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Crack propagation in CBN insert in hybrid machining of RBSN ceramic

George Petrescu

University of Nevada, Las Vegas

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UMI
CRACK PROPAGATION IN CBN INSERT IN
HYBRID MACHINING OF RBSN CERAMIC

by

George Petrescu

Bachelor of Science,
Technical University for Constructions, Bucharest, Romania
1995

Master of Science,
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1999

A dissertation submitted in partial fulfillment
of the requirements for the

Doctor of Philosophy Degree in Mechanical Engineering
Department of Mechanical Engineering
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Graduate College
University of Nevada, Las Vegas
August 2005
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The Dissertation prepared by
George Petrescu

Entitled
Crack Propagation in CBM Insert in Hybrid Machining of RBSN Ceramic

is approved in partial fulfillment of the requirements for the degree of
Ph.D. Mechanical Engineering

Examination Committee Chair

Dean of the Graduate College
ABSTRACT

Crack Propagation in CBN Insert in Hybrid Machining of RBSN Ceramic

by

George Petrescu

Dr. Zhiyong Wang, Examination Committee Chair
Professor of Mechanical Engineering
University of Nevada, Las Vegas

This research investigates the phenomenon that cryogenic cooling can significantly improve the cutting tool life and the workpiece quality in hybrid machining of advanced materials, such as Reaction Bonded Silicone Nitride (RBSN). Several finite element models are developed in order to analyze the temperature fields developed and the stress distributions in the Polycrystalline Cubic Boron Nitride (PCBN) cutting insert during a hybrid turning process. The analyses performed reveal that the hybrid machining with cryogenic cooling of the cutting tool leads to a decrease of stresses in the cutting insert, especially when micro-cracks are present in the insert exhibits. It is found that a decrease in cutting temperature from 1740°C to 597°C led to approximately 66% stress reduction at the tip of the micro-cracks on the flank face of the cutting tool, thus significantly reducing the wear rate of the cutting insert. The findings are consistent with previously published experimental data.
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CHAPTER 1

INTRODUCTION

Advanced materials are generally considered materials that can perform successfully under extreme working conditions. They can survive high working temperatures, have excellent thermal shock resistance, are inert in many chemical environments and can endure high mechanical stresses. Because of these qualities they are widely used in aerospace industry, oil industry, electronics industry, manufacturing industry and many other industries. Examples of such materials include titanium alloys, Inconel alloys, ceramics, and metal matrix composites.

Ceramics are a class of materials that typically include inorganic, nonmetallic solids, such as salts formed by combination of cations and anions. Ceramics tend to have atomic structures in which the valence electrons are located at each atomic center rather than floating in a metallic bonding. This is why ceramics are generally poor electrical and thermal conductors.

Commonly, metal oxides are referred to as ceramics. However, many other materials are also included in this class: glass, zirconia, quartz, sapphire, carbides, metal nitrides, graphite and diamond.
Ceramics are widely used in extreme conditions, such as extreme heat, high stresses and in chemically aggressive environments. One of the main applications of ceramics is cutting inserts for the manufacturing industry.

Ceramic Cutting Inserts

Although the development and usage of ceramics can be traced back to the early 1900s, they were first commercially used in the United States in the mid 1950’s. Typically, the ceramic cutting inserts are made from aluminum oxides (\(\text{Al}_2\text{O}_3\)) by means of power-metallurgical processes and are primarily composed of very small particles of aluminum oxide that are bonded together by a high-temperature sintering process. Additives such as chromium oxide, magnesium oxide, titanium oxide, nickel oxide, and refractory metal carbides are added to achieve favorable properties at the cutting edge. Utilization of high-pressure compaction processes allows the use of very fine grain size powders that maximizes the density of the mix and improves the material’s low toughness.

Ceramics, among other tool materials, exhibit the best resistance to failure at elevated temperatures and usually perform better at high-speed ranges when compared to carbide tools. In addition, ceramic tools are very hard and have high wear-resistance. Applications include cutting conditions at elevated temperatures that would cause failure to other tool materials. As can be seen in Figure 1, ceramic tools retain a high level of hardness at elevated temperatures which makes them perform better in the high-speed ranges designated as the hot heat range.
Ceramic tools are most successful in high-speed turning of cast iron and steel. If high cutting speeds, low feed and depth of cut, and rigid work setups are employed, ceramics can also be used for finish turning operations on hardened steels.

![Diagram showing typical relationship hardness-temperature of high-speed steel tools, cobalt tools, and ceramic tools.](Gorczyca, 1987)

On the other hand, ceramic tools also render some disadvantages like:

- relatively low transverse rupture strength;
- high brittleness;
- low heat coefficient of thermal conductivity.
Those characteristics make them susceptible to chipping, cracking, fracturing, and gradual wearing by abrasion and limit their machining capabilities, especially in situations of interrupted cuts. In addition, ceramic tools exhibit a tendency towards a welding action between the tool and the workpiece as well as a certain degree of plastic deformation of the tool at elevated temperature.

Figure 2 illustrates the relative relationship between hot hardness (wear resistance) and strength of toughness for the four major cutting tool materials. On a relative scale, ceramic tools exhibit a combination of very high hardness and a low toughness when compared to other cutting tool materials. The major application of ceramic tools is in high-speed turning of cast irons, very high-strength steels and other hard-to-cut materials, as well as in finishing operations at high-speed ranges.

Figure 2. Relationship of four tool materials (Gorczyca, 1987)
Some commercially available ceramic tool materials include cubic boron nitride (BN), SiAlON (made out of silicon nitride and aluminum oxide (SiN- $\text{Al}_2\text{O}_3$)) and titanium carbide ($\text{Al}_2\text{O}_3\text{-TiC}$). They also include aluminum oxide reinforced with single-crystal whiskers of silicon carbide. These tools are usually intended for special applications such as machining Inconel 718 and silicon nitride (SiN), etc.

Polycrystalline Cubic Boron Nitride

Polycrystalline Cubic Boron Nitride, or PCBN, was introduced in 1970’s (Broskea, 2001). It is a ceramic material that cannot be found in the nature (Heath, 1986). This material is synthesized using boron nitride micro-sized powder bonded with a carbide substrate (Halprin, 1998). In the manufacturing of this material the graphitic, lubricious particles of boron nitride are converted into hard and abrasive crystals that are similar to the structure of diamond.

