ORNL/SUB/92-SL807/2

**OAK RIDGE
NATIONAL
LABORATORY**

**High Speed Low Damage Grinding
of
Advanced Ceramics**

PHASE II FINAL REPORT

**Joseph A. Kovach
Stephen Malkin**

MANAGED AND OPERATED BY
LOCKHEED MARTIN ENERGY RESEARCH CORPORATION
FOR THE UNITED STATES
DEPARTMENT OF ENERGY

This report has been reproduced from the best available copy.

Reports are available to the public from the following source.

National Technical Information Service
5285 Port Royal Road
Springfield, VA 22161
Telephone 703-605-6000 (1-800-553-6847)
TDD 703-487-4639
Fax 703-605-6900
E-mail orders@ntis.fedworld.gov
Web site <http://www.ntis.gov/ordering.htm>

Reports are available to U.S. Department of Energy (DOE) employees, DOE contractors, Energy Technology Data Exchange (ETDE) representatives, and International Nuclear Information System (INIS) representatives from the following source.

Office of Scientific and Technical Information
P.O. Box 62
Oak Ridge, TN 37831
Telephone 423-576-8401
Fax 423-576-5728
E-mail reports@adonis.osti.gov
Web site <http://www.osti.gov/products/sources.html>

Reports produced after January 1, 1996, are generally available via the DOE Information Bridge.

Web site <http://www.doe.gov/bridge>

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

HIGH SPEED LOW DAMAGE GRINDING
OF
ADVANCED CERAMICS
PHASE II FINAL REPORT

Date Published: Feb. 2000

Prepared by
Dr. Joseph A. Kovach, P.E.
Director, Manufacturing Process Technology
Eaton Corporation
Manufacturing Technologies Center
Willoughby Hills, Ohio

and

Dr. Stephen Malkin
Professor of Mechanical Engineering
Director, Program on Grinding Fundamentals and Applications
University of Massachusetts
Department of Mechanical Engineering
Amherst, Massachusetts

Funded by
U.S. Department of Energy
Assistant Secretary for Energy Efficiency and Renewable Energy
Office of Transportation Technologies
Heavy Vehicle Propulsion System Materials Program
EE 07 01 00 0

for
OAK RIDGE NATIONAL LABORATORY
Oak Ridge, Tennessee 37831-6285
Managed by
LOCKHEED MARTIN ENERGY RESEARCH CORPORATION
for the
U.S. DEPARTMENT OF ENERGY
under Contract DE-AC05-96OR22464

TABLE of CONTENTS

	<u>PAGE</u>
ABSTRACT	1
1.0 INTRODUCTION and OBJECTIVES	2
2.0 BACKGROUND	3
3.0 TECHNICAL APPROACH	4
4.0 RESULTS and DISCUSSION:	5
4.1 HSLD GRINDING FORCE and WEAR BEHAVIOR.	5
4.1.1 Grinding Conditions.	5
4.1.2 Hydrodynamic Force Considerations	6
4.1.3 Bonded Wheel Force Behavior	10
4.1.4 Bonded Wheel Wear Behavior	11
4.2 HSLD GRINDING MECHANISMS & ENERGY CONSIDERATIONS	13
4.2.1 Plowed Surface Area Analysis	13
4.2.2 Plowed Surface Energy and Workpiece Properties	15
4.3 HSLD GRINDING THERMAL ANALYSIS	16
4.3.1 Preliminary Thermal Calculations	16
4.3.2 Energy Partition (ϵ) & Temperature Determination	17
4.4 HSLD GRINDING FLEXURAL STRENGTH CONSIDERATIONS	20
4.4.1 Wheel Dressing Considerations	20
4.4.2 Effect of Step-over Distance	21
4.4.3 MOR Flexural Strength as a Function of Wheel Speed . .	22
4.4.4 Predicting MOR Flexural Strength	23
4.5 HSLD PRODUCTION COST DRIVERS & ECONOMIC ANALYSIS	24
4.5.1 HSLD Cost Model Development	24
4.5.2 HSLD Cost Model Input Data	26
4.5.3 HSLD Cost Model Results	27
4.6 HSLD CENTERLESS GRINDING CONSIDERATIONS	28

TABLE of CONTENTS (cont'd.)

4.6.1 Static Analysis of Spinning	29
4.6.2 Kinematic Analysis of Above and Below Center Grinding . .	30
4.6.3 Software Development for Kinematic Rounding	31
4.6.4 Simulation Results	31
5.0 SUMMARY	33
6.0 ACKNOWLEDGMENTS	37
7.0 REFERENCES	38

LIST of TABLES

<u>TABLE</u>		<u>PAGE</u>
1	Data summary for high speed grinding tests	40
2	Mechanical properties of materials tested	41
3	Experimental results with second two-color system	42
4	HSLD flexural strength summary	43
5	HSLD cost model variables and values	44
6	Total cost per piece as functions of dressing cycle & labor rate	45
7	Summary of centerless simulation results	46

LIST of FIGURES

<u>FIGURE</u>	<u>PAGE</u>
1 Normal force vs. wheel speed - 120 grit plated wheel	47
2 Normal force vs. wheel speed - 180 & 320 resin, 180 Vit.	48
3 Wheel wear vs. wheel speed - 240 resin	49
4 G-Ratio vs. wheel speed - 240 resin	50
5 Ground surface and swarf analysis - SEM's	51
6 Grinding power vs. plowed surface area rate S'_w	52
7 Grinding power vs. material and grinding parameters	53
8 Predicted grinding zone temperature vs. wheel speed - 120 grit	54
9 Measured grinding temperature vs. wheel speed	55
10 Energy partition vs. wheel speed - 180 & 320 resin, 180 Vit.	56
11 Specific energy vs. wheel speed - - 180 & 320 resin, 180 Vit.	57
12 Energy partition & specific energy comparison - 180 grit Vit.	58
13 Energy partition & specific energy comparison - 180 grit resin	59
14 Energy partition & specific energy comparison - 320 grit resin	60
15 Measured grinding temperature vs. wheel speed - 180 Vit.	61
16 Surface topography from 0.070" cross-feed	62
17 Surface topography from 0.015" cross-feed	63
18 MOR strength as a function of cross-feed	64
19 MOR strength as a function of wheel speed - 4 wheel types	65
20 MOR strength as a function of plowed surface area rate	66

LIST of FIGURES (cont'd.)

<u>FIGURE</u>	<u>PAGE</u>
21 Cost vs. removal rate - labor rate & speed comparisons	67
22 Cost vs. removal rate - 3 wheel speeds	68
23 Cost vs. removal rate - 18,000 sfm, total cost profile	69
24 Cost vs. removal rate - 18,000 sfm, dressing time effects	70
25 Cost vs. removal rate - 6,000 sfm, dressing time effects	71
26 Centerless grinding: above-center schematic	72
27 Centerless grinding: below-center schematic	73
28 Centerless grinding: 27° below-center, actual part geometry	74
29 Centerless grinding: 27° below-center, simulated part geometry	75
30 Centerless grinding: 20° below-center, simulated part geometry	76

HIGH SPEED, LOW-DAMAGE GRINDING OF ADVANCED CERAMICS

ABSTRACT

In the manufacture of structural ceramic components, grinding costs can comprise up to 80% of the entire manufacturing cost. As a result, one of the most challenging tasks faced by manufacturing process engineers is the development of a ceramic finishing process to maximize part throughput while minimizing costs and associated scrap levels. The efforts summarized in this report represent the second phase of a program whose overall objective was to develop a single-step, roughing-finishing process suitable for producing high-quality silicon nitride parts at high material removal rates and at substantially lower cost than traditional, multi-stage grinding processes. More specifically, this report provides a technical overview of **High-Speed, Low-Damage (HSLD)** ceramic grinding which employs elevated wheel speeds to achieve the small grain depths of cut necessary for low-damage grinding while operating at relatively high material removal rates. The study employed the combined use of laboratory grinding tests, mathematical grinding models, and characterization of the resultant surface condition. A single-step, roughing-finishing process operating at high removal rates was developed and demonstrated.

Research sponsored by the U.S. Department of Energy, Assistant Secretary for Energy Efficiency and Renewable Energy, Office of Transportation Technologies, as part of the Heavy Vehicle Propulsion System Materials Program, under contract DE-AC05-96OR22464 with Lockheed Martin Energy Research Corporation.

1.0 INTRODUCTION and OBJECTIVES

With increasing demands placed on improving the performance and cost effectiveness of heat engines in a worldwide marketplace, additional emphasis is continually being placed upon maximizing manufacturing productivity while utilizing the state-of-the-art engine designs and materials. In particular, the role of structural ceramics in heat engines has evolved from the onset of being an academic curiosity to the point where the primary concern is now one of reducing final manufacturing costs. Unfortunately, the mechanical and physical characteristics which make these materials desirable from a product performance standpoint usually render them far from ideal in terms of manufacturability. Typically, structural ceramic finishing costs alone can account for up to 80% of the entire component manufacturing cost. Consequently, one of the most challenging tasks now faced by manufacturing process engineers is the development of a ceramic finishing process which maximizes part throughput rate while minimizing costs and associated scrap levels.

To maximize material removal rates in the grinding operations necessary for finishing structural ceramics, it is essential that the relationships between product performance, material behavior, and manufacturing processes are clearly defined in addition to understanding the effects of the required material removal process. Since the consequences of in-service ceramic failure can be catastrophic, extremely slow finishing operations are usually employed in an attempt to minimize process induced defects. Usually, a painstaking multi-step ceramic finishing process is deployed which requires numerous grinding wheels, additional capital equipment, additional perishable dressing tools, and additional labor. From this perspective, the development of improved grinding processes for structural ceramic heat engine components will require not only strict attention to the mechanics of the grinding process but requires a quantitative understanding of the aforementioned interrelationships.

Based on the concerns outlined above, the efforts summarized in this report represent the second phase of a two-phase program whose overall objective is to develop a single step, roughing-finishing process suitable for producing high-quality silicon nitride ceramic parts at high material removal rates and at substantially lower cost than traditional, multi-stage grinding processes. Initial implications from Phase I have suggested that HSLD grinding of Si_3N_4 is technically feasible. Accordingly, to achieve the overall program objective, the Phase II effort is focused on:

- 1.0** Continued expansion of the HSLD science base
- 2.0** Further development of the enabling HSLD technologies required for successful implementation, and
- 3.0** Economic analysis of the HSLD production cost drivers.

2.0 BACKGROUND

Previous research on the grinding of ceramics indicates that the abrasive/workpiece interactions generally involve both ductile flow and brittle fracture. As the abrasive engages the workpiece, initial cutting action occurs by plastic or ductile flow which is followed by fracture if the grain depth of cut becomes sufficiently large. By analogy with indentation fracture mechanics, two principal types of cracks are generated: "lateral" cracks, which lead to material removal, and "radial or median" cracks, which cause strength degradation.

One implication from the past research is that fracture damage may be avoided or minimized if the grain depth of cut and/or unit grain load is kept below some critical level such that predominantly low-damage grinding occurs. The common approach to achieve this objective is to use extremely small removal rates (fine depths of cut and low workpiece velocities), which are **not** economical in production. From analytical considerations of grinding kinematics, the only apparent way to overcome this drawback, namely to achieve large removal rates while maintaining a small grain depth of cut, is through the use of high wheel speeds and fine grit wheels.

It should be noted that low damage and ductile mode grinding generally require a much higher specific energy (i.e. grinding energy input per volume of material removed) than mixed mode grinding which involves both ductile flow and fracture. It is hypothesized that the mechanical and thermal grinding effects can act in concert to enhance material removal rates while minimizing incipient microcracking tendencies in the ceramic workpiece. On the other hand, extreme heat can reduce diamond wheel life. In conventional grinding of metallic alloys with aluminum oxide wheels the majority of the grinding energy enters the workpiece as heat.¹ Conversely, when using superabrasive wheels, a large portion of the heat energy is transferred into the wheel due to its superior thermal conductivity.² Since this "reverse" heat transfer phenomenon is even more pronounced when grinding ceramic materials with diamonds, it is important to understand the grinding "energy balance" to avoid reducing wheel life from excessive heat, while maximizing the thermal benefits arising from plastic deformation in the workpiece surface.

In order to calculate the transient temperature field in the workpiece, it is first necessary to determine what portion of the total grinding energy enters the workpiece as heat. Typically, an inverse heat transfer method is employed whereby temperatures measured in the workpiece are used to obtain the heat input at the grinding zone.³ One approach, described in this study, involves using an infrared pyrometer with an optical fiber. Subsequently, through modification of the Carslaw-Jaeger⁴ moving band heat source model, a useful *thermal model* of ceramic grinding can be developed for approximate calculation of HSLD wheel/grinding zone temperatures. With the above considerations in mind, the following technical approach was developed.

3.0 TECHNICAL APPROACH

The purpose of this section is to present the general approach used throughout the investigation. Many of the procedural details are provided as necessary in the appendix. Based on the background material presented earlier, it became apparent that a rather broad technical approach would be required to meet the feasibility objectives defined in the introduction. Owing to the large number of independent process variables and difficulty associated with on-line measurement of many of the dependent variables, an interdisciplinary approach which combines laboratory experimentation with previously established process models was employed.

The overall program utilized a team consisting of representatives from the Eaton Corporation and the University of Massachusetts. In addition, a close relationship with the High Temperature Materials Laboratory (HTML) at Oak Ridge National Laboratory (ORNL) was maintained to help develop an in-depth understanding of the resulting surface topography and its relationship to the mode(s) of material removal. Plastic flow and/or brittle fracture was determined through the use of scanning electron microscopy or, when possible, using high power optical atomic force microscopes. The majority of the laboratory grinding experimentation was conducted at Eaton's Corporate Manufacturing Research Laboratory (MRL) utilizing four fully instrumented grinding machines covering several grinding processes (conventional surface grinding, creep feed surface grinding, I.D. grinding, O.D. cylindrical grinding, and centerless grinding). Where applicable, the spindle power and grinding forces were measured. As a function of the process under examination, the following conditions were collectively varied to establish low damage grinding behavior at high material removal rates:

- Wheel speeds ranging from 5000 to 35,000 ft/min (25 - 178 m/s)
- Wide array of grinding wheel types with:
 - Various bond systems (plated, resin, etc.) and,
 - Various diamond grit sizes (ranging from 180 to 400)
- Workpiece velocities covering:
 - Low speed creep feed conditions at 1 in/min (2.54 cm/min) to,
 - High speed O.D. grinding levels at 400+ ft/min (122 m/min)

Building on fundamental grinding kinematics and existing thermal grinding models, the University of Massachusetts, in conjunction with Eaton's MRL, utilized the above grinding tests to calculate specific grinding energies and corresponding grinding zone temperatures. In addition, a two-color infrared pyrometry system was developed by UMass to measure ceramic grinding zone temperatures in real time. Subsequently, numerous MOR bar tests were performed to correlate material strength with HSLD grinding conditions. Beyond this, projected finishing costs were generated using actual wear data and production costs. Details of these efforts are presented in the following sections.

4.0 RESULTS and DISCUSSION

4.1 HSLD GRINDING FORCE and WEAR BEHAVIOR

During the initial Phase I effort numerous surface grinding tests were conducted using plated wheels at speeds up to 35,000 SFM (178 m/sec) and material removal rates up to 1 in²/min (11mm²/sec) to grind Si₃N₄ (specifically Sintered RBSN). However, due to the limited capabilities of plated wheels, the Phase II program focused on the behavior of dressable wheels operating under HSLD conditions. In this section, the relative performance of vitrified, plated, and resin bonded wheels are compared.

4.1.1 Grinding Conditions

Prismatic S/RBSN specimens were ground on a custom built "semi-creep feed" surface grinding machine capable of operating at workpiece velocities between 1 - 180 in/min (0.42 - 76 mm/sec) with wheel speeds up to 40,000 SFM (203 m/sec). Aside from the prismatic specimen tests, limited cylindrical grinding was performed on a modified Weldon 1632 Universal ID/OD grinder for the high speed O.D. grinding studies. Wheel speeds as high as 40,000 SFM (203 m/sec) are also attainable on the modified Weldon.

