \quad (2.3)$$

where β is a constant of order 1. In this form, the contact pressure is equivalent to the workpiece material hardness H , as measured with an axisymmetric indenter [18]. The stress field equation can now be rewritten in a normalized form as:

$$\frac{\sigma_{ij}}{p_0} = \beta \left(\frac{a}{r}\right)^2 [\lambda F_{ij}(\phi)_n + \mu F_{ij}(\phi)_t] \quad (2.4)$$

which is valid for $a/r > 1$. The principal normal stresses for a given value of ϕ can be calculated from Equation 2.4, and the results, for a contact force inclined at 30 degrees from vertical, are shown in Figure 2.4 [18]. In this Figure, the σ_{11} stresses are tensile while the σ_{33} stresses are compressive. A similar linear elasticity solution for a sliding Hertzian contacts produces essentially the same stress field, with a circular region of compression for all three principal stresses in the vicinity of the contact [19].

Clearly, at some point the stress field beneath an abrasive grain must exceed either the plastic flow stress or the fracture stress of the workpiece if material removal is to take place. Hence, while an elasticity analysis may qualitatively lead us to the threshold of material removal, it cannot model the micromechanics of the removal process itself. Introducing plasticity into the model is an essential additional step, but one that is difficult to make. Perhaps the best starting point for this analysis is through an investigation of the micromechanics of indentation. Chiang et al [20] approached the problem of indentation plasticity by using a classical elastic-plastic solution for the deformation and stresses produced in a pressurized spherical cavity [21]. From this model, the radial and tangential stresses within and outside of the plastically deformed zone beneath the indenter could be obtained with relatively good accuracy. The stress field can be calculated for pressurization of the cavity (indentation) and after the cavity pressure was removed (indenter unloading). These stress fields correspond to the peak load stress and the residual

Figure 2.4. Principal normal stresses for the elastic solution of an inclined point load. Top: σ_{11} ; Center: σ_{22} ; Bottom: σ_{33} [7].

stress, respectively. This particular elastic-plastic analysis incorporates extensive symmetry arguments to obtain stress field solutions. To extend this exact solution for a pressurized cavity to the bilaterally symmetric case of a sliding abrasive grain would be difficult. Nevertheless, Chiang's success in determining a threshold for fracture during indentation on hard materials using this model make it worthy of further inquiry.

In fact, Evans [23] suggests that the micromechanical analysis of grinding could directly utilize the elastic-plastic solutions generated from indentation solutions. His rationale is that the addition of tangential forces (i.e the essential difference between indentation and single grit grinding) will only alter the stresses in the sliding direction, contributing a compressive stress component forward of the grain and a tensile stress trailing the grain. The stress components in the plane perpendicular to the sliding direction should be largely the same as those developed in indentation. These stress components in the plane perpendicular to the motion of the abrasive grain are responsible for the propagation of lateral cracks, and subsequent brittle material removal. Thus, the stresses responsible for the onset of brittle fracture in grinding could be evaluated by analyzing the stresses generated in indentation. These tensile lateral stresses reach a maximum just outside of the plastic zone of the indentation, after the indenter load is removed. The maximum tensile stress obtained (using the spherical cavity analogy) is:

$$\sigma_{\max} \approx H/2 \quad (2.5)$$

where H is the material hardness. This analysis was coupled to a stress-intensity factor solution for an axisymmetric crack developed by Lawn [18]. The result predicts the existence of an absolute minimum load required for crack propagation of:

$$P = C \left[\frac{K_c^4}{H^3} \right] \quad (2.6)$$

where: The amount of work done during a plastic indentation event is:

$$W_p = \int_0^{x_m} P \cdot dx \quad (2.7)$$

P = Normal load on an abrasive grain

C = Constant, depending on geometry and size of plastic zone

K_c = Mode I fracture toughness

W_p = plastic work done

H = Material hardness

P_n = normal load

x = plastic penetration depth

x_m = maximum penetration

The exact evaluation of the constant C is speculative, though the parameter K_c^4/H^3 is considered by Evans to be a reliable *relative* measure of the influence of material variable on the fracture threshold. Similar, more quantitative formulations of Empirically, it has been determined that for brittle materials undergoing diamond material fracture thresholds will be described later in this Chapter.

2.3.1 Thermal models

Thermally induced stresses in grinding might also influence the material removal process, and should be modeled to explore their significance. Two influences that

warrant consideration are: the thermal stresses due to plastic work by the abrasive grain, and the temperature rise due to frictional sliding between the grinding wheel and the workpiece.

Considering a single abrasive grain that first indents and then traverses the workpiece material, one can establish analytical estimates of thermally induced stresses. The amount of work done during a plastic indentation event is:

$$W_p = \int_0^{z_m} P_n dz \quad (2.7)$$

where:

W_p = plastic work done

P_n = normal load

z = plastic penetration depth

z_m = maximum penetration

The plastic zone beneath the grain is roughly spherical [20] and of radius $r_p \approx a$.

The plastically deformed volume, V_p , then, is given by:

Empirically, it has been determined that for brittle materials undergoing diamond indentation,

$$P_n = H\beta\pi a^2 \quad (2.8)$$

where:

$$W_p \sim Q = mc_p \Delta T \quad (2.12)$$

H = hardness

β = dimensionless indenter shape factor (of order 1)

a = characteristic dimension of the residual impression

Equation 2.8 remains valid unless extensive subsurface fracture develops, therefore it can be used for the present analysis. For simplicity, we can consider a pyramid shaped grain (Figure 2.5). The relationship between the contact diagonal (a) and the penetration depth (z) for this type of grain is simply:

$$z = a \cot \theta \quad (2.9)$$

Combining Equations 2.7, 2.8, and 2.9 yields:

$$W_p = \frac{H\beta a^3 \cot \theta}{3} \quad (2.10)$$

The plastic zone beneath the grain is roughly spherical [20] and of radius $r_p \approx a$.

The plastically deformed volume, V_p , then, is given by:

$$V_p \approx \frac{4}{3}\pi a^3 \quad (2.11)$$

To estimate the maximum possible temperature increase due to such an indentation event, it can be assumed that all of the plastic work is converted into heat.

Therefore:

$$W_p \sim Q = mc_p \Delta T \quad (2.12)$$

where:

Q = heat energy generated by material flow

m = mass of plastic zone

c_p = specific heat of workpiece material

Figure 2.5. Indenter geometry for a pyramid shaped grain showing the indentation geometry and the plastically deformed zone beneath the indentation [20].

where:

Q = heat energy generated by material flow

m = mass of plastic zone

c_p = specific heat of workpiece material

Clearly, the actual temperature rise in the plastic zone will depend to a large extent upon the rate of the plastic deformation since heat will be conducted away from the plastic zone in accordance with Fourier's law:

Substituting for W_p from Equation 2.10 gives:

$$\Delta T = \frac{H \cot \theta}{2\pi \rho c_p} \quad (2.13)$$

which is independent of the depth of penetration [16]. This independence is in general agreement with the analysis of Evans, though the form of the temperature equation is slightly different in his analysis.

Considering a silicon workpiece indented by a diamond pyramid with $\theta = 68^\circ$ and substituting the appropriate material constants:

$$H = 10.6 \text{ GPa}$$

$$\rho = 2325 \frac{\text{kg}}{\text{m}^3}$$

$$c_p = 678 \frac{\text{Nm}}{\text{kg}^\circ\text{K}}$$

melting temperature. The overall cutting zone temperature rise due to the rubbing into Equation 2.13 yields: and the workpiece can be estimated using the sliding friction models introduced by Blok, generalized by Jaeger, and proposed for use in grinding by Shaw [24, 25, 26].

$$\Delta T = 433^\circ K$$

Consider the workpiece to be a long square slider sliding along the periphery of Clearly, the actual temperature rise in the plastic zone will depend to a large extent upon the the rate of the plastic deformation since heat will be conducted away from the plastic zone in accordance with Fourier's law:

$$\frac{\partial^2 T}{\partial r^2} = \alpha \frac{\partial T}{\partial t} \quad (2.14)$$

where:

T_{max} = maximum contact temperature

r = radial distance from plastic zone (area)

α = thermal diffusivity

t = time

l = length of contact

V = grinding wheel velocity

The increase in temperature in the plastic zone will cause stresses in the workpiece material both inside and outside of the plastically deformed zone. Also, the material properties in the vicinity of this local temperature rise may affected. For most brittle materials, however, the temperature rise is significantly lower than the material

melting temperature. The overall cutting zone temperature rise due to the rubbing contact between the wheel and the workpiece can be estimated using the sliding friction models introduced by Blok, generalized by Jaeger, and proposed for use in grinding by Shaw [24][25][26].

Consider the workpiece to be a long square slider sliding along the periphery of the grinding wheel, which is assumed to be a semi-infinite heat sink, following the analysis of Blok. The maximum surface temperature will be:

$$T_{max} = \frac{2q}{k} \left(\frac{\alpha l}{V} \right)^{\frac{1}{2}} \quad (2.15)$$

where:

T_{max} = maximum contact temperature

q = thermal heat flux (energy/time/area)

k = thermal conductivity

α = thermal diffusivity

l = length of contact

V = grinding wheel velocity

This formulation assumes full areal contact between the workpiece slider and the grinding wheel. The heat input q is in units of power/unit area. This quantity can

be evaluated as:

$$q = \frac{F_t V}{l^2} \quad (2.16)$$

where F_t is the tangential grinding force.

For a typical plunge grinding operation in the ductile regime $F_t \leq 4N$, $V \leq 10m/s$, and $l \sim 0.006m$, yielding $q \leq 10^6 \frac{J}{m^2s}$.

To calculate the temperature rise, it is appropriate to use the physical constants of the grinding wheel bond, because most of the heat generated goes into the wheel at these velocities, and most of the surface area of the wheel is covered with the wheel bond.[24]. For a bronze bond:

$$T_{max} = \frac{3qFNl}{4\sqrt{2k}} \quad (2.17)$$

$$\Delta T \sim 2^\circ C$$

where:

From this analysis, the surface temperatures generated by full rubbing contact are negligible. In fact, the measured surface temperatures are higher than this predicted value. Thermocouple measurements made $\sim 1mm$ beneath the surface of a silicon specimen indicate bulk temperature increases of $\sim 100^\circ C$.

A difficulty with this analyses is that it assumes complete contact between the grinding wheel and the workpiece. This is often not true in a machining operation. An additional problem with the sliding contact model is that it assumes that each rubbed section of the grinding wheel has time to return to ambient temperature over the course of a single wheel revolution. Shaw has proposed that this is not

the case in plunge grinding, and that a cumulative effect must be modeled for an accurate representation.

An alternative formulation of the thermal contact problem could be found in an analysis similar to that of Marscher [27], who developed a thermal model for asperities on rubbing contacts. In the case of a diamond wheel that has been ideally dressed, the bond matrix would not contact the workpiece surface, and the diamond grains could be considered as asperities. Thermal transfer of heat would still be limited by the bronze bond, since the thermal conductivity of diamond is so large. This model predicts:

$$T_{maz} = \frac{3qd^2N^{\frac{1}{2}}}{4\sqrt{2}k} \quad (2.17)$$

where:

2.4 The Ductile-Regime Grinding Model

When machined, a brittle material can deform through a variety of mechanisms. If the critical resolved shear stress at any point within the material exceeds the elastic yield stress, the mechanism of deformation will change from one of reversible energy storage via elastic interatomic bond stretching to one of irreversible energy dissipation. Examples of irreversible deformation include macroscopic fracture propagation, microcrack formation, phase transformation, dislocation motion (in crystals), and intermolecular sliding (in amorphous materials). While all of these mechanisms involve material flow, the extent of the damaged zone and the

For the same value of heat flux used in the previous model, and a $6\mu m$ grain

size diamond wheel with 0.19 volume fraction of diamonds ($N = 400,000$), the estimated maximum surface temperature rise is $T_{max} = 38^{\circ}C$. As with the Jaeger model, this analysis does not account for temperature buildup on successive passes of the workpiece.

From analysis of the thermal influences of both plastic work and rubbing contact, therefore, it can be seen that the temperature rise in the cutting zone will not approach the material melting temperatures for these microgrinding conditions. Nevertheless, the influence of elevated temperatures on the material properties in the cutting zone remains an uncertainty. As an initial approximation, it will be assumed that the material properties measured at room temperature are appropriate for microgrinding.

2.4 The Ductile-Regime Grinding Model

When machined, a brittle material can deform through a variety of mechanisms. If the critical resolved shear stress at any point within the material exceeds the elastic yield stress, the mechanism of deformation will change from one of reversible energy storage via elastic interatomic bond stretching to one of irreversible energy dissipation. Examples of irreversible deformation include macroscopic fracture propagation, microcrack formation, phase transformation, dislocation motion (in crystals), and intermolecular sliding (in amorphous materials). While all of these mechanisms involve material flow, the extent of the damaged zone and the residual stress condition of the machined surface are strongly linked to the specific

deformation process. It is important to discriminate between the different modes of deformation if the machined surface features and residual stresses are of interest.

Phenomenologically, irreversible material removal mechanisms can be divided into two types: brittle and ductile. In brittle mechanisms, material removal is accomplished through the propagation and intersection of cracks, while ductile mechanisms produce plastic flow of material and generate severely deformed machining chips.

Recent improvements in machining tolerances have exposed a new possibility for material removal from brittle substances. It has been noted that ductile chips are formed in the machining of brittle substances if the scale of the machining operation is small (less than $1\mu m$ depth of cut). Similar ductile chip morphologies and structures have been observed in fine scale wear debris from a wide range of ceramics, glasses, and metals. This suggests that the process of chip formation is largely independent of nature of the material [12]. Plastic deformation in brittle materials has been documented for indentation, scratching, lapping, and even grinding processes. Experimental diamond indentation studies have concluded that elastic/plastic deformation without fracture is possible in soda-lime glass if the penetration depths are kept below $1\mu m$ [12]. Equivalently, the prevention of subsurface cracking in glass has been empirically related to the load on the diamond indenter, with a predicted fracture threshold of $0.01N$ [7]. Single grain scratch tests, which more closely approximate the conditions of grinding than simple indentation, have

been used to correlate the onset of fracture damage to the width of the machining groove made by a diamond pyramid indenter [12]. These tests were given a theoretical framework as early as 1978 by Kendall, who showed from energy considerations that there is a lower limit to the size of a particle that will fracture [28]. Evans [23], in an analysis of the fracture mechanics of brittle materials subjected to scratch tests, noted that the tensile stresses responsible for lateral crack formation arise primarily during load *removal*. The formation and propagation of these lateral cracks is the principal method of brittle material removal in hard substances. A schematic diagram of the lateral cracking mechanism is illustrated in Figure 2.6. The model proposed by Kendall predicts that crack propagation cannot occur in particles of dimension smaller than:

$$d_c = \frac{10ER}{Y^2} \quad (2.18)$$

where:

d_c = Critical particle dimension

E = Modulus of elasticity

R = Fracture energy

Y = Yield Stress

While this model was established for compression testing specimens and not for

Figure 2.6. Fracture beneath a sliding indenter. The median cracks are formed beneath the indenter, while the lateral cracks form after the indenter passes. The intersection of lateral cracks with each other or the material surface causes brittle material removal in excess of the smaller scale plastic material removal zone.

grinding processes, it describes the key elements for determining a transition from ductile to brittle behavior, which are fracture energy (or fracture toughness) and yield strength. Lawn, Jensen and Aurora [29] proposed a similar formulation for a critical depth for fracture during indentation of hard materials, based on a Griffith fracture propagation criterion. Their critical-depth-of-indentation was:

$$d_c = \frac{ER}{H^2} \quad (2.19)$$

where H is the material hardness. Macroscopically, ($H \sim 3\sigma_y$) so this formulation is roughly equivalent to that independently obtained by Kendall in Equation 2.18. To apply these material property formulations to the grinding process first requires an evaluation of the material constants R , and H (or σ_y).

At the small scale of grinding required to achieve the ductile regime on a relatively hard material, there is a definite size effect: hardness is not an invariant material property but rather it varies with the depth of indentation. Typically, hardness increases with a decreasing indentation depth. Since microgrinding occurs very near the material surface, the bulk material hardness may not valid for use in the preceeding equations. This indentation size effect (ISE) in hardness can be characterized by a numerical quantity [30]

$$n = \frac{\frac{\delta F}{F}}{\frac{\delta x}{x}} \quad (2.20)$$

where: For materials that exhibit a plastic zone near the crack tip, the value of n can be evaluated using Griffith's classical crack propagation analysis. One

n = Material hardness size effect

F = Indentation force

z = Indentation depth

$$R \sim \frac{K_I^2}{H} \quad (2.22)$$

For microgrinding of hard materials, the machining depth will range from 50nm to 1 μ m. For indentation in this range of depths, most brittle materials will follow a so called Meyer's hardness relation [31]:

$$H \sim 1.85az^{n-2} \quad (2.21)$$

where:

H = Material hardness

a = Material constant

n = Constant depending on chemical structure

z = Indentation depth

Measuring the fracture energy in Equation 2.19 is even more difficult than measuring hardness. For ideally brittle materials it approaches the energy for surface generation γ . For materials that exhibit a plastic zone near the crack tip, the value of R can be evaluated using Griffith's classical crack propagation analysis. One

approach to defining fracture energy at small scales is to replace it with a dimensionally analogous measure of the energy needed to propagate cracks, namely:

$$R \sim \frac{K_c^2}{H} \quad (2.22)$$

Broadly defined, the role of the machine structure for any machine tool is to allow accurate and repeatable control of the motion of rapidly moving components that are often subjected to time varying forces. The machine structure design process Marshall and Lawn [32] have called K_c^2/H an effective quantification of brittleness, and this representation can be combined with the critical depth model of Equation 2.19 to yield:

$$d_c \propto \left(\frac{E}{H}\right) \left(\frac{K_c}{H}\right)^2 \quad (2.23)$$

To generalize the role of machine structure for precision grinding, one can the machine's structural characteristics into four broad categories: stiffness, mass, as a measure of brittle transition depth-of-cut. Experimental results along these lines for indentation testing have shown a remarkable degree of consistency, even using bulk properties to evaluate H . If this type of analysis were adopted for grinding, d_c would represent a measure of the critical grinding infeed rate expected to change the material removal mechanism from a ductile regime to a brittle one.

Evaluation of K_c could follow directly from post-indentation crack measurement. In this approach, values of E/H could also be obtained by an unconventional utilization of a standard Knoop hardness testing apparatus [32].

2.5.1 Stiffness

Thus, presumably a series of brittle materials could be ranked according to their values of the parameters of the preceding equation to determine the grinding wheel infeed rate below which fracture would not occur. grinding forces characteristic of

2.5 Design Criteria for a Microgrinding Apparatus

Broadly defined, the role of the machine structure for any machine tool is to allow accurate and repeatable control of the motion of rapidly moving components that are often subjected to time varying forces. The machine structure design process used in the development of a microgrinding apparatus for grinding brittle materials in a ductile regime is a special case of the design procedure for any grinding machine, in this case with an atypical emphasis on precision.

To generalize the role of machine structure in precision grinding, one can break the machine's structural characteristics into four broad categories: stiffness, motion errors, environmental sensitivity, and motion control systems. Each of these structural characteristics influences the ultimate performance of the grinder in a unique way. The net effect of these parameters upon the grinder is to ultimately determine the operational precision of the machine. In ductile-regime grinding, for example, the maintenance of controlled low grinding forces is entirely dependent on these four parameters. This discussion, then, will focus upon each of these elements individually, with regard to how they affect the grinding process and how they are regulated in ductile-regime grinding.

2.5.1 Stiffness

Traditionally, grinding machines have been built with a relatively rigid structure, to prevent excessive flexure from the high grinding forces characteristic of

stock material removal. Though the required forces are greatly reduced in precision grinding, high machine stiffness is still essential. This is because the motion tolerances in precision grinding are of the order of $\sim 0.1\mu m$, while the forces generated are on the order of 1N, necessitating machine stiffness on the order of $10MN/m$.

The advantage to high stiffness in the structural loop between the grinding wheel and the workpiece is that it diminishes deflection error motions due to grinding (or disturbance) forces. On the other hand, high rigidity magnifies the forces that result from motion between the wheel and workpiece. Traditionally, this increased normal cutting force has been of little concern; in ductile-regime grinding it is a vitally important consideration. As a design goal for the development of a microgrinding apparatus, the static normal rigidity of the structural loop between the wheel and the workpiece should be $\sim 50MN/m$, roughly an order of magnitude stiffer than that of conventional precision grinders.

2.5.2 Error Motion

To obtain precise dimensional tolerances, the error motions of the machine tool components must be controlled or eliminated. Left uncontrolled, error motions on a grinding machine leave their imprint upon the part being ground. This has a negative influence on both contour tolerances and surface finish. The form of error motions are: axial, tilt, and radial runout of the grinding wheel spindle, and the six degrees of freedom inherent in each slideway. The relative influence of error motions on the grinding process is a function of the grinding configuration. To

study the physics of the material removal processes occurring in microgrinding, it is desirable to limit possible sources of motion error by minimizing the number of degrees of freedom of the system. A plunge grinding apparatus serves well in this regard; in this configuration the only two relative motions required are rotation of the grinding wheel and infeed of the workpiece. To produce useful components, at least one more axis of motion is necessary, but all of the essential elements of the *material removal* process can be studied using this simplified single-axis plunge geometry.

For a plunge-mode grinder using a cup wheel, the only motions that are *desired* are the infeed motion of the workpiece and the rotation of the grinding wheel. Deviations in straightness of travel, roll, pitch, yaw, and wheel runouts are error motions, and must all be reduced to levels that will not interfere with the desired part tolerances. Fortunately, most of the error motions described only affect the infeed motion in a second order way. Errors in radial spindle runout, for example, will alter the grinding wheel position with respect to the workpiece in a direction normal to the feed direction, and are therefore relatively unimportant in plunge grinding. In fact, the only error motion that is of first order in the direction of the feed is that of axial spindle runout, which directly influences the position of the wheel with respect to the workpiece. For this reason, the critical error motions to be controlled in a plunge grinding apparatus are infeed error and spindle axial runout. The influence of these error motions can be significantly diminished by

judicious selection of the infeed device and spindle. As a design criterion, the workpiece positioning accuracy should be 5nm , and all error motions in the direction of grinding infeed should be maintained below this value.