PCBN is the third hardest substance after diamond and silicone-magnesium-aluminum-boron (DeGaspary, 2000). It is thermally stable and has very good wearing resistance (Trent, 1984). Unlike diamond, PCBN is not prone to graphitization and is stable at high temperatures that develop during machining of hard-to-cut materials. PCBN also wears slower than silicone-magnesium-aluminum-boron (DeGaspary, 2000) and has a lubricating effect that makes it very useful as a separating agent for molding processes. Its very high thermal conductivity and stability makes it ideal for use in inert and reducing atmospheres up to 2800°C and in oxidizing environments up to 850°C. PCBN has a considerable wear resistance hardness, fracture toughness and thermo-
chemical stability in machining not only hardened irons and steels but also surface hardened components and hard facing alloys. In addition, research has demonstrated that the use of coolants facilitates a drastic decrease of the thermal deformation of PCBN (Kocherovski, 1989), and therefore the rectilinear errors of the workpiece are practically reduced to zero. PCBN has a price tag of ~$7,000 per pound and is more expensive than diamond (~$2,000 per pound) and than silicone-magnesium-aluminum-boron (estimated at ~$700 per pound) (DeGaspary, 2000). However, PCBN’s excellent properties led to the sales of cutting inserts exceeding $250 million in 2002 (Huddle, 2002). PCBN cutting materials are mainly used for finishing work on nickel-based alloys (Konig, 1999) and are one of the most frequently used materials for Cutting Inserts in machining aeroengine alloys (Ezugwu, Bonney and Yamane, 2003).

The physical, mechanical, thermal and electrical properties of PCBN can be found in Table I, as published at www.abrasive.com (not dated) and www.matweb.com (not dated).

Reaction Bonded Silicone Nitride

Reaction Bonded Silicon Nitride (RBSN) has the strongest covalent bond next to silicone carbide. As a member of the nitrides class, RBSN maintains high strength even at 1400°C, has low thermal expansion coefficient, shows excellent thermal shock resistance and displays first-rate wear and corrosion characteristics.
The physical, mechanical, thermal and electrical properties of RBSN are presented in Table I, as published at www.matweb.com (not dated), www.ferroceramic.com (not dated) and www.cercomceramics.com (not dated).

The main applications of RBSN are parts subjected to high heat sources, such as valves, pumps, pipes, shafts and engine components. Other uses include armor and protection tubes used for the measurement, distribution and containment of materials in molten state, such as aluminum, zinc, sodium, potassium and lithium.

Its exceptional material characteristics make RBSN a difficult-to-machine material. Therefore, special cutting inserts have to be used to cut this material, one of the most used being Polycrystalline Cubic Boron Nitride.
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<th>RBSN</th>
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<tr>
<td><strong>Physical</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Chemical Formula</td>
<td>BN</td>
<td>Si₃N₄</td>
</tr>
<tr>
<td>Density, g/cm³</td>
<td>3.12</td>
<td>2.5</td>
</tr>
<tr>
<td>Color</td>
<td>Amber/gray</td>
<td>dark gray</td>
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<tr>
<td>Crystal Structure</td>
<td>cubic</td>
<td>hexagonal</td>
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<tr>
<td>Water Absorption, %</td>
<td>0-1</td>
<td>0.0</td>
</tr>
<tr>
<td>Hardness, Mohr's</td>
<td>2</td>
<td>9</td>
</tr>
<tr>
<td>Hardness, Knoop, kg/mm²</td>
<td>3000</td>
<td>2200</td>
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<tr>
<td><strong>Mechanical</strong></td>
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<td></td>
</tr>
<tr>
<td>Open Porosity, %</td>
<td>13</td>
<td>20</td>
</tr>
<tr>
<td>Grain Size, μm</td>
<td>N/A</td>
<td>75</td>
</tr>
<tr>
<td>Modulus of Elasticity, GPA</td>
<td>680</td>
<td>170</td>
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<tr>
<td>Flexural Strength, MPa</td>
<td>N/A</td>
<td>675</td>
</tr>
<tr>
<td>Tensile Strength, MPa</td>
<td>N/A</td>
<td>124</td>
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<tr>
<td>Compressive Yield Strength, MPa</td>
<td>1600</td>
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<th>RBSN</th>
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<td>Young’s Modulus, MPa</td>
<td>675,686</td>
<td>172,368</td>
</tr>
<tr>
<td>Poisson Ratio</td>
<td>0.22</td>
<td>0.2</td>
</tr>
<tr>
<td>Fracture Toughness, MPa√m</td>
<td>2.6</td>
<td>5.8</td>
</tr>
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</table>

**Thermal**

| Coefficient of Thermal Expansion, 1000°C, μm/m°C | 4.9  | 3.1  |
| Thermal Conductivity, W/m-K                    | 100  | 12   |
| Heat Capacity, J/g.°C                          | 0.19  | 1.1  |
| Maximum Service Temperature, Air, °C           | 1400  | 1500 |
| Thermal Shock Resistance, °C                   | 1500  | 750  |

**Electrical**

| Electrical Resistivity, Ohm-cm                | 10^{13} | 10^{13} |
| Dielectric Strength, kV/mm                   | 374     | 17.7    |
| Dielectric Constant, MHz                     | 4.08    | 7       |
Machining Hard-to-Cut Materials - Literature Survey

In order to overcome the difficulties of machining advanced ceramics, a wide variety of traditional and non-traditional methods have been proposed (Konig et al., 1990).

**Traditional Methods**

The most used traditional methods are Forming, Pre-Sinter (Green) Machining and Grinding (Saint-Gobain, not dated).

Forming methods include Isostatic Pressing, Dry Pressing, Slip Casing, Injection Molding and Extrusion. The decision factors in choosing which process to use are the production volume, the part geometry and the geometric tolerances of the final part. Consequently, Isostatic Pressing is suitable for prototypes and low volume manufacturing; Dry Pressing, Slip Casing and Injection Molding that can be economically used for volumes of 300 parts or more; while Extrusion is used for high volume, constant section parts. (Saint-Gobain, not dated).

Machining ceramics in green state is regularly performed on conventional CNC lathes and mills. The processes are inexpensive since the material can be removed up to 15 times faster than when the ceramic is in sintered state. The parts are machined up to 0.5%-1% of their final tolerances, with surface qualities of 32-64 μin (Saint-Gobain, not dated).

Suzuki, Uematsu and Nakagawa (1986) described Diamond Grinding. Tolerances of up to 0.0005in can be achieved, with surface finishes up to 4μin by means of honing, lapping and polishing. However, the costs grow exponentially with the
tolerance tightness (Saint-Gobain, not dated). In spite of these drawbacks, high quality surface results and high material removal ratios were reported when high speed machining was combined with EDM in using PCBN grinding tools for machining Inconel 718 (Aspinwall, Dewes, Burrows, Paul and Davies, 2001).