Data logging was accomplished using a PC-based data acquisition package and/or strip chart recorders. In the surface grinding modes (pendulum and creep feed) force data was measured using a standard 3-axis Kistler force dynamometer platform. Of particular interest are the normal and tangential grinding forces. In the cylindrical grinding modes, the normal force was measured using Kistler piezoelectric load washers built into the feed screws of the machine tool. In all cases, spindle power was measured using watt transducers. In subsequent tests, the temperature data was recorded for all surface grinding tests using an infrared pyrometer equipped with an optical fiber input.

All surface grinding tests discussed in this section were conducted using Eaton S/RBSN material. Material blank fabrication was done using Eaton's pre-production manufacturing facilities at the CoRD-DC center. Documented material properties are similar to those of the Kyocera SN-220 silicon nitride material (approx. 600 MPa flexural strength, 3.25 g/cc density, 300 GPa E-modulus, etc.). It is important to note that the OD grinding studies were performed under plunge grinding conditions using Norton NT551 material.

Initially, the Phase I tests were designed to identify how high wheel speeds and removal rates might impact resulting grinding forces, finishes, and surface quality. A relatively coarse (120 grit) brazed diamond wheel was used. As a function of the process under examination, the following conditions were collectively varied to establish low damage grinding behavior at high material removal rates. Note that wheel speed was increased from 5000 SFM (25 m/s) to 35,000 SFM (178 m/s) in increments of 10,000 SFM (51 m/s) while the material

removal rate was varied from 0.125 in²/min (1.34 mm²/s) to 1 in²/min (10.75 mm²/s) by incrementing the depth of cut and workpiece velocity as follows:

Depth of Cut inches (mm)	Part Velocity in/min (mm/s)	Removal Rate in ² /min (mm ² /s)
0.0025 (.0635)	50 (21)	0.125 (1.34)
0.0050 (.1270)	50 (21)	0.250 (2.69)
0.0025 (.0635)	100 (42)	0.250 (2.69)
0.0050 (.1270)	100 (42)	0.500 (5.38)
0.0150 (.3810)	50 (21)	0.750 (8.06)
0.0100 (.2540)	100 (42)	1.000 (10.8)

In general, it was shown that with plated wheels the grinding forces decrease considerably with increased wheel speed and/or reduced material removal rate (Figure 1) and that a tendency toward improved surface finish is developed as wheel speed is increased. Moreover, an improved surface finish was also obtained by going to high material removal rates (see Phase I final report); even while operating at conventional wheel speeds. Interestingly, it was also found that increasing wheel speed, while operating at a relatively low material removal rate, can dramatically reduce surface fragmentation or “pull out”. As will be shown in the subsequent sections, increasing wheel speed was also found to *improve* MOR strength.

4.1.2 Hydrodynamic Force Considerations

After characterizing plated wheel performance, the Phase II effort focused on dressable wheel behavior. As an important aside, however, it must be noted that utilizing high wheel speeds can also lead to the potential generation of large *hydrodynamic forces*. This phenomenon primarily arises when using non-porous wheels which can “sustain” relatively high fluid film pressures. In general, the theoretical maximum hydrodynamic force for surface grinding would occur when using a perfectly smooth non-porous disk rotating above a flat plate. As the disk rotates, it draws in coolant and produces a fluid film under pressure. The pressurized fluid, acting over the contact area acts to lift the wheel off of the workpiece. As the surface of the disk (or grinding wheel) becomes rougher (as in coarser grits, more open bond structure, etc.), the ability to develop a hydrodynamic film is diminished. The resultant force, however, can still be significant and in certain cases, greater than the actual grinding force.

Consequently, in order to determine the grinding forces *directly* associated with material removal, the hydrodynamic forces must *first* be determined and *subtracted* from the *total* grinding force. To understand these effects, the following three different diamond wheels were studied using Safety Cool 612 (at 10:1) supplied to the workzone at 30 psi and 6 GPM:

- 1) 180 grit vitrified bond (D91-KZ125-VFR)
- 2) 180 grit resin bond (D180-100-UI841)
- 3) 320 grit resin bond (D320-100-UI841)

The table below summarizes the resulting hydrodynamic force test data (normalized per unit width) generated for each of the three wheels. Note that the wheel was dressed and considered to be very open prior to the determination of these forces. Therefore, as the wheel dulls, the hydrodynamic force should increase. As expected, the 320 grit resin wheel produced the largest hydrodynamic force. Conversely, as the grit size is increased the hydrodynamic force was reduced.

Wheel Speed (sfm)	180 grit, Resin bond (lb/in)	320 grit, Resin bond (lb/in)	180 grit, Vit. Bond (lb/in)
5,000	11.6	14.0	3.5
10,000	17.3	23.1	7.5
15,000	24.6	39.9	8.7
20,000	18.8	59.7	5.2
25,000	24.6	81.9	3.9

Interestingly, when a porous vitrified wheel is used, the hydrodynamic force behavior can be considerably different. Hydrodynamic forces generated with the 180 grit vitrified wheel increase as wheel speed is increased from 5,000 to approximately 15,000 ft/min. However, as wheel speed is further increased, the magnitude of the hydrodynamic force is actually reduced. To understand this behavior, recognize that the porous wheel can act like a pump - - - carrying away fluid caught in the pores instead of trapping the fluid between the wheel surface and the workpiece. Given a constant supply of coolant, the hydrodynamic forces will increase with speed *until* the pumping action of the wheel reduces the localized fluid quantity below a critical level such that the pores are no longer full and cannot sustain a full hydrodynamic film.

From a theoretical perspective, a hydrodynamic analysis was developed at the University of Massachusetts⁵ to quantitatively account for the experimental results and predict the hydrodynamic forces in actual grinding.

Prior to conducting each experiment, a grinding pretest was run to establish the zero gap thickness location ($h_0=0$). For this purpose, an actual grinding pass was taken with a depth of cut $a=0.025$ mm (0.001 inch) and workpiece velocity $v_w=4.2$ mm/s (10 inches per minute). Three sparkout passes were then taken without any additional downfeed but with the same wheel speed and workspeed. The gap thickness was then set by raising the wheel a distance h_0 above this zero reference location. A total of 30 tests were conducted in the "down" grinding mode to measure the hydrodynamic force with various combinations of wheel speed v_s and preset gap thickness h_0 . The workspeed was maintained at 4.2 mm/s for all the tests. The first experiment was conducted with a wheel speed $v_s=16$ m/s and zero gap thickness. Tests were then repeated for wheel velocities up to $v_s=48$ m/s

in steps of 8 m/s. This same procedure was then used to obtain the forces with gap thicknesses set at $h_0 = 2, 5, 10, 15$, and 25 microns.

As expected, the forces decrease with a bigger gap thickness, which is consistent with hydrodynamic behavior. Extrapolating these results to $h_0 = 0$ would allow us to predict the hydrodynamic forces in a more generic way as in the case of actual grinding without any gap thickness.

In an attempt to quantitatively analyze these experimental results, consider the apparent hydrodynamic situation where fluid from the nozzle enters the converging gap between the rotating wheel (shaft) and the workpiece (journal). The workpiece is approximated as being stationary because its velocity (4.2 mm/s) is negligible relative to the peripheral wheel velocity (16–48 m/s). Since the gap thickness is much less than the grinding width, the situation can be considered to be two dimensional.

Now for an incompressible fluid of constant viscosity flowing between two perfectly smooth rigid surfaces, the differential equation for the pressure variation developed in a converging wedge (fluid film) can be written as (see ref. 6):

$$\frac{dp}{dx} = -12\mu\left(\frac{v_s}{2h^2} - \frac{Q}{h^3}\right) \quad (1)$$

where dp/dx is the pressure gradient in the x-direction, μ is the dynamic viscosity of the grinding fluid, v_s is the wheel velocity, Q is the volumetric fluid flow rate passing under the wheel and h is the separation height between the wheel and the workpiece which varies from a minimum value h_0 at $x=0$ to a maximum value at the fluid inlet ($x=l$). The fluid flow rate Q is given as:

$$Q = \frac{v_s}{s} \frac{\int_0^l \frac{dx}{h^2}}{\int_0^l \frac{dx}{h^3}} + \frac{p(0) - p(l)}{12\mu \left(\int_0^l \frac{dx}{h^3} \right)} \quad (2)$$

where $p(0)$ at $x=0$ is taken as atmospheric pressure and $p(l)$ is the pressure developed at the fluid inlet ($x=l$). To facilitate the analysis, the circular wheel surface bounding the fluid film can be approximated as a parabola⁷, in which case the separation height can be expressed as:

$$h = \frac{x^2}{d_s} + h_0 \quad (3)$$

where d_s is the grinding wheel diameter and h_0 is the gap thickness corresponding to the minimum separation distance at $x=0$.

In order to calculate the hydrodynamic force, it is necessary to obtain the pressure distribution from Equation (1) and integrate it from $x=0$ to $x=l$. For this purpose, the boundary condition for the pressure at $x=l$ is approximated as the pressure head developed due to the velocity of the fluid from the nozzle which is given by:

$$p(l) = p(0) + \frac{\rho}{2} \left(\frac{Q_a}{A} \right)^2 \quad (4)$$

where ρ is the density of the fluid, Q_a is the applied volumetric flow rate, and A is the cross-sectional area of the nozzle. Integrating Equation (1) from $x=0$ and defining Q in terms of $p(l)$ leads to the pressure distribution:

$$p(x) = p(0) + 6\mu v_s \int_0^x \frac{dx}{h^2} - 12\mu Q \int_0^x \frac{dx}{h^3} \quad (5)$$

A critical parameter for computing the force is the fluid gap length " l ", which is the distance from $x=0$ directly under the wheel center to the point where the fluid strikes the wheel. Point "A" is where the fluid exits the nozzle and point "B" is where it strikes the wheel. For the geometrical setup which was used, the fluid gap length " l " corresponding to the horizontal distance from $x=0$ to point "B" was equal to 70 mm. The orientation of the fluid flow exiting the nozzle and striking the wheel was approximately 12 degrees from the horizontal.

The force was calculated with minimum gap thicknesses of $h_0 = 0, 5, 10, 15, 20$ and 25 mm for wheel speeds of 1.5 m/s and 16 m/s to 48 m/s in discrete steps of 8 m/s. In the experiments, the hydrodynamic force was obtained as the measured difference between the force at the wheel velocity v_s minus that measured at the slowest velocity $v_0 = 1.5$ m/s. Therefore for calculating the forces, the forces computed at the slowest wheel speed $v_s=1.5$ m/s were subtracted from the forces calculated at v_s in each case for comparison with the experimental results. The force at velocity v is obtained as:

$$F_v = (F_{v_s} - F_{v_0})b = \mu(v_s - v_0) f(h_0) \quad (6)$$

where

$$f(h_0) = b \left[12 \left(\frac{Q - Q_0}{v_s - v_0} \right) \int_0^l \left(\int_0^x \frac{dx}{h^3} \right) dx - 6 \int_0^l \left(\int_0^x \frac{dx}{h^2} \right) dx \right] \quad (7)$$

and Q is given by Equation (2). It can be readily shown using Equation (2) that $f(h_0)$ depends only on h_0 for given values of v_s , v_0 , m and l .

One way to compare the experimental and computed forces is to divide Equation (6) by $v_s - v_0$, which leads to :

$$\frac{F_{v_s} - F_{v_0}}{(v_s - v_0)} = \mu f(h_0) \quad (8)$$

If the experimental results fit the hydrodynamic theory as presented above, then a plot of $F_v/(v_s - v_0)$ (with $v_0 = 1.5$ m/s) versus $f(h_0)$ should yield a straight line through the origin with a slope equal to μ . The experimental results plotted in this way do not fit such a proportional relationship.

For the hydrodynamic analysis, the wheel surface was assumed to be smooth. But the grinding wheel surface is certainly not smooth. The peak to valley roughness was measured to be approximately 20 mm for the 400 grit wheel, and 40 mm for the coarser 120 grit wheel. From the data, it appears that the difference between the analytical and experimental results may be rationalized by

adding a correction factor h_c to the gap thickness in order to account for the roughness of the rotating wheel, in which case Equation (3) becomes:

$$h = \frac{x^2}{d_s} + h_0 + h_c \quad (9)$$

Different values were tried for h_c . From the results using different values of h_c , it appeared that a large value of h_c is needed. Therefore, it was decided simply to use the peak-to-valley wheel roughness as the correction factor h_c . In essence this would suggest that the hydrodynamic thickness is effectively increased by an amount h_c equal to the peak to valley wheel roughness of 20 mm. The kinematic viscosity of the soluble oil used in the experiments was quoted by the supplier as 350 Saybolt Universal Seconds at 25°C, which corresponds to approximately 80 centistokes. Using a simple law of mixtures, the 5% solution of this oil in water (kinematic viscosity 1 centistoke) used in the experiments would imply a kinematic viscosity of 5 centistokes, which corresponds to a dynamic viscosity of 0.005 N-s/m².

4.1.3 Bonded Wheel Force Behavior

Once having determined the hydrodynamic forces for the wheels listed in the previous section, actual grinding forces as a function of wheel speed were determined. Normal and tangential forces, power, and grinding zone temperature were all measured in-process for wheel speeds ranging from 5,000 to 25,000 ft/min (data summarized in Table 1). The material removal rate was held constant with the workpiece velocity at 100 in/min and depth of cut equal to 0.010 inches. Safety Cool 612 coolant at a 10:1 dilution ratio was supplied to the grinding zone at 30 psi and 6 GPM. Prior to each test, the wheel was dressed with a titanium roll dresser followed by conditioning with a 220 grit Al₂O₃ stick. The tests were randomly conducted using two replications per condition.

Figure 2 shows a plot of the normal force as a function of wheel speed for each of the three wheels. For comparison purposes, data generated with the 120 grit plated wheel used in the HSLD Phase I feasibility study is also provided. Note that with the plated wheel, the normal force drops consistently as wheel speed is increased. The bonded wheels behave similarly, to a point. However, at speeds above 15,000 ft/min, the normal forces developed with the resin and vitrified bonded wheels start to increase. Because of this increased force behavior at speeds above 15,000 ft/min, it is recommended that 18,000 sfm be considered as a reasonable production limit for wheels made with this conventional bond technology. Intuitively, operating at or near 18,000 sfm will produce low grinding forces and correspondingly low wheel wear. (Data presented in the following section confirms this supposition.)

Recognize that the increase in normal force above 15,000 sfm for the resin wheels is probably associated with thermal softening of the bond. While the wheel manufacturer claimed that this was a high temperature resin (polyimide), the bond obviously softened and required a higher normal force to achieve grit penetration into the workpiece at the highest speeds.

As for the vitrified wheel, the bond should not be as sensitive to thermal softening. As shown in Figure 2, the forces tend to decrease with increasing wheel speed. However, a slight increase in normal force is apparent as wheel speed is increased above 15,000 ft/min. As indicated in the previous section regarding hydrodynamic forces, it may be possible that the pumping action of the porous wheel at high speeds removed too much coolant from the workzone resulting in localized fluid starvation. This, in turn, could reduce cutting efficiency resulting in a slightly higher normal force at the highest speeds. In this regard, improved fluid application techniques become more critical at increased wheel speeds.

Also presented in Table 1 are tangential forces as a function of wheel speed for the three bonded wheels and the previously used 120 grit plated wheel. In each case, the tangential force is reduced with increasing wheel speed for all wheels, as expected. In addition, Table 1 also provides a comprehensive summary of the corresponding grinding power, specific energy, measured grinding surface temperature, and the fraction of the total grinding energy entering the workpiece as heat (i.e. energy partition). A discussion of these additional dependent grinding parameters is presented in subsequent sections.