2.5.3 Environmental Sensitivity

Vibration and thermal expansion are other categories of error motions that, because they may or may not emanate from the machine tool, are collectively known as environmental error motions. These error motions detrimentally affect the tolerances and surface finish of the part. They are best addressed by first eliminating their sources, then diminishing their remaining effects.

Vibrations can result from the grinding machine itself or from an external source. Internally generated vibrations arise from the driving motors of the machine, rotation of an unbalanced load, or errors in the grinding wheel trueness. For the driving motors, a way to reduce to vibrational errors is to isolate the motor from the grinding wheel spindle by means of a flexible drive connection. This can be done in several ways, from a simple belt drive to a more elaborate flexure coupling. To attenuate external vibration sources, the microgrinding apparatus should be mounted on a vibration isolated table.

There are two complementary approaches for elimination of thermal expansion problems in a grinding machine environment. The first of these is to build the apparatus with a structural loop from a single type of material, thus effectively cancelling the effects of thermal growth. An even more effective (and expensive) approach is to

use materials (zerodur, fused quartz) that have negligible coefficients of thermal expansion. In addition to these design techniques, the influence of thermal expansion can be diminished by the utilization of a thermally controlled environment.

2.5.4 Motion Control System

With all of the error motions reduced to acceptable levels, the task remains to control desired relative motions of the grinding system components. The purpose of the controller is to measure the current position of the workpiece with respect to the grinding wheel, evaluate the desired current position, and use this information to generate an appropriate feedback signal to the infeed actuator. The important considerations for this control system are: establishment of a stable metrology base, determination of the control speed range of interest, and incorporation of an appropriate control algorithm.

The infeed rates required for microgrinding are fairly slow ($0.002\mu\text{m/s}$ to $2.5\mu\text{m/s}$), therefore a simple integral feedback control scheme can be utilized.

2.6 Summary

In the preceeding Chapter, the origins of ductile-regime microgrinding of brittle materials have been outlined, and the link between increased machine precision and increased grinding ductility has been identified.

Analytical models based upon the micromechanics, thermal contact conditions, and material removal mechanisms have been explored. To some degree, it is pos-

sible to study the grinding process using the described models. Gross geometrical models have been shown to be inapplicable in the determination of brittle/ductile grinding regimes, and micromechanical models of the elastic-plastic stress field have been used instead. A critical-depth-of-cut model was proposed that takes both the fracture toughness and the yield strength, or hardness, into account. Verification of this model will form the basis of the experimental portion of this study.

A framework for designing a microgrinding apparatus has been established. Through attention to such details as high rigidity, precision actuation components, microtrueing of the grinding wheel, vibration isolation, temperature control, in-process sensing and integral feedback control, the normal grinding forces can be maintained below a threshold value without even monitoring force. A central design criterion for achieving a ductile material removal regime has been postulated: reduction of the grinding infeed rate to levels previously not found in fixed abrasive grinding systems.

The apparatus used in this study was designed using the criteria set forth in Chapter 2. A schematic of the device is illustrated in Figure 3.1. This test bed has been given the acronym PEGASUS (*Precision Engineering Grinding Apparatus for Super-finishing Ultrahard Surfaces*). The machine provides a mechanism for plunge grinding with the 6mm wide rim of a 100mm diameter cup shaped grinding wheel. The workpieces to be ground are rectangular parallelepipeds measuring 6mm x

3 Microgrinding Apparatus

3.1 Detailed Specification

A systematic investigation of the ductile grinding regime demands a grinding apparatus utilizing state-of-the-art actuation mechanisms guided by closed loop feedback control. For the purposes of this study, the overriding requirement is that the grinding machine provides accurate, repeatable control of the workpiece position with respect to the grinding wheel. From Yoshioka's early attempts to grind hard materials in the ductile regime, it can be seen that a workpiece positioning resolution of $\sim 50nm$ or better is required to maintain a sufficiently small depth of cut to form ductile chips in glass [13]. This represents a level of machine precision that is not often associated with the grinding process. Such motion accuracy in turn demands an extremely rigid structural loop, so that disturbance forces experienced by either the workpiece or the grinding wheel will not be translated into significant relative motion between the two.

The apparatus used in this study was designed using the criteria set forth in Chapter 2. A schematic of the device is illustrated in Figure 3.1. This test bed has been given the acronym PEGASUS (*Precision Engineering Grinding Apparatus for Super-finishing Ultrahard Surfaces*). The machine provides a mechanism for plunge grinding with the 6mm wide rim of a 100mm diameter cup shaped grinding wheel. The workpieces to be ground are rectangular parallelepipeds measuring 6mm x

Figure 3.1. Schematic of the PEGASUS machine

6mm x 18mm.

As described previously, single axis plunge grinding uses two basic machine motions: rotation of the grinding wheel and infeed of the workpiece. Since ductile-regime grinding demands unusually small relative motion between the workpiece and the grinding wheel, both of these actuation mechanisms must have small ($< 50nm$) error motions in the infeed direction. The rotary motion of the grinding wheel is accomplished with a 10cm diameter air bearing spindle¹. This spindle allows rotary grinding speeds of up to 5000RPM with axial error motions less than 40nm. In addition, most of this axial error motion is repeatable within a revolution of the spindle and could be accounted for deterministically. The spindle is rotated by a DC motor having a maximum output torque of 1Nm. The coupling between the motor and the air bearing spindle is an annular flexure assembly² consisting of two parallel annular flexures, connected to each other by ribs. The axis of this flexure is concentric to the axes of the grinding spindle and the spindle motor. The purpose of the flexure assembly is to transmit torque from the motor shaft to the spindle, while preventing any axial or tilt motions of the motor shaft from influencing the spindle. Two other coupling systems were investigated: a helical flexure and a belt drive. The annular flexure assembly proved to be the best of the three in the compromise between error motion isolation and torque capacity, as will be described in Section 3.3 on the structural evaluation of the grinding apparatus.

¹Professional Instruments Corp. Blockhead, Model 4B

²Renbrandt Inc., Flexy-mite coupling

The infeed mechanism for the PEGASUS apparatus was designed to achieve high rigidity, $5nm$ resolution, and relative ease in the fixturing of the workpiece. This workpiece positioning device has three subsystems of increasingly finer precision that work in series in the infeed direction. Workpiece interchange is facilitated by a clamped dovetail slideway with a $15cm$ linear range. Preliminary positioning of the workpiece with respect to the grinding wheel face is achieved with a clamped double-reed flexure assembly, actuated by an 80-pitch manually-adjusted lead screw. The parallel flexures ensure a nearly horizontal translation of the workpiece over a $0.5mm$ range of travel with $1\mu m$ resolution. A pair of clamping bolts permits rigid fixturing of this assembly before the actual grinding infeed is begun. The final tier of motion actuation along the workpiece infeed axis is that of grinding infeed itself. This is accomplished with a preloaded piezoelectric actuator capable of linear motion over a $10\mu m$ range with a resolution of $5nm$ (using closed loop feedback).

In addition to fine control of the workpiece position with respect to the grinding wheel, precision ductile-regime grinding also requires high machine stiffness to minimize error motions due to disturbance forces and vibration. The PEGASUS apparatus was designed to obtain an appreciable stiffness. The high precision air bearing spindle is the least rigid element in the structural loop, with a stiffness of $120MN/m$ in the direction normal to the grinding contact. The spindle is mounted on a vertical steel support frame, with a design stiffness of $360MN/m$. The design stiffness of the workpiece holding flexure in the direction normal to the grinding con-

tact is $180MN/m$, including both bending and shear deflections. The piezoelectric actuator has a calculated normal stiffness of $180MN/m$. Theoretically, from the contributions of the spindle, spindle frame, and workpiece holder, and piezoelectric actuator, the stiffness of the entire structural loop should be $\sim 50MN/m$.

It may be important to note, while on the subject of stiffness, that measurements of the overall machine stiffness fail to consider the local bond stiffness at each diamond abrasive. Since each abrasive grain is seated in an elastic foundation, this stiffness acts in series with the stiffness of the machine's structural loop. Because this bond stiffness influences the local grinding forces, which are directly responsible for grinding ductility, the rigidity of the abrasive grain support is of some concern. While it would be difficult to measure the local bond stiffness it can be theoretically estimated. It can be assumed that each abrasive grain appears to the bond material as a rigid cylindrical die loading a semi-infinite matrix. For a single grain loaded against the matrix, there will be an elastic deformation of the matrix and a corresponding displacement of the grain. Timoshenko, [34], gives a formulation for this elastic displacement:

$$d = \frac{P(1 - \nu^2)}{2rE} \quad (3.24)$$

where:

d = Elastic deformation of foundation, m

P = Normal grain force, N

ν = Poisson's ratio of the bond material

r = Cross-sectional grain radius, m

E = Modulus of elasticity of the bond material, N/m^2

While this formulation is greatly simplified, ignoring actual grain geometry and the influence of adjacent grains, it will serve to illustrate the importance of the local stiffness properties of the grinding wheel.

For a bronze bond ($E = 110GPa$) a $6\mu m$ average diamond radius, the local stiffness can be determined from Equation 3.24:

$$K_{bronze} = \frac{P}{d} = \frac{2rE}{1 - \nu^2} = 730kN/m$$

For a resin bond ($E = 7GPa$) with the same diamond grains:

$$K_{resin} = 46kN/m$$

Clearly, the relative local stiffness depends linearly on the elastic modulus of the bond material, and this stiffness is ~ 15 times larger for bronze bonds than for resin bonds. Macroscopically, however, the small local stiffness of the individual diamonds combine in parallel to create an extremely rigid surface. Assuming that only

half of the 400,000 surface diamonds in the contact area of the grinding wheel (concentration number = 75) are actually contacting the workpiece, the overall stiffness of the wheel surface can be estimated:

$$K_{surface} = \frac{N}{2} K_{resin} = 9GN/m$$

which has virtually no effect on the overall system stiffness when added in series to the stiffness of the other elements in the structural loop.

Thus, through selection of the appropriate bond material, the forces of the grinding process can be manipulated on a local scale with respect to the cutting grains to permit a more even distribution of force per grain, resulting in an increased likelihood of uniform ductile behavior.

With high stiffness and accuracy designed into the PEGASUS apparatus, attention must now be focused upon consideration of precision required of the workpiece position controller. This is important from the standpoint of both metrology and control: how much resolution is needed in measurement and how closely must the infeed be controlled. A calculation of the machine's required infeed precision proceeds as follows. From the critical indentation depth model proposed in Chapter 2:

$$d_c \propto \left(\frac{E}{H} \right) \left(\frac{K_c}{H} \right)^2 \quad (3.25)$$

the required minimum grain depth-of-cut that will allow ductile material removal can be estimated. For design purposes, the nominal bulk properties of the workpiece

material can be used in evaluating this critical-depth-of-cut. As an example, for the nominal properties of silicon, $d_c \sim 70nm$. This, then is the nominal grain depth of cut that is predicted to cause a transition from ductile to brittle material removal on silicon workpieces. If the normal load on a diamond abrasive grain is similar in form to the normal load on a diamond indenter, the load corresponding to a given depth of cut can be calculated as follows. For a pyramid shaped abrasive grain, the following relationships were established in Chapter 2.

$$z \sim a \cot \theta \quad (3.26)$$

$$P_n \sim H a^2 \quad (3.27)$$

where:

z = Abrasive indentation depth

a = Abrasive contact diameter

θ = Abrasive pyramid half-angle

P_n = Normal load on an abrasive grain

H = Material hardness

Choosing silicon as a workpiece material and $\theta = 68^\circ$, the normal load on a given grain that will induce fracture (i.e. when $z = d_c$ can be calculated from Equa-

tions 3.25, 3.26, and 3.27. For silicon, this critical load is:

$$P_n = 337\mu N.$$

For a cup diamond grinding wheel, concentration #75 (0.19 vol fraction diamond), consisting of $6\mu m$ grit diamond, there are $\sim 10^{10}$ diamonds/ m^2 . In the PEGASUS configuration, with a 6mm x 6mm contact area, this means that there are nominally 4×10^5 exposed diamonds that could potentially contact the surface at a given time. It must be assumed, however, that these exposed diamonds can protrude from the bond by as much as $3\mu m$ (1/2 of their diameter) to as little as $0\mu m$ (flush with bond), even on a wheel that has been trued. Scanning electron microscope photographs of the grinding wheel confirm this apparent variation in diamond protrusion (Figure 3.2). A linear distribution for the diamond protrusion can be presumed, which gives:

$$x_n = \frac{3 \cdot 10^6 n}{N} \quad (3.28)$$

where:

x_n = Abrasive grain protrusion for the n^{th} abrasive grain

N = Number of diamonds in the contact area

Figure 3.2. Scanning electron photomicrographs of a trued grinding wheel surface.

A maximum penetration (d_c) that will just avoid brittle fracture can be determined from Equation 6.48. By assigning this penetration to the outermost diamond, the number of diamonds in contact with the workpiece N_c can be estimated by:

$$N_c = \frac{Nd_c}{3 \cdot 10^6} \quad (3.29)$$

Therefore:

$$x_n = \frac{nd_c}{N_c} \quad (3.30)$$

The indentation depth for the n^{th} grain is given by:

$$z_n = d_c - x_n \quad (3.31)$$

The load on the n^{th} grain can be calculated by combining Equations 3.26, 3.27, 3.30, and 3.31:

$$P_n = \frac{Hd_c^2}{\cot^2\theta} \left(1 - \frac{n}{N_c}\right) \quad (3.32)$$

The total load exerted by the wheel on the workpiece will be:

$$F_n = \sum_{n=1}^{N_c} P_n \quad (3.33)$$

By substituting the formula for P_n into this equation and simplifying the summation (assuming large N_c):

$$F_n \approx \frac{5Hd_cN_c}{6\cot^2\theta} \quad (3.34)$$

For silicon, $F_n \sim 2.5N$. Therefore, the total normal wheel load must be kept below $\sim 2.5N$ to achieve ductile grinding on silicon on the PEGASUS apparatus. Since this analysis assumes a rigid grain support matrix, it will be conservative in its estimation of permissible load. Because of the elastic nature of the grain supports, the number of contacting diamonds will be larger than predicted here, increasing the ductile-brittle transition normal force on the wheel.

In summary, the required infeed rate precision for a ductile-regime grinder is $\sim 10nm$, or about one tenth of the grain-depth-of-cut that produces a transition from ductile to brittle material removal. The normal load per abrasive grain must be kept below $\sim 300\mu N$, and the overall normal load on the wheel must be maintained below $\sim 2.5N$.

A piezoelectric actuator, chosen as the workpiece feeding mechanism, allows an infeed resolution of $\approx 5nm$. The grinding wheel spindle is specified with an axial runout of $40nm$, but measurements made on a trued grinding wheel indicate that most of this runout is repeatable. This means that *continuous* cutting depths smaller than $40nm$ are not possible, but intermittent cutting, corresponding to the "high" spot of the spindle's axial runout, can be made with cutting depths of less than $5nm$.

3.2 Metrology

Both displacement and force can be monitored on the PEGASUS grinder. The range, bandwidth, and resolution of these measurement systems are well matched

to the requirements stated in the earlier parts of this section. For displacement, a lever-type inductive gage is used. This contacting probe is capable of $5nm$ resolution over a $0.5mm$ range at frequencies from DC to $10Hz$. It is positioned to measure the workpiece infeed at a point adjacent to the grinding zone on the leading face of the workpiece. Because it measures at the leading edge of the workpiece, any thermal expansion of the workpieces during grinding can be directly compensated by the controller. The gage is fixed to the rigid saddle of the dovetail slideway, which supports the grinding wheel spindle. Before use, the inductive gage was calibrated against a capacitive gage to verify its resolution and bandwidth.

For force measurement, two different systems are used. the tangential force of the workpiece/wheel interface (i.e. the *material removal* force) is monitored by measuring the current applied to the DC motor (which was servo-controlled to provide constant rotational speed). Because there is a linear relationship between motor current and motor torque, a simple resistive circuit (provided on the servo-controller) allows measurement of motor torque. The resolution of this torque measurement is $\sim 5 \times 10^{-4} Nm$, with a bandwidth of $1Hz$. Since the grinding wheel's contact area with the workpiece is at a nominal radius of $5cm$, the tangential grinding force resolution provided by this torque signal is $\sim 10mN$. The total deliverable torque of the motor is $\sim 1Nm$, corresponding to a tangential force of $21N$. For most of the materials to be ground on this apparatus, this range and resolution is well suited for measurement of the microgrinding tangential forces.

Normal force measurement is a more difficult task, since the gage itself becomes part of the structural loop, comprising the overall stiffness of the machine. To measure normal force, a strain gage load cell was inserted into the workpiece holder between the piezoelectric actuator and the workpiece itself. This gage has a range of $450N$ with a resolution of $0.1N$ and a bandwidth of $5kHz$.

3.3 Structural Evaluation

A series of tests were carried out to verify the design features of the PEGASUS apparatus and to evaluate the overall stiffness and environmental sensitivity of the system. These are the parameters that will have a bearing on the ductility of the material removal process.

3.3.1 Machine Stiffness

The axial stiffness (normal to the grinding direction) at the interface between the workpiece and the grinding wheel was measured by inserting a strain gage load cell, a piezoelectric actuator, and a displacement gage between the workpiece and the grinding wheel. A sinusoidal forcing function was exerted by the piezoelectric actuator, and both the force and displacement histories were recorded on an oscilloscope. For the quasi-static stiffness, measured with a forcing frequency of $0.25Hz$, the average stiffness was $52MN/m$ with a measurement standard deviation of $6MN/m$. This compares well with the design stiffness, calculated at $50MN/m$. As the forcing frequency is increased, the dynamic stiffness of the grinding contact can be deter-

mined. For a 25Hz forcing frequency, the average stiffness of the system increases to $116\text{MN}/\text{m}$, with a measurement standard deviation of $28\text{MN}/\text{m}$. These values of normal stiffness are at least an order of magnitude larger than that encountered on a conventional grinding machine. Nonetheless, such rigidity is common to high precision machining systems; e.g. diamond turning machines.

While the *largest* forces generated in ductile-regime plunge grinding are normal to the grinding contact, the material removal forces are in a direction tangent to the contact (parallel to the wheel motion at the contact). The stiffness of the wheel/workpiece contact in this tangent direction was measured by applying a dead weight loading to the workpiece holder and measuring its tangential deflection relative to the grinding wheel spindle. This measurement was made with a lever-type inductive gage and loads from $0\text{--}10\text{N}$. The average tangential stiffness was $38\text{MN}/\text{m}$, with a measurement standard deviation of $1.5\text{MN}/\text{m}$. While the stiffness in this direction is lower than in the normal direction, the grinding forces here are also lower, by approximately an order of magnitude. For minimizing position errors in the presence of unknown disturbance forces, then, the limiting stiffness is still that in the normal direction of grinding.

The addition of the normal force sensor to the workpiece holder diminishes the overall system stiffness, in both the normal and tangential directions. Static stiffness measurements were made in each of these directions as outlined above for the workpiece holder alone. In the normal direction to the grinding contact, the

average stiffness was reduced to $19MN/m$, with a measurement standard deviation of $3MN/m$. This is approximately $1/3$ of the normal stiffness that was measured without the load cell. Even more significant, however, is the change of stiffness in the grinding direction with the addition of the load cell. In this direction, the static stiffness average drops to $0.61MN/m$, $1/60$ as rigid as the system was without the load cell. This loss in stiffness is due to the design of the load cell, which must remain insensitive to lateral forces while measuring normal forces. Thus, lateral compliance is built into the gage. Clearly, this is an inappropriate design for use in a grinding system that generates forces in two directions. Nonetheless, it is important to evaluate the relative size of the normal forces in grinding, and the *temporary* inclusion of this gage for measuring these normal forces is useful, even though the system stiffness is altered by the measurement.

3.3.2 Environmental Sensitivity

To ensure dimensional accuracy on the grinder and its concomitant metrology systems, the effects of thermal and mechanical disturbances must be addressed. Thermally, dimensional expansion and contraction of the components and sensors on PEGASUS is limited by controlling the thermal environment in the room to $\pm 0.1^{\circ}C$. Additionally, by constructing the entire structural loop out of the same material (steel), the effects of differential thermal expansion are diminished. As a final passive measure against thermal gradients during grinding, the wheel and workpiece are enclosed within a plexiglass shroud.

Mechanical vibrations comprise another category of environmental disturbance that must be minimized to avoid error motion. These can be divided into two types: those external to the grinding apparatus and those generated by the grinding itself. To attenuate external vibration sources, the dovetail slideway base of the grinder bolted to a 500kg vibration-isolation table, which acts as a low pass filter for mechanical vibrations. The table's natural frequency is 1Hz and it attenuates vibrational frequencies of 10Hz and above by more than 40dB. Frequencies below 1Hz are not attenuated by the vibration isolation table, but these are more likely to cause rigid body motion of the entire apparatus than they are to cause relative motion of its constituent parts. Such rigid body motion will have little influence on the mechanical accuracy of the grinding process.

Internally generated vibrations result primarily from three sources: grinding wheel axial runout (lack of trueness), grinding wheel balance, and spindle motor vibration. Wheel trueness will be discussed later in this Chapter. Balance of the grinding wheel is accomplished statically, through the adjustment of four counter weighting set screws located on the periphery of the air bearing spindle. Because the air bearing offers almost no frictional resistance, it is easily balanced by moving the set screws in or out of their threaded holes so that there is no "preferred" rest position. Upon achieving this level of balance, the major contributor to the internal vibration of the apparatus is the vibration of the motor driver. This vibration is diminished by the use of a flexible coupling between the motor and the spindle.