Non-Traditional Methods

Due to the difficulties presented by the traditional machining methods, several non-traditional processes were proposed for machining hard-to-cut materials. Ultra-Sonic Machining (USM) (Cruz, Kozak and Rajurkar, 1992, Cruz and Rajurkar, 1993) is adequate in machining hard-to-cut materials, but it is limited to machining of holes due to the very nature of the process, thus making it reasonable to use for mass machining. For this process guidelines exists on designing the ultrasonically excited tools and the transmission components (Lucas, Petzing, Cardoni, Smith and McGeough, 2001). Abrasive Jet Machining (AJM) was proposed (Jordan, 1986), but its applications are limited to initial rough cutting due to the large surface tolerances obtained. Plasma Enhanced Machining (PEM) is a method that heats the workpiece in order to soften it. This method increases the rate of metal removal and the tool life over the conventional machining process (Uehara and Takeshita, 1986, Kitagawa and Maekawa, 1990 and Mielnik, 1994). However, the cutting insert experiences premature wearing due to the heating effect, thus making the process expensive to use. Another process proposed for machining hard-to-cut materials was Electrical Discharge Machining (EDM) (Martin, Cales, Vivier and Mathiew, 1989, Rajurkar et al., 1991). However, the material removal rate was found to be very slow, thus restricting this process to very few applications.
A new hybrid technology that combines EDM and ECM, called Electro Chemical Discharge Machining (ECDM) was introduced (Schopf, et al., 2001). ECDM is the ideal technology for trueing and dressing of metal bonded diamond tools and hence for the grinding of new cutting materials (Cermets, Ceramics, PCD).

Some of the above mentioned methods can achieve good workpiece quality, but they are not always accompanied by a reduction in the cutting tool wearing, especially in cases of PCBN Cutting Inserts. It was speculated that decreasing the temperatures at the cutting zone would increase the life of the Cutting Insert and improve the dimensional characteristics of the resulting workpiece, thus decreasing the usage price of the inserts and of the workpiece. Cryogenic Machining was proposed in mid 90's (Wang, Rajurkar and Murugappan, 1996) for machining RBSN with PCBN Cutting Inserts. In about a decade the technology became mature enough to be commercially available (Korn, 2004), with the productivity about 200% than the Conventional Turning. The use of Cryogenic Machining was expanded for machining tantalum (Wang, Rajurkar, Fan, Petrescu, 2002) using a tungsten-carbide Cutting Insert, where it was reported to decrease the cutting forces by ~60% and to surface roughness of the workpiece by ~200%.

Rajurkar et al., 1996, proposed a Hybrid Machining approach, which uses a cryogenically cooled cutting insert (PCBN) to machine a heated RBSN ceramic workpiece (attribute that reduces the mechanical resistance of the workpiece, making it more machinable). The experiments presented in this work show drastic reductions of the tool wear when cooling methods were used for machining. Hybrid Machining was as well proposed for machining Inconel 718 (Wang, Rajurkar, Petrescu et al., 2003) and was
found to extend the tool life by \(\sim 170\%\), reduce the cutting forces by up to 50\% and to improve the surface roughness of the workpiece by up to 250\%.

**FEA and Machining – Literature Survey**

Since it is very difficult to experimentally determine the temperatures and stresses in the cutting tool and at the tool-chip interface during the machining process (Shaw, 1984), the Finite Element Method (FEA) was selected for this evaluation. This method was found capable of handling the complexities of the orthogonal metal cutting, which is an approximation of the conventional turning in which the tool edge is straight and normal to both the cutting direction and the feed direction. Since mid seventies, FEA was used to determine the temperature distributions (Tay, Stevenson, De Vahl and Davis, 1974) in orthogonal machining. In mid 1980’s more results were reported on predicting chip geometry, residual stresses in the workpiece and tool stresses and forces (Strenkowski and Caroll, 1985). In late 1980’s The Finite Element Method was expanded based on a thermo-viscoplastic finite element model for analyzing the temperatures in the chip, workpiece and tool (Strenkowski and Luf, 1989), in machining Aluminum 6061-T6 and polycarbonates. This work used an Eulerian approach, in which the workpiece and the chip are treated as a control volume and the finite element grid is stationary relative to the tool cutting edge and the workpiece material flows through it. Later that decade, a finite element analysis performed using the Eulerian method proved that there is no need for a chip separation criterion or empirical data (Strenkovski and Moon, 1990). Also, a finite element analysis of the cutting temperatures for the liquid nitrogen cooled orthogonal cutting was reported (Ding and Hong, 1995), where the
convective heat transfer coefficient of liquid nitrogen was determined experimentally. Most of the work described above was done for machining materials that have continuous chips.

More recently, several authors expanded the research in the area of applying the theory of finite element method to metal cutting. It was found (Dirikolu and Childs, 2000) that the selection of an initial mesh comparatively dense in the critical regions of the geometrical model and the appropriate selection of the material property model and reference frame (Lagrangian or Eulerian) for the chip flow improve the shortcomings of the finite element analysis. A technique that estimates unknowns of the workpiece material model was presented (Ozel and Zeren, 2004). The method uses a workpiece constitutive model based on the Johnson and Cook material work flow stress model and friction model based on the normal stress on the rake face and based on the length of the plastic deformation region of the chip. Orthogonal cutting tests for various cutting conditions were performed in order to determine the method’s parameters. More research employed FEA techniques to simulate the effects of the tool flank wear and chip formation on residual stress during orthogonal cutting of a Titanium alloy (Chen, El-Wardany and Harris, 2004). A crack propagation for the module was incorporated into the FEA to determine the segmental chips produced during machining.

A novel approach that considers the contact mechanics at asperity level and resulting thermal constriction resistance phenomenon was proposed for predicting tool temperature (Attia and Kops, 2004). The findings, supported by an FEA model, showed that the tool coating may cause significant reduction in the heat flowing into the cutting insert.
FEA was successfully used to simulate the stresses generated during grinding (Weinert and Schneider, 2000). This work compared very well to the temperature measurements, proving the validity of the FEA method in describing the temperatures involved in the grinding process.