4.1.4 Bonded Wheel Wear Behavior

Realizing that the majority of potential ceramic heat engine components are cylindrical, an evaluation of wheel life in OD plunge grinding mode was conducted. All tests were performed on a modified Weldon 1632 OD grinder. The Weldon has been upgraded with hybrid ceramic spindle bearings and is capable of 14,000 RPM operation. Additionally, a Kistler piezoelectric washer system was installed in the ball nut of the wheel head axis to measure grinding forces in process. Spindle power was monitored using a Load Controls Inc. power monitor. Data was collected using a PC based data acquisition system.

To determine wheel wear rates as a function of wheel speed and removal rate, a 12" x 0.5" x 5" 240 grit polyimide resin bonded diamond wheel (D240-100-UI841) was plunged into Norton NT551 cylindrical specimens using material removal rates of 0.1, 0.5, and 1.0 in³/min/in while operating at wheel speeds of 6,000; 12,000; and 18,000 ft/min. Constant workpiece velocities and removal rates were employed even as part diameter varied. Cincinnati Milacron CX-270 (an experimental version of the fluid now sold as Quantalube 270) diluted at a ratio of 12:1 was used as the grinding fluid for all tests. Fluid was supplied at 9.7 GPM at 33 PSI using a standard nozzle design (0.625" x 0.1" outlet size) from a standoff distance of 0.5 inches above the wheel/workpiece interface.

Prior to testing the grinding wheel was balanced to better than 0.3 microns at the maximum operating speed. Truing was done after mounting each wheel and when necessary to remove any wheel wear. Dressing was done before each test but not in between passes for a given test condition. The resin wheel was trued and dressed using the method described in the previous section.

It is important to note that to achieve a constant material removal rate as the workpiece diameter changed, both the part RPM and the in-feed rate (in/min)

need to be continuously updated as a function of part diameter. In this manner, the part surface speed was kept constant at 42 ft/min for all tests. (Recognize that the workpiece velocity can vary greatly if the RPM of the workhead is kept constant even over a 1/8 inch diameter change.) In addition, maintaining a constant material removal rate also requires maintaining a constant infeed rate per revolution of the workpiece. As such, the relationship between workpiece diameter and the required programmed feed rate in inches per minute as a function of part diameter was programmed into the machine tool controller to achieve the desired feed per revolution.

Since the NT551 workpieces were received in the "as-fired" condition, all workpieces were ground to a dimension of 1.000 inches using the 240 resin wheel prior to testing. The workpiece was held in a six-jaw chuck and rotated on a CNC controlled workhead capable of constant SFM operation. A contact width of 0.400 inches was kept constant and the wheel was plunged to take the workpiece to a nominal diameter of 0.25 inches. Approximately 1.5 in^3 of material was removed per test. In all cases, grinding was always done as close to the jaws as possible to maintain constant workpiece stiffness for each of the five plunges made on one specimen. The workpiece was indexed one half inch after a plunge grind and the subsequent plunge made. Note that each plunge left the diameter of the workpiece 0.010 inches larger than from the previous grind to allow for wheel wear monitoring.

By using this test method, the grinding contact width was kept constant at 0.400 inches to allow a portion of the wheel (total 0.500" wide) to remain in a virgin condition thereby serving as a datum for wheel wear measurements (Step 1). To avoid grinding off the datum, each subsequent plunge into the specimen was stopped 0.005 inches radially from the previous cut. After a grinding test, the entire wheel width was then lightly plunged into a S/RBSN bar (Step 2). If the wheel had worn during the test, a step would be imprinted onto the bar. The resulting step was measured using an optical comparator to determine the gross radial wheel wear (Step 3) per each test.

At the conclusion of the tests, a plot of radial wheel wear as a function of wheel speed for the three material removal rates was generated (see Figure 3). The data clearly shows that *increasing* wheel speed will *reduce* wheel wear. At the $1.0 \text{ in}^3/\text{min}/\text{in}$ material removal rate, **increasing wheel speed** from 6,000 to 18,000 ft/min **results in over a three fold increase in wheel life**. It is interesting to note that even at this high removal rate the resulting wheel wear at high speed is about *half* of that observed using conventional removal rates ($0.1 \text{ in}^3/\text{min}/\text{in}$) and wheel speeds (6,000 sfm). In other words, increasing the wheel speed from 6,000 to 18,000 ft/min allowed for a 10 fold increase in material removal rate while cutting wheel wear in half!

Alternatively plotted, Figure 4 represents G-Ratio (Volume of material ground divided by the Volume of wheel consumed) as a function of wheel speed and removal rate. Again, as the wheel speed is increased for a given material removal rate a significant improvement in G-Ratio is realized. G-Ratios ranged from roughly 50 to 200. The G-ratio for the resin wheel improved by a factor of

over three at the low material removal rate by increasing wheel speed from 6,000 to 18,000 SFM.

4.2 HSLD GRINDING MECHANISMS and ENERGY CONSIDERATIONS

The magnitude of the specific grinding energy and its dependence on the operating parameters is a direct consequence of the prevailing grinding mechanisms. As shown in Figure 5, SEM observations of grinding debris and ground surfaces suggest that the abrasive-workpiece interactions involve both brittle fracture and ductile flow. In this section an attempt is made to quantitatively account for the magnitude of grinding energy.

Although material removal appears to occur mainly by brittle fracture, an estimation of the brittle fracture energy indicates that it constitutes only a negligible portion of the total energy⁸. It was demonstrated that the specific energy tends to decrease with uncut chip thickness. In the past, such behavior has been attributed to an increased amount of brittle fracture with a bigger undeformed chip thickness, but this explanation can not account for the specific energy.

In this section, a quantitative analysis is developed to account for the magnitude of the grinding energy. The magnitude of the grinding energy and its dependence on the grinding parameters is analyzed in terms of the area of plowed surface generated.

4.2.1 Plowed Surface Area Analysis

SEM observations suggest abrasive-workpiece interactions involve both brittle fracture and ductile flow. That portion of the grinding energy associated with brittle fracture can be estimated as the product of the surface area generated by fracture and the fracture energy per unit area, which constitutes of only a negligible portion of the measured grinding energy. This would indicate that most of the grinding energy is expended by ductile flow, even though most of the material removal is by brittle fracture (Fig. 5).

SEM observations reveal a deformed layer on the workpiece surface typically extending from about two to five microns into the sub-surface. The generation of this deformed surface layer is apparently related to plowing by numerous abrasive points passing through the grinding zone, thereby leading to surfaces with multiple overlapping scratches and grooves. Therefore, it might be worthwhile to analyze the grinding energy in terms of the plowed area generated by the active cutting points on the wheel surface.

For the purpose of estimating the plowed surface area, it is convenient to first consider the plowing (scratching) geometry for a single undeformed chip with a triangular cross-section having semi-included angle θ (e.g. $\theta = 60^\circ$ for a triangular cross-section). For each undeformed chip, the corresponding plowed surface area A_g generated at the sides of the groove is then given by:

$$A_g = \frac{h_m l_c}{\cos \theta} \quad (10)$$

where h_m , the maximum undeformed chip thickness can be written as³:

$$h_m = \left[\frac{3}{C \tan \theta} \left(\frac{v_w}{v_s} \right) \left(\frac{a}{d_e} \right)^{1/2} \right]^{1/2} \quad (11)$$

and C is the number of active cutting points per unit area. Therefore, multiplying by the number of cutting points per unit time per unit width of grinding leads to an expression for the rate of total plowed surface per unit width generated by the cutting points:

$$S'_w = C v_s A_g \quad (12)$$

Substituting for h_m from Equation (11) and noting that $l_c = (ad_s)^{1/2}$ leads to

$$S'_w = \left(\frac{6C}{\sin 2\theta} \right)^{1/2} (v_w v_s)^{1/2} (a)^{3/4} (d_e)^{1/4} \quad (13)$$

The measured values of the grinding power per unit width for RBSN (Coors/Eaton RBSN) with the 400 grit size wheel are plotted versus the corresponding calculated values of S'_w using the measured values of $C = 107 \text{ mm}^{-2}$ with $\theta = 60$ degrees in Figure 6. The results show a nearly proportional relationship between power per unit width P'_m and S'_w . A similar result was obtained with the coarser grit wheel using $C=21 \text{ mm}^{-1}$ and $\theta = 60$ degrees. Such relationships were also found for numerous other workpiece materials.

Subsequently, the plots of the measured power per unit width versus total rate of plowed surface area for the various workpiece materials ground with two different grit size wheels were fitted to a simple linear relationship of the form:

$$P'_m = J_s (S'_w) + B_p \quad (14)$$

where J_s and B_p are fitting constants. Assuming that the total grinding energy is associated only with this plowing (scratching) and neglecting the influence of B_p , the slope J_s would correspond to the average energy per unit area of plowed surface generated. The slopes J_s for all the experimental results together with their standard errors and correlation coefficients from least square fitting of the data are listed in Table 2. Except for the S/RBSN, the standards errors are only about 5% of the estimated values and the correlation coefficients exceed 0.9. In all cases, the values of J_s are much bigger than the corresponding fracture surface energies, $G_c/2$, which is a further indication that most of the energy is associated with ductile flow on the plowed surface.

A simple linear relationship between P'_m and S'_w applies to all the experimental results *except* near the origin. All the plots of P'_m versus S'_w , except for the RBSN workpiece have smaller slopes near the origin, which may imply a smaller energy per unit surface area generated in this region. It is not clear why this different behavior exists near the origin. For the RBSN (Figure 6) an opposite

trend was observed with a steeper slope near the origin followed by a shallower slope as S'_w increases. The difference in behavior might be related to the inherent pores (holes) observed for this material.

4.2.2 Plowed Surface Energy and Workpiece Properties

The J_s values are nearly constant for a given workpiece regardless of grinding conditions and grit size. This would indicate that J_s is a “characteristic” property associated with plowing. Therefore it might be possible to relate J_s to the mechanical properties of the workpiece: elastic modulus E , fracture toughness K_c , and hardness H . The results obtained in the previous section for J_s are summarized in Table 2 together with the mechanical properties for the various workpiece materials.

From indentation fracture mechanics, the volumetric material removal per unit length obtained for a Vickers indenter can be related to the lateral crack size and the mechanical properties [9, 10]. From these models, the normal load can be expressed in terms of the mechanical properties for a given volumetric removal per unit length of travel, V'_l , as^[9]:

$$(P)^{5/4} \propto (V'_l)(K_c^{3/4}H^{1/2}) \quad (15)$$

A similar expression can also be obtained which includes the elastic modulus [10]:

$$(P)^{9/8} \propto (V'_l) \left(\frac{K_c^{1/2}H^{57/40}}{E^{4/5}} \right) \quad (16)$$

From Equations (15) and (16), it *might* be expected the plowed surface energy J_s for a given volume removal per unit length might be related to the combined mechanical properties on the right hand side of these equations, namely $K_c^{3/4}H^{1/2}$ and $K_c^{1/2}H^{57/40}E^{-4/5}$. However, a plot of J_s versus $K_c^{3/4}H^{1/2}$ yielded only a mediocre correlation with J_s proportional to $(K_c^{3/4}H^{1/2})^2$. Moreover, no real correlation was seen between J_s and $K_c^{1/2}H^{57/40}E^{-4/5}$ at all.

In this regard, the best correlation from all the above attempts was obtained using a proportional relationship between J_s and $(K_c^{3/4}H^{1/2})^2$, which indicates that $J_s \propto K_c^{3/2}H$. By utilizing this relationship, the measured values of grinding power per unit width were plotted versus $(K_c^{3/2}H)S'_w$. All the results in Figure 4.8 tend to fall close to the same straight line. For all the materials ground with both the coarse and fine grit wheels, the power per unit width can be approximated as:

$$P'_m = M(K_c^{3/2}HS'_w) \quad (17)$$

where $M \approx 6.4 \times 10^{-20} \text{ (N}^{-3/2}\text{m}^{13/4}\text{)}$.

4.3 HSLD GRINDING THERMAL ANALYSIS

In the previous two sections, the benefits of High-Speed, Low-Damage (HSLD) grinding were characterized in terms of grinding forces, energy, wheel wear behavior, and overall grinding mechanisms. In this section, additional insight is provided relative to the resulting workpiece temperatures developed under high speed diamond grinding conditions.

Typically, the grinding of ceramics at high wheel speeds (i.e. small grit depths of cut) requires high specific energies which, in turn, *may* lead to high grinding zone temperatures. Intuitively, high temperatures generated in grinding of ceramics may significantly affect the grinding mechanisms and resulting surface quality. However, it has been suggested that elevated grinding zone temperatures¹¹ could inhibit fracture and thereby promote a non-detrimental ductile mode of grinding. This may be related to the formation of a glassy amorphous layer on the workpiece surface as seen in Figure 5. Unfortunately, grinding with high wheel speeds *may* also adversely affect the process by reducing the useful life of a grinding wheel at extremely high speeds. In this regard, a significant portion of this program involved efforts to analytically and experimentally determine the HSLD grinding energy partition and resulting zone temperatures.

4.3.1 Preliminary Thermal Calculations

In an initial attempt to gain some preliminary understanding of how HSLD grinding parameters might affect the workzone temperatures, approximate thermal calculations were performed using the Carslaw-Jaeger^[4] moving band heat source solution as given by:

$$\Theta_m = \frac{1.13\alpha^{1/2}a^{3/4}V_w^{1/2}(\epsilon \cdot U_{total})}{k d_e^{1/4}} \quad (18)$$

Where;

- Θ_m = maximum grinding zone temperature rise
- α = thermal diffusivity = 0.016 in²/sec = 0.10cm²/sec
- a = depth of cut
- V_w = workpiece velocity
- ϵ = fraction of grinding energy conducted as heat into workpiece
- U_{total} = total specific grinding energy
- k = thermal conductivity = 17.34 BTU/hr.ft.^{°R} = 30 Watt/m^{°K}
- d_e = equivalent grinding wheel diameter

It must be emphasized that, as with most thermal analyses, a linear heat transfer model has been used with the assumption of *constant* thermal properties *independent* of temperature. In addition, this initial model does not include convective cooling effects. More importantly, the actual fraction (ϵ) of the total grinding energy (u_{total}) which enters the workpiece as heat is yet to be

identified. Typically, (ϵ) is on the order of 80 - 90% for conventional grinding of ferrous alloys (good conductors of heat) with aluminum oxide wheels (thermal insulator). In this case, however, since silicon nitride (thermal insulator) is being ground with diamonds (best conductor of heat) a **constant** value of $\epsilon = 50\%$ was used for these preliminary calculations. Based on these commonly used assumptions it is clear to see that *increasing* the specific grinding energy, U_{total} , will lead to *increased* temperature rise (Θ_m) within the workzone as predicted by equation (18).

Using the above relationship with mean thermal property data and actual grinding test data as given in the Phase I report [12] general temperature trends were then plotted in Figure 8. Recognize that the primary purpose of this graph is *not* to predict absolute workzone temperatures, but rather to illustrate what maximum grinding zone temperature trends might be predicted using commonly accepted grinding assumptions. From this perspective, the graph clearly shows the tendency toward increased workzone temperatures at elevated wheel speeds and material removal rates. The obvious concern, however, is just how accurate can these trends be using these assumptions?

4.3.2 Energy Partition (ϵ) Determination

In order to more accurately calculate the temperatures generated during grinding, it is necessary to know the fraction (ϵ) of the total grinding energy conducted as heat to the workpiece. At the University of Massachusetts, an inverse heat transfer method was developed to estimate the energy input to the workpiece from temperature measurements in the subsurface [13]. This energy is then compared with the total measured energy input, obtained from the measured grinding forces and power, to determine the fraction of the grinding energy conducted as heat to the workpiece. The technique using a thermocouple embedded in the workpiece was shown to be unreliable for ceramics. In this program, a two-color infrared detector and optical fiber system was developed for measuring temperatures in ceramic grinding [13].

Within the scope of this program, two 2-color infrared detector and optical fiber systems were developed. The first system utilizes a DC cell and an AC cell. The second system uses two DC cells. Extensive experiments were conducted to measure temperature rise while grinding silicon nitride ceramic with diamond wheels. The measured temperature results together with a thermal analysis were used to obtain the energy partition to the workpiece.