Three types of flexible couplings were explored for PEGASUS: a flat belt drive, a helical flexure, and an annular flexure.

The flat belt drive consists of a pair of 5cm diameter crowned pulleys and a 30cm x 1cm x 0.1cm elastomer belt. The helical coupling consists of a 1.5cm diameter x 2.5cm long cylinder, with a 0.6cm bore. The walls of this cylinder are machined through to the interior with a helical groove, which provides the required compliance for the device. The annular flexure coupling consists of a pair of annular rings measuring 3.8cm on the outside diameter, 2.0cm on the inside diameter, and 0.25mm thick. These are connected concentric to each other by short, stiff ribs. Each annular ring is connected by ribs to an end sleeve that is also concentric with each ring.

The latter two couplings require careful concentric alignment of the axes of the coupling induced vibration. This flexure device exhibits small spectral peaks at motor shaft and the spindle for adequate performance. The structural vibration transmitted by each of these couplings was evaluated, as was the maximum torque capacity of each. From these considerations, an evaluation of the optimal coupling can be made. The desired range of cutting velocities demands spindle rotational speeds from 0-2500RPM. The limits of structural vibration, then, can be determined by measuring the axial acceleration of the spindle frame while driving the motor through a range of rotational speeds from 0 – 2500RPM. Sending this vibration signal to a spectrum analyzer and acquiring a "peak" power spectrum provides a convenient window into the vibration induced by the coupling. A peak power

spectrum is one in which the maximum vibrational energy at each rotational frequency is recorded for the frequency range under analysis. By slowly increasing the spindle rotation from $0-2500RPM$ while acquiring a peak acceleration power spectrum, the spectral energy for frequencies from DC to $42Hz$ ($0 - 2500RPM$) can be obtained. From this power spectrum, the maximum structural vibration at a given rotational frequency can be determined simply by finding the spectral energy associated with that frequency.

Peak acceleration spectra for the three couplings are illustrated in Figure 3.3. All three power spectra are scaled equally. The uppermost power spectrum is of the belt drive system. This coupling system exhibits a resonance at $4Hz$ ($240RPM$) and a smaller harmonic at about $8Hz$ ($480RPM$). Above these speeds, the vibration is uniform and relatively low in amplitude. The middle spectrum is of the helical coupling induced vibration. This flexure device exhibits small spectral peaks at $3Hz$ ($180RPM$), $5Hz$ ($300RPM$), and $14Hz$ ($840RPM$), and resonances at $23Hz$ ($1400RPM$), and $44Hz$ ($2600RPM$). Between these spectral peaks, the level of the vibration varies, but is usually higher than that exhibited by the belt drive. The bottom spectrum is of the annular ring flexure assembly. The resonant frequency for this device is at $38Hz$ ($2300RPM$) with smaller spectral peaks at $41Hz$ ($2500RPM$) and $14Hz$ ($840RPM$). Aside from these peaks, the vibrational coupling is uniformly lower than that obtained for either of the other couplings.

For torque delivery, both the helical coupling and the annular ring assembly can

Figure 3.3. Peak vibration power spectra for three motor-to-spindle couplings.

Top: Flat belt. Middle: Helical coupling. Bottom: Annular flexures.

transmit the entire $1Nm$ output capacity of the motor. The belt drive, on the other hand, begins to slip at $0.4Nm$ torque.

From these results, the annular flexure was deemed to be the most appropriate compromise between high torque capacity and low structural vibration. At $500RPM$, the speed at which the majority of the microgrinding experiments were conducted, the *peak* vibration level of the spindle due to the motor is $< 10nm$ with this coupling. This vibration will appear as a once-per-revolution contribution to the axial runout of the spindle.

3.4 Infeed Control System

Perhaps the most important element of the grinding apparatus that determines its capacity for ductile-regime grinding is the regulation of the infeed of the workpiece. Closed-loop real-time feedback control is the only method of ensuring sub-micrometer precision for this infeed, and is thus a necessary component of the grinding apparatus. The first step in designing a controller is to evaluate the transfer function of the controlled variable; in this case, the piezoelectric (PZT) workpiece actuator.

The transfer function of the system is made up of two stages: the PZT power amplifier and the transducer. The power amplifier stage transforms the input control signals into high voltages to be applied to the piezoelectric transducer. The transducer stage transforms the voltage applied to the piezoelectric into a corresponding motion. The transfer function of the PZT power amplifier transfer function exhibits

90° of phase lag and 7dB of attenuation from *DC* to 2kHz. Over this same frequency range, the transfer function of the piezoelectric actuator is relatively flat in both phase and magnitude. The combined spectrum is illustrated in Figure 3.4.

Based on the relative lack of complexity of the transfer function and low grinding infeed rates required, an integral feedback scheme was chosen as the system controller. This type of control is relatively insensitive to the system model, and ensures an elimination of all steady state errors. The algorithm implemented uses a digitally-generated ramp signal as a reference input (desired position), and measures the difference between the actual position and the desired position. This error signal is multiplied by the system gain and added to the current digital output signal (integrated). This signal is then converted from a digital signal to an analog one. A low pass filter follows the d/a converter, to smooth the output step. This filtered control signal is sent into the power amplifier, which actuates the piezoelectric transducer. The resulting motion of the workpiece is then detected by the displacement sensor, converted to a digital signal, and compared to the currently desired reference level to generate a new error signal, completing the feedback loop. The overall infeed of the workpiece, then, will follow the shape of the digitally generated reference signal. This algorithm is demonstrated schematically in Figure 3.5. Figure 3.6 shows an example of the effectiveness of this algorithm in controlling the infeed of a workpiece. In these plots, the lower curve is workpiece position versus time, which faithfully follows the prescribed ramp. The upper curve is the input

Figure 3.4. Overall Transfer function of the workpiece actuation system.

Figure 3.5. Schematic of the closed-loop infeed controller.

signal versus time for the piezoelectric actuator during this infeed. As can be seen, the piezoelectric actuator was being influenced by an oscillatory disturbance (in this case, traced to a $\pm 0.5^\circ\text{C}$ room temperature fluctuation). This illustrates the effectiveness of feedback control in eliminating the influence of undesired environmental disturbances. It is important to consider the *disengagement* of the workpiece from the wheel. If this is not done quickly, there may be some ambiguity about the final infeed rate experienced by the workpiece. To ensure immediate disengagement, an interrupt signal is sent to the controller upon full extension of the PZT or upon completion of the specified ramp. For either of these conditions, the PZT is fully retracted on the next computer clock cycle.

The integral gain and the frequency cutoff of the low-pass filter are the two variables that determine the accuracy and smoothness of the infeed ramp. While the overall slope of the ramping infeed is important, it may not be as important as the *smoothness* of the infeed. Consider the example of a ramping infeed that consists of many discrete unit steps. The *instantaneous* feed rate at any given moment is either zero (during the dwell periods) or infinite (during the step advance), while the average infeed is some intermediate value. For ductile-regime grinding, the instantaneous infeed may well be an important consideration, and as such must be regulated. Figure 3.7 shows a portion of the actual infeed of the workpiece for two different integral gains. While both have the same average slope, the lower infeed signal is clearly more uniform. Figure 3.8 illustrates the importance of a filter on

Figure 3.6. Control of PEGASUS infeed. Bottom curve: Workpiece position vs time for a $10\ \mu\text{m}$ infeed. Top curve: Output signal to PZT, indicating the presence of an oscillatory thermal disturbance.

the d/a output. The lower curve shows a portion of infeed motion obtained without any filtration, while the upper curve shows a portion of a $0.25\mu\text{m/s}$ infeed motion with a 100Hz low pass filter on the d/a output signal. The integral gain and d/a output filtration were experimentally optimized, and permit infeed rates from 5nm/s to $1.25\mu\text{m/s}$ with no more than 250nm deviation from the desired position over the entire infeed, and no more than 5nm deviation from the desired position over a given revolution of the grinding wheel.

3.5 Trueing the Grinding Wheel

Trueing is the process of reducing the runout of the mounted grinding wheel so that the contact forces between the abrasive grains and the workpiece can be reliably controlled. Trueing accuracy represents an additional factor that needs to be controlled to ensure knowledge of the position of the workpiece with respect to the grinding wheel. In the PEGASUS configuration, this requires machining the rim of the cup wheel to reduce its axial runout. Since the wheel is mounted on an air bearing spindle that has an *unrepeatable* runout of less than 10nm , this value represents a lower limit of the obtainable trueness of the grinding wheel on this apparatus.

There are two predominant methods of trueing that are currently used for precision diamond grinding wheels: wear of the wheel against another solid abrasive surface, and single point diamond facing of the wheel. Traditionally, the former method has utilized a brake-controlled trueing wheel of a less abrasive nature (sil-

Figure 3.7. The effect of controller gain on grinding infeed. Upper curve gain:

0.1. Lower curve gain: 0.03

Figure 3.8. The effect of low-pass filtration on the grinding infeed. Upper curve filter: 100 Hz . Lower curve filter: 1000 Hz .

icon carbide or alumina). More recently, brake-controlled trueing with another diamond wheel has been successfully demonstrated. With this technique the tops of the abrasive diamonds on both wheels become flattened, an effect that may have some influence on the cutting characteristics of the grinding wheel. On the PEGASUS apparatus, both of the trueing techniques described above were tried, with limited success. Trueing against a solid abrasive, diamond or otherwise, was exceptionally slow, and reduced the as-mounted axial wheel runout from $4\mu\text{m}$ to $1\mu\text{m}$ in 14 hours of continuous abrasion. Single point diamond trueing showed greater promise, reducing the wheel runout to less than $0.6\mu\text{m}$ after several passes of the tool. A catastrophic increase in runout, probably due to chipping of the diamond tool, showed this trueing method to be extremely sensitive to the trueing conditions.

The trueing technique that was found to be most successful was that of diamond paste "lapping" of the grinding wheel. Feeding a pyrex lap into the grinding wheel with a generous supply of diamond paste in the contact region proved effective in reducing the runout from $4\mu\text{m}$ to less than $0.12\mu\text{m}$ peak-to-valley after four $100\mu\text{m}$ infeeds of the lap (Figure 3.9). This level of trueing accuracy ranks as the state-of-the-art for diamond grinding wheels.

Figure 3.9: Reduction in runout after four individual infeeds of $100\mu\text{m}$ each, using

It was found that a paste composed of diamonds ($9\mu\text{m}$ diameter) that were about the same size as the diamonds in the wheel ($4\text{--}8\mu\text{m}$ diameter) were most efficient in the trueing process, as compared to larger ($25\mu\text{m}$ diameter) and smaller ($1\mu\text{m}$ and

3 μ m diameter) diamond pastes. This truing technique was effective for both resin and bronze bonded grinding wheels.

3.6 Summary

PROGRESSION OF WHEEL RUNOUT AFTER TRUEING WITH DIAMOND PASTE.

Figure 3.9. Reduction in runout after four individual infeeds of 100 μ m each, using pyrex lap and 9 μ m grit diamond paste.

$3\mu\text{m}$ diameter) diamond pastes. This trueing technique was effective for both resin and bronze bonded grinding wheels.

3.6 Summary

As illustrated in Figure 3.10, the microgrinding apparatus described in this chapter extends well into the "microgrinding gap" described in Chapter 1. A summary of the specifications, structural evaluation, precision, sensors, and control system comprising the PEGASUS apparatus is presented below:

-
- 0–5000RPM spindle driven through either a belt or a flexible direct coupling by a dc servomotor.
 - Torque measurement capability through dc motor current monitoring.
 - Wheel trueing to 125nm overall axial error, 10nm unrepeatable.
 - Flood coolant
 - $4\text{--}8\mu\text{m}$ diamond grinding wheels, bronze or resin bond
 - Infeed measurement for feedback and control via a lever-type inductance gage (resolution $\sim 5\text{nm}$). Piezoelectric infeed overall range; 0 – $10\mu\text{m}$.
 - Closed loop integral infeed control.
 - Controlled infeed rates: 12.5nm/s to $1.25\mu\text{m/s}$.

4 Experimentation

4.1 Plan of Experiments

The hypothesis set forth in Chapter 1 was that hard, normally brittle materials can be ground in a ductile regime if the scale of material removal can be made small enough. More specifically, this machining must take place at material removal rates that are greater than those used in polishing but less than those previously obtain-

ACHIEVABLE MATERIAL REMOVAL RATES IN ABRASIVE MACHINING

Figure 3.10. The "microgrinding gap" versus the operating range of PEGASUS.

4 Experimentation

4.1 Plan of Experiments

The hypothesis set forth in Chapter 1 was that hard, normally brittle materials can be ground in a ductile regime if the scale of material removal can be made small enough. More specifically, this machining must take place at material removal rates that are greater than those used in polishing but less than those previously obtainable in grinding; i.e., the so-called microgrinding gap. Chapter 3 details the design of an apparatus that largely covers the range of material removal rates comprising the microgrinding gap. What remains, then, is to develop an experimental program to test the validity of the ductile-regime grinding hypothesis. In particular, an evaluation of the material removal regime as a function of the *machining conditions* and *material properties* is required, with the goal of establishing a correlation between these parameters and the ductility of the grinding process.

The critical-depth-of-cut model proposed in Chapter 2 predicts that a transition between ductile and brittle material removal will occur for any brittle material and that the ductility of the material removal regime will increase with a decreasing grinding depth-of-cut.

Verification of this model demands several difficult measurements. The ductility or brittleness of the material removal regime, for example, must somehow be ascertained, either during the grinding process or by a post-grinding analysis of the

ground surface. Unfortunately, this measurement of surface ductility is by not an established science. Currently, no method of quantification of ductility is available, yet this is an essential ingredient in any formulation of a brittle-ductile transition model. The *transition* from ductile machining to brittle machining is itself an uncharted area of research. Whether this transition is sharp and easily identified or subtle and indistinct is uncertain.

These unknowns then, along with the basic hypothesis of a critical depth-of-cut, form the basis for launching into an experimental program. Additional experiments that can shed light onto the role of other variables on surface ductility must also be explored. The proposed critical-depth-of-cut model does not include the machining variables of speed, stiffness, or grinding force, nor the environmental variables of coolant or grinding temperature, nor the material variables relating to thermal properties, chemistry, or structure.

A comprehensive experimental investigation of all of these variables is a monumental task, yet to ignore their influence when so little is known about the phenomenon of ductile-regime material removal is unwise. A more reasonable approach is to launch an exploratory study, testing the variables that are most likely to have an effect on the grinding regime with a view toward establishing trends and relationships rather than providing comprehensive statistical data.

The experimental tasks can be delineated as follows:

- Grinding Tests

- * Verify the microgrinding capabilities of PEGASUS.
- * Select and grind various hard materials in search of the ductile-to-brittle transition.
- * For model materials, investigate the influence of speed and coolant on the ductility of the material removal.
- * Explore the influence of grinding wheel bond on grinding ductility.
- Measurements
 - * Establish a technique for quantification of grinding ductility.
 - * Measure the relevant surface properties of the brittle materials selected for the microgrinding tests.
 - * Measure the microgrinding residual stress.
 - * Measure acoustic emission signals during microgrinding to explore their potential as an in-process measure of grinding ductility.

4.2 Verification of the Microgrinding Capabilities of PEGASUS

As an initial test of the PEGASUS apparatus and its capacity for ductile-regime grinding, a material that normally undergoes a brittle material removal regime in conventional grinding was prepared for a "proving" grind on the PEGASUS machine. The sample, single crystal silicon, was ground on both a conventional

surface grinder and on PEGASUS. The resulting surfaces were compared in an SEM to distinguish any qualitative differences. Figure 4.1 illustrates these differences. At the top is a conventionally ground surface showing characteristic regions of brittle fracture and rough topography while at the bottom is the surface ground on PEGASUS demonstrating a nearly completely plastic material removal regime. While no attempt was made to enumerate all of the differences between the two machines, this figure provides evidence that the guidelines used in developing the PEGASUS grinder were successful in obtaining the ductile grinding regime. This result might have been anticipated for silicon, since its estimated "critical-depth-of-cut" d_c is approximately $0.42\mu\text{m}$, which is large in comparison to most other 'hard' materials. Thus, it would be expected that the large d_c of silicon (about 4 times as high as d_c for soda-lime glass) would make it relatively easy to grind in a ductile regime.

4.3 Procedure for Grinding Experiments

A protocol was established for the microgrinding tests to ensure experimental repeatability, and is outlined here. The material workpiece samples were cut to size ($5\text{mm} \times 5\text{mm} \times 5\text{mm}$ parallelepipeds) with a diamond wafering saw. Several samples of each material were machined, with care taken to ensure common orientations for the crystalline sample materials. Each specimen was then bonded to a steel workpiece base with a cyanoacrylate adhesive. The leading face of the workpiece surface was then ground flat on a conventional reciprocating surface grinder

Figure 4.1. Silicon ground on a conventional precision grinder (top), and on the PEGASUS apparatus (bottom)

Wheel Type	360 grit, diamond
Wheel Speed	23m/s
Table Speed	0.15m/s
Downfeed	10 μ m/pass
Coolant	H ₂ O

Table 4.1. Grinding conditions for workpiece surface preparation

under the conditions listed in Table 4.1

The PEGASUS grinding apparatus was prepared by first trueing the wheel to an R_{max} of 125nm or less. The workpiece base containing the sample to be ground was then bolted onto the workpiece holding device. Next, the dovetail slideway saddle upon which the workpiece holding device is mounted was advanced toward the grinding wheel. When the workpiece surface was within 0.5mm of the grinding wheel, the dovetail slideway's movable saddle was locked down with two clamping levers. The piezoelectric actuator was then extended to one half of its full scale travel. Following this, the parallel flexure assembly was slowly advanced, using the 80 pitch lead screw, until a resistance was felt in a manual rotation of the air bearing spindle. The PZT was then retracted so that there was no longer contact between the workpiece and the grinding wheel, and the flexure assembly was then clamped into position. Next, the spindle motor was turned on and accelerated to a selected speed. The coolant flow was begun, and the contacting displacement gage

was adjusted for a readout of zero. To eliminate any transient thermal effects, the system was then allowed to run for at least five minutes, during which time the workpiece displacement is monitored on an oscilloscope. Control of the infeed is then turned over to the computer control program via the microcomputer. This computer was used to monitor the infeed and to control the piezoelectric actuator, the motor speed, and the reference signal (infeed ramp).

Throughout the infeed and retraction process, the workpiece displacement and tangential grinding force were also monitored and recorded on a digital oscilloscope. The peak average value of the tangential force was maintained for each cut. Upon completion of a $10\mu\text{m}$ infeed, the flexure was unclamped, advanced $8\mu\text{m}$, then reclamped. The displacement gage was then reset to zero, and the next $8\mu\text{m}$ infeed was begun. This cycle was repeated at the end of each infeed until the required depth of cut was obtained.

4.4 Selection and Grinding of Various Hard Materials in Search of the Ductile-to-Brittle Transition

With the qualitative operational characteristics of the ductile-regime grinding apparatus established, quantification of the grinding regime as a function of the material properties of the workpiece and the operational variables of the grinder is the next task.

4.4.1 Material Selection

Selection of the materials to test grind is driven by several objectives: reproducibility of results, relevance of the resulting data to commercially important materials, and inclusion of a broad variety of structures and properties. First, to ensure that the experimental results will be reproducible, a series of model crystalline solids were included. These materials present known quantities in terms of structure, homogeneity, and bulk properties. Also, the fracture mechanics and the physics of plastic deformation via dislocation motion are known for these materials, and can be interpreted on an atomic scale. Additionally, these materials are readily probed by Raman Spectroscopy to determine residual surface and subsurface stresses. By using single crystals, the effects of grain boundaries on the small scale material properties can be eliminated. The single crystal materials selected were: silicon, germanium, and quartz. Silicon and germanium are commonly used semiconductor materials, whereas quartz is a representative optical material. Much of the commercial grinding of hard materials is performed on amorphous glasses. For this reason, this was chosen as another group that ought to be represented in this experimental study. These materials have many properties in common with the hard single crystals, but their amorphous structure gives them an entirely different fracture behavior, lacking in cleavage planes and preferred slip orientations. The mechanisms of macroscopic fracture and plastic deformation are well explored for amorphous glasses, but their microscopic phenomenology is less well known than

for single crystals. The amorphous glasses chosen for experimental grinding were soda-lime glass, fused silica, and zerodur. Soda-lime glass is the most common of the amorphous glasses, and is simply "window glass." Fused silica is a structural glass, sometimes used in lens molding. Its chemical composition and many of its properties are the same as its single crystal counterpart, quartz. A final interesting feature of fused silica is its tendency to irreversibly *densify* under an applied hydrostatic compressive stress, a characteristic that has earned it the title of an "anomalous glass." Zerodur is a glass-ceramic, consisting of both amorphous and polycrystalline phases, that is most notable for its unusually low coefficient of thermal expansion ($\sim 10^{-7} m/m^{\circ}C$). Because of this feature, zerodur is often used in precision devices that cannot tolerate the dimensional deformation that normally accompanies temperature variations within a material.

The final major category of hard materials included in this study is advanced ceramics. These materials are finding widespread and accelerated use, largely because of their exceptional wear resistance and heat resistance. The structure of these materials is not achieved through thermodynamic processing, but rather through the processes of sintering, reaction bonding, and powder technology. As a result of this processing, the microstructure of these materials is often complex, and there is little guarantee that the bulk properties will be similar to the microscopic properties of the constituent particles. Because of their expanding commercial use, however, it is important that advanced ceramics are included in any comprehensive study

of the phenomenon of grinding of hard materials. The advanced ceramics chosen for experimental grinding were: reaction bonded silicon carbide, alumina, zirconia, and toughened zirconia. Silicon carbide is a common structural ceramic that is often used for lens molding. The microstructure of reaction bonded silicon carbide consists of grains of silicon carbide surrounded at the grain boundaries by pockets of silicon. This provides an interesting case study for ductile-regime grinding by introducing the effect of a second phase on the overall ductility of the surface. Alumina is the chemical equivalent of sapphire, though their different structures cause significantly different behavior between the two. Alumina is often noted for the presence of glassy phases and voids, and these will no doubt affect the grindability of the surface. For this study, a relatively void-free alumina was chosen. Zirconia is an important ceramic that can exhibit uncharacteristically high toughness due to a local irreversible phase-transformation that occurs when the ceramic is subjected to a stress. This phase transformation is not volume conserving; the region in the vicinity of the transformation is subjected to a crack-arresting compressive stress due to a volume increase of the transformed phase. The influence of this transformation toughening is dramatic on a macroscopic scale, with toughened zirconia exhibiting many times the fracture toughness of untoughened zirconia. This ductile-regime grinding study includes both toughened and untoughened zirconia samples, and a comparison of their machining properties should yield insight into the contribution of toughening mechanisms to surface ductility.