Crack Propagation in Machining Advanced Materials- Literature Survey

Irwin pioneered the crack propagation theory in 1957 based on a method by Westergaard published in 1939 (Tada, Paris, Irwin, 2000). Several analytical methods exist that find the likeliness of a crack to advance in a material. If the materials are considered linear elastic, finding the fracture toughness $K$ is considered a sufficient criterion. If large plastic zones at the crack tip area are considered, the problem becomes theoretically non-linear and several methods are use to analyze the phenomena developed at the crack tip: the crack tip opening displacement, the R-curve analysis and the $J$-integral analysis. In addition, it was proved that Finite Element Analysis can be successfully used for 2D problems of determining the likeliness of a crack to advance (Tada, Paris, Irwin, 2000).

As machining generally involves not only high forces, but high heat sources as well, special research work has been done to address this particular case of crack propagation in the Cutting Insert. An FEA with local remeshing algorithm was proposed to investigate the size of exit edge chipping as related to flaws of materials and loading conditions during machining glass-ceramics (Cao, 2001). The research found close
correspondence between the analysis and the experiments. Another test was performed (Bhalla et al, 2003), in which a coupled temperature-displacement FEA was used to analyze the temperature field ahead of a crack in a 2D plane-stress analysis. The results were found in excellent correlation with experiments performed using infra-red imaging.

On another note, Paulino et al (2001) used 3D and 2D FEA to investigate the crack propagation in titanium and found good correlation between the FEA results and the calculated J-integral values.

In order to analyze the effects of thermal conditions and the friction between the crack faces, Giannopoulos and Anifantis (2005) used FEA to analyze several types of materials and cracks in order to evaluate the stress intensity factors. They found good correlation between the FEA results and other literature results. The results of their theoretical research can be applied to orthogonal cutting.

**Description of the Work**

The objective of this research was to find an explanation of the observed phenomenon that hybrid machining (turning using cryogenic cooling of the cutting insert to machine a heated workpiece) reduces the wearing of the cutting insert. A quantitative evaluation of the stresses in the cutting insert during hybrid machining was performed to determine the influence of the decrease in the temperature field on the stresses at the tip of the micro-cracks that pre-exist on the flank face of the cutting tool.

It was hypothesized that the growth of the temperatures in the cutting process led to an increase in the stresses at the tips of the micro-cracks that pre-exist in the cutting
insert prior to machining. Consequently, those micro-cracks would enlarge and advance, leading to material loss in the tool and to tool wear. It was theorized that a decrease in the cutting temperatures at the tool-workpiece interface (as is the case in the hybrid machining process) led to a decrease in the stresses in the cutting tool, thus creating less favorable conditions for the pre-existing micro-cracks to grow. This would explain the amelioration of the wear of the cutting insert under hybrid machining conditions.

A finite element model was developed to simulate the cutting conditions during the conventional turning of a workpiece of Reaction Bonded Silicone Nitride with a Polycrystalline Cubic Boron Nitride cutting insert and to analyze the stress distribution. A worst-case scenario for a micro-crack location was found on the flank face of the cutting insert and the stress distribution on the insert in conventional machining regime was analyzed.

A second finite element model was developed with a micro-crack added to the model at the location determined above. The cutting regime was maintained the same as in the previous analysis. The analysis showed that the stress at the location of the tip of the micro-crack increased compared with the case where no micro-crack was present. This proves that the presence of the micro-crack at the tool flank face weakens the cutting insert during conventional turning.

A third finite element analysis was developed to simulate the machining conditions under the hybrid-cutting regime, with the pre-existing micro-crack present. The temperature field was varied to simulate the decrease in temperature due to the hybrid machining conditions. It was noticed that the stress values at the micro-crack tip decreased when the temperature decreased.
The investigation proved that the decrease in the temperature field in the cutting insert led to the creation of less favorable conditions for growing of the pre-existing micro-cracks, thus improving the life of the cutting insert.

Chapter 2 presents the experiment performed for this research and defines the problem, Chapter 3 presents the theoretical basis for the problem and Chapter 4 presents the preparation of the FEA model. The analysis of the results is presented in Chapter 5, while Chapter 6 and 7 present the results, the analysis of the results and the conclusions drawn. Chapter 8 includes a proposal for future work.
CHAPTER 2

EXPERIMENT AND PROBLEM DEFINITION

Experiment

An experiment was performed to evaluate the effects of the liquid nitrogen cooling on the machining a RBSN workpiece (Rajurkar, Wang, Murugappan, 1996). The experiment used:

- a Clausing Colchester 15 lathe with a PCBN50 insert from Sandvik Company
- a liquid nitrogen cooling system
- a temperature measurement system
- a Kistler dynamometer
- a data acquisition system with appropriate amplifiers and A/D converters.

The post-process measurements used:

- a surface profilometer for measuring workpiece surface roughness
- a set of calipers for measuring diameter and chip thickness
- a tool maker's microscope for measuring the tool wear and the tool notching wear.

The experimental setting is presented in Figure 3.
The machining conditions used in the experiment were:

- a speed of 840rpm
- a depth of cut of 0.495mm
- a feed rate of 0.1016mm per revolution.

The experiment measured the tool wear and the cutting forces among other factors. After each cut, the cutting insert was removed, cleaned in acetone and studied under the microscope.

The tool wear happens in two major ways. One of them is crater wear and appears on the rake face, caused by the friction between the newly formed chip and the rake face. The other one is flank wear, and is caused by the friction between the newly
formed surface and the flank face of the tool. The flank wear affects very seriously the work-piece quality (Rajurkar, Wang, Murugappan, 1996).

One of the conclusions of this experiment was that both the width and length of the tool’s flank wear increased rapidly with the cutting length when machining was done without Liquid Nitrogen. The tool wear increased at a lower rate when machining was performed using Liquid Nitrogen. In the case of machining without coolant, the length and width of the tool’s flank wear increased at a rate of about three times faster than when machining was performed using the coolant (Rajurkar, Wang, Murugappan, 1996). Data extracted from this research can be seen in Figure 4.