The infrared system utilizes a hole from the bottom side of the workpiece, analogous to the thermocouple system. A fiber inserted into the hole transmits radiation to the detector. A two-color detector was selected which consists of two photocells. The advantage of using a two-color detector rather than single detector is that the temperature is determined by the ratio of the signals received by the two cells, such that the emissivity of the material is not required to measure temperature. The surface conditions on both ends of the fiber also have less influence on the temperature measurement with the two-color detector.

Furthermore, any movement of the fiber along its length can affect the accuracy of the single detector system, but not the two-color system.

The infrared pyrometry system consisted of an Indium Antimonide (InSb) cell, a Mercury Cadmium Telluride (MCT) cell and a chalcogenide optical fiber of 0.020 inches core diameter. The system was calibrated by measuring the radiation from a preheated copper blackbody as it is slowly cooled in air. Two thermocouples were embedded in the blackbody to monitor its actual temperature. Additional system details are provided in [12].

After the calibration of the system, tests were conducted to measure the temperature response when grinding sintered reaction bonded silicon nitride (S/RBSN) using a 400 grit resin bonded diamond wheel. The operating conditions incorporated a workspeed $v_w=127$ mm/s, wheelspeed $v_s=30$ m/s, and depth of cut $a=0.0375$ mm.

By matching the theoretical temperature to the measured temperatures, the energy partition to the workpiece was obtained. The energy partition using the first system under conventional (i.e. slow speed) grinding was found to be 59% for **dry** grinding with the 120 grit wheel, 12% for wet grinding with the 120 grit wheel, and around 25 to 28% for wet grinding with the 400 grit wheel. After the set up and calibration of the second system, additional grinding tests were conducted to measure the sub-surface temperature responses, which were further used to calculate the energy partition to the workpiece. Some results are summarized in Table 3. The energy partition ranges from a value of about 9% to 21% for conventional diamond grinding.

Upon completion of the above tests at the University of Massachusetts, the second system was then shipped to Eaton to determine the grinding zone temperatures and energy partition under High-Speed, Low-Damage grinding conditions. The following wheels were used in a surface grinding mode at a material removal rate of 1 in³/min-in and wheel speeds up to 25,000 SFM. Test data is summarized in Table 1.

1) 180 grit vitrified bond	(D91-KZ125-VFR)
2) 180 grit resin bond	(D180-100-UI841)
3) 320 grit resin bond	(D320-100-UI841)

Using the approach described above the resulting grinding zone temperature was measured for each of these wheels as a function of wheel speed (see Figure 9). It can be seen that as wheel speed is initially increased, the temperature **decreases**. Note that the temperatures associated with the vitrified wheel *decrease continuously* for increasing wheel speeds over the range tested (5,000 to 20,000 ft/min). However, temperature for each of the resin wheels only decreases to a point (approximately 18,000 sfm) and then starts to increase with increasing wheel speed. Also note that the surface temperatures are much lower than expected. Measured temperatures range from 200 to 500°F, while the earlier estimates assumed that the temperature would be three to five times higher.

Why are the surface temperatures so **low** and why do they **decrease** with increasing wheel speed? As can be seen in Figure 10, the energy partition (ϵ) is

certainly not constant and actually *drops* as the wheel speed is *increased* - - - which indicates that the wheel is removing more heat from the workzone as wheel speed is increased. Intuitively, more “cold” diamonds are going through the workzone per unit time as wheel speed is increased, thus removing more heat.

It is also important to note that the energy partition is only on the order of 4 to 12%. This is very low compared to conventional steel grinding. For example, when grinding steel with conventional aluminum oxide wheels the energy partition can be as high as 80 or 90%. Even when CBN abrasive is used, this value is still around 20 to 40% - - - which is three to eight times higher than observed in these test cases. Apparently, when grinding a thermal insulator (ceramic) with the best conductor of heat known to man (diamond) the heat flow tends to go from the workpiece to the wheel.

As shown in Figure 11, however, there are two “competing processes” in action here - - - Although the energy partition generally decreases with speed, the specific energy increases with higher wheel speeds (due to increased rubbing and plowing), thereby resulting in a nearly constant or reduced workpiece surface temperature with increasing wheel speed. These competing processes can be seen more clearly in Figures 12 - 14. Aside from wheel speed effects, the affect of material removal rate on resulting surface temperature was also considered. As expected, increasing material removal rate generally tends to increase temperature (see Figure 15). For example, a doubling of the removal rate increases workpiece temperature approximately 1.5 times.

Given the above workpiece temperature behavior (i.e. low surface temperatures which further decrease with increasing wheel speed) a brief discussion on “bulk” versus “flash” temperatures is mandated. It is important to recognize that the infra-red pyrometer system measures an average field temperature (i.e. the optical fiber is 0.020 inches in diameter and has a field of view of 60°). Moreover, most heat transfer calculations for grinding have been historically developed for steel grinding applications - - - where the thermal grinding “damage” arises from **bulk** time/temperature effects, *not* from the localized flash temperatures at each grit/workpiece interface. However, based the data presented above, it appears that although the gross surface temperatures may be low, the localized flash temperatures at each diamond grit *can* still be very high and must not be neglected. Additional investigation is required to fully understand the thermal/mechanical attributes of HSLD grinding of ceramics.

4.4 HSLD GRINDING FLEXURAL STRENGTH CONSIDERATIONS

Thus far it has been shown that increasing wheel speeds will *generally* reduce grinding forces, improve surface finish, reduce "pull out" or surface fragmentation, increase wheel life, and reduce bulk workpiece temperatures. As indicated previously, the exception to this statement occurs when the wheel bond system (e.g. phenolic resins) cannot sustain wheel temperatures generated at speeds above 18,000 sfm. Consequently, in this section we examine the effect of increased wheel speed on resulting workpiece surface integrity using a maximum wheel speed of 18,000 sfm.

To evaluate workpiece surface integrity a series of Eaton S/RBSN tiles were ground lengthwise to produce approximately 22 transverse ground MOR specimens per tile. These specimens were then tested in a 4-Point bending test per MIL-STD-1942. For each of these tests, the Weibull modulus and characteristic strength (63.2 percentile value) were fully determined. Prior to conducting any tests, a comprehensive baseline material property characterization was performed using MIL-STD-1942. Under these baseline conditions the Eaton S/RBSN test material displayed a **baseline** flexural strength of **83.6 ksi** with a Weibull modulus of **16.68**.

4.4.1 Wheel Dressing Considerations for MOR Strength Evaluation

It has been shown that the condition of the grinding wheel surface can directly affect the resultant strength of MOR specimens. Therefore, to eliminate any "noise" in the data due to wheel dressing variations, a repeatable dressing and truing process that results in a uniform wheel surface was developed prior to conducting any MOR tests. While it may be nearly impossible to generate the exact same wheel surface before each test, the dressing procedure should yield a surface that will allow the best chance of evaluating other process parameters (i.e. feeds and speeds) independently of dominant dressing influences.

In these tests a 180 grit resin bonded wheel (D180-100-UI841) was employed. Surface evaluation of samples ground to characterize the dressing process revealed deep scratches and valleys in the workpiece indicating a slightly tapered wheel with some "high diamonds." Given this condition of the wheel, it would be very difficult to ascertain the effects of grinding parameters (i.e. wheel speed, material removal rate, etc.) on the modulus of rupture (MOR) strength of the specimens. As such, further dressing techniques were studied.

The wheel was initially dressed with a titanium roll dresser followed by a 220 grit Al₂O₃ stick. A grinding pass, where the entire width of the wheel was ground across a S/RBSN workpiece, was then made to assess the "goodness" of dress. The profile of the cut made in the ceramic specimen was measured using a Mahr Perthen F2P Formtester and a Precision Devices Surfometer. Surface finish traces made from the workpiece ground after the titanium/sticking process show an Rt value of 219 microinch and the peakcount (for a 50 minch threshold) is 207. The Rt value indicates that deep scratches are being

introduced into the workpiece while the peakcount represents the quantity of scratches.

After several iterations a second dressing method was developed. Following the same titanium dressing process as before, the grinding wheel was fed across a horizontal cup wheel mounted on a Read Diamond Products RS-60 dresser. The cup wheel was a 320 grit green carborundum abrasive. A pass was again made across an S/RBSN specimen and characterized as described above. The surface of the wheel had improved to the point that it was now generating a surface with an Rt value of 94.1 microinch and a peak count of 33. The quantity and severity of the scratches in the workpiece has been diminished significantly.

4.4.2 Effect of Step-over Distance

Once having established a uniform dressing procedure the effect of crossfeed on transversely ground MOR specimens was evaluated. To date, considerable discussion has taken place on the validity of longitudinal versus transverse grinding to determine MOR strength as a function of grinding conditions. With longitudinal grinding, the grinding scratches are parallel to the primary stress direction. Typically, transverse grinding is desired such that the grinding scratches are perpendicular to the primary stress direction thereby accentuating the effects of the grinding conditions. In this regard, it is generally felt that longitudinally ground MOR bars do not give a true indication of MOR strength as a function of grinding parameters. Recognize that a typical MOR bar is 3 x 4 x 45 mm while the wheel is on the order of one half to one inch wide. Therefore, in order to generate transverse grinding topography, the wheel must be crossfed across the bar. This crossfeeding routinely results in a "scratch" or ridge being left at each step-over interval.

The ceramic tile specimens used in these tests were approximately 2" wide x 6" long. Ideally, in order to grind the specimens lengthwise, a wheel at least 2" wide would be required to grind the entire width without any crossfeed (step-over). Unfortunately, this "wide wheel" approach was not possible due to limitations with the available grinding machine. Consequently, most transverse ground MOR specimens produced today are ground using a relatively narrow wheel which is crossfed or stepped-over the workpiece width. Typically, a 0.070 inch step-over is used in these type of tests.

With the above concerns in mind, an initial starting point of 0.070 inch step-over was used. Then, after running two tests with the 180 grit resin wheel (at 6,000 and 12,000 ft/min), the workpiece surfaces were examined. As presented in Figure 16, this 0.070 inch step-over created 1 micron sized defects at each step interval. In an effort to minimize the potential impact of these crossfeed effects, several step-over distances were evaluated. Based on resulting workpiece surface condition and corresponding test time a step-over distance of 0.015" was selected. As shown in Figure 17, the resulting surface is not quite as straight as with the 0.070" step-over, however the incremental defect size is considerably less.

Based on the above finding, a series of MOR tests (i.e. 22 MOR bars per condition) were then conducted to compare the relative strength under each step-over condition. The results of these tests are shown in figure 18. It can be seen that at 6,000 SFM, the effect of step over amount on flexural strength appears negligible (i.e. only 2 KSI increase). However, as wheel speed is increased to 12,000 sfm, the smaller crossfeed produces 4 KSI increase in strength. Given these trends it was anticipated that further increases in wheel speed would result in even greater MOR strength differences. As such, all of the following MOR tests were conducted using a crossfeed of 0.015" step-over.

4.4.3 MOR Flexural Strength as a Function of Wheel Speed

To evaluate the effect of grit size, bond type and wheel speed on S/RBSN flexural strength, a 3×2^2 factorial experiment was developed. Two grit sizes, two bond types, and three wheel speeds were selected while the material removal rate was kept constant at a high level ($1 \text{ in}^3/\text{min-in}$). Again, the philosophy behind these tests was to identify a single step, roughing-finishing process suitable for producing high-quality silicon nitride ceramic parts at high material removal rates and at substantially lower cost than traditional, multi-stage grinding processes.

Based on previous testing, it was shown that a 120 grit wheel was far too coarse for effective finishing while a 320 grit was too fine for high-speed roughing. From this perspective, grit sizes of 180 and 240 were selected. From practical application considerations, wheel bond types were limited to readily dressable polyimide resin and conventional vitrified systems. Although metal bonds do show considerable potential, excessive dressing times will reduce overall cost effectiveness (see following economic cost model section). Accordingly then, wheel speeds were limited to 6,000; 12,000; and 18,000 ft/min to avoid spontaneous bond ignition and/or disintegration at excessively high speeds with these types of bond systems. The following wheels were evaluated:

180 resin D180-100UI841	240 resin D240-100UI841	180 vitrified D91KZ125VFR	240 vitrified D64KZ125VFR
----------------------------	----------------------------	------------------------------	------------------------------

Using the above conditions, S/RBSN tiles (6 in x 2 in) were up-ground in the six inch direction. Cincinnati Milacron CX-270 coolant diluted at 12:1 was supplied at 40 PSI and 6 GPM throughout all tests. The material removal rate was kept constant at $1.0 \text{ in}^3/\text{min/in}$ for all of the tests with a step over distance of 0.015 inches. After grinding, the tiles were then sliced in the two inch direction to yield transverse ground MOR Type B specimens which were subsequently broken in a 4-Point flexure test. The resulting flexural strength and Weibull Modulus data presented in Table 4 and Figure 19. (Recall that the baseline strength of this S/RBSN material, per ASTM C 1161/MIL-STD-1942 is 83.6 KSI with a Weibull modulus of 16.68).

Figure 19 provides a plot of flexural strength versus wheel speed for the above four wheel types. For all wheels, as wheel speed is **increased**, the

flexural strength is **increased**. What is amazing to note is that at 18,000 SFM the 240 grit resin bonded wheel produced much higher quality parts (i.e. 85.6 KSI with a Weibull modulus of 19.49) than those generated under slow MIL-STD-1942 (ASTM C 1161) conditions! Recognize that the material removal rate used in these tests was over **10 times greater** than that used in the baseline MIL-STD-1942 condition! Even grinding with the coarse 180 grit resin wheel produced parts which exceeded baseline properties while grinding at over 10 times the MIL-STD-1942 removal rates! This is also important to note, since, in general, coarser grit wheels will last longer than finer grit wheels [3]. The 240 vitrified wheel also performed well at the highest wheel speed (80.1 KSI and 22.36 Weibull). Considering that vitrified bond wheels have higher temperature capabilities than resin systems, even higher wheel speeds could probably be employed to achieve baseline like material properties with very low wheel wear *while* sustaining high material removal rates. On the other hand, what this data also clearly shows is that the use of relatively coarse wheels operating at high removal rates with *conventional* wheel speeds (i.e. 6,000 sfm) will reduce material property performance - - - as expected.

Considering the performance attributes of HSLD grinding demonstrated thus far (i.e. reduced forces, reduced wheel wear, improved surface finish, increased MOR strength, and elevated material removal rates) it is fair to state that a single step, roughing-finishing process suitable for producing high-quality silicon nitride ceramic parts at high material removal rates has been developed.

4.4.4 Predicting MOR Flexural Strength

Once having generated the above MOR test data, attempts were made to predict the resulting S/RBSN material strength as a function of the grinding parameters. Building upon the grinding mechanics model developed in section 4.2, it was felt that the "plowed" surface area generation rate (S'_w) may correlate well with resulting MOR strength for HSLD grinding. However, by plotting the resulting MOR flexural strength for **all** of the above tests as a function of (S'_w) a bi-modal linear relationship resulted. As Figure 20 indicates, the data falls into two families. It can be seen that for a given (S'_w), the resin wheels display a higher MOR strength than the vitrified bonded wheels.

Recall from Section 4.2.1, the rate of plowed surface area generation was given by Equation (13) as:

$$S'_w = \left(\frac{6C}{\sin 2\theta} \right)^{1/2} (v_w v_s)^{1/2} (a)^{3/4} (d_c)^{1/4}$$

It is important to recognize that this parameter is based upon the theoretical or geometric arc length of contact (l_c), where $l_c = (ad_s)^{1/2}$. In reality, however, elastic deflections between the wheel and the workpiece will cause the actual arc length of contact to be considerably longer [14-16] than the geometric contact length (l_c). In this case the soft polyimide resin will deflect far more than

the vitrified bond under heat and pressure, thereby increasing contact length and reducing unit load per grit. Interestingly, if the geometric contact length were multiplied by a factor of 1.7 for the resin bonded wheel, then all of the data would fall on one line. However, for purposes of completely predicting MOR strength these expressions would become far more complex requiring numerous empirical correlation coefficients which are beyond the scope of this program. Obviously, additional analyses are required.