4.4.3 Microgrinding of Other Materials

Wheel	4-8 μ m diamond, bronze bond
Coolant	H ₂ O
Work Material	Fused Silica
Cutting Speeds	500RPM (2.5m/s)
Infeed Rates	1.5, 7.5, 37.5, 50.0, 75.0 nm/rev
Total Plunge	40 μ m

Table 4.2. Microgrinding conditions used to test for the brittle-ductile transition on fused silica.

4.4.2 Microgrinding of Fused Silica

Having selected a broad range of materials, the experiments to test for transition from ductile to brittle grinding can begin. The first experimental data-set was made on fused silica, with the goal of trying to identify the transition from ductile to brittle material removal. The machining conditions used for this test are given in Table 4.2.

This set of experiments corresponds to material removal rates per unit wheel width from $8 \times 10^{-5} \text{ mm}^3/\text{mm s}$ to $3 \times 10^{-5} \text{ mm}^3/\text{mm s}$. This range covers the lower portion of the microgrinding gap. Indeed, this is where one would expect to find a transition for fused silica, which has low values of toughness and estimated critical-depth-of-cut.

4.4.3 Microgrinding of Other Materials

For the next series of tests, each sample from the set of different hard materials was ground using a small subset of fixed machining conditions. The purpose of this experiment was to test the critical-depth-of-cut model, and to ascertain the grinding ductility of the various hard materials as a function of the grinding infeed rate. The scope of this series of experiments are outlined in Table 4.3. It is expected that most of the materials represented will show some variation in ductility over this range of grinding infeed rates. These experiments should provide insight into the material properties important to microgrinding ductility.

4.5 Experiments to Test the Influence of Microgrinding Speed

The critical-depth-of-cut model is not affected by the grinding wheel speed. Judging from other studies of brittle material removal, this may prove to be a valid assumption. Single diamond turning tests on silicon and germanium, for example, have shown little change in surface fracture resulting from changes in cutting speed [35]. Pioneering single point grinding studies on various glasses were made by Schinker. His conclusions indicated a phenomenological transition in the material fracture process between $1m/s$ and $100m/s$ cutting speeds, yet the critical depth-of-cut to produce chipping damage and fracture remained virtually unchanged over this range of grinding speeds [8].

To measure the influence of grinding wheel speed on surface ductility, a series

Work Materials	Soda-Lime Glass
	Alumina
	Zerodur
	Quartz
	Zirconia
	Toughened Zirconia
	Germanium
	Silicon
	Silicon Carbide
Wheel	4-8 μ m diamond, bronze bond
Coolant	H ₂ O
Cutting Speeds	500RPM (2.5m/s)
Infeed Rates	1.5, 7.5, 37.5 nm/rev
Total Plunge	40 μ m

Table 4.3. Microgrinding conditions used to test the expanded material matrix.

Work Materials	Fused Silica Zerodur Silicon
Wheel	4-8 μ m diamond, bronze bond
Coolant	H ₂ O
Cutting Speeds	0.5, 1.25, 2.5, 5.0, 12.5 m/s
Infeed Rate	7.5 nm/rev
Total Plunge	40 μ m

Table 4.4. Microgrinding conditions used for tests of the influence of grinding speed on grinding ductility.

of experiments were planned in which the grinding chip thickness (i.e. the grinding infeed per revolution of the wheel) was maintained at a constant value, while the grinding wheel speed itself was varied from 0.5m/s to 12.5m/s.

Table 4.4 summarizes the conditions used in this series of tests.

4.6 Experiments to Test the Influence of Grinding Wheel Bond

The origin of the critical-depth-of-cut model is in fact not rooted in a critical depth analysis; rather, it is the product of a critical *force* analysis. The critical

depth conversion only arises from the indentation hardness relationships:

$$P_n = H\beta\pi a^2 \quad (4.35)$$

$$z = a \cot\theta \quad (4.36)$$

where:

P_n = indenter load, N

H = hardness, GPa

β = dimensionless indenter shape factor (~ 1)

a = characteristic dimension of the residual impression, m

z = indentation depth, m

θ = indenter half-angle

The test devised to check this hypothesis is outlined in Table 4.5.

4.7 Experiments to Test the Influence of the Grinding Fluid

For a grinding wheel, the relationship between infeed rate (depth of cut) and normal force is not as simply defined as it is for indentation. In particular, the infeed versus force relationship on a given grinding grain depends on the height distribution of the diamonds in the wheel and on the nature of the diamond's elastic support matrix. The maximum load experienced by the most protruding diamond will be larger for a widely varied diamond height distribution than for a more uniform height distribution. Similarly, this maximum load will be larger for a stiff matrix than for a compliant one. Also, the thermal, lubrication, and chemical properties of the

To test this hypothesis, a pair of grinding wheels were prepared with all variables constant except for the bonding matrix. The first of these wheels contained a bronze bonding matrix, while the second was fabricated with a polymer resin bond. The elastic moduli of these bonds are 110GPa (bronze) and 7GPa (resin). Since the diamond grain support stiffness is linearly proportional to elastic modulus, these wheels present a 15:1 ratio in local diamond support stiffness. It can be assumed that distribution of the approximately 2×10^7 surface diamonds on the two wheels were similar after diamond paste trueing to 125nm . The resin bond should allow a *lower* maximum force due to its lower support stiffness. Therefore, for the same infeed rate, a more ductile surface is expected for the resin-bond wheel than for the bronze-bond wheel.

The test devised to check this hypothesis is outlined in Table 4.5.

4.7 Experiments to Test the Influence of the Grinding Fluid

The properties of the grinding fluid also do not appear in the critical-depth-of-cut model. Nevertheless, the grinding fluid can impact the material removal process in at least three important ways: as a coolant, as a lubricant, and as a chemically reactive surface modifier. The effects of grinding fluids on surface ductility in material removal are not well known. This is primarily due to the complex, material-dependent nature of the chemical interaction between the fluid and the workpiece. Also, the thermal, lubrication, and chemical properties of the

Work Materials	Fused Silica
	Quartz
	Silicon
Wheel	4-8 μ m diamond, bronze bond
Coolants	H ₂ O

Work Materials	Fused Silica
Cutting Speed	500RPM (2.5m/s) Zerodur
Wheels	4-8 μ m diamond, bronze bond 4-8 μ m diamond, resin bond
Coolant	H ₂ O
Cutting Speed	500RPM (2.5m/s)
Infeed Rates	1.5, 7.5, 37.5 nm/rev
Total Plunge	40 μ m

Table 4.5. Microgrinding conditions used to test for the influence of grinding wheel bond on grinding ductility.

Cutright has measured the influence of the Rebinder effect (chemical surface alteration) on ductility in the diamond-bit drilling of glass [37]. From his results, a cutting fluid consisting of a 1:1 ethanol-methanol mixture produced a much more ductile surface than was produced using water as the grinding fluid.

To evaluate the extent of the influence of the grinding fluid, selected samples were ground under identical conditions, with two different fluid coolants. The specific materials and conditions used in these experiments are listed in Table 4.5.

Work Materials	Fused Silica
	Quartz
	Silicon
Wheel	4-8 μ m diamond, bronze bond
Coolants	H_2O
	1:1 Ethanol-Methanol mixture
Cutting Speed	500RPM (2.5m/s)
Infeed Rates	1.5, 7.5, 37.5 nm/rev
Total Plunge	40 μ m

Table 4.6. Microgrinding conditions used to test for the influence of grinding fluid on grinding ductility.

fluid may affect one another in myriad ways.

Cuthrell has measured the influence of the Rebinder effect (chemical surface alteration) on ductility in the diamond-bit drilling of glass [37]. From his results, a cutting fluid consisting of a 1:1 ethanol-methanol mixture produced a much more ductile surface than was produced using water as the grinding fluid.

To evaluate the extent of the influence of the grinding fluid, selected samples were ground under identical conditions, with two different flood coolants. The specific materials and conditions used in these experiments are listed in Table 4.6.

4.8 Measurement of Material Properties

To evaluate the critical-depth-of-cut model, the relevant material properties of the sample materials must be measured *at the sample surface*. These values may or may not coincide with the bulk or nominal properties of the substance, but they are the ones needed to study the localized, small scale material removal process. The model depends on three properties: K_c , H , and E , to quantify the ease of material deformation through fracture, plastic deformation, and elastic deformation, respectively.

While there are many macroscopic techniques for determination of these values, there is growing evidence that the properties of the material surface, particularly a previously machined surface, can differ dramatically from the bulk properties. Fortunately, these surface properties can be estimated using micro-indentation techniques.

4.8.1 Hardness Measurement

Hardness measurements can be made directly, using either Vickers or Knoop microhardness indenters. Using a diamond pyramid, hardness can be assessed in terms of the load applied to the diamond divided by the projected cross sectional area of the indentation. This will produce hardness values that differ slightly from Vickers hardness values, which are measured using the actual surface area of the indentation, rather than its projected area. For ceramic materials, microhardnesses

in the range of 5 – 25 GPa are typical. For each sample, hardness was measured at loads from 0.2 N to 2 N. While lower loads would be more appropriate for measurement of the surface layer properties relevant to microgrinding, the error in optical measurement of the indentation diagonal becomes unacceptably large for such small indentation loads.

Ideally, hardness is linearly related to indentation load and follows an inverse square relationship with the indentation diagonal. For materials that work harden, however, hardness tends to increase with a decreasing indentation scale (smaller loads or smaller indentation diagonals). It is important, then, to measure hardness *as a function of the indentation size*.

Empirically, it has been determined that for brittle materials: For low loads, The Meyer's hardness relationship described in Chapter 2 can be used. That is, a power law of the form $H = Kd^n$ can be expected, where H is hardness and d is the indentation diagonal. By measuring hardness data over a range of indentation diagonals, the exponent (n) can be determined. For $n = 0.5$, hardness is independent of load. For $n < 0.5$, the material hardness increases with a decreasing load (hard surface layer), while for $n > 0.5$, the opposite is true (soft surface layer). As a standardization method, the interpolated value of hardness for a 10 μm indentation diameter was chosen for each material. This value, designated H_{10} , was used in determining the critical-depth-of-cut.

4.8.2 Elastic Modulus Measurement

Measurement of elastic modulus in the surface layer can be performed indirectly by determination of the E/H ratio for a given material. This ratio, in turn, can be derived from optical examination of a simple Knoop indentation [32]. Knoop diamond indenters are elongated pyramids, with the long diagonal measuring about seven times the length of the shorter diagonal. For a perfectly plastic indentation, the indentation will have the same aspect ratio as the diamond indenter. If the material has a large *elastic* modulus, however, there will be a certain amount of elastic recovery upon removal of the indenter. Because of the structural rigidity of the surrounding material, the short diagonal will experience more elastic recovery than the long diagonal. The aspect ratio of the indentation, therefore, will contain information about the relative influence of plasticity (H) versus the elasticity (E).

Empirically, it has been determined that for brittle materials:

$$\frac{b'}{a'} = \frac{b}{a} - \alpha \frac{H}{E} \quad (4.37)$$

where:

$\frac{b}{a}$ = nominal ratio of short to long diagonal (0.15)

$\frac{b'}{a'}$ = indentation ratio of short to long diagonal

α = dimensionless constant (~ 0.45)

Since this quantity is relatively constant for a wide range of loads on many brittle materials, it can be related to the material toughness. Specifically, a measure of the

Therefore, by measuring the aspect ratio of the indentation, a value of E/H can be obtained.

$$K_I = \left(\frac{E}{H} \right)^{1/2} \frac{P}{c^{3/2}} \quad (4.39)$$

4.8.3 Critical Stress Intensity Factor Measurement

Estimation of material toughness in the form of the Mode I critical stress intensity factor K_{Ic} is also possible through indentation techniques. For this measurement, the load on the diamond indenter is increased to levels that cause radial cracks to extend from the corners of the indentation. These cracks extend beneath the indentation in a semicircular arc. Therefore, from tip to diametrically opposite tip, the length of the cracks visible on the surface corresponds to the diameter of the radial cracks. It has been shown that for many brittle materials:

$$\frac{P}{c^{3/2}} \approx \text{Constant} \quad (4.38)$$

Sample preparation for indentation testing is an important consideration. To where:

measure properties that are relevant to the grinding process, it is appropriate that the surfaces that are measured are similar to the surfaces that are ground. Grinding

P = indentation load, N
in the ductile regime results in a highly deformed surface layer, typically with a compressive residual stress extending somewhat below the surface. To emulate

these conditions, the material samples undergoing indentation testing were first polished to produce a smooth, machined surface. This preparation included five minutes of polishing with each of four diamond pastes, ranging from 15 μm to 1 μm

Since this quantity is relatively constant for a wide range of loads on many brittle materials, it can be related to the material toughness. Specifically, a measure of the critical stress intensity factor is obtained from the formula:

$$K_c = \left(\frac{E}{H} \right)^{\frac{1}{2}} \frac{P}{c^{\frac{3}{2}}} \quad (4.39)$$

It is important to note that this value of K_c may well differ from that obtained in the standard macroscopic tests for determining K_c (four point bend tests). Indeed, at the material surface, a crack propagating in response to an applied stress might well be expected to differ from a crack propagating in the bulk material.

A summary of the techniques for determining H , E/H , and K_c is perhaps most easily achieved through illustration. Figure 4.2 illustrates examples of three different indentations and the equations that are used to derive the corresponding physical surface constants.

4.8.4 Error Sources in Material Property Measurement

Sample preparation for indentation testing is an important consideration. To measure properties that are relevant to the grinding process, it is appropriate that the surfaces that are measured are similar to the surfaces that are ground. Grinding in the ductile regime results in a highly deformed surface layer, typically with a compressive residual stress extending somewhat below the surface. To emulate these conditions, the material samples undergoing indentation testing were first polished to produce a smooth, machined surface. This preparation included five minutes of polishing with each of four diamond pastes, ranging from $15\mu\text{m}$ to $1\mu\text{m}$

$$P_n = H\beta\pi a^2$$

$$\frac{b'}{a'} = \frac{b}{a} - \alpha \frac{H}{E}$$

$$K_c = \left(\frac{E}{H}\right)^{\frac{1}{2}} \frac{P}{c^{\frac{3}{2}}}$$

Figure 4.2. Photomicrographs of the three types of indentations used for property evaluation.

If an error (ϵ) is assumed to exist in the measurement of b' , then the percentage

in grit size. For some of the materials, notably the amorphous glasses, this residual compressive stress introduces a significant amount of scatter in the radial crack length data. To obtain repeatable values of K_c , these glass samples had to be treated with a one hour etch in 5% Hydrofluoric acid after polishing. The result was to remove the compressive layer introduced by polishing, allowing repeatable radial crack formation.

The magnitude of the errors made in the evaluation of these material properties must be estimated. Major sources for these errors arise from indentation diagonal measurement, crack length measurement, and the influence of surface treatment on crack propagation.

For all measurements of the indentation diagonals, the optics of the micro-indentation microscope and the ambiguity of the edge shape of the indentation allow no better than $\pm 0.25\mu m$ resolution. Since this is the only real source of error in hardness measurement, the error in hardness values with diagonals measuring $10\mu m$ or larger is $< 2.5\%$. The smallest indentation measured was $2.8\mu m$, which would have an estimated measurement error of 9% .

Errors in measurement of the indentation diagonals can have a greater effect on E/H determination than on hardness determination. Recalling Equation 4.37:

$$\frac{b'}{a'} = \frac{b}{a} - \alpha \frac{H}{E}$$

If an error (ϵ) is assumed to exist in the measurement of b' , then the percentage

error in the H/E value is given by:

$$\%Error = \frac{100\epsilon}{\alpha a'} \left(\frac{E}{H} \right) \quad (4.40)$$

A typical value of a' is $50\mu m$. If ϵ is assumed to be $0.25\mu m$, Equation 4.40 reduces to:

$$\%Error = 1.1 \left(\frac{E}{H} \right) \quad (4.41)$$

For the materials used in this study, E/H ranges from 5 to 30, producing error margins from 6% to 35%. Therefore indentation measurements of E/H are likely to measurement errors, especially for materials with large ratios of E/H .

Since the crack lengths measured for determination of K_c values are generally longer than $30\mu m$, the measurement errors from this source can be maintained below 2%. Unfortunately, the strong dependence of crack propagation on localized surface morphology is enough to cause significant scatter in the "load versus crack length" data. Chief among these local influences is the stress state in the surface region, which not only affects the randomness of the data, but can also influence the mean value of crack length for a given load (i.e. the measured toughness). On the other hand, if these machining residual stresses are eliminated before indentation to provide more uniform cracks (e.g. by etching the surface), a change in the measured stress-intensity factor may be seen. The reason for this difference in measured crack length can be illustrated using an elastic-plastic analysis of the crack-propagating tensile stresses that are generated beneath a sharp indenter. The driving forces for radial crack propagation can be compared for surfaces with and without a pre-

existing machined surface layer by examining the magnitude of these tensile residual stresses.

A 2-D numerical finite element program (NIKE2D, updated Lagrangian elastic-plastic model) was used to study the effect of compressive surface stresses on the residual tensile stresses experienced in indentation. While the analogy between this finite element problem and the actual conditions of indentation are strained by the two dimensional nature of the model, some insight can nonetheless be gained from this exercise.

Two cases were run using this numerical code. In Case I, indentation of a stress-free surface is modelled, while Case II models the indentation of a machined surface. For Case I, a diamond indenting wedge was forced into the surface of a hard workpiece, which is modelled to have a work hardening modulus of 1% of the elastic modulus for stresses larger than the yield stress. The base of the workpiece was fixed, and a displacement of the diamond was prescribed. Following this indentation, the diamond was retracted, leaving a hemispherical zone of residual compression and a subsurface region of residual tension. This residual tensile stress is the driving force for the propagation of both radial and lateral cracks, the former of which is to be used as a measurement of K_{IC} .

For Case II, a rounded diamond grain was dragged across the workpiece surface before the indentation. Figure 4.3 shows the resulting plastically deformed zone. This mechanical deformation of the surface instilled a compressive stress into the

surface layer before the indentation was begun. Following this plastic deformation of the workpiece surface, an indentation exactly similar to Case I was performed. The tensile residual stresses arising from the indentation in Case II are somewhat less than that for the Case I, even though the same depth of indentation is prescribed for the indenter. Contours of the maximum principle stresses in the indented workpiece are illustrated for both cases in Figure 4.4. Because of the plastic working of the surface, the *tensile residual stresses* that nucleate residual cracks are less in Case II than that of Case I. Also contributing to the diminished size of the radial cracks expected on the machined surface are the compressive surface stresses seen at the surface in Case II. These surface stresses will inhibit cracks generated in the subsurface region from propagating to the material surface. Since K_c depends on a measure of cracks at the material surface, it is clear that the pre-machined surface will result in larger measured K_c values than the unmachined (or etched) surface.

It has been pointed out in the preceeding section that there may be significant scatter, and errors in the property measurements made by indentation testing. The utility of these measurements is further compromised by their possible dependence on temperature and environment. Glass, for example, has a much lower toughness in the presence of water than in other lubricant environments [36]. Toughened zirconia, on the other hand, has a toughness that is strongly dependent on the length scale of interest (i.e. it tends to become less tough with a decreasing scale of indentation). Such factors will, no doubt, influence the grinding ductility, yet

CONTOURS OF MAXIMUM PRINCIPAL STRESS

CONTOURS OF EFFECTIVE PLASTIC STRAIN

Figure 4.3. Plastically Deformed Surface

CONTOURS OF MAXIMUM PRINCIPAL STRESS

Figure 4.4. Top: Free Surface Indentation. Bottom: Machined Surface Indentation

they are difficult to incorporate into the model in any way other than to be aware of their influence.

With this caveat, calculation of the critical-depth-of-cut, d_c , contains errors of up to 50%, based on measurement standard deviations and estimated systematic errors. Nonetheless, for a broad range of materials, this value should remain indicative of the ductility of the material as related to grinding surface finish.

4.9 Quantification of Grinding Ductility

After grinding a given hard material under a specified set of machining conditions, a critical task remains: measurement of the amount of fracture on the ground surface. No quantitative standards, and few qualitative guidelines exist for such an evaluation. Traditionally, the peak or average roughness of the ground component is taken as an indicator of the quality of its surface. While this is a reasonable approach (in general, fracture produces a rougher surface than plastic flow), it is not easily accomplished for samples ground on the PEGASUS apparatus. Because there is no cross-slide on this machine, the ground surface replicates the profile of the grinding wheel. This wheel is trued along its *circumference* to $< 125\text{nm}$, but it is not *radially* flat, and the resulting impression on the workpiece consists of a series of grooves in a direction parallel to the cutting direction.

The unusual surface generated by this plunge grinder thus exhibits an extremely small roughness (characteristic of the true surface finish) when measured in a direction parallel to the grinding grooves, and a relatively large roughness (characteristic

of the grinding wheel's radial profile) when measured perpendicular to the grinding grooves. Though profile tracing along the grinding grooves appears to be a promising technique of determining surface quality, it represents a considerable alignment problem. Deviations in parallelism of even a fraction of a degree can cause significant errors in roughness measurement. Compounding this problem is the fact that the grinding grooves themselves are not straight; they follow an arc having radius of curvature of 5cm.