Figure 4. Flank Wear of PCBN 50 during Hybrid Machining and Standard Turning (as reported by Wang et al., 1996) (data extracted)
Influence of the Internal Cracks in the Wear of the Cutting Insert

Previous work clarified the debate of the influence of the internal cracks versus tool edge surface cracks as a cause for wear of the Cutting Insert during the hybrid machining of RBSN ceramic using a PCBN Cutting Insert (Wang, Rajurkar, Fan, Petrescu, 2004). Because the internal cracks are all started inside the Cutting Insert, it is very difficult to identify them before they actually propagate outside the body of the insert. Even after this phenomenon occurs, it is very difficult to discern between cracks that propagated internally or externally of the Cutting Insert due to the wear marks on the fractured surface caused by the machining process. The research used a J-integral analysis as fracture criteria to determine the likeliness of a crack to propagate inside the body of the Cutting insert and a finite element analysis was employed to determine the temperature gradient in the Cutting Insert. It was shown that the temperatures and the temperature gradients inside the body of the Cutting Insert are lower than those on the faces of the Cutting Edge. The further a crack is away from the edge surface, the lower the J value is. Therefore the J value at any point in the Cutting Insert is always less than the J value at the outside edge of the Cutting Insert, showing that the tendency of micro-crack propagation inside the Cutting Insert is lowered. Based on the J-integral analyses it is reasonable to say that surface cracks and crack adjacent to surfaces are more likely to cause tool fractures.
Problem Definition

It is known that the material forming processes of the cutting inserts and material imperfections of those inserts generate micro-cracks at the physical surface of the material (that can be detected via microscopy or X-ray technology), as well as inside the volume of the material. These kind of geometrical defects are places where stress concentration factors are high, especially during machining conditions where high forces and high temperatures are present, which is the case of machining ceramics.

It was hypothesized that reducing the temperatures in the cutting processes by using hybrid machining process leads to reducing the values of the stresses at the tips of the pre-existing micro-cracks. Thus, the tendency of crack propagation is reduced and the tool wearing is improved.

The logical flow of this hypothesis is illustrated in Figure 5.

A Finite Element Analysis (FEA) was employed to evaluate those temperature and stress fields. The purpose was to find the variation of the stresses with the temperatures at the tip of a pre-existing micro-crack and to compare them to the stresses at the same location, but when no micro-crack is present.
The advantages of using FEA to find the temperature fields and stresses over the experimental means are:

1. FEA is capable of determining the whole temperature field on the cutting insert and the workpiece (Tay, Stevenson, De Vahl and Davis, 1974); experiments would only indirectly determine localized temperatures, thus adding to errors;
2. FEA can be easily tuned to obtain an accuracy of the results of ±5% to ±10%, generally considered no worse than the capabilities of experimental detection of temperatures;

3. FEA can accurately determine the stress distribution in the system cutting tool – workpiece (Strenkowski and Caroll, 1985)
CHAPTER 3

THEORETICAL APPROACH

The analysis used was based on the finite element theory, FEA. The theory behind the FEA is presented below and is based on the work published by Logan, 1997.

In the case in discussion, the starting point is an approximation of the real-scale geometrical structure of the system cutting insert – workpiece - chip to a two-dimensional orthogonal cutting geometrical model. This approximation is generally accepted as sufficient when analyzing the turning process (Grover, 1996). The two-dimensional approximation is also economic in terms of time and disk space used from the numerical analysis point of view.

The validity of the use of FEA as a method to determine the stresses due to the forces developed in the cutting process in the system cutting insert – workpiece – chip is demonstrated by the theoretical development that follows (Logan, 1997).
Stress FEA Theory

The two-dimensional geometrical model has to be discretized into triangular and quadrilateral elements so the temperatures and the stresses at the nodes can be evaluated. The triangular and quadrilateral types of elements used are presented in Figure 6.

![Fig 6](image_url)

Figure 6. Triangular and Quadrilateral Finite Elements used in FEM Analysis

In Figure 6 every node \( P \) is described by the location parameters \((x_p, y_p)\) and by the temperature parameter \( t_p \).

The elements in Figure 6 can be described in terms of the displacements of the nodes as in the equation (3.1), where the values for \( d_i, u_i \) and \( v_i \) are non-existent for the triangular element:

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By selecting a linear displacement function for each element as:

\[
\begin{align*}
\{u\}(x,y) &= a_1 + \alpha_2 \cdot x + \alpha_3 \cdot y \\
\{v\}(x,y) &= a_4 + \alpha_5 \cdot x + \alpha_6 \cdot y
\end{align*}
\]  

(3.2)

we ensure that the displacements along the edges and at the nodes are equal. Here, \(\alpha\)'s are the unknowns. In the following, the discussion will refer at the triangular elements, for the sake of simplicity. Similar results can be obtained using quadrilateral elements.

After substituting the coordinates of the nodal points and solving for \(\alpha\), we will get:

\[
\begin{align*}
\{a_1\} &= \frac{1}{2 \cdot A} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} u_i \\ v_i \\ u_m \end{bmatrix} \\
\{a_2\} &= \frac{1}{2 \cdot A} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} u_j \\ u_m \end{bmatrix} \\
\{a_3\} &= \frac{1}{2 \cdot A} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} v_i \\ v_j \end{bmatrix} \\
\{a_4\} &= \frac{1}{2 \cdot A} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} v_i \\ v_j \end{bmatrix}
\end{align*}
\]  

(3.3)

where:

\[
\begin{align*}
\{\alpha_i\} &= x_i \cdot y_m - x_m \cdot y_i \\
\{\beta_i\} &= y_j - y_m \\
\{\gamma_i\} &= x_m - x_j \\
\{\alpha_j\} &= x_m \cdot y_i - x_i \cdot y_m \\
\{\beta_j\} &= y_m - y_i \\
\{\gamma_j\} &= x_i - x_m \\
\{\alpha_m\} &= x_i \cdot y_j - x_j \cdot y_i \\
\{\beta_m\} &= y_i - y_j \\
\{\gamma_m\} &= x_j - x_i
\end{align*}
\]
and $A$ is the area of the triangular element.

Using the equations for $a_i$'s we will obtain the equations for $u$'s and $v$'s, as follows:

$$
\{u\} = \frac{1}{2 \cdot A} \begin{bmatrix} 1 & x & y \end{bmatrix} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} u_i \\ u_j \\ u_m \end{bmatrix}
$$

and:

$$
\{v\} = \frac{1}{2 \cdot A} \begin{bmatrix} 1 & x & y \end{bmatrix} \begin{bmatrix} \alpha_i & \alpha_j & \alpha_m \\ \beta_i & \beta_j & \beta_m \\ \gamma_i & \gamma_j & \gamma_m \end{bmatrix} \begin{bmatrix} v_i \\ v_j \\ v_m \end{bmatrix}
$$

(3.4)

Or, using the abbreviated matrix form, we will have:

$$
\{\Psi\} = \begin{bmatrix} u(x,y) \\ v(x,y) \end{bmatrix} = [N] \cdot \{d\}
$$

(3.5)

where $N$ is a $2 \times 6$ matrix whose elements $N_{i,j}$ represent the shape functions of the variables $u$ and $v$ when plotted over the surface of the element. This is in the case where the shape function at one node in one direction has a unit value and the other degrees of freedom are equal to zero.