4.5 HSLD PRODUCTION COST DRIVERS AND ECONOMIC ANALYSIS

To characterize HSLD cost performance, a comprehensive cost model was developed to predict grinding related costs for a single plunge centerless grinding operation over a range of material removal rates and wheel speeds. Actual HSLD G-Ratio data was used to develop the abrasive costs and dressing intervals as a function of removal rate and wheel speed. Several objective functions were analyzed using the fundamental model. The initial model was constrained to find the maximum number of parts that can be produced on one machine given a total machine uptime (hours/yr). Outputs from the model, as a function of material removal rate and wheel speed, include the following:

- Total cost per piece
- Wheel cost per piece
- Labor cost per piece
- Overhead cost per piece
- Capital cost per piece (10 year depreciation)
- Wheel cost per piece
- Dressing roll cost per piece
- Number of pieces produced
- Number of pieces per dress
- Number of wheels required
- Grind time per piece
- Total cycle time per piece

4.5.1 HSLD Cost Model Development

The total cost per piece is determined as a function of the various costs associated with the manufacturing process: capital, labor, and consumables. Two approaches can be taken to constrain the model based on capacity. One method is to set a production target and then determine the number of machines required. This initial model, instead, was constrained by fixing capital at one machine and determining the number of pieces that could be produced.

Following is the development of the expressions used to compute the costs per piece and maximized production on one machine. The equations were subsequently coded in Matlab and integrated with a graphical user interface to create an interactive cost model.

The first expression is the grinding time required for one piece. For an OD plunge operation, the expression is simply the amount of material to be ground away divided by the material removal rate.

$$t_{\text{grind}} = \frac{\pi \cdot \text{RadialStockEnvelope} \cdot (\text{InitialWorkDiameter} - \text{RadialStockEnvelope})}{\text{SpecificMaterialRemovalRate}} \quad (19)$$

Intuitively, the higher the material removal rate, the less time is spent grinding the part. However, as the experimental wheel wear data indicated (Section 4.1.4), at a given wheel speed increasing the material removal rate also increases the wheel wear rate. An Increased wear rate then requires more frequent dressing and wheel changes - - thus reducing the amount of time available to make parts and *possibly* increasing costs. Therefore, it is important to understand the relationship between process parameters and the wheel wear rate. Referring to Figure 4, the experimental G-Ratio (grinding ratio) is given as a function of removal rate and wheel speed for a standard 240 grit resin bonded diamond wheel. From this data, an expression to predict the G-Ratio as a function of material removal rate and wheel speed was derived:

$$G - Ratio = [-31.408 \cdot MaterialRemovalRate + 44.713] \cdot e^{(0.0001 \cdot WheelSpeed)} \quad (20)$$

Using the G-Ratio equation (20) and the requirement that the wheel must be dressed after a given level of radial wheel wear has occurred, the number of pieces per dress can be calculated as:

$$NumberofPiecesperDress = G - Ratio \cdot \frac{InitialWheelDiameter^2 - WornWheelDiameter^2}{InitialWorkDiameter^2 - FinalWorkDiameter^2} \quad (21)$$

The total time to make one piece is comprised of load/unload time, an averaged value for setting up the machine, time actually spent grinding the part, and an averaged time spent dressing the wheel:

$$TimeperPiece = t_{load/unload} + \frac{t_{setup}}{LotSize} + t_{grind} + \frac{t_{dress}}{NumberofPiecesperDress} \quad (22)$$

Given the total number of hours machine available to make parts and the total time per piece (22), the number of pieces that can be produced in a year is:

$$TotalPiecesMade = \frac{TotalTimeAvailablefor Production}{TimeperPiece} \quad (23)$$

TotalPiecesMade represents the total number of pieces that were processed on the machine. To determine the number of good parts produced, this number must be modified by the scrap factor.

$$NumberofGoodPiecesProduced = (1 - ScrapRate) \cdot TotalPiecesMade \quad (24)$$

Further utilization of equation (20) will predict the number of wheels that will be required in grinding the total production run.

$$NumberofWheels = \frac{TotalPiecesMade}{G - Ratio} \cdot \frac{InitialWorkDiameter^2 - FinalWorkDiameter^2}{InitialWheelDiameter^2 - FinalWheelDiameter^2} \quad (25)$$

Depending on the efficiency and other requirements of the dressing process, it may require several "dressing media" per wheel. The exact number would be based upon dressing method and experience.

$$\text{NumberofRolls} = \text{NumberofRollsperWheel} \cdot \text{NumberofWheels} \quad (26)$$

The capital cost/piece, wheel and roll costs per piece are functions of the cost of each respective item, the number of each required (recall that capital was constrained to one machine) and the number of good pieces produced. Capital cost per piece is also dependent on the depreciation rate, which was assumed to be linear over 10 years for this model.

$$\text{CapitalCostperPiece} = \frac{\text{MachineCost}}{\text{DepreciationRate} \cdot \text{NumberofGoodPieces Produced}} \quad (27)$$

$$\text{WheelCostperPiece} = \frac{\text{WheelCost} \cdot \text{NumberofWheels}}{\text{NumberofGoodPieces Produced}} \quad (28)$$

$$\text{RollCostperPiece} = \frac{\text{RollCost} \cdot \text{NumberofRolls}}{\text{NumberofGoodPieces Produced}} \quad (29)$$

Labor and overhead costs are functions of the amount of time spent making a part. Multiplying the *TimeperPiece* by the appropriate cost rate gives the labor and overhead costs per piece. This initial model assumed that one operator would be assigned to the one machine.

$$\text{LaborCostperPiece} = \text{TimeperPiece} \cdot \text{LaborRate} \quad (30)$$

$$\text{OverheadCostperPiece} = \text{TimeperPiece} \cdot \text{OverheadRate} \quad (31)$$

Finally, a total cost per piece can be calculated by summing the individual contributing costs:

$$\text{TotalCostperPiece} = \text{CapitalCostperPiece} + \text{WheelCostperPiece} + \text{RollCostperPiece} + \text{LaborCostperPiece} + \text{OverheadCostperPiece} \quad (32)$$

4.5.2 HSLD Cost Model Input Data

Table 5 details the input variables to the model and values selected to represent a nominal operating condition. A centerless plunge operation using a 240 grit resin wheel grinding Norton NT551 was selected for this scenario.

While all of the assigned values are arbitrary, they were selected as reasonable values for a one shift operation to grind blanks that are either *near net* or have been processed leaving a *finishing* stock envelope. Also, it was assumed that one operator would be assigned to the one machine.

The total number of hours was based on one annual shift (2080 hours total available). After subtracting out vacations, holidays, downtime and other

non-production periods, 1,600 hours remain. A value of 1,600 hours is on par with industrial engineering studies of typical manufacturing environments representative of this scenario which indicate that a machine is utilized roughly 400 minutes out of every 480 minute shift.

It should be cautioned that the G-ratio data used in the model is specific to this wheel grit size and bond type, material, coolant type and supply. Further, the model should be limited to the range of wheel speeds [6,000 - 18,000 ft/min] and material removal rates [0.1 - 1.0 in³/min/in] which were used to experimentally determine process performance. Extrapolating to values outside this test region could lead to erroneous results.

Building upon the equations above and the nominal input data given in Table 5, a cost sensitivity analysis was performed as a function of material removal rate, grinding wheel speed, and the following variables:

Labor Rate:

1. \$10 / hour, 0% overhead rate (low)
2. \$15 / hour, 80% overhead rate (nominal)
3. \$20 / hour, 150% overhead rate (high)

Dressing Time:

4. 20 seconds (low)
5. 2 minutes (nominal)
6. 15 minutes (high)

It is important to recognize that in many production operations the overhead rate is generally much higher (up to 600% for some operations). However, in this case, most of the costs associated with overhead have been already accounted for directly (e.g. wheels, dressers, etc.). As such, the overheads given above have been modified accordingly to reflect only those costs which have not been directly incorporated in this cost model. Beyond this, the dressing times given above were based on experience with conventional resin and Vit bond systems and do not include the dressing associated with mounting/truing a new wheel which may have considerable runout.

4.5.3 HSLD Cost Model Results

Results of the above sensitivity analysis are presented in Table 6. What is interesting to note is that in all cases increasing wheel speed reduces costs. As previously shown by the G-Ratio data (Figure 4), increasing wheel speed reduces the wheel wear rate. Obviously then, increased wheel life results in more pieces per dress, higher output and lower per piece cost. What is even more astonishing, however, is that the cost benefits gained by increased wheel speeds far outweigh those achieved from reducing labor rates! As shown in Figure 21, the final per piece costs obtained at 18,000 sfm with a high labor rate are **lower** than those using a conventional wheel speed of 6,000 sfm and a low labor rate. Moreover, due to the dominant abrasive cost effects, the cost

advantages arising from high wheel speeds become even more pronounced as the material removal rate is increased.

These cost advantages are more clearly illustrated in Figure 22, which shows cost as a function of removal rate for three wheel speeds (6,000, 12,000, and 18,000 sfm) while using the high labor rate and the nominal 2 minute dressing cycle time. Again, as wheel speed is increased the costs are reduced. However, as the material removal rate is increased, the costs initially tend to decrease slightly, then begin to increase due to excessive wheel wear. This behavior can best be seen in Figure 23 which focuses on the costs developed using the high wheel speed and high labor rate.

Figure 23 provides a plot of all costs (total cost per piece, labor cost per piece, consumables cost per piece and capital cost per piece) versus material removal rate. Notice that at the low material removal rates, the labor cost dominates since total wheel wear is being minimized. Then, once the material removal rate becomes sufficiently high to cause rapid wheel wear, the consumables begin to dominate. In all cases, the cost contribution from capital is minimal. Consequently, by increasing material removal rate, a minimum cost is reached at the point where the production rate is sufficient to cost effectively utilize labor even though the consumables content of the total price increases continuously. Therefore, it is critical to increase wheel speed to extend the life of the wheel.

Further analysis of Figure 23 shows that the minimum cost occurs at a material removal rate of approximately $0.5 \text{ in}^2/\text{min}$. The trade-off of speed versus wheel life appears to be balanced at this point. However, should improvements be made in the process (more durable wheel bond systems, improved coolant and delivery, etc.), even higher material removal rates could be cost-effectively achieved.

Other important system improvements must address wheel dressing. As shown in Figure 24, the per piece costs and annual production rates can vary significantly depending on the speed of the dressing procedure. Obviously, the shorter the dressing cycle time the lower the resulting costs. Significant gains are achieved by going from a fifteen minute dressing cycle to a 2 minute dressing cycle - - - particularly at the highest removal rates where dressing frequency is a concern due to increased wheel wear. Notice, however, the benefits arising from using a 20 second dress versus a 2 minute dress only amount to a 1 cent difference when using high wheel speeds. For comparison, the resulting costs for various dressing rates are shown in Figure 25 using a slow 6,000 sfm wheel speed. Again, the benefits of improved dressing are clear. However, by comparing Figures 24 & 25, it is obvious that high wheel speeds are the real key to cost effective ceramic grinding.

4.6 HSLD CENTERLESS GRINDING CONSIDERATIONS

Centerless grinding is widely used in industry for precision machining of cylindrical components because of its high production rate, easy automation, and

high accuracy. Most centerless grinding is performed in the above-center mode, with the workpiece center located above the grinding wheel and regulating wheel centers. The workpiece supported by the work rest blade is driven by the regulating wheel as the grinding wheel machines away material.

The ability of the centerless grinding operation to produce parts with acceptable roundness depends on the geometric set-up conditions. However, unless the grinding wheel and workpiece are on the same horizontal centerline, the geometric conditions progressively change as the grinding wheel wears away and becomes smaller. Furthermore, uncontrolled workpiece spin often occurs with small and light parts, such as ceramic components, whereby the peripheral workpiece velocity becomes much faster than that of the regulating wheel. One way to avoid workpiece spinning is by using below-center grinding. In this case, the workpiece center is located at the same height as the grinding wheel center but below the regulating wheel center. However, geometric rounding stability has been found to be more difficult to achieve with below-center grinding. Practical experience suggests that there is only a relatively narrow range of set-up conditions which can give satisfactory results without excessive lobing.

The purpose of this part of the work is to simulate the centerless grinding process on a personal computer. The simulation program consists of two major parts. In the first part, a static analysis is presented to predict whether or not the workpiece will spin under various setup conditions. In the second part, a kinematic simulation of the process is presented and also calculates the profile of a workpiece after a number of workpiece revolutions from the initial workpiece profile and setup conditions for both above-center and below-center grinding.

4.6.1 Static Analysis of Spinning

For predicting the spinning condition, a static analysis of the centerless grinding operation was conducted. Based on this analysis, a spinning parameter was introduced which is a function of set up conditions including blade angle, below center angle, friction coefficients between various contacts, grinding conditions, force components, and workpiece weight. A positive spinning parameter indicates that the part will spin, and a negative spinning parameter indicates that the part will not spin.

From a simple torque balance on the workpiece, it can be readily shown that spinning is avoided if

$$\Omega = 1 - \frac{\mu_s F_s + \mu_r F_r}{\lambda F_n} \leq 0 \quad (33)$$

where μ_s and μ_r are friction coefficients at the workpiece-blade contact and the workpiece-regulating wheel contact, respectively, λ is the ratio of the tangential to normal grinding force, F_n is the normal grinding force, and F_s and F_r are the normal forces at the workpiece-blade contact and the workpiece-regulating wheel contact, respectively. Furthermore, force equilibrium of the workpiece also requires that

$$\begin{bmatrix} I & \cos\phi_{rs} - \mu_r \sin\phi_{rs} \\ \cos\phi_{rs} - \mu_r \sin\phi_{rs} & I \end{bmatrix} \begin{bmatrix} F_r \\ F_s \end{bmatrix} = \begin{bmatrix} F_n(\lambda \cos\phi_r - \cos\phi_r) + W \cos(\phi_r - \phi_w) \\ F_n(\lambda \sin\phi_s - \cos\phi_s) + W \cos(\phi_w - \phi_s) \end{bmatrix} \quad (34)$$

where ϕ_s , ϕ_r , and ϕ_w are angles from the grinding point to the workpiece-blade contact, the workpiece-regulating wheel contact, and the vertical direction as shown in Figures 26 and 27, $\phi_{rs} = \phi_r - \phi_s$, and W is the workpiece weight downwards. For below-center grinding (Figure 27), $\phi_w = 90$ deg.

From equation (33), it is evident that bigger friction coefficients, μ_s and μ_n , and a smaller force ratio, λ , favor the no-spinning condition. The no-spinning condition also depends on the angles ϕ_s and ϕ_r . It can be seen that a smaller ϕ_s and larger ϕ_r are desirable to avoid spinning. For conventional above-center grinding typically $\phi_r = 174$ deg and $\phi_s = 60$ deg, whereas for below-center grinding $\phi_r = 210$ deg and $\phi_s = 100$ deg. Clearly, the non-spinning condition can be satisfied much more readily for below-center grinding than for above-center grinding. For this example, the workpiece will not spin if $\phi_s < 135$ deg for below-center grinding and $\phi_s < 60$ deg for above-center grinding.

4.6.2 Kinematic Analysis of Above and Below Center Grinding

The centerless grinding process generally begins with an out-of-round workpiece. The object of the simulation is to predict how the workpiece geometry progressively changes as grinding proceeds. During grinding, the workpiece supported by the work rest blade is driven by the regulating wheel as material removal occurs at the contact zone between the workpiece and the grinding wheel. At any instant, the position of the workpiece is determined by its contact points with the regulating wheel and the work rest blade. Neglecting system deflection, that part of the workpiece interfering with the grinding wheel is considered to be removed by grinding.