In addition to the difficulties inherent in measuring the roughness of the ground samples, there is a more fundamental complication: roughness may not correlate with ductility in a one-to-one relationship. The plowing that often accompanies plastic flow, for example, will result in irregular contours from pileup of plowed material at the sides of a groove. This would register as an increased roughness, but does not represent increased brittleness or fracture on the surface.

Perhaps the most straightforward method of determining surface ductility is by direct observation of the machined surface using a scanning electron microscope. The advantage of this method is that it allows direct observation and interpretation of surface quality. Interpretation, however, is a two-edged sword: with the ability to directly image the surface comes the subjectivity of the observer in assessing surface "quality."

Distinguishing one surface from another in terms of the amount of brittle damage on each demands a continual assessment of the features that comprise brittleness

and those that comprise ductility. This assessment of features is the source of uncertainty in ductility measurement by direct surface imaging.

To *quantify* grinding ductility, the relative "amount" of fracture damage on the surface can be measured. In its most simplistic form, this measurement can be of the area fraction of ductile surface (or brittle surface). Since the *mechanisms* of fracture and plasticity are fundamentally different, their surface presence can be viewed as a problem of distinguishing two mutually exclusive phases: the area fraction of brittle surface added to the area fraction of ductile surface is equal to one. Put another way, a distinct boundary is assumed to exist between ductile surface regions and brittle surface regions. While the actual surface image may not reveal such sharp boundaries between such regions of plasticity and fracture, the presumption of this two-phase surface is the first vital step in the quantification of surface ductility.

The characteristic of grinding ductility is then numerically represented by the areal fraction of the ductility of the ground surface. The three tasks involved in determining this areal fraction are:

- Distinguishing between ductile and brittle surface regions
- Determination of the appropriate image magnification
- Measurement of the areal fraction

As mentioned previously, the first of these tasks is an issue of interpretation; for-

Unfortunately the differences between fractured regions and plastically deformed regions on the surface are usually obvious. Fracture results in randomly oriented, prominent, jagged edges, while plastic grooving leaves smooth, parallel lines running in the direction of grinding.

Adding to the ease of detection is an artifact of electron microscopy: sharp edges (characteristic of fracture) tend to build up an electrostatic charge, thus causing the fractured areas to outline themselves in the SEM image with a bright emission of secondary electrons.

Determination of the magnification of interest is a relatively straightforward task. The procedure is to find a magnification that is high enough to allow easy distinction of the two types of surface features, while low enough to permit a statistically random view of the surface. Conditions for this appropriate magnification are thus led by the approximate size of the plastic and brittle regions. Observations of many types of material under many machining conditions indicate a characteristic dimension of fracture zone that varies from $\sim 1\mu m$ to $\sim 10\mu m$. To get several of these features into a given field of view while ensuring resolution of at least $1\mu m$, magnifications from 2000X to 5000X are appropriate.

Upon determining an appropriate magnification, an SEM micro-photograph of the surface is taken. The actual measurement of areal fractions of the different surface regimes is accomplished through a point-counting technique. This involves tabulating the phase (ductile or brittle) found beneath each of a specified number

(N) of randomly oriented points placed over the surface image.

Because the fracture zones are *themselves* randomly distributed, a regular grid of points can be placed over the image without any loss in the randomness of the data. The areal fraction of each phase is simply the fraction of the total number of points tabulated for that phase. Measurement of areal fraction is independent of the magnification of the photograph. Thus, as long as the image is representative of the entire surface, the "grid-counting" method provides a straightforward method for determining the amount of fracture on a ground surface.

The *numerical* measurement errors in the areal determination by the the grid-count method are easily evaluated. Because this is a counting process, the sample distribution will be Poisson rather than Gaussian, hence the standard deviation of N counts is $\sqrt{N}$. For example, if there are 25 grid points corresponding to fracture regions, out of a total of 100 points counted, then the area fraction of fracture phase is 0.25 ± 0.05 .

Determining the number of grid points to count for a given photograph represents a tradeoff between measurement errors and redundancy. While it is appealing to oversample and thereby reduce the magnitude of the counting errors, these errors are only one component of the total measurement error. Variability in the surface itself (i.e. the *representativeness* of the selected photograph) is the other source of error. A prudent choice of the number of grid points will ensure that the errors due to grid counting are the same order of magnitude as the errors due to the limited

size and resolution of the image.

A grid size of 100 points (10x10) was chosen to evaluate the surface fracture regions. A magnification of 5000X was used for the photographs, providing a $17\mu\text{m}$ x $22.5\mu\text{m}$ image size.

Figure 4.5 illustrates an SEM photograph of a fused silica surface that was ground on PEGASUS. The features on this photograph indicate that a large number of small fracture events occurred during the machining process. These fracture regions exist on a background of generally ductile surface. At this magnification, the fracture surfaces are relatively easy to resolve. In addition, they are evenly distributed over the image surface, allowing a measure of confidence in the representative nature of this image. A grid count on this photograph using 100 points puts the area fracture damage at 10%. Repeated measurements made on six different regions of this fused silica surface indicate an *average* damage area of 11.3%, with a standard deviation of 2.4%. The *measurement* error due to counting is 3.2% ($\sqrt{10}$). From the similarity of these measurement errors, this choice of grid points and magnification appears well suited for the measurement of fracture damage area, at least on fused silica. Of course, these measurements could be made automatically by an image analysis device. Using one such device and a strongly enhanced image contrast, as well as an edge discriminator, a portion of the image of Figure 4.5 was converted into a two color computer image, where the black regions correspond to fracture damage and the white regions correspond to plastically ground surface

Figure 4.5. A fused silica surface with 10% fracture damage, as measured by grid counting

(Figure 4.6). For this image, a fractional area of 8.72% of fractured surface was obtained by the computer, agreeing within one standard deviation with the measured value. The promise of this type of automated image analysis is clear. Nonetheless, the distinction between fracture and plastic flow is not immediately distinguishable to the image processor: it must be defined using the appropriate image enhancement techniques. Specific edge thresholds and gray levels corresponding to one regime or the other may vary from one material to the next, and indeed from one image to the next on the same part. Hence, while automation of the determination of ground surface fracture is possible, it is not straightforward. For this research, a manual grid counting method was adopted utilizing a 100-point mesh and a 2000X-5000X image magnification.

4.10 Residual Stress Measurement

Beyond their influence on dimensional tolerances and surface finish, the ductility of the grinding process also affects the existence and extent of subsurface damage and residual stresses in the brittle workpiece material. The extent of the subsurface damage of a ground brittle material is difficult to assess. Classic work by Rabinowitz predicts a depth of damage that is approximately equal to the depth of material removed in a grinding pass. Empirical studies have produced conflicting results: subsurface damage depth on ground hard materials was related to the rate of material removal by a power law in one study and was shown to be unrelated to material removal rate in another. For ductile-regime grinding, preliminary measure-

Image width = 15.3012048 um.
 Reference area = 176.977791 Sq.um.

Fractional area = 8.72 %

Figure 4.6. Computer enhanced image of the photograph in the previous figure.

ments made by the National Physical Laboratory showed no detectable subsurface damage in both glass and quartz samples [45]. Techniques for reliable measurement of subsurface damage are at present not firmly established. Procedures vary from direct measurement via x-ray topography to indirect statistical measurements of fracture toughness.

Residual stresses in a machined surface are caused by inhomogeneous plastic deformation, non-uniform heating or cooling, or volumetric changes via phase transformation. Mechanically induced residual stresses in grinding are usually compressive, while thermally induced stresses are predominantly tensile, reflecting a flux of heat from the cutting zone into the workpiece [6]. Stresses due to volumetric changes are compressive if the phase transformation causes a surface volume increase, and tensile otherwise [39].

Residual stress measurement is in an evolutionary stage in its development. In spite of promising recent attempts to indirectly measure stress via Raman spectroscopy, the currently accepted measurement technique is still the *curvature* method. This method employs a thin, flat, polished rectangular specimen that is ground on one side. The ground surface is progressively etched, and bending in the sample upon relief of residual grinding stresses is measured in two orthogonal directions. This bending is then correlated mathematically to residual stress at progressive depths of etch penetration. Using this technique, Chandrasekar [6] shows that in grinding ceramics, a compressive residual stress state exists near the surface, while

a zone of residual tension exists deeper into the material (figure 4.7). This general dependence of residual stress on depth characterizes many different brittle materials, and the magnitude of the maximum tensile and compressive residual stresses increase with the toughness of the ground material.

Raman Spectroscopy provides an optical, nondestructive method for the determination of residual stresses in the near surface region of crystalline materials. Stresses in the crystal lattice cause a shift in the peak spectral frequency of the light scattered from an incident laser beam. The direction of the spectral peak shift is an indication of the sign of the residual stresses (i.e. tensile or compressive), and its magnitude scales directly with the magnitude of the stress. This phenomenon, known as the Raman Effect, has been utilized to measure surface stress in various crystalline materials.

Presumably, the purely ductile grinding process will produce higher residual stresses as a result of the plastic deformation at the material surface. Conversely, the brittle grinding process removes most of the plastically deformed zone upon propagation and intersection of lateral cracks. This will result in a lower residual stress level. In the conventional grinding of metals, the plastically deformed layer causes compressive residual stresses at the material surface. The same behavior is expected from plastically ground hard materials.

The measurement of residual stresses in germanium or silicon by Raman Spectroscopy was carried out for samples ground with various infeed rates, representing

Figure 4.7. Residual stress in a Ni-Zn ferrite surface ground with a 400 grain size resin bonded diamond grinding wheel [6].

a limited range of grinding ductility.

4.11 Acoustic Emission Monitoring of Grinding Ductility

For many hard materials a transition from the brittle to the ductile grinding regime can be accomplished on PEGASUS via control of the grinding feed rate, with all other machining variables held constant. The particular feed rate corresponding to this transition, however, is a function of many other machining variables and material properties. Therefore, open-loop control of the grinding ductility via an enforced infeed rate is difficult. In fact, the "transition" infeed rate itself might vary during a single grinding cycle due to gradual changes in either the material or the grinding conditions. If the ductile regime of grinding is to be ensured, it must first be *measured* in real-time for use in an active closed-loop control of the feed rate.

The measurement of Acoustic Emissions (AE) shows some promise for an in-process method of determining the ductility of the grinding process. Acoustic emissions are elastic waves generated by abrupt localized changes in the stress in a solid. These waves then travel to the surface of the solid, where they are detected as acceleration signals by a piezoelectric transducer. The frequency of AE signals is in the range of 100kHz to 2MHz , above the frequency range of typical mechanical vibrations. The propagation of cracks creates detectable AE signals, as does intense plastic shear. AE monitoring has been used extensively as a laboratory tool to investigate the mechanics of deformation and fracture during mechanical testing [40].

The quantity that is measured by most commercially available AE instruments is the amplitude of the AE signals and the total number of bursts occurring at or above a particular amplitude per unit of time. In a material processing application such as grinding, the AE signals are due to either fracture or plastic flow. The amplitude of AE signals due to fracture is much larger than the amplitude of AE signals due to plastic flow. This amplitude difference has been utilized to effectively monitor chip breaking in single point turning operations at the National Bureau of Standards [41]. In that study the production of continuous plastic chips resulted in a continuous, low-level AE signal emanating from the shear zone and the chip/tool interface. Upon fracture of the chip, a high amplitude AE burst was observed, indicating effective chip breaking. The ratio of amplitudes between the plastic and brittle mechanisms was found to be material dependent, with a more marked amplitude difference noted for harder materials. In another study using AE signals, the effect of coolant on surface ductility in hard materials was examined [42]. It was found that the AE signals associated with fracture during the machining of brittle materials can be used as a sensitive diagnostic tool for monitoring surface ductility. Figures 4.8 and 4.9 provide an impressive illustration of this sensitivity.

In figure 4.8 are SEM photographs of two silicon surfaces that were diamond drilled under the same machining conditions, except that the sample on the left was cut in a CCl_4 environment that resulted in a more ductile material removal while the sample on the right was cut in an embrittling H_2O environment that resulted in a

Figure 4.8. SEM photograph of Silicon. Left: Machined in CCl_4 , ductile regime. Right: Machined in H_2O , brittle regime.

Figure 4.9. Acoustic emission signal for previous SEM photo. Number of events vs. AE amplitude

more brittle material removal [43]. Figure 4.9 shows the AE amplitude distributions recorded as the two samples were machined. Clearly the amplitude peaks for the two machining regimes are quite distinct. In fact, Cuthrell notes that the AE amplitude demonstrates a near-linear dependence on the wear rate [43]. It might be expected that wear rate would increase with the brittleness of the machining process, because material removal by lateral crack propagation removes considerably more material than that removed by plastic grooving at the same depth of cut.

A schematic of the equipment used for this AE spectrum analysis is shown in Figure 4.10.

In spite of the strong AE amplitude difference between plastic and brittle machining the *absolute* amplitude of the AE signal from either regime is a function of the specific material properties and cutting conditions. Structural vibrations of the machine will not influence the AE signal, since the AE signal lies within the frequency band from 100kHz to 2MHz , well above most structural natural frequencies. The AE amplitude signal, then, is a measure of the intensity of the *machining* activity. It would perhaps be useful to seek a less variable quantity than AE amplitude to characterize the ductile to brittle transition. To this end, the use of AE *frequency* signals could be explored as a more reliable process control signal. It might be expected that the frequency of AE signals will show a dependence on the regime of material removal, with fracture occurring at one frequency and plastic flow at another. If this were the case, then a pair of band-limited filters could provide a small window into the frequency domain. Energy in the frequency window correlated to ductility could thus be continually compared to energy in the

frequency window correlated to fracture. Using such a scheme, the plasticity of the material removal regime could thus be monitored and controlled.

For the current work, acoustic emission signals were monitored for both germanium and soda lime glass samples, ground over a broad range of infeed rates. The power spectrum of the AE signals was recorded to afford a view of both the amplitude and frequency content of the acoustic emissions.

A schematic of the equipment used for this AE spectrum analysis is shown in Figure 4.10.

Data from the AE sensor was collected by a digital storage oscilloscope in the form of a 1024 point time series, with a sampling rate of 200ns/sample seconds. This sampling will provide a spectrum from $0-2.5\text{MHz}$, with a frequency resolution of 5KHz . To prevent aliasing, the output of the AE transducer was low pass filtered at 1MHz . The digital data was then subjected to a fast Fourier transform, using a Hanning window to reduce spectral leakage. Six data sets were collected for each grinding condition, and their corresponding spectra were averaged. An example of the resulting spectrum is illustrated in Figure 4.11 for germanium ground at a 3nm/rev infeed rate. The peak in this spectrum presumably relates to the acoustic emission activity due to local crack propagation in the germanium. A measure of the spectral power surrounding the peak (e.g. from 300kHz to 500kHz) is indicative of the acoustic emission power if the grinding process.

Figure 4.10. Schematic of the measurement system used to record AE spectra.

4.12 Summary of Experiments

An experimental plan has been devised to test the influence of machining conditions and material properties on the ductility of the grinding process. Specifically, the experiments test the effect of the grinding infeed rate on the grinding ductility

Figure 4.11. Acoustic Emission spectrum for germanium microgrinding

4.12 Summary of Experiments

An experimental plan has been devised to test the influence of machining conditions and material properties on the ductility of the grinding process. Specifically, the experiments test the effect of the grinding infeed rate on the grinding ductility to validate the hypothesis that ductility in microgrinding of hard materials is fundamentally linked to a decrease in the grinding infeed rate. Implicit in this hypothesis is an independence of the grinding ductility from the speed of material removal. Microgrinding tests have been specified to test this independence.

It has been postulated that the "grinding ductility" of a given material can be determined from the material properties (i.e. K_c , H , and E). Such a critical-depth-of-cut model has been used successfully to predict fracture depths in static indentation. Testing of this model requires measurement of these properties at the material surface. Experimental micro-indentation testing techniques have been proposed as a means of estimating these properties. With these values, the critical-depth-of-cut model can be put to the test. If necessary, this model can be modified to include the material properties important to grinding that may be ignored for indentation (i.e. thermal material properties).

Other machining variables likely to influence the grinding ductility are the bond matrix stiffness and the grinding fluid. Exploratory tests have been proposed to investigate the extent of this influence.

A quantitative method for determining the grinding ductility has been estab-

lished, in which the extent of fracture on the ground surface is measured. This numerical result is indicative of the grinding regime, and can be used to compare the two material removal mechanisms (fracture vs. plastic flow) that are active in micro-grinding. Possible automation techniques for this manual grid-counting method have been explored.

A possible correlation between grinding ductility and near-surface residual stresses can be tested by Raman Spectroscopy for certain crystalline materials. It is proposed that these stresses can be measured to ascertain the dependence of residual stresses on the grinding regime.

Finally, an in-process technique for measuring acoustic emission signals has been proposed as part of the experimental plan. It is hoped that the AE signals will correlate, in amplitude and/or frequency, to the ductility of the material removal process.

From these experiments, it is expected that a physical understanding of the micromechanics and material deformation processes associated with microgrinding will begin to emerge. The results should pinpoint the important material properties and machine parameters that influence the material removal regime in the largely unexplored microgrinding process.

5 Results and Discussion

5.1 The Influence of Grinding Infeed Rate on Grinding Ductility

The grinding experiments to test the influence of infeed rate on ductility demonstrate a pronounced transition from brittle material removal to ductile material removal for fused silica glass. The transition occurs between material removal rates from 1×10^{-4} to $2 \times 10^{-3} \text{ mm}^3/\text{mm s}$, a range contained within the microgrinding gap. A more appropriate variable than material removal rate for defining this transition is the workpiece infeed per revolution of the grinding wheel. The infeed per revolution is always smaller than the repeatable axial runout of the wheel for PEGASUS, so this measurement approach makes sense: the highest spot on the wheel does most of the cutting, contacting the workpiece once per revolution of the grinding spindle. It can be assumed that the equivalent "chip-thickness" for the grinding process is equal to the amount of material removed by this high spot on the wheel per revolution. Viewed in this way, the brittle-ductile transition can be associated with an equivalent chip thickness, or a grinding infeed per wheel revolution. An important advantage of using this grinding infeed rate as a machining parameter is that it allows independent assessment of the effects of the cutting depth and the cutting speed, whereas the material removal rate couples these variables into a single parameter.

Figure 5.1 is a graph of the measured ductile-brittle transition for fused silica ground on the PEGASUS apparatus at various infeed rates, with all other machining parameters fixed. The highest infeed rate produces an almost entirely brittle surface (99% fracture) on this hard, amorphous glass. This grinding behavior is characteristic of a glass surface machined on a conventional diamond grinding apparatus. For the lowest infeed rate plotted in Figure 5.1, there is only 5% fracture on the ground surface, approaching the surface topography typically encountered in polishing. The asymptotic curve overlaying the data illustrates the predicted form of the relationship between surface fracture and grinding infeed. At very high infeed rates, the grinding process will proceed predominately through crack propagation and surface fracture. At very low infeed rates, the nature of material removal will be ductile, producing a plastically deformed surface with little or no evidence of fracture. Between these two asymptotes, there is a transition from a brittle material removal mechanism to a ductile one. The *existence* of this transition between material removal regimes as a function of infeed rate for this material demonstrates the validity of the fundamental hypothesis of ductile-regime grinding: for a small enough scale of material removal, the mechanism of removal for brittle materials will become one of plastic deformation.

Figure 5.1. Fused silica brittle-ductile transition.

Once the form of the asymptotic ductile-brittle transition curve has been established, what remains is to determine the relationship between the transition and the grinding infeed rate. Also of interest is the relative *steepness* of the transition

Fused Silica Brittle-Ductile Transition

Figure 5.1. Fused silica brittle-ductile transition.

curve. For fused silica, it is clear from Figure 5.1 that the transition takes place between infeed rates from $\sim 1\text{nm/rev}$ to $\sim 80\text{nm/rev}$, a range of nearly two orders of magnitude. The infeed rate corresponding to the center of the transition occurs at $\sim 20\text{nm/rev}$. Figure 5.2 illustrates the brittle-ductile transition through a series of SEM photographs (5000X). These photographs are of fused silica samples ground at infeed rates from 37.5nm/rev to 1.5nm/rev . A significant improvement in surface quality can be seen for progressively smaller infeed rates. From grid counting, these surfaces represent 63% (top), 19% (middle), and 5% (bottom) surface fracture .

5.2 Evaluation of the Critical-Depth-of-Cut Model

For the expanded matrix of hard materials, each sample was ground using three different infeed rates, and the resulting surface fracture was quantified with the grid counting method. The results are graphically illustrated in Figure 5.3, a plot of grinding infeed versus percentage of surface fracture. The uppermost curves on this graph represent the more brittle materials. These materials have a lower transition point than the more ductile materials, which are located at the bottom of the graph. It is important to notice that even the materials exhibiting low percentages of surface fracture show an *increase* in surface fracture with higher infeed rates over this range of conditions. The areal percentage of surface fracture for each material and grinding infeed rate are presented in Table 5.1. The measurement error in the “% surface fracture” value is approximately the square root of the measured value.

Figure 5.2. microground fused silica surfaces. Top: 37.5nm/rev infeed rate.

Middle: 7.5nm/rev infeed rate. Bottom: 1.5nm/rev infeed rate.

Figure 5.3. Microgrinding infeed rate versus the resulting percentage of surface fracture for 10 brittle materials.

Material	Infeed Rate		
	1.5nm/rev	7.5nm/rev	37.5nm/rev
Alumina	7	23	49
Soda-Lime Glass	7	15	41
Fused Silica	5	19	63
Zerodur	3	8	25
Quartz	1	6	29
Zirconia	1	4	9
Toughened Zirconia	0	3	7
Germanium	0	2	7
Silicon	2	0	6
Silicon carbide	1	2	3

Table 5.1. Percentage surface fracture for various grinding infeed rates and materials

To test the critical-depth-of-cut model, a single value of infeed (i.e. chip thickness) characterizing the ductile-brittle transition must be assigned to each material using the data from Table 5.1. This "critical infeed" can then be compared to the critical-depth-of-cut, which is calculated from the material properties.