The following columnar matrix gives the strains associated with the two-dimensional triangular element:

$$
\{\varepsilon\} = \begin{bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{bmatrix} = \begin{bmatrix} \frac{\partial u}{\partial x} \\ \frac{\partial u}{\partial y} + \frac{\partial v}{\partial x} \\ \frac{\partial v}{\partial y} \end{bmatrix}
$$

(3.6)
By substituting the displacements equations and working out the derivatives for the shape functions, we will get the expression for the strain as follows:

\[ \{\varepsilon\} = [B] \cdot \{d\} \]  

(3.7)

where:

\[ [B] = \frac{1}{2 \cdot A} \begin{bmatrix} \beta_i & 0 & \beta_j & 0 & \beta_m & 0 \\ 0 & \gamma_i & 0 & \gamma_j & 0 & \gamma_m \\ \gamma_i & \beta_i & \gamma_j & \beta_j & \gamma_m & \beta_m \end{bmatrix} \]  

(3.8)

It can be noticed that the matrix \([B]\) is independent of the x and y coordinates, and depends only on the element nodal coordinates.

Generalizing the Hook’s law (\(\sigma=E\varepsilon\)), the in-plane strain – stress relationship becomes:

\[
\begin{bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{bmatrix} = [D] \cdot \begin{bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{bmatrix}
\]

or:

\[ \{\sigma\} = [D] \cdot [B] \cdot \{d\} \]  

(3.9)

where \(D\) is the constitutive matrix:

\[ [D] = \frac{E}{1-\nu^2} \begin{bmatrix} 1 & \nu & 0 \\ \nu & 1 & 0 \\ 0 & 0 & \frac{1-\nu}{2} \end{bmatrix} \]  

(3.10)

The potential energy functional of the triangular element \(\pi_p\) will be obtained by a summation of the strain energy, potential energy of the body forces, potential energy of the concentrated loads and the potential energy of the distributed loads:
\[
\pi_p = \frac{1}{2} \int \int \int \int \int_{V} \{d\}^{T}[D][B]\{d\} dV - \int \int \int \int_{S} \{d\}^{T}[N]^{T}\{X\} dV - \{d\}^{T}[P] - \int \int \int \int_{S} \{d\}^{T}[N]^{T}\{T\} dS \quad (3.11)
\]

where:

\{X\} = the weight density of the material used;

\{T\} = the surface tractions;

\{P\} = the concentrated loads;

\[V\] = the volume of the whole structure;

\[S\] = the surface of the whole structure.

In the analysis of the problem that is the subject of this research, the surface forces are represented by the temperature field distributed over the area of the cutting insert and the workpiece, whereas the concentrated loads are the cutting forces acting on the rake face of the cutting insert.

Since the equilibrium state of the structure is considered stable, we can apply the Principle of Minimum Potential Energy: that the displacements that satisfy the equations of equilibrium of a system generate a constant value of the potential energy of the system.

At the equilibrium, the potential energy of the element is constant with the respect to the nodal displacements, therefore its first derivative is null:

\[
\frac{\partial \pi_p}{\partial \{d\}} = \left[ \int \int \int_{V} [B]^{T} \cdot [D] \cdot [B] \cdot \{d\} - \{f\} \right] = 0 \quad (3.12)
\]

where \{f\} is the total load of the system.
By carrying out the calculations and differentiating the potential energy with the respect of the vector \( \{d\} \), we obtain the equation for the stiffness matrix of the triangular element, as follows:

\[
[k_s] = t \cdot A \cdot [B]^T \cdot [D] \cdot [B]
\]  
(3.13)

Expanding the stiffness matrices for the individual elements and then adding them up using the principle of superposition we obtain the stiffness matrix of the whole structure:

\[
[K_s] = \sum_{e}^{N} [k_s^e]
\]
(3.14)

where \( N \) is the number of elements of the whole discretized model.

After building the loads and displacement matrices by applying the loads and the boundary conditions at their locations using a columnar matrix, the global force matrix equation can be written as:

\[
\{F_s\} = [K_s] \cdot \{d\}
\]
(3.15)

To solve for the nodal displacements the stiffness matrix is inverted and multiplied with the loads matrix:

\[
\{d\} = [K_s]^{-1} \cdot \{F_s\}
\]
(3.16)

Here, the matrix \( \{F_s\} \) includes the boundary conditions of the global structure at the appropriate element location.
With the known displacements and using the strain definitions, we can obtain the strains, as shown above in the Equation (3.7). Furthermore, the stresses are obtained using the stress-strain relationships using the equation:

\[
\{\sigma\} = [D] \cdot [B] \cdot \{d\}
\]  

(3.17)

Heat Transfer FEA Theory

The theory behind the Heat Transfer FEA is very similar to the Stress FEA theory. The use of the FEA in determining the temperature field in the system cutting insert - workpiece - chip is justified by the following brief outline. In this case the loads are considered the heat sources, while the displacements are considered the temperatures. The causality relationship force - displacements is translated into the heat source - temperatures relationship.

The Figure 7 shows the two-dimensional quadrilateral element subjected to the heat source \( Q \).

Figure 7. Quadrilateral Finite Element used in FEM Heat Transfer Analysis

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The following relations are based on the same three-node triangular element presented on Figure 6. We can define the temperature functions $T$ within each element in terms of the shape functions $[N]$ (whose value is derived as shown above):

$$\{T\} = [N] \cdot \{t\}$$

where:

$$\{t\} = \begin{Bmatrix} t_1 \\ t_2 \\ t_3 \end{Bmatrix}$$

The gradient matrix can be defined similar to the stress matrix in Equation (3.6):

$$\{g\} = \begin{bmatrix} \frac{\partial T}{\partial x} \\ \frac{\partial T}{\partial y} \end{bmatrix}$$

The heat flux is given by the equation:

$$\begin{bmatrix} q_x \\ q_y \end{bmatrix} = -[D] \cdot \{g\}$$

where:

$$[D] = \begin{bmatrix} K_{xx} & 0 \\ 0 & K_{yy} \end{bmatrix}$$

where:

$q_x$ = the heat conducted into the element at the surface edge $x$

$q_y$ = the heat conducted into the element at the surface edge $y$

$[D]$ = the material property matrix

$K_{xx}$ = the thermal conductivity in the $Ox$ direction

$K_{yy}$ = the thermal conductivity in the $Oy$ direction
The following functional is equivalent to the potential energy functional in the Equation (3.11):

\[
\pi_h = \frac{1}{2} \iiint (g^T D g) \, dV - \iiint (\tau^T [N]^T Q) \, dV - \iint_{S_2} (\tau^T [N]^T q^*) \, dS + \frac{1}{2} \iint_{S_3} h \left( [N]^T - T_\infty \right)^2 \, dS
\]

(3.21)

where:

\[ V \] = the controlled volume;

\[ Q \] = the internal heat source;

\[ q^* \] = the heat flux;

\[ h \] = the convection coefficient of the material;

\[ S_2, S_3 \] = separate surface areas over which the heat flux and the convection loss are specified;

\[ T_\infty \] = the boundary condition temperature.