A model governing the kinematic relationship among the workpiece, grinding wheel, regulating wheel, and work rest is needed in order to simulate a grinding process in a personal computer. Consider the typical centerless grinding process as shown in Figure 26. After the initial setup, positions of the work rest and the regulating wheel center are determined. At any moment during grinding, the position of the workpiece is determined by the contact point between the workpiece and the regulating wheel, and the contact point between the work rest and the workpiece. Part of the workpiece that interacts with the grinding wheel will be removed. A model describing this kinematic relationship has been developed[17]. Depending on the initial profile of the workpiece and the setup conditions of the grinding process, an initially non-round part may or may not be ground to round.

4.6.3 Software Development for Kinematic Rounding

The kinematic simulation program consists of three modules. An input module is for inputting the initial profile of the workpiece, grinding process setup conditions, displays the initial workpiece profile and the grinding process setup. A grinding module "grinds" the workpiece from its initial profile according to requirements imposed by the setup parameters and process parameters. An output module writes the final profile of the workpiece into a file and displays the final profile with the initial profile.

The grinding module works as follows. For a workpiece of given initial profile, its profile is first transformed into a polar coordinate system (ρ_i, θ_i) . Then the program finds the contact point between the workpiece and the work rest, which is the point corresponding to the maximum projection of the workpiece profile in the direction perpendicular to the work rest. The workpiece is then made to contact with the work rest at this point. By finding the maximum of the summation of the projection of workpiece profile and the projection of the regulating wheel profile in the direction parallel to the work rest, the contact point between the workpiece and the regulating wheel is obtained. By making the workpiece contact both the work rest and the regulating wheel, its center position at this moment is determined. Similarly, by finding the maximum of the summation of the projection of the workpiece profile and the projection of the grinding wheel profile in the horizontal direction, the initial contact point between the workpiece and the grinding wheel is obtained. The grinding wheel is then brought into contact with the workpiece. The distance between the center of the regulating wheel and the grinding wheel at this moment is recorded. The workpiece center position relative to the regulating wheel center is also recorded. The "grinding" is then started.

The workpiece is made to rotate a small angle. The new contact point between the workpiece and the work rest and that between the workpiece and the regulating wheel are recalculated. The new workpiece center position is also found. The grinding wheel is moved towards the workpiece by an amount of the infeed during this period of time. The summation of the projection of the workpiece profile, the projection of the grinding wheel in the horizontal direction near the grinding point, and the distance between the workpiece center and the regulating wheel center is compared with the distance between the center of the grinding wheel and the center of the regulating wheel at this moment. If the former is greater than the latter for any point, material removal occurred for that point. The workpiece polar coordinate for this point is updated based on the amount of material removal. Then the workpiece is made to rotate successive increments until the grinding cycle is over. A complete description is given in [17].

4.6.4 Simulation Results

Both the grinding machine set-up conditions and initial workpiece profile influence the final workpiece profile. In this section, a few simulation examples

are presented for below-center grinding.

For the first example, consider the following setup: wheel diameter $d_s=407.4$ mm (16 inches), regulating wheel diameter $d_r=228.6$ (9 inches), workpiece diameter $d_w=9.525$ mm (3/8 inch), blade angle $\beta=15$ deg, and below center angle $\alpha=27$ deg. The simulation was run for workpiece profiles initially containing 2, 3, 4, and 6 lobes. The final workpiece results are given in Table 7. It can be seen that a six-lobe profile is generated for an initial profile with 2, 3 (see figure 28) and 6 lobes, and an eight-lobe profile for an initial profile with 4 lobes. Since the final lobe amplitudes are bigger than the initial ones, this would indicate that the set-up is unstable for a below-center angle of 27 deg.

In order to verify the simulation results, incoming TTZ parts which displayed 2 and 3 lobes in the “as-fired” condition, were ground on a Modler below-center machine with the following parameters: $\alpha=27$ deg, $d_s=407.4$ mm, $d_r=228$ mm, $\beta=30$ deg, wheel velocity $v_s=22.5$ m/s, regulating wheel velocity $v_r=2.4$ m/s, and infeed velocity $v_i=63.5$ mm/s. The experimental results, shown in Figure 29, clearly show that incoming parts with 2 or 3 lobes will degenerate to a six lobe configuration when the machine is setup using a below-center angle of $\alpha=27$ degrees. Additional experimental results with different regulating wheel speeds, infeed rates, and through-feed velocity also gave 6-lobe profiles. In terms of lobe generation, the simulation results agree quite well with the experimental results.

In order to find satisfactory below-center angles which can produce parts without excessive lobing, the simulation was run with the below-center angle α varied from 5 to 30 deg for workpieces initially having 2, 3, 4, and 6 lobes. The results are summarized in Table 7. Here it can be seen that there is no single set-up condition which will eliminate workpiece lobing. In most cases, below-center angles $\alpha=23$ deg or bigger appear to be unfavorable for lobe removing. For a 20-degree below-center angle, the 3-lobe and 6-lobe profiles can be ground round, but not the 2-lobe and 4-lobe profiles. For below-center angles less than 20 deg, higher frequency lobes are generated (except for the 3-lobe profile which remains a 3-lobe profile), but the lobe amplitudes become much smaller. For a 10-degree below-center angle, the rounding effect for 2, 4 and 6-lobe profile is very good, but not for the 3-lobe profile. It should be noted that one of the main reasons to use below-center grinding was to prevent workpiece spin for small and light parts under aggressive grinding conditions. However, smaller below-center angles tend to increase the tendency for workpiece spin.

Based on these simulation results, another set of experiments was conducted on the Modler for the same TTZ parts as above using $\alpha=20$ degrees. As can be seen in Figure 30, the in-coming parts having either 3 or 6 lobes round-up quite nicely using this 20 degree below-center setup. However, at $\alpha=20^\circ$ the 2-lobed parts tend to degenerate to an 8-lobed configuration, as was shown in the simulation (Table 7). Given all of this information, a good “compromising” HSLD centerless setup might use a below-center setup with $\alpha=13^\circ$.

5.0 SUMMARY

The overall objective of this contract was to develop a single step, roughing-finishing process suitable for grinding high-quality silicon nitride ceramic parts at high material removal rates and at substantially lower cost than traditional, multi-stage grinding processes. The program employed the combined use of laboratory grinding tests, mathematical grinding models, and characterization of the resultant material surface condition. More specifically, development of logical methodologies to enhance and optimize the High Speed Low Damage (HSLD) ceramic grinding process required characterizing the effects of wheel speed and material removal rate on the resulting mode of material removal, limiting grinding forces, and approximate grinding zone temperatures. Subsequently, strategies for production utilization were established. To characterize HSLD cost performance, a comprehensive production cost model was developed to predict grinding related costs for a single plunge centerless grinding operation over a wide-range of material removal rates and wheel speeds. To facilitate production implementation, a centerless grinding computer simulation was developed for selection of grinding setup conditions necessary to achieve satisfactory roundness.

From a theoretical perspective, the University of Massachusetts focused on providing a fundamental understanding of the HSLD grinding mechanisms and associated hydrodynamic effects. Initially, grinding experiments were performed on various ceramics and a soda-lime glass to measure the grinding forces and power over a wide range of conditions. Measured grinding forces were observed to increase as either the depth of cut or workpiece velocity increases and to decrease as the wheel velocity increases. Specific energies for various ceramics ranged from 6 J/mm^3 to about 450 J/mm^3 depending on the removal rates. The specific energy became extremely large at low removal rates especially with a finer grit wheel, and decreased with higher removal rates tending toward a nearly constant value.

SEM observations of the grinding debris revealed powder-like fine particles approximately 1 mm in size, which may have been generated by crushing, and much larger plate-like fractured particles up to about 250 mm in size which may have been generated by lateral cracking. SEM observations of the ground surfaces mainly revealed characteristic scratches associated with plowing along the grinding direction. For silicon nitride ceramics, a smeared surface layer was removed by etching with hydrofluoric acid (49% HF), thereby indicating that it consists of glassy phase probably formed by oxidation at elevated grinding temperatures. Irreversible deformation was found to extend $1 \sim 5 \text{ mm}$ beneath the ground surface. Material removal appeared to occur mainly by brittle fracture, although ground surfaces reveal a heavily deformed layer associated with ductile flow.

The specific brittle fracture energy associated with the formation of grinding debris was estimated to constitute a negligible portion of the measured specific grinding energy. It was concluded that most of the grinding energy is expended by ductile flow. The grinding energy was analyzed in terms of the plowed surface area generated by the cutting points on the wheel surface. For this purpose, the rate of plowed surface area generated per unit width S'_w was obtained from the grinding kinematics. The measured power per unit width for a given workpiece material was found to increase nearly proportionally with S'_w . This suggests a constant energy per unit area of plowed groove J_s . Values for J_s are much bigger than the corresponding fracture surface energy, $G/2$, which further indicates that most of grinding energy is associated with ductile flow on the plowed surface. A proportional relationship was found between J_s and $K_c^{3/2}H$.

Additional experiments were conducted to measure the hydrodynamic forces in grinding. Hydrodynamic forces can become quite large when using non-porous wheels at higher speeds. Conversely, for porous wheels (e.g. vitrified bonds) the hydrodynamic forces were shown to reach a peak then decrease with increased speed. For non-porous wheels, it was found that classical hydrodynamic theory could be used to estimate the hydrodynamic force at the inlet to the grinding zone by adding a correction factor to the gap thickness equal to the peak-to-valley roughness of the wheel surface.

A two-color infrared detector system was also developed to measure grinding zone temperatures in real time. Extensive experiments were conducted for grinding silicon nitride ceramics using diamond wheels while measuring the workpiece temperature rise and grinding power. The energy partition was estimated by matching the measured temperature response to the thermal analysis. For dry grinding, the energy partition reached 59%. However, for conventional wet grinding, the energy partition to the workpiece was between 9 - 21%. Under HSLD grinding conditions it was interesting to observe that the energy partition decreases with increasing wheel speed and is only on the order of 4 to 12%. Consequently, the resulting grinding zone temperature was shown to decrease (in most cases) as the wheel speed was increased. In general, the measured surface temperatures were much lower than expected - - - being on the order of only 200 to 500°F using HSLD grinding conditions.

Although relatively low levels of grinding energy actually enter the workpiece as heat, it was observed that considerable wheel heating can occur under HSLD conditions. Given sufficient high-temperature bond strength this is generally not an issue. However, for most resin wheels, a maximum wheel speed of 18,000 sfm was used to avoid spontaneous bond ignition. Based on this apparent speed/temperature speed limitation a series of wheel wear tests were performed to characterize wheel wear as a function of wheel speed and removal rate. It was shown that *increasing* wheel speed will *reduce* wheel wear. At 1.0 in³/min/in material removal rate, **increasing wheel speed** from 6,000 to 18,000 ft/min **produced over a three fold increase in wheel life**. It is interesting to note that even at this high removal rate the resulting wheel wear using high wheel speeds is about *half* of that observed at conventional removal

rates ($0.1 \text{ in}^3/\text{min}/\text{in}$) and wheel speeds (6,000 sfm). In other words, increasing the wheel speed from 6,000 to 18,000 ft/min allowed for a 10 fold increase in material removal rate while cutting wheel wear in half!

In addition to demonstrating that increasing wheel speeds will *generally* reduce grinding forces, improve surface finish, reduce “pull out” or surface fragmentation, increase wheel life, and reduce bulk workpiece temperatures, numerous tests were also performed to identify the relationship between increasing wheel speed and resulting MOR strength. To evaluate the effect of grit size, bond type and wheel speed on S/RBSN flexural strength, a 3×2^2 factorial experiment was developed. Two grit sizes, two bond types, and three wheel speeds were selected while the material removal rate was kept constant at a high level ($1 \text{ in}^3/\text{min}/\text{in}$). For all wheels, as wheel speed is **increased**, the flexural strength is **increased**. It was shown that at 18,000 SFM a 240 grit resin bonded wheel produced much higher quality parts than those generated under slow MIL-STD-1942 conditions. Recognize that the material removal rate used in these tests was over **10 times greater** than that used in the baseline MIL-STD-1942 condition. Even grinding with a coarse 180 grit resin wheel at 18,000 sfm produced parts which exceeded baseline properties while grinding at over 10 times the MIL-STD-1942 removal rates!

To characterize overall HSLD cost performance, a comprehensive cost model was also developed to predict grinding related costs for a single plunge centerless grinding operation over a range of material removal rates and wheel speeds. Actual HSLD G-Ratio data was used to develop the abrasive costs and dressing intervals as a function of removal rate and wheel speed. What was interesting to note was that in all cases increasing wheel speed reduced costs. Moreover, the cost benefits gained by increased wheels speeds far outweigh those achieved from reducing labor rates. It was shown that the final per piece costs obtained at 18,000 sfm using a high labor rate are actually **lower** than those using a conventional wheel speed of 6,000 sfm and a low labor rate. Due to the dominant abrasive cost effects, the cost advantages arising from high wheel speeds become even more pronounced as the material removal rate is increased.

Plots of all costs (total cost per piece, labor cost per piece, consumables cost per piece and capital cost per piece) showed that at low material removal rates, the labor cost dominates since total wheel wear is being minimized. Then, as the material removal rate becomes sufficiently large to promote increased wheel wear, the consumables begin to dominate. In all cases, the cost contribution from capital is minimal. Consequently, by increasing material removal rate, a minimum cost is reached at the point where the production rate is sufficient to cost effectively utilize labor even though the consumables content of the total price increases continuously. Further analysis showed that this minimum cost occurs at a material removal rate of approximately $0.5 \text{ in}^2/\text{min}$.

Significant cost reductions are also achieved by reducing the time required to dress from fifteen minutes to 2 minutes - - - particularly at the highest removal rates where dressing frequency is a concern due to increased wheel wear. However, further benefits arising from reducing the 2 minute dress time to

20 seconds only amounted to a 1 cent reduction in total cost. At this point it became obvious that high wheel speeds are the real key to cost effective ceramic grinding.

In conclusion, the results of this program clearly indicate that a single step, roughing-finishing process suitable for producing high-quality silicon nitride ceramic parts at high material removal rates and reduced costs has been developed. In general, increasing wheel speeds will:

- Reduce grinding forces
- Improve surface finish
- Increase wheel life
- Reduce bulk workpiece temperatures
- Allow for high material removal rates
- Meet or exceed all baseline material properties
- Significantly reduce total finishing costs

More specifically, for a standard 240 grit resin wheel, operating at 1.0 in³/min/in removal rate, merely increasing the wheel speed from 6,000 to 18,000 SFM resulted in a 3-fold increase in wheel life while exceeding baseline material properties and reducing total per piece finishing costs by a factor of three.

Upon further examination of the per piece cost data, it appears as though even greater benefits could be realized by increasing wheel speed beyond 18,000 ft/min. During development of the HSLD science base, wheel speeds as high as 35,000 ft/min were evaluated. However, 18,000 ft/min appears to be an upper wheel speed limit for readily available resin and vitrified bonded wheels. With advances in bond technology, higher wheel speeds should be attainable, further reducing costs and increasing production. It is important to realize, however, that when utilizing high wheel speeds, extra attention must be paid to the entire grinding system - - machine capability including stiffness and damping, extremely fine wheel balancing, precise wheel truing, and proper coolant introduction. Failure to address any one of these "support" technologies will prevent realizing the full benefits of High-Speed, Low-Damage grinding.

6.0 ACKNOWLEDGMENTS

Research sponsored by the U.S. Department of Energy, Assistant Secretary for Energy Efficiency and Renewable Energy, Office of Transportation Technologies, as part of the Ceramic Technology Project of the Materials Development Program, under contract DE-AC05-84OR21400 with Lockheed Martin Energy Systems, Inc.