To determine an infeed rate characteristic of the transition, a curve can be fitted to each material, using the data from Table 5.1. Then, for each curve, a specific level of surface fracture can be arbitrarily chosen as representative of the ductile-brittle transition. The infeed corresponding to that particular percentage of surface fracture is the critical infeed rate for that material.

Because of the relatively large measurement errors inherent in the data, it is preferable to *interpolate* rather than *extrapolate* in determining the critical infeed rate. The maximum sensitivity (and thus minimum errors) for determining the critical infeed rate from a curve like that of Figure 5.1 occurs at a 50% surface fracture, where the slope of the transition is the steepest. The sensitivity is worst at either extreme of surface fracture, where small changes in the surface fracture can represent large changes in infeed rate. A reasonable tradeoff between extrapolation errors and sensitivity errors occurs at ~10% surface fracture for the data of Table 5.1. For this level of surface fracture, extrapolation is necessary for only half of the materials, and a reasonable amount of sensitivity is preserved. In addition, this value might represent a practical machining goal: reducing the surface fracture damage to 10% of the machined surface area. It is important to recall that mini-

mizing the fracture of the ground surface is the goal of ductile-regime grinding, so an assessment of critical infeed rate for low percentages of surface fracture is more useful than an assessment at higher percentages of surface fracture.

The form of the asymptotic curve depicted in Figure 5.1 can be described by the function:

$$f = \frac{1}{\pi} \tan^{-1}(a \ln(r) + b) + \frac{1}{2} \quad (5.42)$$

where:

f = Areal fraction of surface fracture

r = Grinding infeed rate

a = Parameter representing the width of the transition

b = Parameter representing the location of the transition

Fitting the data to a curve of the form of Equation 5.42 produces large error in the evaluation of the transition, because only a small portion of the overall curve is represented by the data for each material. A more reasonable technique for determining the critical grinding infeed rate is to fit the data from Table 5.1 to a simple power law of the form:

$$f = a r^n \quad (5.43)$$

This fit works well for those materials requiring interpolation or short range extrapolation. For the two most ductile materials, however, the errors introduced by the power law fit are large, due to extrapolation uncertainty. Because the data for these two materials lie along the linear lower asymptote of the transition curve, a linear fit was used to extrapolate to the critical infeed rate for both silicon and silicon carbide.

The results of these curve fitting operations for determination of the grinding infeed rate corresponding to 10% surface fracture are summarized in Table 5.2

These values for critical grinding infeed rate can be used to check the critical-depth-of-cut model. For this validation of the model, however, the material properties K_c , H , and E must first be determined from the indentation tests.

5.3 Material Property Evaluation

For each material, at least ten hardness values were measured using a range of indentation loads from $0.2N$ to $2.0N$. Figure 5.4 is a graph of these results for fused silica (other materials produced similar results, which are included in Appendix A). As described in Chapter 4, microhardness is dependent on the load or, equivalently, the length of the indentation diagonal. It has been postulated that the hardness varies in a power law relationship with the length indentation diagonal. Namely:

$$H \propto a^n \quad (5.44)$$

Material	Critical Infeed for 10% Surface Fracture, nm/rev
Alumina	2.5
Soda-Lime Glass	3.1
Fused Silica	3.6
Zerodur	9.7
Quartz	13
Zirconia	37
Toughened Zirconia	46
Germanium	54
Silicon	67
Silicon carbide	180

Table 5.2. Calculated critical grinding infeed rates for various brittle materials

Figure 5.4. Microhardness data for fused silica

The dimensional scale of microgrinding material removal is generally less than $1\mu\text{m}$, so indentations at or below this size would be appropriate for material property evaluation. Unfortunately, such small scale indentations are generally not possible, due to the limits of the microindentation apparatus. Nevertheless, property measurement should be done on as small a scale as possible to ensure reasonable evaluation of the surface.

Fused Silica Hardness Measurements

Figure 5.4. Microhardness data for fused silica

Knoop measurements for calculation of E/H were made with loads from 1n to 6N. Figure 5.5 is a plot of the data for fused silica. Since there is no discernable relationship between measured E/H and load, the average E/H from all of the Knoop indentations was calculated for use in the critical-depth-of-cut model. For the fused silica, this average value is 8.24. A rough estimate of the validity of this value can be obtained by multiplying the calculated E/H by the calculated microhardness

The dimensional scale of microgrinding material removal is generally less than $1\mu m$, so indentations at or below this size would be appropriate for material property evaluation. Unfortunately, such small scale indentations are generally not possible, due to the limits of the microindentation apparatus. Nevertheless, property measurement should be done on as small a scale as possible to ensure reasonable evaluation of the surface characteristics of the material.

For microhardness loads from $0.2N$ to $2.0N$, the indentation diagonal is on the order of $10\mu m$ for most hard materials. For this reason, a value of hardness corresponding to a $10\mu m$ indentation diagonal was selected as the characteristic microhardness for each material. As an example, for the fused silica data of Figure 5.4, a power law fit through the data yields a value of hardness of $11.2GPa$ for a $10\mu m$ diagonal. This reflects a microhardness that is about twice as large as the macrohardness (bulk material hardness) measured for fused silica at higher loads ($> 5N$). Similar surface hardening effects were noticed for all of the materials with power law coefficients varying from -0.22 for alumina to -0.67 for germanium.

Knoop measurements for calculation of E/H were made with loads from $1n$ to $6N$. Figure 5.5 is a plot of the data for fused silica. Since there is no discernable relationship between measured E/H and load, the average E/H from all of the Knoop indentations was calculated for use in the critical-depth-of-cut model. For the fused silica, this average value is 6.24 . A rough estimate of the validity of this value can be obtained by multiplying the calculated E/H by the calculated microhardness

Fused Silica E/H Measurements

Figure 5.5. E/H data for fused silica (from Knoop indentations)

value, and comparing the result with the nominal modulus of elasticity of fused silica. The calculated E is 73GPa , while the nominal E for fused silica is 70GPa , showing good agreement for this material (in fact, this is an exceptionally good agreement). For the materials, the average agreement between the nominal and calculated elastic modulus is 21%, confirming the general validity of this method of estimating E/H .

The most difficult quantity to measure from indentation tests is the critical stress intensity factor (K_c). Because of the sensitivity of crack propagation on the particular stress field at the surface, the calculated K_c values showed a great deal of scatter. A typical plot of K_c vs. indentation load for fused silica is illustrated in Figure 5.6. These indentations were obtained with loads from 1N to 6N .

Alumina, toughened zirconia, and soda lime glass did not exhibit radial cracks for indentations less than 5N . For toughened zirconia, K_c values were obtained for loads of 5N and 6N . For alumina, no microindentations produced cracks, and macroscopic diamond pyramid indentations were required. K_c values were finally obtained for loads from $40 - 90\text{N}$. For soda lime glass, the occurrence of radial cracks was very erratic for the polished sample. Either work hardening or uneven tempering could have been responsible for the wide spread in the measured K_c values on the samples. To reduce these errors, the soda lime glass was etched for 45 minutes in a 5% hydrofluoric acid solution. Subsequent indentation produced extremely repeatable K_c estimates, which were used in the calculation of the critical-

Fused Silica Critical Stress Intensity Factor Measurements

Figure 5.6. Critical stress intensity factor for fused silica

depth-of-cut.

Although most of the materials tested exhibit relatively constant K_c values over a broad range of indentation loads, this behavior is *not* expected for alumina or toughened zirconia. These materials exhibit toughness that *decreases* with a decreasing scale of deformation (i.e. indentation load or crack size). If the toughness or critical stress intensity factor is measured for these materials using large indentation loads, the resulting value may not be appropriate for use in the critical-depth-of-cut model. Ideally, fundamental material properties that remain invariant with loading condition and crack size would be desired for such a model, but such a property (for fracture toughness evaluation) is unavailable for alumina and toughened zirconia.

For alumina, the higher toughness associated with larger cracks can be attributed to grain-localized ligaments that stretch across the newly created surfaces behind the advancing crack tip [44]. For small cracks ($c < \text{grain size}$) the toughness of the alumina is constant, and at its minimum. For larger cracks ($c > \text{grain size}$), the "ligaments," which behave like fiber reinforcements, begin to add to the forces resisting crack growth, thus increasing the apparent K_c of the material. For very large cracks ($c \gg \text{grain size}$), the K_c values again reach a uniform level.

For toughened zirconia, the invariance of toughness with crack size is regulated by an entirely different mechanism: transformation toughening. For this material, the zirconia matrix is "stabilized" so that it contains metastable tetragonal phase particles. Upon encountering large local stresses (such as those found at a crack

tip), the tetragonal particles undergo a volume increasing phase transformation, which adds a compressive stress field to the intrinsic forces resisting crack growth. This transformation process triples the macroscopic toughness of the ceramic. The K_c value of toughened zirconia increases rapidly with an increasing crack size from that of the untoughened matrix at crack length ~ 0 , to a maximum at a crack length $\sim 200\mu m$. For cracks longer than $200\mu m$, the toughness remains largely invariant. Microgrinding requires scales of material removal closer to $0\mu m$ than to $200\mu m$. Thus, it is expected that the measurements made at higher loads will overestimate K_c for these materials, resulting in a calculated critical-depth-of-cut that is too large.

While determination of K_c values by measurement of radial crack lengths works well for most other brittle materials, it does not predict accurate values of K_c for anomalous glasses (e.g. fused silica). Fused silica exhibits a greater tendency toward plastic deformation in hydrostatic stresses than in shear. Thus, the plasticity analysis used to relate the fracture toughness (K_c) to the load (P) and the crack length (c) is largely in error. Marshall and Lawn have shown that the measurement of K_c by microindentation produces a value that is ~ 2.7 times too large. The value measured by indentation for the present study was $\sim 1.67MN m^{1/2}$. The manufacturer's specification for K_c is $0.62MN m^{1/2}$; a nominal error of a factor of 2.7. For fused silica, then, it would appear to be judicious to use the manufacturer's specified K_c value (rather than the measured value) for the purposes of evaluation

of the critical depth-of-cut.

A summary of the material properties measured by indentation testing is presented in Table 5.3. Also tabulated is the critical-depth-of-cut, d_c , that is calculated from these properties..

By comparing the grinding ductility measurements from Table 5.3 with the critical-depth-of-cut calculated in Table 5.2, the validity of the critical-depth-of-cut model can be assessed. Figure 5.7 is a graph of the results for the ten materials. The ordinate is representative of the actual grinding ductility of the material, whereas the abscissa is representative of the grinding ductility calculated from the indentation properties. As expected, the calculated critical depths for alumina and toughened zirconia have been overestimated with respect to the grinding ductility. In fact, the toughened zirconia is remarkably like untoughened zirconia in its grinding ductility. This would be expected for crack lengths $< 1\mu m$, where the toughness of the two materials is nearly identical. If the measured K_{Ic} for toughened zirconia is replaced by the K_{Ic} measured for the untoughened zirconia sample, a new critical-depth-of-cut can be calculated for the tough zirconia. This modified critical-depth-of-cut value falls directly in line with the data for the other brittle materials, as is illustrated in Figure 5.8.

Aside from alumina and toughened zirconia, the data appears to verify a linear dependence relationship between the calculated critical-depth-of-cut and the grinding infeed for 10% surface fracture.

Material	H_{10}	E/H	K_c	d_c
	GPa		MPa m ^{1/2}	nm
Alumina	15.2	10.9	4.77	1070
Soda-Lime Glass	6.43	9.82	0.34	27
Fused Silica	11.7	6.24	0.62	18
Zerodur	11.2	7.20	1.14	75
Quartz	14.6	10.6	1.46	106
Zirconia	15.3	14.1	2.74	452
Toughened Zirconia	13.0	17.0	7.23	5260
Germanium	9.63	30.8	1.24	511
Silicon	10.4	13.6	1.84	426
Silicon carbide	24.2	13.2	5.32	638

Table 5.3. Material properties, as measured by microindentation techniques.

Figure 5.7. Measured critical grinding infeed for 10% surface fracture versus the calculated critical-depth-of-cut

Figure 5.8. Modified critical-depth-of-cut for toughened zirconia

The *slope* of the best fit line through the data should ideally be 1, according to the model. The actual best fit slope of the line is ~ 0.93 . An explanation of this slope deviation from unity can be found in a fundamental difference between the indentation stress field (on which the calculated-critical-depth is based) and grinding stress field (which controls the actual grinding ductility). Beneath a grinding wheel, the stress field is determined not only by the diamonds (i.e. indenters) but also by the surrounding bond matrix and the influence of adjacent diamond grains. For grinding wheels with small grinding grains in high concentration, the bond matrix and adjacent grains could impart significant compressive stresses on the workpiece surface. These compressive stresses act to prohibit material fracture. While it is difficult to measure the magnitude of compressive stresses, monitoring the normal force during a plunge grinding operation can provide insight into how compressive grinding stresses are related to infeed rate. By inserting a load cell into the structural loop of PEGASUS, normal force variations in grinding of fused silica were measured.

A 60% increase in grinding normal force resulted from increasing the grinding infeed rate from $0.0075\mu\text{m}/\text{rev}$ to $0.0375\mu\text{m}/\text{rev}$. This greater force is at least partially due to the increased compressive stress of the bond, acting to reduce the amount of fracture on the ground surface.

The actual critical machining depth that will cause fracture is increased by an increasing compressive stress at the workpiece surface (i.e. the compressive stresses

allow deeper cuts before the onset of fracture). The critical-depth-of-cut model arises from indentation analyses for which there is no increasing compressive stress with cutting depth. Thus, the calculated critical depth becomes erroneous as the compressive surface stresses (and infeed rate) of grinding get larger.

The only remaining parameter to be determined for the original critical-depth-of-cut model is the geometrical proportionality constant relating critical depth of cut to critical grinding infeed. In this case, a constant equal to ~ 0.15 converts the calculated critical depth to critical grinding infeed for 10% fracture. The final form, then, of the critical grinding infeed is:

$$d_c = 0.15 \left(\frac{E}{H} \right) \left(\frac{K_c}{H} \right)^2 \quad (5.45)$$

The correlation of grinding ductility with the proposed critical-depth-of-cut model is fundamentally limited by the difference between material properties measured by indentation and material properties encountered by the grinding wheel. That is, the model is deficient for materials exhibiting *surface* properties that deviate from those of the bulk material. This deficiency is caused in large measure by the evaluation of K_c , the material property related to fracture resistance. An alternative procedure for evaluating K_c at the material surface could significantly improve the model. Unfortunately, no such procedure is currently available.

5.4 Other Influences on Grinding Ductility

5.4.1 The Influence of Grinding Wheel Speed

A series of grinding tests were performed to evaluate the influence of grinding speed on the material removal regime. These tests were performed on fused silica, zerodur, and silicon samples. For all wheel speeds tested, the "grinding chip thickness" (grinding infeed in nm/rev) was held constant at $7.5nm/rev$. The results of these tests for wheel speeds from $1.5m/s$ to $12.5m/s$ are plotted in Figure 5.9. From the figure, it can be seen that there is almost no dependence on wheel speed for the three materials in the speed range tested. Fused silica shows some tendency toward more brittle behavior at higher wheel speeds, however, these might be due to the dynamic influences of the machine in response to the higher rotational frequency of the wheel. In any case, the influence of wheel speed is clearly less significant than the influence of grinding infeed rate for controlling ductility in microgrinding. Recent work by [8] involving grinding glass at speeds of up to $100m/s$ may provide further insight into the influence of grinding speed. In his study of single grit grinding at speeds from $1m/s$ to $20m/s$, the critical-depth-of-cut for fracture initiation was virtually *unchanged* over this speed range, though the subsurface residual stress levels were shown to be larger for grinding done at lower speeds.

The relative independence of grinding ductility from grinding speed adds some measure of reinforcement to the critical-grinding-depth-of-cut model, which is independent of cutting speed. In addition, this result points to a relatively weak

Figure 5.9. Effect of grinding wheel speed on ductility

influence of thermal effects, as such effects should be related to the material removal rate. If thermal influences were primarily responsible for grinding ductility, then an increased speed at constant chip thickness would be expected to alter the grinding regime.

5.4.2 The Influence of Grinding Wheel Bond

In Chapter 4, a significant relationship was postulated between the stiffness of the grinding wheel bond matrix and the ductility of the material removal process. It was proposed that a less rigid bond should allow local compliance for individual abrasive grains, resulting in a more ductile material removal process. Because of the vast number of diamond grains in the grinding contact area, this local compliance does not significantly alter the overall rigidity of the grinding loop. Grinding tests to verify this hypothesis were carried out on both fused silica and zerodur using both metal and resin bond wheels, for a range of grinding infeed rates from 1.5nm/rev to 37.5nm/rev .

The results of these tests are plotted in Figure 5.10. Substantial improvement in grinding ductility was found for both of these materials with the more compliant resin bond. The critical grinding infeed for 10% surface fracture was increased from 2.0nm/rev to 3.7nm/rev for fused silica and from 2.7nm/rev to 11.9nm/rev for zerodur. This represents a significant increase in the transition infeed rate for each of these materials. Putting it another way, for machining a particular material in the transition range of grinding infeeds, the areal percentage of surface fracture is

Figure 5.10. Effect of wheel bond on grinding ductility. Top: fused silica. Bottom: zerodur.

roughly four times higher for a metal bond than for a resin bond.

The ratio of elastic moduli between metal and resin bonds is ~ 15 . If the improvement in grinding ductility experienced with the resin bond is due to its greater compliance, then the results of these tests indicate that even more ductility could be found in a more compliant bond. The ideal bond would have a low elastic modulus (local stiffness), while remaining stiff enough to avoid adding a significant contribution to the overall compliance of the grinding structural loop.

5.4.3 The Influence of the Grinding Fluid

There are three ways in which the grinding fluid can affect the ductility of the grinding process. They are:

- Chemical interactions with the material surface
- Cooling of the grinding zone
- Lubrication of the grinding contact

Each grinding fluid can influence grinding ductility in any or all of these three categories. Chemical interactions between the grinding fluid and the workpiece surface is the most enigmatic influence of the grinding fluid on the grinding ductility. In spite of the efforts of Rebinder and his followers to construct simple theories of surface active chemical behavior, generalizations concerning the selection of an optimal fluid from a surface chemistry stand point have remained elusive.

Nevertheless, the effects of the grinding fluid on the grinding ductility are significant. Figure 5.11 illustrates the results for a series of grinding tests on fused silica, quartz, and silicon, comparing the grinding ductility with two different grinding fluids: water and a 1:1 mixture of ethanol and methanol. For the fused silica glass and the single crystal quartz, a significant reduction in grinding ductility occurs with the use of the ethanol-methanol mixture. This result contradicts the theory put forth by Cuthrell that a 1:1 ethanol-methanol mixture should result in an increased machining ductility. For silicon, a slight improvement in grinding ductility accompanies the use of the ethanol-methanol mixture. This suggests that the influence of the grinding fluid on grinding ductility is linked to the properties of the workpiece material as well as those of the fluid itself.

The tangential grinding force measured during these tests was not a strong function of the grinding fluid, making it unlikely that the lubrication properties (i.e. coefficient of friction) of the two fluids are responsible for the difference in grinding ductility. From the stand point of coolant properties, the Prandtl number is indicative of the heat transfer properties of the fluid. At room temperature, the Prandtl number for water is ~ 7 , for ethanol-methanol it is ~ 12 . Thus, the ethanol-methanol mixture would be expected to provide somewhat more heat transfer than water. This enhanced heat transfer may be responsible for the increased grinding ductility observed for silicon, but would not explain the increased brittleness observed for the quartz and fused silica.

Figure 5.11. Effect of grinding fluid on grinding ductility.

5.5 Residual Stress Measurement

Macroscopic Raman Spectroscopy was used to evaluate the residual stress state in ground germanium samples. Using an argon-ion incident laser beam, the Raman signals are generated predominantly in the top 0.5 μm of the sample surface. Thus,

Since the chemical compositions of quartz and fused silica are identical, but different from that of silicon, it is tempting to attribute the results of this experiment to a chemical interaction between the grinding fluid and the material surface. The exact nature of the chemical interaction and its effect on the surface properties is uncertain, however. What can be stated with certainty is that the grinding fluid is an influential parameter in the ductility of the grinding process, and that this influence is not based on the fluid properties alone, but rather on some interaction properties between the grinding fluid and the free surface of the workpiece material.

An important point to make here is that the grinding fluid chemistry and its effect on the grinding ductility are not addressed by the critical-depth-of-cut model. The good correlation demonstrated in Figure 5.7 between the model and the actual grinding behavior for many brittle materials is a fortunate benefit of choosing water as the grinding fluid. The results of the same tests might be quite different if the ethanol-methanol mixture were used instead of water. This suggests that water may be a suitable baseline grinding fluid for microgrinding experiments, since its effects on the ductility of the grinding regime seem to be uniform for a wide range of brittle materials.

5.5 Residual Stress Measurement

Macroscopic Raman Spectroscopy was used to evaluate the residual stress state in ground germanium samples. Using an argon-ion incident laser beam, the Raman signals are generated predominantly in the top $0.5\mu\text{m}$ of the sample surface. Thus,

the technique provides an average measure of the surface residual stress in the sample.

The results, in terms of residual stress vs. areal percentage of surface fracture, are plotted in Figure 5.12. The measured residual stress is compressive, with the magnitude of compressive stress increasing for surfaces with less fracture damage. In other words, the residual stress level increases for more ductile material removal. For fully ductile grinding, however, the average residual stress is only 100MPa , a fraction of the yield strength of the germanium. Although this stress level is not significant, its apparent one-to-one correspondence with surface fracture could provide a sensitive measure of grinding ductility, particularly if the Raman signals could be monitored in real-time on the grinding apparatus.