Using the Principle of Minimum Potential Energy stated above, substituting the equations (3.18) – (3.20) into the \( \pi_h \) equation (3.21) and by carrying out the symbolic calculations we obtain:

\[
\frac{\partial \pi_h}{\partial \{t\}} = \iiint (B^T D B) \, dV - \iiint (N^T Q) \, dV - \iint_{S_2} (N^T q^*) \, dS + \iint_{S_3} h \left( [N]^T \right)^2 \, dS - \iint_{S_3} h T_\infty \, dS = 0
\]

(3.22)

The last three terms represent, respectively, the heat source \( \{f_Q\} \), the heat flux \( \{f_q\} \) and the heat convection \( \{f_h\} \):
Writing the element equations in the form of the equation (3.15) yields
\[
\{f\} = \{k,\} \{t\} \tag{3.24}
\]
where \([k,]\) represents the element conduction matrix:
\[
[k,] = \iiint_{V} \{N\}^{T} [D] \{B\} dV + \iint_{S} [N]^{T} [N] dS \tag{3.25}
\]
Expanding the stiffness matrices and then adding them up based on the Principle of Superposition yields the conduction matrix of the whole structure:
\[
[K,] = \sum_{e}^{N} [k,] \tag{3.26}
\]
where \(N\) is the number of elements of the whole discretized model.
The same principle can be applied to the loads to obtain the global force matrix:
\[
\{F\} = \sum_{e}^{N} \{f^e\} \tag{3.27}
\]
The global system of equations can be derived from the matrix equation:
\[
\{F\} = [K,] \{t\} \tag{3.28}
\]
The temperature field due to the heat sources is obtained by using:
\[
\{t\} = [K,]^{-1} \cdot \{F\} \tag{3.29}
\]
The matrix \( \{F_t\} \) includes the boundary conditions of the global structure at the appropriate element location.

Solving the Coupled Force-Thermal Stress FEA Problem

The system cutting insert – workpiece – chip is subjected to cutting forces and to heat sources that generate high temperatures. The FEA can be employed to analyze the stresses resulting from these loads, as can be seen from the following brief outline of the coupled analysis.

The total strain that a structural element similar to ones shown in Figure 6 undergoes is given by the addition of the strain due to thermal loads and the strain due to force loads:

\[
\varepsilon = \varepsilon_t + \varepsilon_i \quad (3.30)
\]

If we let \( 1/E = D^{-1} \) and considering Hook’s Law \( (\sigma = E\varepsilon) \), the equation (3.30) can be written as:

\[
\varepsilon = [D]^{-1} \cdot \sigma + \varepsilon_i \quad (3.31)
\]

Solving for the stress matrix \( \sigma \) yields:

\[
\sigma = D \cdot (\varepsilon - \varepsilon_i) \quad (3.32)
\]

At the same time, if we consider the strain energy density of the element as:

\[
u_0 = \frac{1}{2} [D] \cdot (\varepsilon - \varepsilon_i) \quad (3.33)
\]
and if we plug equation (3.32) into equation (3.33), we obtain:

\[ u_0 = \frac{1}{2} \cdot (\varepsilon - \varepsilon_i)^T \cdot D \cdot (\varepsilon - \varepsilon_i) \]  (3.34)

The total strain energy of the whole structure is obtained by integrating the value of the elemental strain energy density over the whole volume of the structure:

\[ U = \int u_0 dV \]  (3.35)

By considering the equation (3.7), plugging the equation (3.34) into equation (3.35) and simplifying, we obtain:

\[ U = U_s + U_i + ct \]  (3.36)

where:

\[ U_s = \frac{1}{2} \int \mathbf{d}^T \cdot \mathbf{B}^T \cdot \mathbf{D} \cdot \mathbf{d} \cdot dV \]
\[ U_i = \int \mathbf{d}^T \cdot \mathbf{B}^T \cdot \mathbf{D} \cdot \varepsilon_i \cdot dV \]  (3.37)
\[ ct = \frac{1}{2} \int \varepsilon_i^T \cdot \mathbf{D} \cdot (\mathbf{d} \cdot \varepsilon_i) \cdot dV \]

By applying the Principle of Minimum Potential Energy we obtain:

\[ \frac{\partial U_s}{\partial d} = \frac{1}{2} \int \mathbf{d}^T \cdot \mathbf{B}^T \cdot \mathbf{D} \cdot \mathbf{d} \cdot dV \]  (3.38)

\[ \frac{\partial U_i}{\partial d} = \int \mathbf{d}^T \cdot \mathbf{B}^T \cdot \mathbf{D} \cdot \varepsilon_i \cdot dV = \{ \{ \cdot \} \} \]  (3.39)
The equation (3.38) represents the general form of the element stiffness matrix, while the equation (3.39) represents the load due to the temperature change in the element.

The thermal strain matrix for a plane stress element of an isotropic material is given by:

\[
\{ \varepsilon_i \} = \alpha \cdot \{ T \} 
\]  

(3.40)

where \( \alpha \) is the coefficient of thermal expansion and \( T \) is the temperature rise in the element.