In addition, the authors would like to acknowledge the efforts of the following key personnel:

Eaton Manufacturing Technologies Center -

Michael A. Laurich, Advanced Engineering Support
 Michael R. Cooney, Sr. Laboratory Technician
 Dr. Karl R. Ziegler, Sr. Materials Engineering
 Dr. Jonathan Bo Zhu, Thermal Testing and Analysis
 John M. Gorse, Sr. Laboratory Technician
 John Brennan, High-Speed Component Design

University of Massachusetts -

Professor J. Edward Sunderland, Thermal Measuring Techniques
 Dr. Changsheng Guo, Centerless Grinding Simulation, Thermal Analysis
 Dr. Tae Wook Hwang, Grinding Mechanism Development
 Mahesh Ganesan, Hydrodynamic Force Modeling

Oak Ridge National Laboratory -

Dr. P. J. Blau, Technical Program Guidance
 Dr. S. Srinivasan, Material Surface Analysis - Phase I

University of North Dakota -

Dr. B. Bandyopadhyay, Material Surface Analysis - Phase I

Case Western Reserve University -

Dr. J. Lewandowski, 4-point bend tests

7.0 REFERENCES

- 1) Kovach, J.A., and Malkin, S., "Grinding Energy Relationships for Superalloy Materials", Proc. Seventeenth North American Manufacturing Engineering Research Conference, SME, May 1989, pp. 185-192
- 2) Guo, C., and Malkin, S., "Heat Transfer in Grinding", 1st International Conference on Transport Phenomena in Processing, Hawaii, 1992.
- 3) Malkin, S., Grinding Technology, Ellis Horwood series in Mechanical Engineering, 1989.
- 4) Carslaw, H.S., and Jaeger, J.C., Conduction of Heat in Solids, Oxford University Press, London, 1959, pp. 266-270.
- 5) M. Ganesan, C. Guo, A. Ronen, and S. Malkin, "Analysis of Hydrodynamic Forces in Grinding," *Trans. of the NAMRC/SME*, Vol. 24, 1996, pp. 105-110.
- 6) Schlichting, H., Boundary Layer Theory, Mc Graw-Hill, New York, 1955.
- 7) Malkin, S., Grinding Technology : Theory and Application of Machining with Abrasives, John Wiley & Sons, New York.
- 8) Malkin, S., Hwang, T.W., "Grinding Mechanisms for Ceramics", *Annals of the CIRP*, Vol. 45/2, pp. 558-69, 1996.
- 9) Evans, A.G., Wilshaw, T.R., "Quasi-Static Solid Particle Damage in Brittle Materials: I", Acta Metal., Vol 24, pp. 939-956, 1976.
- 10) Evans, A.G. and Marshall, D.B., "Wear Mechanisms in Ceramics", Fundamentals of Friction and Wear of Materials, Edited by D.A. Rigney, ASME, 1981, p. 439.
- 11) Nishioka, T., et al., Development of High Strength Si₃N₄ Sintered Body for the Valve Systems of Automotive Engines, SAE Technical paper #920384, February 24-28, 1992.
- 12) Kovach, J.A., Malkin, S., High-Speed, "Low-Damage Grinding of Advanced Ceramics, Phase I", ORNL/Sub/92-SL807/1 Final Report, Published March 1995.
- 13) Zhu, J.B., "Thermal Aspects of Ceramic Grinding", Ph.D. Dissertation, University of Massachusetts, May 1996.
- 14) Brown, R.H., and Wager, J.G., "An Examination of the Wheel-Work Interface Using and Explosive Device to Suddenly Interrupt the Surface Grinding Process," *Annals of the CIRP*, Vol. 25/1/1977, pp. 143 - 146.

- 15) Hahn, R.S., and Linday, R., "On the Effects of Real Area of Contact and Normal Stress in Grinding," *Annals of the CIRP*, Vol. XV, 1967, pp. 197 - 204.
- 16) Kovach, J.A., "Thermally Induced Grinding Damage in Cast Equiaxed Nickel-Based Superalloys", Ph.D. thesis, Case Western Reserve University, May 1986, pp. 69 - 93.
- 17) Guo, C., Malkin, S., Kovach, J., Laurich, M., "Computer Simulation of Below-Center and Above-Center Centerless Grinding", *Machining Science and Technology Journal*, Vol. 2, 1997.

Table 1 Data summary for high speed grinding tests**180 grit vitrified bond**

Wheel Speed (ft/min)	Normal Force (lb)	Tangential Force (lb)	Power (hp)	Specific Energy (in-lb/in ³)	Surface Finish (R _a , minch)	Surface Temperature (°F)	Energy Partition
5,000	80.7	27.3	4.9	4,734,104	33	259	4.7%
10,000	53.8	12.9	4.3	4,473,988	30	239	6.2%
15,000	54.2	11.2	5.2	5,826,590	22	209	4.7%
20,000	70.4	12.5	7.6	8,670,520	22	205	3.3%

180 grit resin bond

Wheel Speed (ft/min)	Normal Force (lb)	Tangential Force (lb)	Power (hp)	Specific Energy (in-lb/in ³)	Surface Finish (R _a , minch)	Surface Temperature (°F)	Energy Partition
5,000	98.0	13.6	3.8	2,356,522	28	448	11.6%
10,000	71.4	7.1	4.0	2,469,565	29	358	9.6%
15,000	69.6	4.4	4.2	2,269,565	28	306	8.0%
20,000	83.5	5.3	6.3	3,652,174	28	339	5.9%
25,000	96.0	5.4	7.2	4,652,174	27	328	5.0%

320 grit resin bond

Wheel Speed (ft/min)	Normal Force (lb)	Tangential Force (lb)	Power (hp)	Specific Energy (in-lb/in ³)	Surface Finish (R _a , minch)	Surface Temperature (°F)	Energy Partition
5,000	130.5	32.3	5.0	5,623,188	23	491	10.2%
10,000	71.6	15.0	4.4	5,204,522	17	230	5.3%
15,000	105.4	12.2	5.2	6,384,870	16	230	4.4%
20,000	103.0	9.8	5.7	6,782,609	19	323	5.4%
25,000	145.1	8.9	7.8	7,728,696	21	392	4.9%

120 grit plated

Wheel Speed (ft/min)	Normal Force (lb)	Tangential Force (lb)
5,000	124	34
15,000	85	11
25,000	63	11

Table 2 Mechanical properties of the materials tested and the corresponding plowed surface energy per unit area with a correlation coefficient r .

	Wheels	Materials	E (GPa)	H (GPa)	K_{IC} (MPa $m^{1/2}$)	$G_c/2$ (J/m ²)	J_s (J/m ²)	r
1	DN400-	S/RBSN	297	15.0	6.6	73.3	15,300±1,520	0.83
2	N100B-	RBSN	165	10.0	3.6	39.3	3,900±236	0.92
3	1/4	RBSN	165	10.0	3.6	39.3	5,400±255	0.95
4		HPSN	325	19.6	5.0	38.5	18,000±705	0.96
5		Al ₂ O ₃	380	13.3	4.5	26.6	7,900±255	0.97
6	DN180-	HPSN	276	18.2	5.7	58.9	14,200±585	0.95
7	N100B-	HPSN	325	19.6	5.0	38.5	15,900±365	0.98
8	1/4	SiC	410	27.4	3.5	14.9	10,000±400	0.96
9		SiC	427	27.0	3.0	10.5	9,100±590	0.91
10		Glass	70	5.5	0.75	4.0	1,800±85	0.95

S/RBSN - Coors/Eaton, RBSN - Coors/Eaton, HPSN - Norton NC132, HPSN - Kyocera SN220, Al₂O₃ - Wesgo, Al995, SiC - Carborundum, Hexoloy SA, SiC - Norton, Glass - Soda lime Glass

Table 3 Experimental results with second two-color system with 400 grit size diamond wheel

Test Number	wheel speed v_s (m/s)	workpiece speed v_w (mm/s)	depth of cut a (mm)	specific energy u (J/mm ³)	heat flux q (W/mm ²)	energy partition
1	30	127	0.038	38	53	17%
2	30	63.5	0.076	39	39	12%
3	30	84.7	0.038	41	39	16%
4	19.2	84.7	0.038	28	26	21%
5	19.2	66.7	0.025	41	25	18%
6	19.2	16.7	0.102	42	12.7	$9 \pm 3\%$

Table 4 Flexural Strength Summary - Transverse ground S/RBSN at 1 in³/min-in material removal rate.

Wheel Type	Wheel Speed (SFM)	Flexural Strength (KSI)	Weibull Modulus
180 Resin	6,000	69.0	14.82
180 Resin	12,000	74.3	24.21
180 Resin	18,000	84.1	17.33
180 Resin	6,000	68.5*	13.00*
180 Resin	12,000	69.9*	18.63*
240 Resin	6,000	71.0	56.70
240 Resin	12,000	83.1	22.69
240 Resin	18,000	85.6	19.49
180 Vitrified	12,000	49.0	22.20
180 Vitrified	18,000	55.2	17.57
240 Vitrified	6,000	55.5	15.86
240 Vitrified	12,000	61.8	23.96
240 Vitrified	18,000	80.1	22.36

* NOTE: All tests were run with 0.015" step-over except for tests marked with *. Tests marked with a * used a 0.070" step-over.

Table 5 HSLD cost model variables and values for a nominal case.

Model Variable	Nominal Value
Machine cost	\$350,000
Wheel Type	240 Resin
Wheel Cost	\$20,000
Wheel speed - low	6,000 ft/min
Wheel speed - high	18,000 ft/min
Wheel diameter	12 inch
Bond thickness	1/4 inch
Dressing roll cost	\$1,200
Number of rolls/wheel	1
Dressing frequency	0.0001 inch
Dressing time	2 min.
Material type	NT551
Radial stock envelope	0.005 inch
Final part diameter	0.375 inch
G-Ratio	Calculate as function of Vs, Q'w
Material removal rate - low	0.1 in ² /min
Material removal rate - high	1.0 in ² /min
Scrap rate	5%
Lot size	N/A
Number of hours per year	1,600
Setup time	N/A
Load+Unload time	2 sec
Labor rate	\$15/hour
Additional Overhead	80% O.H.

Table 6 Total cost per piece as functions of dressing cycle and labor rate.

	0.1 in ² /min	0.5 in ² /min	1.0 in ² /min
	Nominal case - 2 minute dressing cycle, nominal labor rate		
6,000 ft/min	\$0.29	\$0.35	\$0.70
12,000 ft/min	\$0.19	\$0.21	\$0.40
18,000 ft/min	\$0.14	\$0.13	\$0.23
	20 second dressing cycle		
6,000 ft/min	\$0.26	\$0.30	\$0.62
12,000 ft/min	\$0.18	\$0.18	\$0.35
18,000 ft/min	\$0.13	\$0.12	\$0.21
	15 minute dressing cycle		
6,000 ft/min	\$0.51	\$0.66	\$1.40
12,000 ft/min	\$0.32	\$0.38	\$0.78
18,000 ft/min	\$0.21	\$0.23	\$0.44
	Low labor rate		
6,000 ft/min	\$0.25	\$0.32	\$0.66
12,000 ft/min	\$0.16	\$0.18	\$0.37
18,000 ft/min	\$0.11	\$0.11	\$0.21
	High labor rate		
6,000 ft/min	\$0.34	\$0.39	\$0.77
12,000 ft/min	\$0.24	\$0.24	\$0.44
18,000 ft/min	\$0.18	\$0.15	\$0.27

Table 7 Summary of simulation results

α (deg)	5	10	13	17	20	23	27	30
initial lobes	lobes after grinding							
2	1 (OK)	13 (Good)	12 (OK)	8 (Bad)	8 (Bad)	6 (Bad)	6 (Bad)	6 (Bad)
3	3 (Bad)	3 (Bad)	3 (Bad)	12 (OK)	None (Good)	6 (OK)	6 (Bad)	6 (Bad)
4	None (Good)	16 (Good)	12 (OK)	8 (OK)	8 (Bad)	8 (Bad)	8 (Bad)	None (Good)
6	None Good	18 (Good)	12 (OK)	12 (OK)	None (Good)	6 (OK)	6 (Bad)	6 (Bad)

Good: lobe magnitude is significantly reduced from initial values. There is no distinct lobing.

Bad: lobe magnitude is significantly increased as compared with the initial values.

OK: lobe magnitude is reduced as compared to the initial lobe, but distinctive lobing occurs.

Normal Force versus Wheel Speed

S/RBSN Bars
120 Grit Plated Wheel

Figure 1.

Normal Force vs. Wheel Speed **S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$**

— 320 resin ··· 180 Vitrified - - - 180 Resin - · - 120 Plated

* = 18,000 SFM

Appears to be "Reasonable Limit" for polyimide bond system

Figure 2.

Radial Wheel Wear vs. Wheel Speed

OD Plunge Grind NT551

240 Resin

— $Q'w=0.1 \text{ in}^3/\text{min}/\text{in}$ - - - $Q'w=1.0 \text{ in}^3/\text{min}/\text{in}$ - - $Q'w=0.5 \text{ in}^3/\text{min}/\text{in}$

Figure 3.

G-Ratio vs. Wheel Speed

OD Plunge Grind NT551

240 Resin

Figure 4.

Surface and “Swarf” Analysis

Ground surface

HPSN-Kyocera SN220,

$a = 5 \mu\text{m}$, $v_w = 5 \text{ mm/s}$, $v_s = 40 \text{ m/s}$

Grinding debris

RBSN - Coors/Eaton,

$a = 22 \mu\text{m}$, $v_w = 100 \text{ mm/s}$, $v_s = 32 \text{ m/s}$

- Grinding swarf comprised of fine particles $1 \mu\text{m}$ in size and plate like particles about $250 \mu\text{m}$ in size
- Analysis of particles suggests material removed mainly by brittle fracture
- Ground surface texture is indicative of ductile flow

Figure 5.

Grinding Power vs. “Plowed” Surface Area Rate Eaton RBSN

Figure 6.

Grinding Power as a Function of Key Material and Grinding Parameters

Figure 7.

Effect of Wheel Speed on Grinding Zone Temperature

Predicted Values ($\epsilon = 0.50$)

S/RBSN Bars - 120 Grit Plated Wheel

Figure 8.

Temperature versus Wheel Speed

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

— 320 Resin — - - 180 Resin — — 180 Vitrified

Figure 9.

Energy Partition versus Wheel Speed

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

Figure 10.

Specific Energy versus Wheel Speed S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

Figure 11.

Energy Partition and Specific Energy Comparison

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

180 Grit Vitrified

Figure 12.

Energy Partition and Specific Energy Comparison

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

180 Grit Resin

— — Specific Energy — Energy Partition

Figure 13.

Energy Partition and Specific Energy Comparison

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$
320 Grit Resin

Figure 14.

Temperature versus Wheel Speed

S/RBSN Bars $Q'_w = 0.5, 1.0 \text{ in}^3/\text{min}/\text{in}$
180 Grit Vitrified Bond Wheel

Figure 15.

S/RBSN Surface Effects Due to Step-Over 180 Grit Resin Wheel

MEAS. RANGE 4921 μ in STRAIGHTNESS LSS
SAMPLE RATE 394 μ in Δ : -0°10'0.7" INC: -5273.0 μ in/in

Workpiece surface using 0.070 inch step-over

Figure 16.

S/RBSN Surface Effects Due to Step-Over 180 Grit Resin Wheel

Workpiece surface using 0.015 inch step-over

Figure 17.

Effect of Step-Over on Flexural Strength

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

180 Resin

Figure 18.

Flexural Strength versus Wheel Speed

S/RBSN Bars $Q'_w = 1.0 \text{ in}^3/\text{min}/\text{in}$

Figure 19.

Flexural Strength vs. "Plowed" Surface Area Rate Eaton S/RBSN

Figure 20.

Finishing Cost per Piece vs. Material Removal Rate

Comparison of Labor Rate & Wheel Speed

240 Grit Resin, Norton NT551

Figure 21.

Finishing Cost per Piece vs. Material Removal Rate 240 Resin NT551

Figure 22.

Cost per Piece vs Material Removal Rate NT551 Silicon Nitride 240 Resin at 18,000 ft/min

Figure 23.

Finishing Cost per Piece vs. Material Removal Rate

Comparison of Dressing Cycles

240 Resin at 18,000 ft/min NT551

Figure 24.

Finishing Cost per Piece vs. Material Removal Rate

Comparison of Dressing Cycles

240 Resin at 6,000 ft/min NT551

Figure 25.

Above-center Grinding

Figure 26.