The behavior of the residual stress (higher compressive surface stresses for more ductile material removal) can be attributed to the elastic-plastic stress field beneath the diamond abrasive grains. A passing abrasive grain will cause plastic deformation in the near surface region of the workpiece material, and elastic (predominantly compressive) stresses in the region just beneath the plastically compressed surface. Upon passage of the abrasive grain, the plastically compressed surface layer prevents the *elastically* compressed subsurface zone from returning to its original unstressed state, resulting in a residual compressive zone near the material surface.

In the event of fracture (at higher infeed rates), the plastically compressed surface region is partially removed by the merging of subsurface cracks. Because this

plastically compressed layer was the cause of the subsurface residual elastic compressive stress, removal of the plastic layer releases this elastic stress. Therefore, the residual stress level near the surface of the material will be lower for a fractured surface (no plastic work) than for a plastically deformed surface.

5.6 In-Process Measurement of Grinding Ductility

5.6.1 Acoustic Emission Monitoring

Acoustic emission signals were monitored during the grinding of glass and germanium at feed rates from 0.5mm/rev to 150mm/rev. Through the data acquisition and Fourier transformation systems described in Chapter 4, the power spectrum of the acoustic emission sensor was recorded for each of four feed rates in this range. For both germanium and soda lime glass, the shape of the spectrum remains largely invariant over the entire range of feeds with the major peak always centered at 320kHz.

The amplitude of the acoustic emission spectrum changes significantly with an increase in the grinding feed rate. The increased acoustic emission amplitude can be attributed to either one or both of two effects: an increase in the brittleness of material removal or an increase in the material removal rate. Figure 5.13 is a plot of the AE power spectrum peak amplitude versus the % surface fracture for germanium and soda lime glass. The AE power spectrum amplitude peak for each of the two sets of data has been normalized by the background AE level (i.e., no

Figure 5.12. Surface residual stress versus surface fracture for germanium

plastically compressed layer was the cause of the subsurface residual elastic compressive stress, removal of the plastic layer relieves this elastic stress. Therefore, the residual stress level near the surface of the material will be lower for a fractured surface (no plastic zone) than for a plastically deformed surface.

5.6 In-Process Measurement of Grinding Ductility

5.6.1 Acoustic Emission Monitoring

Acoustic emission signals were monitored during the grinding of glass and germanium at infeed rates from 0.3nm/rev to 150nm/rev . Through the data acquisition and Fourier transformation systems described in Chapter 4, the power spectrum of the acoustic emission sensor was recorded for each of four infeed rates in this range. For both germanium and soda-lime glass, the shape of the spectrum remains largely invariant over the entire range of infeeds, with the major peak always centered at 320kHz .

The *amplitude* of the acoustic emission spectrum changes significantly with an increase in the grinding infeed rate. The increased acoustic emission amplitude can be attributed to either one or both of two effects: an increase in the brittleness of material removal or an increase in the material removal rate. Figure 5.13 is a plot of the AE power spectrum peak amplitude vs. the % surface fracture for germanium and soda lime glass. The AE power spectrum amplitude peak for each of the two sets of data has been normalized by the background AE level (i.e. no

grinding activity is equivalent to an AE power spectrum amplitude of 1). The percent-surface-fracture values used in this plot were calculated from the data in Table 5.1. A nearly linear dependence between AE amplitude and surface fracture is obtained for both soda lime glass and germanium, though the slope differs for the two materials. Also notable is the relatively large sensitivity of the AE signal vs. grinding ductility: an increase in AE amplitude of more than two orders of magnitude accompanies a rise in surface fracture from 5% to 100% for soda lime glass. Over a range from 0 to 25% surface fracture, the AE power spectrum peak for germanium increases by a factor of 150. The results of these tests indicate that acoustic emission signals could provide a sensitive in-process measure of grinding ductility, though they would require calibration for different materials.

5.6.2 Microgrinding Forces as a Measure of the Brittle-Ductile Transition

There is some evidence that the measurement of tangential machining forces can provide a clue to the material removal process. It has long been established that the specific grinding energy (energy per unit volume of material removal) is lower for brittle machining than for ductile machining. The simple explanation for this behavior is that brittle machining causes fracture propagation beneath the material surface, removing material in largely undeformed pieces, whereas ductile machining progresses by plastic deformation of the entire volume of material that is removed. If a grinding process undergoes a transition from brittle material removal to ductile

Figure 5.13. AE power spectrum peak level versus grinding ductility

material removal, the specific grinding energy will increase.

A measure of specific grinding energy may help to evaluate the critical grinding infeed rate. The specific grinding energy for a plunge grind is given by:

$$E = \frac{F}{r w} \quad (5.46)$$

where:

E = Specific grinding energy, J/m^3

F = Grinding tangential force, N

r = Grinding infeed rate, m/rev

w = Width of Grinding contact, m

In particular, the specific grinding energy ratio for two different infeed rates is given by:

$$R = \frac{E_h}{E_l} = \frac{F_h r_l}{F_l r_h} \quad (5.47)$$

where the subscripts h and l refer to the high and low infeed rates, respectively. For no change in grinding ductility, R should be roughly equal to 1. For a transition, R should be less than 1. Thus, a measure of the specific grinding energy ratio for a fixed pair of grinding infeed rates should correlate with the degree of transition

occurring over that infeed rate range. In other words, if both infeed rates used in the calculation of R will produce ductile material removal, then R should be roughly 1. R will also approach 1 if both infeed rates selected produce *brittle* material removal. If a transition occurs between the two infeed rates, however, R should drop to a value very much less than 1.

Figure 5.14 is a plot of the measured specific grinding energy ratio (R) for nine brittle materials. The ratio was determined using a grinding infeed rate variation from 37.5nm/rev to 1.5nm/rev. The percentage of change in surface fracture from the high infeed rate to the low infeed rate is a measure of the amount of the brittle-ductile transition that takes place over this particular range of infeed rates.

A clear inverse relationship exists between the grinding energy ratio and the percentage of transition corresponding to these infeed rates. This points to an important relationship between the grinding tangential forces and the degree of grinding ductility. Moreover, this relationship appears to hold for a broad range of hard materials, from single crystals to advanced ceramics.

One potential use of the specific grinding energy ratio suggested by this relationship is in-process monitoring of grinding ductility. Changes in the specific grinding energy ratio could provide a sensitive indicator of the onset of the ductile-brittle transition, allowing closed loop control of the material removal regime. Of course, such a system demands real-time measurement of both the material removal rate and the grinding tangential force. Nevertheless, since the specific grinding energy

the differences between workpiece materials, it is a promising technique for real-time control of the microgrinding process.

Figure 5.14. Specific grinding energy ratio versus % brittle-ductile transition

the differences between workpiece materials, it is a promising technique for real-time control of the microgrinding process.

The primary goal of this research has been to investigate microgrinding as a technique for machining brittle materials in a ductile grinding regime. Achieving this goal has required progress along two frontiers of research: determination of the machining parameters relevant to ductile-regime grinding and isolating the material properties that control the transition from brittle to ductile material removal. Through experimental work and analysis along these two fronts, a framework has been constructed to characterize the variables controlling ductile-regime grinding, and to describe the relationship between machining conditions, material properties, and grinding behavior.

6.1 Design of a Ductile-Regime Grinding Apparatus

Microgrinding has been defined as a machining process combining the contour accuracy achievable by fixed abrasive grinding with the fine surface finish achievable by loose abrasive polishing. By utilizing an ultraprecision mechanical system, real-time feedback motion control, and state-of-the-art truing techniques it is possible to merge the high rigidity of a precision grinder with the low machining forces necessary for ductile material removal. Using these principles, a versatile plunge-

6 Summary and Conclusions

The primary goal of this research has been to investigate microgrinding as a technique for machining brittle materials in a ductile grinding regime. Achieving this goal has required progress along two frontiers of research: determination of the machining parameters relevant to ductile-regime grinding and isolating the material properties that control the transition from brittle to ductile material removal. Through experimentation and analysis along these two fronts, a framework has been constructed to characterize the variables controlling ductile-regime grinding, and to describe the relationship between machining conditions, material properties, and grinding ductility.

6.1 Design of a Ductile-Regime Grinding Apparatus

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mode microgrinding apparatus (PEGASUS) has been designed, constructed, and structurally evaluated. The relevant features of PEGASUS include:

- Rigid Structural Loop ($50MN/m$)
- Piezoelectric Infeed Actuation ($10\mu m$ range)
- Air Bearing Wheel Spindle
- Closed Loop Infeed Control ($1.5nm/rev$ to $150nm/rev$)
- Vibration Isolation from External Disturbances and Motor Driver
- Thermally Controlled Environment ($\pm 0.1^{\circ}C$)
- Precision Wheel Trueing ($125nm$ axial runout)

With this apparatus, the process of ductile-regime grinding on brittle materials has been explored for a broad range of machining conditions and material properties. The material removal rates achievable on PEGASUS coincide with the "microgrinding gap." That is, the apparatus is capable of grinding with smaller controlled cutting depths than are available on any commercial grinder. This has proven to be the quintessential requirement for ductile-regime grinding of brittle materials.

6.2 The Dependence of Grinding Ductility on Grinding Infeed Rate

A basic hypothesis has been postulated for ductile-regime grinding: all materials, regardless of their hardness or brittleness, will undergo a transition from a brittle machining regime to a ductile machining regime if the grinding infeed rate is made small enough. Below this threshold depth-of-cut, the energy required to propagate cracks is larger than the energy required for plastic yielding, so plasticity becomes the predominant grinding mechanism.

For a hard material, the predominant material removal mechanism will be brittle fracture for large grinding infeed rates. The ductile-regime grinding hypothesis predicts that there will be a transition to a predominantly ductile material removal mechanism at lower infeed rates. The existence of this transitional infeed rate was demonstrated on the PEGASUS apparatus through a series of test grinds on fused silica.

Using scanning electron microscopy as a post-grinding analysis tool, the ground material surfaces were examined for evidence of surface fracture, which would be indicative of the grinding ductility. A grid counting technique was devised to quantify the areal percentage of surface fracture. By applying this measure of grinding ductility to fused silica samples that were ground using a range of infeed rates, the transitional grinding infeed rate was determined for this material. It was found that a change in grinding infeed rate from 75nm/rev to 1.5nm/rev resulted in a tran-

sition from 99% surface fracture to 5% surface fracture. This result is important for three reasons. First, it verifies that the design principles used in the development of PEGASUS were substantially appropriate for ductile-regime grinding. In addition, the microgrinding range of material removal rates is shown to be an important, unexplored region of machining, with significant potential for improving the machinability of glasses and ceramics. Finally, from this series of grinding tests the basic hypothesis of ductile-regime grinding (ie, brittle-ductile transition for a reduced infeed rate) is validated for fused silica. Fused silica is a relevant material for this study because it is particularly brittle and thus difficult to machine in a ductile regime. In spite of this complication, fused silica is an important engineering material, resistant to high temperatures and severe thermal shock. Because of these properties, it is often used in precision molding applications, for which contour accuracy and surface finish are both critical. Thus, both the challenge and the potential benefits of microgrinding are demonstrated by the use of fused silica as a model material.

6.3 Testing the Critical-Depth-of-Cut Model

To investigate the influence of material properties on the brittle-ductile transition rate, a broad range of amorphous glasses, single crystals, and advanced ceramics were chosen for grinding on PEGASUS. By comparing the grinding ductility of these materials to their intrinsic properties, a critical-depth-of-cut model has been established for microgrinding. This model relates the measured critical grinding

infeed rate for 10% surface fracture to a calculated critical-depth-of-cut based on the material properties. The basic model consists of the properties of the material that characterize its fracture resistance (K_c), its plastic deformation resistance (H) and its elastic deformation resistance (E).

These properties were measured for each material using microindentation techniques. While the measurement of hardness by indentation is a standard procedure, determination of K_c and E by indentation is a developing area of research.

The properties of the material surface vary with the indentation depth at which they are measured. This surface property variability is especially troubling for the measurement of K_c , which changes by up to an order of magnitude for some materials, and remains invariant for others. Such material-related property variations complicate the extrapolation of properties from the scale of indentation ($\sim 10\mu m$) to the scale of microgrinding ($< 1\mu m$). In spite of these problems calculating the material properties, a reasonable correlation was obtained between the calculated critical-depth-of-cut and the measured critical grinding infeed rate (i.e. the grinding infeed that will produce 10% surface fracture). The final form of the model is given by:

$$d_c = 0.15 \left(\frac{E}{H} \right) \left(\frac{K_c}{H} \right)^2 \quad (6.48)$$

Only materials exhibiting significant variations in K_c with indentation depth were not well represented by the model. For these materials, estimates of the K_c behavior at small indentations must be used to modify the critical depth.

6.4 Other Machine Variables Influencing Grinding Ductility

After establishing a material model for grinding ductility, an investigation was begun to isolate machining parameters other than infeed rate that might influence the grinding ductility. The variables tested were: grinding speed, wheel bond material, and the grinding fluid chemistry. This series of tests was by no means exhaustive; the goal was simply to discriminate between those variables that affect the grinding ductility and those that do not.

The influence of the grinding wheel speed on the grinding ductility was measured for fused silica, zerodur, and silicon. The wheel speeds tested covered a range from 1.5 m/s to 12.5 m/s , and all other machine parameters were maintained at constant values (including the workpiece infeed rate per wheel revolution). For all three of the materials, no significant change was found in the grinding ductility as a function of wheel speed over this range. To a first order approximation, then, the grinding wheel speed has no effect on the ductility of the material removal process. To some extent, this speed independence implies a minimal influence of the grinding temperature on ductility, since the contact temperature is linked to the grinding speed. If thermal effects in grinding were influential in determining grinding ductility, it would be expected that this influence would be made apparent by a large change in the grinding speed.

While the contour accuracy inherent in a grinding apparatus is directly related

to its large structural stiffness, this quality makes it difficult to achieve the low cutting forces required for ductile material removal. A way to decrease the abrasive grain forces without measurably altering the rigidity of the structural loop is to use a more compliant abrasive grain bonding matrix in the grinding wheel. The primary effect of this less rigid matrix is to more evenly distribute the load among the many diamonds in the contact regions. In addition, this local compliance reduces the forces that accompany small undesired motions (e.g. runout) of the wheel with respect to the workpiece. This theory was tested by grinding fused silica and zerodur using both metal ($E = 110\text{GPa}$) and polymer resin ($E = 7\text{GPa}$) bonded grinding wheels. The range of infeed rates chosen (1.5nm/rev to 37.5nm/rev) for these tests encompasses some of the brittle-ductile transition range for both these materials, so the effect of the bond matrix on grinding ductility could be assessed. As expected, the more resilient bond provided enhanced ductility for both materials, increasing the critical grinding infeed rate (corresponding to 10% surface fracture) by a factor of 2. This significant influence of local grain compliance can be exploited by designing grinding wheels specifically for ductile-regime grinding. The ideal bond matrix would have a low elastic modulus, a strong bonding affinity for diamond, and a good resistance to wear.

The influence of the grinding fluid is perhaps the most difficult variable to characterize in microgrinding. A series of experiments on fused silica, quartz, and silicon using both water and a 1:1 ethanol-methanol mixture has provided some insight into

the potential effect of the grinding fluid on the ductility of the material removal process. For both fused silica and quartz, a dramatic increase in the brittleness of the material removal process accompanied a change in grinding fluid from water to the alcohol mixture. For single crystal silicon, a slight decrease in the brittleness of the grinding process accompanied the same change in grinding fluid. This result points toward a chemical effect: the chemical compositions of amorphous fused silica and crystalline quartz are virtually identical and different from that of crystalline silicon. If the effect could be attributed to differences between the thermal or lubrication properties of the two fluids, it would be expected to influence the grinding ductility of the three materials in the same way, if not by the same amount. Chemical interactions between the grinding fluid and the material workpiece can harden or soften the surface layer, and advance or retard crack growth. As such, these effects must be individually categorized for each fluid/workpiece pair to determine the optimum fluid for a given material workpiece.

6.5 Measurement of Surface Residual Stresses in Microgrinding

Microscopic Raman Spectroscopy was used to evaluate the residual stress state in the near surface region ($< 0.5\mu m$) of ground germanium samples. The measured residual surface stress is compressive, and increases in magnitude with the ductility of the material removal. For fully plastic material removal, the average residual stress is only 100 Pascals, a small fraction of germanium's yield strength. One

explanation for the monotonic increase in compressive stress with grinding ductility is that the relaxation of subsurface *elastic* compressive stresses after the passage of an abrasive grain is impeded by the *plastically* deformed surface region, resulting in a compressive residual subsurface stress state. Fracture acts to relieve these residual stresses, lowering the overall subsurface compression by removing a portion of the deformed surface layer.

6.6 In-Process Measurement of Grinding Ductility

If a method existed for determining (in real-time) the relative amounts of plasticity and fracture occurring during grinding, then closed loop control of grinding ductility would be possible. Two techniques were explored for in-process monitoring grinding ductility.

Acoustic Emission (AE) monitoring is a promising avenue for real-time control of grinding ductility. AE signals result primarily from fracture events in the material workpiece, and thus a direct measure of the grinding ductility can be made. Measurements of the grinding ductility (i.e. surface fracture) versus the peak amplitude of the AE power of spectrum for both germanium and soda line glass were recorded. As a measure of grinding ductility the peak amplitude of the AE power spectrum is remarkably sensitive (more than a hundredfold increase in amplitude for a 20% change in grinding ductility for germanium). In addition, for these two materials, the relationship between the AE peak and percent surface fracture is lin-

ear over the ranges measured. The only complication to this technique is that both the sensitivity and the amplitude of the AE signals are intrinsically linked to the specific workpiece material. To use such a measure of grinding ductility in process, then, would require individual calibration for each material type that is machined.

An alternative technique for in-process measurement of grinding ductility is obtained by monitoring the specific grinding energy during the machining process. This variable, which is uniquely determined by the tangential grinding force and the material removal rate, represents the energy required to remove a unit volume of material. Because plastic deformation requires more energy per unit volume of material removal than fracture, monitoring the specific grinding energy can allow a sensitive measure of the grinding regime. In particular, the *ratio* of specific grinding energies for two different grinding infeed rates is a reliable measure of the *change in ductility* over that particular range of grinding infeed rates. If there is no change in grinding ductility, the specific grinding energy ratio (R) will be ~ 1 . For a change toward more brittle grinding R will be less than one, and in-fact will tend toward zero. This unique inverse relationship between the specific energy ratio and the ductile-brittle transition has been demonstrated to be largely independent of the material properties for all of the hard materials tested. Though it requires real-time measurement of both grinding infeed and grinding tangential force, this material independence makes the use of specific grinding energy ratio a promising candidate for in-process control of grinding ductility.

6.7 Future Research Directions

Several important variables have been left unexplored for their influence on grinding ductility. Notably, the size and concentration of diamonds in the grinding wheel will undoubtedly influence the material removal process. The traditional philosophy of superfinishing has been that smaller abrasive grains produce a better surface finish, but this is not necessarily true in ductile-regime grinding. Recent research has challenged this idea by demonstrating that large grain wheels can be effectively utilized for superfinishing if the exposed diamond grains are first machined flat [13]. In this way, each flattened diamond grain appears to the workpiece as a large, sharp, zero degree rake cutting tool. One avenue of future research, then, is to compare this type of wheel to a fine grain wheel with random diamond orientation, to see which is more effective in ductile-regime grinding. In any case, a study of the role of the diamond grain characteristics in material removal would contribute significantly to the overall understanding of the micromechanics of ultraprecision grinding.

Because this research was aimed at a fundamental understanding of the brittle-ductile transition in grinding, there has been little effort made to extend these ideas to a ductile-regime grinding apparatus capable of producing useful components. Extension of the results of this work to the development of a prototype grinding station is not trivial. Wheel trueing, motion control, and machine stiffness all become variables of two or three dimensions, instead of just one dimension. Such an extension of

the simple PEGASUS configuration is a vital step in the research of ductile-regime grinding. Indeed, an extension to two dimensions might *simplify* characterization of grinding ductility; flat surfaces could be measured using profilometry techniques to assess both their form and surface finish. This measurement could provide information about the ground surface quality that is easier to obtain and quantitatively more repeatable than the SEM "grid-counting" technique.

Finally, a promising area for research is the pursuit of a closed-loop control of grinding ductility. The potential for monitoring either acoustic emission or specific grinding energy ratios as an in-process measure of grinding ductility has been clearly identified. The utility of such real-time, in-process control is that it would allow material removal to progress in stages: by a brittle process for larger rates of stock removal of material and by a ductile process for finishing. The principles and guidelines established by this study of ductile-regime grinding make this type of sophisticated control of the grinding process a realistic research goal.

6.8 Summary

The following is an abbreviated summary of the experimental results, problems, and recommendations brought out by this research.

- Ductile-Regime grinding is a newly established material removal technique.

By controlling a stiff, accurate grinding apparatus so that it has exceptionally small grinding forces, brittle materials can be machined in a ductile manner.

- A ductile-regime grinding apparatus requires a stiff structural loop real-time control of the grinding infeed, relative isolation from environmental disturbances, and state-of-the-art wheel truing techniques.
- All brittle materials will undergo plastic flow rather than fracture if the depth of grinding is small enough.
- The brittle-ductile transition occurs in a range of grinding infeeds below $0.1\mu\text{m}/\text{rev}$ for most hard materials.
- An estimate of the grinding critical infeed that will produce 10% surface fracture a given material can be made using the calculated material properties: K_c , H , and E , which are indicative of the fracture resistance, plastic flow resistance, and elastic resistance of the material.
- Calculation of these properties is hindered by the fact that they are all functions of depth. The properties at the material surface, which are relevant for grinding, differ from the bulk material properties.
- Indentation techniques allow property evaluation for the near surface region of the material ($< 10\mu\text{m}$). This is still an order of magnitude deeper into the material than micro-grinding. For fracture toughness evaluation, indentation techniques can produce large errors for some materials.
- Grinding ductility can be quantified by SEM analysis of the ground surface. The areal percentage of surface fracture is a measure of the ductility of the

- material removal process. *the work includes*
- Grinding wheel speed has little influence on grinding ductility for low to moderate speeds ($1 - 15\text{m/s}$).
- The grinding wheel bond matrix plays an important role in grinding ductility by allowing local compliance at the diamond grain, a more elastic bonding matrix enhances the ductility of material removal.
- The grinding fluid is an influential parameter in the ductility of material removal. The mechanism of this influence is largely chemical in nature, and is therefore a strong function of the material composition and structure of the workpiece.
- Residual stresses in ductile-regime grinding can be measured using Raman Spectroscopy. These stresses are compressive at the material surface, and are larger for surfaces machined in a ductile regime than for those machined in a brittle regime. In all cases, however, the average compressive stresses at the surface are small ($< 100\text{Pa}$).
- Two techniques show potential for in-process measurement of grinding ductility. They are: acoustic emission monitoring and measurement of the specific grinding energy ratio. AE monitoring provides a more sensitive measure of the onset of fracture in grinding, whereas the specific grinding ratio is somewhat more material independent measure of the ductile-brittle transition.

- Recommendations for future work include:

Ref * Study of the role of the abrasive grain size, concentration, and orientation in grinding ductility

* Development of a ductile-regime grinder to produce flat surfaces

* Pursuit of closed loop control of grinding ductility

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A - Material Property Data

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.01	20.7	7.5
1.01	21.2	7.1
1.20	18.4	10.2
1.20	18.5	10.0
0.81	11.9	11.8
0.81	12.2	10.9
0.41	7.8	13.8
0.41	8.7	10.9
0.17	4.8	18.0

$$H=K_1 \cdot (d^{n+1}) \quad K_1: 0.281E-01 \quad n: -0.62 \quad r: 0.96$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
5.8	130.0	10.1	6.22
4.9	120.0	10.0	6.75
4.9	121.0	10.0	6.69
2.9	96.0	6.5	6.05
2.9	93.0	7.5	6.49
2.0	80.0	6.3	6.32
2.0	71.0	5.5	6.15
2.0	74.3	5.5	5.92
1.0	47.9	3.3	6.55

$$E/H \text{ Standard Deviation: } 0.38$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
5.8	75.5	1.57
5.8	72.0	1.63
4.9	70.0	1.48
4.4	52.0	2.07
2.9	50.5	1.45
1.0	29.2	1.80

$$Kc = \text{SQRT}(E/H) \cdot (P/c^{1.5}), \quad Kc \text{ Standard Deviation: } 0.24$$

Fused Silica

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	20.7	7.5
1.61	21.2	7.1
1.20	15.4	10.2
1.20	15.5	10.0
0.81	11.9	11.5
0.81	12.2	10.9
0.41	7.8	13.5
0.41	8.7	10.9
0.17	4.3	18.0

$H=K_1*(d^{**n})$: K_1 : 0.281E-01

n : -0.52 r : 0.96

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
5.8	130.0	10.1	6.22
4.9	120.0	10.0	6.75
4.9	121.0	10.0	6.68
2.9	86.0	6.5	6.05
2.9	93.0	7.5	6.49
2.0	80.0	6.3	6.32
2.0	71.6	5.5	6.15
2.0	74.3	5.5	5.92
1.0	47.9	3.3	5.55

E/H Standard Deviation= 0.38

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
5.8	75.5	1.57
5.8	72.0	1.68
4.9	70.0	1.48
4.4	52.0	2.07
2.9	50.5	1.45
1.6	29.2	1.80

$Kc = \text{SQRT}(E/H)*(P/c^{**1.5})$, Kc Standard Deviation: 0.24

H10 HARDNESS: 11.65

E/H AVERAGE: 6.24

Kc AVERAGE: 1.67

CRITICAL INDENTATION: 0.129 indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	25.2	5.1
1.61	25.9	4.8
1.20	25.9	3.6
1.20	22.0	5.0
0.81	18.7	5.6
0.81	16.4	6.3
0.41	11.4	6.3
0.41	10.9	6.9
0.17	6.8	7.2
0.17	7.3	6.2

 $H=K1(d^{*-n})$: K1: 0.132E+00

n: -0.34 r: 0.83

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	130.0	13.0	9.00
4.9	134.0	13.9	9.73
4.9	135.0	14.0	9.72
2.9	101.0	10.0	8.63
2.9	97.0	10.0	9.89
3.9	112.0	12.5	11.72
3.9	110.0	12.6	12.62
5.9	143.0	14.0	8.12
5.9	146.0	14.5	9.00

E/H Standard Deviation: 1.06

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (N/mm ^{1.5})
0.6	60.0	0.37
0.7	60.6	0.34
1.0	70.8	0.36
1.2	85.0	0.36
1.5	90.7	0.39
1.7	94.0	0.42
2.0	112.5	0.36
2.4	135.1	0.36

Soda Lime Glass

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	25.2	5.1
1.61	25.9	4.8
1.20	25.9	3.6
1.20	22.0	5.0
0.81	16.7	5.8
0.81	16.4	6.0
0.41	11.4	6.3
0.41	10.9	6.9
0.17	6.8	7.2
0.17	7.3	6.2

$$H = K_1 \cdot (d^{**n}): K_1: 0.132E+00 \quad n: -0.34 \quad r: 0.83$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	130.0	13.0	9.00
4.9	134.0	13.9	9.73
4.9	135.0	14.0	9.72
2.9	101.0	10.0	8.83
2.9	97.0	10.0	9.59
3.9	112.0	12.5	11.72
3.9	110.0	12.6	12.69
5.9	148.0	14.0	8.12
5.9	145.0	14.5	9.00

$$E/H \text{ Standard Deviation} = 1.46$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
0.6	50.0	0.37
0.7	60.6	0.34
1.0	70.8	0.36
1.2	85.0	0.35
1.5	90.7	0.38
1.7	94.0	0.42
2.0	112.5	0.36
2.4	135.1	0.35

2.9	153.0	0.34
3.4	167.5	0.35
3.9	184.7	0.35
4.9	210.7	0.35

$K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.02

H10 HARDNESS: 6.43
 E/H AVERAGE: 9.82
 K_c AVERAGE: 0.36
 CRITICAL INDENTATION: 0.031

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
2.0	60.0	6.4	10.36
5.4	91.0	9.8	10.92
5.4	89.0	9.8	11.28

E/H Standard Deviation: 0.45

K_c Data from Vickers Indentation Cracks

load (N)	crack length (um)	K_c (MN/m ^{-1.5})
45.0	170.6	4.81
68.2	182.6	6.44
88.2	245.4	4.11
90.9	269.7	4.78
90.9	292.0	4.25
90.9	291.8	4.21

$K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.38

H10 HARDNESS: 15.24
 E/H AVERAGE: 10.86
 K_c AVERAGE: 4.77
 CRITICAL INDENTATION: 1.062

Alumina

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
3.92	25.4	12.1
3.92	25.0	12.5
3.92	25.2	12.3
1.96	17.1	13.4
1.96	17.0	13.6
1.96	17.1	13.4
2.94	21.0	13.3
2.94	21.5	12.7
2.94	21.0	13.3

$$H = K_1 \cdot (d \cdot n): K_1: 0.119E+01 \quad n: -0.22 \quad r: 0.59$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
2.0	60.0	6.4	10.38
5.4	91.0	9.9	10.92
5.4	89.0	9.8	11.28

$$E/H \text{ Standard Deviation} = 0.45$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
45.9	170.5	4.81
68.2	182.5	6.44
68.2	246.4	4.11
90.9	269.7	4.78
90.9	292.0	4.25
90.0	291.8	4.21

$$K_c = \text{SQRT}(E/H) \cdot (P/c^{1.5}), K_c \text{ Standard Deviation: } 0.88$$

H10 HARDNESS: 15.24

E/H AVERAGE: 10.86

Kc AVERAGE: 4.77

CRITICAL INDENTATION: 1.062

Zerodur

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	22.2	6.5
1.61	21.0	7.3
1.20	17.4	8.0
1.20	16.9	8.4
0.81	12.4	10.6
0.81	11.9	11.5
0.41	8.0	12.9
0.41	7.8	13.5
0.17	4.5	16.4
0.17	4.5	16.4

$H=K1*(d^{**n})$: K1: 0.193E-01

n:-0.55 r: 0.98

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
2.9	92.0	7.5	6.57
4.9	121.0	10.0	6.68
3.9	100.0	8.0	6.43
4.9	113.0	9.3	6.65
2.0	70.0	6.5	7.87
2.0	72.0	6.6	7.71
1.0	49.5	4.6	7.88
1.0	49.9	4.6	7.78

E/H Standard Deviation= 0.66

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
4.9	92.5	1.04
4.9	91.0	1.07
5.9	98.3	1.14
5.9	98.5	1.14
5.9	96.0	1.18
3.9	75.0	1.14
3.9	76.0	1.12
3.9	74.7	1.15
2.9	60.0	1.20

2.9

61.0

1.17

 $K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.05

Hardness Data from Vickers Indentations

	diagonal (um)	hardness (GPa)
H10 HARDNESS:	11.18	
E/H AVERAGE:	7.20	14.5
Kc AVERAGE:	1.14	9.5
CRITICAL INDENTATION:	0.074	13.2

1.30	14.4	11.6
0.52	10.2	15.6
0.53	10.5	14.7
0.41	7.2	15.9
0.41	7.2	15.9
0.17	4.0	20.8
0.17	3.7	24.3

 $H = E/H * (10^{-n})$: E/H : 0.795E-01 n : -0.45 r : 0.93

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	94.3	10.0	10.24
4.9	90.0	9.8	10.95
6.9	112.6	12.0	10.36
6.9	106.5	11.4	10.73
8.9	78.7	8.5	10.72
8.9	83.2	9.0	10.75

E/H Standard Deviation= 0.27

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (NM/m ^{-1.5})
6.9	97.0	1.42
6.9	93.5	1.50
4.9	82.5	1.51
4.9	85.0	1.41
3.9	71.5	1.49
3.9	72.5	1.46
2.9	61.0	1.42
2.9	59.0	1.49

 $K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.04

Quartz

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	14.9	14.5
1.61	18.4	9.5
1.20	13.5	13.2
1.20	14.4	11.6
0.81	10.2	15.6
0.81	10.5	14.7
0.41	7.2	15.9
0.41	7.2	15.9
0.17	4.0	20.8
0.17	3.7	24.3

$$H = K_1 \cdot (d^n): K_1: 0.795E-01 \quad n: -0.45 \quad r: 0.93$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	94.3	10.0	10.24
4.9	90.0	9.8	10.95
6.9	112.6	12.0	10.36
6.9	105.5	11.4	10.73
3.9	78.7	8.5	10.72
3.9	83.2	9.0	10.76

$$E/H \text{ Standard Deviation} = 0.27$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
5.9	97.0	1.42
5.9	93.5	1.50
4.9	82.5	1.51
4.9	86.0	1.41
3.9	71.5	1.49
3.9	72.5	1.46
2.9	61.0	1.42
2.9	59.0	1.49

$$Kc = \text{SQRT}(E/H) \cdot (P/c^{1.5}), Kc \text{ Standard Deviation: } 0.04$$

H10 HARDNESS: 14.55

E/H AVERAGE: 10.62

Kc AVERAGE: 1.46

CRITICAL INDENTATION: 0.107 indentations

load (N)	diagonal (um)	hardness (GPa)
1.01	15.7	13.0
1.01	10.4	11.9
1.20	13.2	13.8
1.20	13.7	12.8
0.91	9.7	17.3
0.91	9.9	18.5
0.41	6.7	18.3
0.41	6.2	21.4
0.17	4.2	18.9
0.17	4.5	18.4

$$H = K_1 \cdot (d + a)^{-n}; K_1: 0.408E+00 \quad n: 0.31 \quad r: 0.83$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	82.0	9.8	14.75
4.9	83.6	10.0	15.24
2.9	82.0	7.2	13.28
2.9	69.0	7.3	13.19
4.9	82.3	9.8	13.02
4.9	85.3	9.7	12.40
2.0	51.6	6.2	15.06
2.0	52.8	6.3	14.67
2.0	50.0	6.0	15.00

E/H Standard Deviation= 1.08

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
4.9	59.0	2.87
4.9	58.0	2.94
2.9	46.0	3.80
2.9	36.0	3.61
5.9	70.0	2.68
5.9	78.0	2.38
5.9	74.0	2.45
5.9	77.5	2.29

Zirconia

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	15.7	13.0
1.61	16.4	11.9
1.20	13.2	13.8
1.20	13.7	12.8
0.81	9.7	17.3
0.81	9.9	16.6
0.41	6.7	18.3
0.41	6.2	21.4
0.17	4.2	18.9
0.17	4.5	16.4

$$H=K1*(d^{**n}): K1: 0.408E+00$$

$$n:-0.31 \quad r: 0.83$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	82.0	9.8	14.76
4.9	83.0	10.0	15.24
2.9	62.0	7.2	13.29
2.9	63.0	7.3	13.19
4.9	82.3	9.5	13.02
4.9	85.3	9.7	12.40
2.0	51.6	6.2	15.08
2.0	52.8	6.3	14.67
2.0	50.0	6.0	15.00

$$E/H \text{ Standard Deviation} = 1.08$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
4.9	59.0	2.87
4.9	58.0	2.94
2.9	46.0	2.50
2.9	36.0	3.61
5.9	70.0	2.66
5.9	78.0	2.26
5.9	74.0	2.45
5.9	77.5	2.28

4.9	58.0	2.94
3.9	44.0	3.56
3.9	55.5	2.51
1.2	23.7	2.77
0.8	20.7	2.29

$K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.44

H10 HARDNESS: 15.28
 E/H AVERAGE: 14.07
 K_c AVERAGE: 2.74
 CRITICAL INDENTATION: 0.453

$R^2 = 0.99$ $r = 0.99$

load (N)	long diagonal (um)	short diagonal (um)	E/H
5.9	100.0	12.3	15.97
5.9	98.0	12.0	16.33
3.9	80.0	9.9	17.14
3.9	80.0	9.8	16.36
4.9	88.5	10.9	16.77
2.0	53.1	6.7	18.80

E/H Standard Deviation: 0.98

load (N)	crack length (um)	K_c (MN/m ^{3/2})
5.9	40.0	6.78
5.9	33.0	7.82
4.9	25.7	6.70
4.9	31.4	8.12

$K_c = \text{SQRT}(E/H) * (P/c^{**1.5})$, K_c Standard Deviation: 0.60

H10 HARDNESS: 12.98
 E/H AVERAGE: 17.03
 K_c AVERAGE: 7.23
 CRITICAL INDENTATION: 5.278

Toughened Zirconia

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	16.0	12.5
1.20	14.2	11.9
1.20	14.2	11.9
0.81	11.2	13.0
0.81	11.9	11.5
0.41	8.2	12.2
0.41	7.7	13.9
0.17	4.5	16.4
0.17	4.5	16.4

$$H = K1 * (d^{**n}): K1: 0.707E+00 \quad n: -0.25 \quad r: 0.89$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
5.9	100.0	12.3	16.67
5.9	98.0	12.0	16.33
3.9	80.0	9.9	17.14
3.9	80.0	9.8	16.36
4.9	88.5	10.9	16.77
2.0	53.1	6.7	18.89

$$E/H \text{ Standard Deviation} = 0.96$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
5.9	40.0	6.78
5.9	38.0	7.32
4.9	35.7	6.70
4.9	31.4	8.12

$$Kc = \text{SQRT}(E/H) * (P/c^{**1.5}), Kc \text{ Standard Deviation: } 0.66$$

H10 HARDNESS: 12.98

E/H AVERAGE: 17.03

Kc AVERAGE: 7.23

CRITICAL INDENTATION: 5.275

Germanium

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	23.9	5.6
1.61	25.9	4.8
1.20	18.2	7.3
0.81	15.7	6.6
0.81	15.2	7.0
0.41	9.0	10.2
0.41	8.7	10.9

$$H=K1*(d**n): K1: 0.435E-02 \quad n:-0.67 \quad r: 0.97$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
1.0	57.2	7.8	33.00
0.5	31.5	4.3	33.35
2.0	62.5	8.2	23.94
2.0	63.2	8.3	24.10
1.0	43.2	6.0	40.50
1.0	45.4	6.2	33.49
2.0	60.0	8.0	27.00

$$E/H \text{ Standard Deviation} = 6.04$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
2.0	76.4	1.15
2.0	77.0	1.14
1.0	43.8	1.32
1.0	41.0	1.46
1.0	45.1	1.27
1.0	44.6	1.29
2.9	91.0	1.33
2.9	88.0	1.40
2.9	93.0	1.28
2.0	72.0	1.26
2.0	78.0	1.11
1.6	69.8	1.08
1.6	70.5	1.06

$K_c = \text{SQRT}(E/H) * (P/c^{1.5})$, K_c Standard Deviation: 0.12

H10 HARDNESS: 9.63 Knoop Indentations

E/H AVERAGE: 30.77 mmol (mm) hardness (GPa)

K_c AVERAGE: 1.24 8.0

CRITICAL INDENTATION: 0.512 7.6

1.20 17.2 8.1

1.20 16.7 8.5

0.81 12.2 10.9

0.81 11.9 11.5

0.41 8.4 11.7

0.41 8.7 10.9

0.17 5.8 11.0

$B = 11 * (H^{0.75})$: K_1 : 0.247E+00 n : -0.33 r : 0.84

E/H Data from Knoop Indentations

Load (N)	long diagonal (mm)	short diagonal (mm)	E/H
4.9	93.5	10.7	12.55
2.9	63.8	9.5	12.65
1.9	40.0	4.7	13.85
1.9	40.0	4.7	13.85
1.9	40.7	4.8	14.03
1.9	32.5	4.7	14.27

E/H Standard Deviation= 0.73

K_c Data from Vickers Indentation Cracks

Load (N)	crack length (mm)	K_c (N/m ^{1.5})
2.0	41.5	1.93
2.0	50.0	1.44
1.6	37.1	1.85
1.2	29.4	1.97
1.0	30.0	1.55
0.8	20.4	1.90

$K_c = \text{SQRT}(E/H) * (P/c^{1.5})$, K_c Standard Deviation: 0.31

H10 HARDNESS: 10.35

E/H AVERAGE: 13.67

K_c AVERAGE: 1.84

CRITICAL INDENTATION: 0.424

Silicon

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	20.0	8.0
1.61	20.5	7.6
1.20	17.2	8.1
1.20	16.7	8.6
0.81	12.2	10.9
0.81	11.9	11.5
0.41	8.4	11.7
0.41	8.7	10.9
0.17	5.5	11.0

$$H = K_1 \cdot (d^{**n}): K_1: 0.247E+00 \quad n: -0.32 \quad r: 0.84$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	93.5	10.7	12.65
2.0	56.8	6.5	12.65
1.0	40.0	4.7	13.85
1.0	40.0	4.7	13.85
1.0	40.7	4.8	14.03
1.0	39.6	4.7	14.37

$$E/H \text{ Standard Deviation} = 0.73$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
2.0	41.5	1.91
2.0	50.0	1.44
1.6	37.1	1.85
1.2	29.4	1.97
1.0	30.0	1.55
0.8	20.4	2.30

$$Kc = \text{SQRT}(E/H) \cdot (P/c^{**1.5}), Kc \text{ Standard Deviation: } 0.31$$

H10 HARDNESS: 10.39

E/H AVERAGE: 13.57

Kc AVERAGE: 1.84

CRITICAL INDENTATION: 0.424

Silicon Carbide

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	10.2	30.9
1.01	12.4	20.9
1.20	11.4	18.8
1.20	9.5	26.7
0.81	8.2	24.2
0.81	7.8	26.7
0.41	4.5	40.6
0.41	4.8	35.7
0.17	2.7	45.7
0.17	2.8	42.5

$$H = K_1 \cdot (d^{-n}) \quad K_1: 0.750E-01 \quad n: -0.50 \quad r: 0.91$$

E/H Data from Sharp Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	58.0	6.5	11.25
2.9	45.0	5.4	13.80
2.9	45.0	5.1	12.37
2.0	35.1	4.2	14.53
2.0	34.6	4.0	13.08

E/H Standard Deviation= 1.19

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
4.9	43.5	4.23
4.9	40.0	4.97
4.9	39.0	5.18
2.9	30.0	4.59
2.9	26.5	6.53
1.2	12.0	7.43

$$K_c = \sqrt[3]{E/H} \cdot (P/c^{1.5}) \quad K_c \text{ Standard Deviation: } 1.13$$

H10 HARDNESS: 24.23

E/H AVERAGE: 13.17

Kc AVERAGE: 5.32

Silicon Carbide

Hardness Data from Vickers Indentations

load (N)	diagonal (um)	hardness (GPa)
1.61	10.2	30.9
1.61	12.4	20.9
1.20	11.4	18.5
1.20	9.5	26.7
0.81	8.2	24.2
0.81	7.8	26.7
0.41	4.5	40.6
0.41	4.8	35.7
0.17	2.7	45.7
0.17	2.8	42.5

$$H = K_1 \cdot (d^{**n}) : K_1: 0.780E-01 \quad n: -0.50 \quad r: 0.91$$

E/H Data from Knoop Indentations

load (N)	long diagonal (um)	short diagonal (um)	E/H
4.9	58.0	6.5	11.86
2.9	46.0	5.4	13.80
2.9	45.0	5.1	12.27
2.0	35.1	4.2	14.83
2.0	34.6	4.0	13.08

$$E/H \text{ Standard Deviation} = 1.19$$

Kc Data from Vickers Indentation Cracks

load (N)	crack length (um)	Kc (MN/m ^{1.5})
4.9	44.5	4.23
4.9	40.0	4.97
4.9	39.0	5.16
2.9	30.0	4.59
2.9	26.5	5.53
1.2	12.0	7.43

$$Kc = \text{SQRT}(E/H) \cdot (P/c^{**1.5}), \quad Kc \text{ Standard Deviation: } 1.13$$

H10 HARDNESS: 24.23

E/H AVERAGE: 13.17

Kc AVERAGE: 5.32

CRITICAL INDENTATION: 0.634