Below-center Grinding

Figure 27.

Part Geometry Relationships- TTZ Plunger, 27° Below Center

Figure 28.

Centerless Grinding Simulation

Resultant Geometry at 27° Below Center

Initial Geometry: 2 Lobes
Final Geometry: 6 Lobes

Initial Geometry: 3 Lobes
Final Geometry: 6 Lobes

Figure 29.

Optimized Part Geometry - Mathematical Model for 20° Below Center

Initial Geometry: 2 Lobes
Final Geometry: 8 Lobes

Initial Geometry: 3 Lobes
Final Geometry: Good Part

Initial Geometry: 6 Lobes
Final Geometry: Good Part

Figure 30.

INTERNAL DISTRIBUTION

L. F. Allard, Jr.
P. F. Becher
T. M. Besmann
P. J. Blau
R. A. Bradley
K. Breder
C. R. Brinkman
T. D. Burchell
A. Choudhury
D. D. Conger
S. A. David
M. K. Ferber
R. L. Graves
C. R. Hubbard
M. A. Janney
D. R. Johnson (5)

R. R. Judkins
M. A. Karnitz
R. J. Lauf
K. C. Liu
W. D. Manly
S. B. McSpadden
T. A. Nolan
A. E. Pasto
M. H. Rawlins
A. C. Schaffhauser
D. P. Stinton
T. N. Tiegs
S. G. Winslow
R. E. Ziegler
Laboratory Records - RC

EXTERNAL DISTRIBUTION

Jeffrey Abboud
U.S. Advanced Ceramics Assoc.
1600 Wilson Blvd., Suite 1008
Arlington VA 22209

B. P. Bandyopadhyay
University of North Dakota
Box 8359 University Station
Grand Forks ND 58202-8359

Donald F. Baxter, Jr.
Advanced Materials & Processes
ASM International
9639 Kinsman Road
Materials Park OH 44073-0002

M. Brad Beardsley
Caterpillar Inc.
Technical Center Bldg. E
P.O. Box 1875
Peoria IL 61656-1875

Ramakrishna T. Bhatt
NASA Lewis Research Center
MS-106-1
21000 Brookpark Road
Cleveland, OH 44135

Bruce Boardman
Deere & Company, Technical Ctr.
3300 River Drive
Moline IL 61265-1792

Michael C. Brands
Cummins Engine Company, Inc.
P.O. Box 3005, Mail Code 50179
Columbus IN 47201

Donald J. Bray
Advanced Refractory Technologies
699 Hertel Avenue
Buffalo NY 14207

Jeff Bougher
Caterpillar Inc.
Technical Center, Bldg. E
P.O. Box 1875
Peoria IL 61656-1875

Mike Bowling
Cummins Engine Company, Inc.
1900 McKinley Avenue
P.O. Box 3005
Columbus IN 47202-3005

Walter Bryzik
U.S. Army Tank Automotive
Command
R&D Center, Propulsion Systems
Warren MI 48397-5000

David Carruthers
Kyocera Industrial Ceramics
5713 East Fourth Plain
Vancouver WA 98661

Ronald H. Chand
Morton Advanced Materials
185 New Boston Street
Woburn MA 01801

William J. Chmura
Torrington Company
59 Field Street, P.O. Box 1008
Torrington CT 06790-1008

William S. Coblenz
Defense Adv. Research Projects Agency
3701 N. Fairfax Drive
Arlington VA 22203-1714

Gloria M. Collins
ASTM
100 Barr Harbor Drive
West Conshohocken PA 19428-2959

Shawn Cooper
FEV Engine Technology
4554 Glenmeade Lane
Auburn Hills MI 48326-1766

Douglas Corey
AlliedSignal, Inc.
2525 West 190th Street, MS:T52
Torrance CA 90504-6099

Keith P. Costello
Chand/Kare Technical Ceramics
2 Coppage Drive
Worcester MA 01603-1252

Gary M. Crosbie
Ford Motor Company
P.O. Box 2053, 20000 Rotunda Drive
MD-3182, SRL Building
Dearborn MI 48121-2053

Pamela Cunningham
WETO Technical Library
MSE, Inc.
Industrial Park, P. O. Box 4078
Butte MT 59702

S. Keoni Denison
Norton Company
1 New Bond Street
Worcester MA 01615-0008

Sidney Diamond
U.S. Department of Energy
Office of Transportation Technologies
EE-33, Forrestal Building
Washington DC 28505

Ernest J. Duwell
3M Abrasive Systems Division
3M Center, Bldg. 251-01-34
St. Paul MN 55144

Michael Easley
AlliedSignal Engines
P. O. Box 52181
MS 551-11
Phoenix AZ 85072-2181

James J. Eberhardt
U.S. Department of Energy
Office of Transportation Technologies
EE-33, Forrestal Building
Washington DC 20585

Jim Edler
Eaton Corporation
26201 Northwestern Highway
P.O. Box 766
Southfield MI 48037

William A. Ellingson
Argonne National Laboratory
Energy Technology Division, Bldg. 212
9700 S. Cass Avenue
Argonne IL 60439-3848

John W. Fairbanks
U.S. Department of Energy
Office of Transportation Technologies
EE-33, Forrestal Building
Washington DC 20585

Ho Fang
Applied Materials
2695 Augustine Drive, MS-0962
Santa Clara CA 95054

Dan Foley
AlliedSignal Ceramic Components
MS:1/5-1, 26000
2525 West 190th Street
Torrance CA 90504

Douglas Freitag
DuPont Lanxide Composites
21150 New Hampshire Avenue
Brookeville MD 20833

Richard Gates
NIST
Bldg. 223, Rm. A-256
Rt. 270 & Quince Orchard Road
Gaithersburg MD 20899

Ludwig J. Gauckler
 ETH Zurich
 Nonmetallic Materials
 Sonneggstr. 5
 CH-8092 Zurich, SWITZERLAND

Allan E. Goldman
 U.S. Graphite, Inc.
 907 W. Outer Drive
 Oak Ridge TN 37830

Robert J. Gottschall
 U.S. Department of Energy
 Metal & Ceramic Sciences, ER-131
 19901 Germantown Road
 Germantown MD 20874-1290

Thomas J. Gross
 U.S. Department of Energy
 Office of Transportation Technologies
 EE-30, Forrestal Building
 Washington DC 20585

Changsheng Guo
 United Technologies Research Center
 Machining Systems, MS 129-46
 East Hartford CT 06108

Darryl Gust
 Cummins Engine Company, Inc.
 1900 McKinley Avenue
 P.O. Box 3005
 Columbus IN 47202-3005

Nabil S. Hakim
 Detroit Diesel Corporation
 13400 Outer Drive West, A08
 Detroit MI 48239-4001

Alan M. Hart
 Dow Chemical Company
 1776 Building
 Midland MI 48674

Michael H. Haselkorn
 Caterpillar Inc.
 Technical Center, Building E
 P.O. Box 1875
 Peoria IL 61656-1875

Deborah A. Haught
 U.S. Department of Energy
 Ofc. of Industrial Crosscut Technologies
 EE-23, Forrestal Bldg.
 Washington DC 20585

Daniel Hauser
 Edison Welding Institute
 Microjoinint & Plastics Tech. Team
 1250 Arthur E. Adams Drive
 Columbus OH 43221-3585

John Haygarth
 Wah Chang
 P.O. Box 460
 Albany OR 97321-0460

Gene Huber
 Precision Ferrites & Ceramics
 5432 Production Drive
 Huntington Beach CA 92649-1525

Thomas A. Johnson
 Lanxide Corporation
 1300 Marrows Road
 P.O. Box 6077
 Newark DE 19714-6077

Adam Jostsons
 ANSTO
 PMB1
 Menai, NSW, Australia 2234

Yury Kalish
 Detroit Diesel Corporation
 Mechanical Systems
 13400 Outer Drive West
 Detroit MI 48239-4001

Roy Kamo
 Adiabatics, Inc.
 3385 Commerce Park Drive
 Columbus IN 47201

Ralph Kelly
 Cincinnati Milacron
 P.O. Box 9013
 Cincinnati OH 45209

W. C. King
 Mack Truck, Z-41
 1999 Pennsylvania Avenue
 Hagerstown MD 21740

Tony Kim
 Caterpillar Inc.
 Defense Products Dept., JB7
 Peoria IL 61629

Joseph A. Kovach
 Parker Hannifin Corporation
 6035 Parkland Boulevard
 Cleveland OH 44124-4141

Edwin H. Kraft
 Kyocera Industrial Ceramics
 5713 E. Fourth Plain Boulevard
 Vancouver WA 98661

Oh-Hun Kwon
 Norton Company
 Saint Gobain Industrial Ceramics
 1 Goddard Road
 Northboro MA 01532-1545

S. K. Lau
 B. F. Goodrich Aerospace R&D
 9921 Brecksville Road
 Brecksville OH 44141

Elaine Lentini
 Saint-Gobain Industrial Ceramics
 Goddard Road
 Northboro MA 01532

Stan Levine
 NASA Lewis Research Center
 21000 Brookpark Road, MS:106/5
 Cleveland OH 44135

Robert H. Licht
 Norton Company
 Saint Gobain Industrial Ceramics
 1 Goddard Road
 Northboro MA 01532-1545

E. Lilley
 Norton Company
 Saint Gobain Industrial Ceramics
 1 Goddard Road
 Northboro MA 01532-1545

B. J. McEntire
 Applied Materials Corporation
 3050 Bowers Avenue
 Santa Clara, CA 95054

James McLaughlin
 Sundstrand Power Systems
 4400 Ruffin Road
 P.O. Box 85757
 San Diego CA 92186-5757

Biljana Mikielj
 Ceradyne, Inc.
 3169 Red Hill Avenue
 Costa Mesa CA 92626

Carl E. Miller
 Delphi Energy & Engine Mgmt. Systems
 4800 S. Saginaw Street, MC 485-301-150
 P. O. Box 1360
 Flint MI 48501-1360

Curtis V. Nakaishi
 U.S. Department of Energy
 Federal Energy Tech. Center
 3610 Collins Ferry Rd.
 P.O. Box 880
 Morgantown WV 26507-0880

Malcolm Naylor
Cummins Engine Company, Inc.
P.O. Box 3005, Mail Code 50183
Columbus IN 47202-3005

Dale E. Niesz
Ceramic & Materials Engineering
607 Taylor Road, Rm. 204
Piscataway, NJ 08854-8065

Thomas J. Paglia
Coors/ACI
3315 Boone Road
Benton AR 72015

Richard Palicka
CERCOM, Inc.
1960 Watson Way
Vista CA 92083

Vijay M. Parthasarathy
Solar Turbines
2200 Pacific Highway, M.Z. R-1
San Diego CA 92186

Magan Patel
Cummins Engine Company, Inc.
Mail Code 50183
Box 3005
Columbus IN 47202-3005

James W. Patten
Cummins Engine Company, Inc.
P.O. Box 3005, Mail Code 50183
Columbus IN 47202-3005

Joe Picone
Norton Company
1 New Bond Street
Box 15008
Worcester MA 01615-0008

Stephen C. Pred
Pred Materials International, Inc.
60 East 42nd Street, Suite 1456
New York NY 10165

Vimal K. Pujari
Norton Company
Saint Gobain Industrial Ceramics
1 Goddard Road
Northboro MA 01532-1545

Fred Quan
Corning Inc.
Sullivan Park, FR-2-8
Corning NY 14831

George Quinn
NIST
I-270 & Clopper Road
Ceramics Division, Bldg. 223
Gaithersburg MD 20899

Mike Readey
Caterpillar, Inc.
Technical Center, Bldg. E
P.O. Box 1875
Peoria IL 61656-1875

Harold Rechter
Chicago Fire Brick Company
7531 S. Ashland Avenue
Chicago IL 60620-4246

Jack A. Rubin
CERCOM, Inc.
1960 Watson Way
Vista CA 92083

Robert J. Russell
Riverdale Consulting, Inc.
24 Micah Hamlin Road
Centerville MA 02632-2107

J. Sankar
North Carolina A&T State Univ.
Dept. of Mechanical Engineering
Greensboro NC 27406

Maxine L. Savitz
AlliedSignal, Inc.
Ceramic Components
2525 West 190th Street
P.O. Box 2960, MS:1/5-1, 26000
Torrance CA 90509-2960

Jim Schienle
AlliedSignal Aerospace
1130 West Warner Road
M/S 1231-K
Tempe AZ 85284

Gary Schnittgrund
Transfer Technology
16401 Knollwood Drive
Granada Hills CA 91344

Robert S. Shane
Shane Associates
1904 NW 22nd Street
Stuart FL 34994-9270

Subu Shanmugham
MicroCoating Technologies
3901 Green Industrial Way
Chamblee GA 30341-1913

Albert J. Shih
North Carolina State University
Mechanical & Aerospace Engineering
2217 Broughton Hall, Box 7910
Raleigh NC 27695

Charles Spuckler
NASA Lewis Research Center
21000 Brookpark Road, MS: 5-11
Cleveland OH 44135-3127

Gordon L. Starr
Cummins Engine Company, Inc.
P.O. Box 3005, Mail Code:50182
Columbus IN 47202-3005

Marian Swirsky
Cambridge Scientific Abstract
Commerce Park, Bldg. 4, Suite 804
23200 Chagrin Blvd.
Beachwood OH 44122

Victor J. Tennery
113 Newell Lane
Oak Ridge TN 37830

Malcolm Thomas
Allison Engine Company
P. O. Box 420 (W06)
Indianapolis IN 46206

Marc Tricard
Norton Company
Superabrasives Division
1 New Bond Street, MS-412-301
P. O. Box 15008
Worcester MA 01615-0008

Marcel H. Van De Voorde
Commission of the European Union
Eeuwigelaan 33
1861 CL Bergen
THE NETHERLANDS

V. Venkateswaran
Materials Solutions International, Inc.
P.O. Box 663
Grand Island, NY 14072-0663

Robert M. Washburn
ASMT
11203 Colima Road
Whittier CA 90604

R. W. Weeks
Argonne National Laboratory
Bldg. 362, E313
9700 S. Cass Avenue
Argonne IL 60439

Sheldon M. Wiederhorn
NIST
Building 223, Room B309
Gaithersburg MD 20899

Matthew F. Winkler
Seaworthy Systems, Inc.
P.O. Box 965
Essex CT 06426

Thomas J. Wissing
Eaton Corporation
26201 Northwestern Highway
P.O. Box 766
Southfield MI 48037

James C. Withers
MER Corporation
7960 S. Kolb Road
Tucson AZ 85706

Dale E. Wittmer
Southern Illinois University
Mechanical Engineering Dept.
Carbondale IL 62901

Egon E. Wolff
Caterpillar Inc.
Technical Center
P.O. Box 1875
Peoria IL 61656-1875

Roy Yamamoto
Ethyl Petroleum Additives, Inc.
500 Spring Street
P. O. Box 2158
Richmond VA 23218-2158

R. L. Yeckley
Kennametal, Inc.
P.O. Box 231
Latrobe, PA 15650

Thomas M. Yonushonis
Cummins Engine Company, Inc.
1900 McKinley Avenue
P.O. Box 3005, Mail Code 50183
Columbus IN 47202-3005

S. Charles Yoon
Cincinnati Milacron, Inc.
P.O. Box 9013
3000 Disney Street
Cincinnati OH 45209-9013

Jong Yung
Sundstrand Aerospace
Dept. 789-6
4747 Harrison Avenue
Rockford IL 61125

A. L. Zadoks
Caterpillar Inc.
Technical Center, Building L
P.O. Box 1875
Peoria IL 61656-1875

Zhenqi Zhu
Stevens Institute of Technology
Department of Mechanical Engineering
Castle Point on Hudson
Hoboken NJ 07030

Department of Energy
Oak Ridge Operations Office
Assistant Manager for Energy
Research and Development
P. O. Box 2001
Oak Ridge TN 37831-8600

For distribution by microfiche
as shown in DOE/OSTI-4500,
Distribution Category UC-332
(Ceramics/Advanced Materials).