For the elements shown in Figure 6, the equation (3.39) becomes:

\[
\{ f_i \} = [B]^T \cdot [D] \cdot \{ \varepsilon_i \} \cdot t \cdot A
\]

(3.41)

By substituting the equations (3.8), (3.10) and (3.40) into equation (3.41) we obtain:

\[
\{ f_i \} = [B]^T \cdot [D] \cdot \{ \varepsilon_i \} \cdot t \cdot A
\]

(3.42)

This way, the constant strain element thermal force matrix becomes:

\[
\{ f_i \} = \begin{bmatrix} f_u \\ f_g \\ f_d \\ f_{ma} \end{bmatrix} = \begin{bmatrix} \alpha_i \\ \beta_i \\ \alpha_j \\ \beta_j \end{bmatrix} \cdot \begin{bmatrix} \alpha_i \\ \beta_i \\ \alpha_m \\ \beta_m \end{bmatrix}
\]

(3.43)

where \( \alpha \)'s and \( \beta \)'s are defined by the equation (3.3).
By adding the thermal force matrix (equation (3.43)) to the force matrix (derived from the equation (3.12)), we obtain the total force matrix for the constant strain element:

\[
\{f\} = \{f_i\} + \{f_t\} = \begin{bmatrix} f_u \\ f_n \\ f_{ul} \\ f_{nm} \end{bmatrix} + \begin{bmatrix} f_u \\ f_n \end{bmatrix} = \begin{bmatrix} \alpha_i \\ \beta_i \\ \alpha_j \\ \beta_j \\ \alpha_m \\ \beta_m \end{bmatrix} + \int_s \begin{bmatrix} N_i & 0 \\ 0 & N_i \\ N_j & 0 \\ 0 & N_j \\ N_m & 0 \\ 0 & N_m \end{bmatrix} \begin{bmatrix} T_{ix} \\ T_{iy} \\ T_{ix} \\ T_{iy} \\ T_{hx} \\ T_{hy} \end{bmatrix} (3.44)
\]

where \{T\} represents the load matrix along Ox and Oy axes.

By superimposing the element stiffness matrices and the element force matrices, we obtain the global stiffness matrix as function of the global force matrix:

\[
\{F_G\} = [K_G]\{d\} (3.45)
\]

equation that can be solved for \{d\} by inverting the \([K_G]\) matrix. The use of the equation (3.17) will result in the stresses over the whole considered structure.

All the calculations involved can were performed using the commercial computer code MSC.Patran/Nastran.
As mentioned before, the problem was analyzed using FEA techniques. The finite element package used was MSC.Patran/Nastran. The geometry was modeled using MSC.Patran. SOL101 was the solver used for the stress analysis while SOL 104 was the solver used for the heat transfer analysis. SOL101 was used one more time to solve the coupled analysis. Both solvers are based on linear FEA.

Two geometries of the cutting insert - workpiece – chip system were used:

1. Without a micro-crack (the No-Crack Model)

2. With a micro-crack at a pre-determined location (the Crack Model). The location of the micro-crack was determined after interpreting the results from the analysis of the first geometry.

The following analyses were performed on the above geometries:

- The force loads determined experimentally during the cutting process (Rajurkar et al., 1996) together with the displacement boundary conditions were applied to the cutting tool and the stress field was obtained.

- The heat sources generated during the cutting process and theoretically determined (according to the theory provided by Goover, 2003) together with
insulation boundary conditions were applied. The temperature fields obtained were used to generate corresponding stress distributions. The resulting stress field will be compared to the one obtained from the thermal analysis.

- Finally, the force loads were combined with the temperature fields obtained from the thermal analysis on a coupled analysis, and the stresses at the crack location were evaluated.

The results from the above three analyses were compared against each other.

**Assumptions**

The following assumptions were postulated:

- the turning process described on Chapter 2 can be described sufficiently accurate by the orthogonal cutting theory. This theory was extensively used before to simulate the turning process (Groover, 2003, Ozel and Zeren, 2004, Attia and Kops, 2004, Chen, El-Wardany and Harris, 2004, among others) and excellent results were obtained; it considers a 2D geometry that simplifies the complexities of the actual three dimensional system cutting insert - chip – workpiece by considering only the two dimensions that play a significant role in the analysis; in spite of ignoring one dimension from the analysis it describes the mechanics of the machining process fairly accurately (Groover, 2003);

- the materials forming the tool and workpiece are continuous and their properties are isotropic;
the problem is two dimensional, with a given thickness on the direction normal to the plane of work. The geometrical constraints imposed (analysis of micro-size geometrical features that are present in real-life geometry) can be defined by the plane-stress case: there are no strains on a plane normal on the plane of analysis, while the stresses in the same plane are non-zero. Although the problem appears to be a case of plane-strain, the area of interest is at the location of a crack. This area can be accurately represented by plane-stress analysis for cracks (Tada, Paris, Irwin, 2000). Moreover, the plane-stress assumptions were widely used with success to analyze the phenomena developed in the crack analysis problems (Bhalla, Zehnder and Han, 2003, among others). The following set of equations represent those assumptions:

\[
\begin{align*}
\sigma_z &= 0 \\
\tau_{xz} &= 0 \\
\tau_{yz} &= 0 \\
\varepsilon_z &\neq 0 \\
\gamma_{xz} &\neq 0 \\
\gamma_{yz} &\neq 0
\end{align*}
\]

- the heat given by the primary and secondary (Q₁ and Q₂) heat sources are linear and evenly distributed along those lines (see Figure 17);
- the tertiary heat source due to the friction between the cutting insert and the newly formed surface (Q₃) (see figure 17) will be neglected as there is no model available to describe it;
all of the above warrant the use of a linear FEA solver, as the material is considered linear and the loads involved in the analysis are not expected to enter the plastic zone of the materials involved.

Tool Geometry

The Cutting Insert was made out of a tool holder with a parallelogram shaped cutting insert. The geometrical characteristics of the cutting process were the same as the process parameters used in the hybrid machining experiment performed by Rajurkar, Wang and Murugappan (1996).

The geometry of the orthogonal machining process, without the micro-crack present is shown in Figure 8. The dimensions considered in orthogonal cutting are also presented.

The geometric characteristics of the tool insert are presented in the Table II while the Figure 9 depicts the geometry of the newly formed chip.
Figure 8. Geometric Model of the Cutting Insert and the Workpiece

Figure 9. Geometric Model of the Tool and the Work-Piece (chip area detail)
TABLE II  Geometrical characteristics of the Cutting Tool

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Rake angle:</td>
<td>10°</td>
</tr>
<tr>
<td>Flank angle:</td>
<td>6°</td>
</tr>
<tr>
<td>Insert thickness</td>
<td>10 mm</td>
</tr>
</tbody>
</table>

The geometrical characteristics of the chip are presented in Table III.

TABLE III  Geometrical characteristics of the chip

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>The length of contact between the</td>
<td>0.1 mm</td>
</tr>
<tr>
<td>chip and the tool:</td>
<td></td>
</tr>
<tr>
<td>Unformed chip thickness:</td>
<td>.05 mm</td>
</tr>
<tr>
<td>Chip thickness:</td>
<td>0.215 mm</td>
</tr>
<tr>
<td>Width of cut</td>
<td>10 mm</td>
</tr>
</tbody>
</table>
After the initial analysis was performed on the No-Crack geometry and data was extracted and analyzed a new geometry was created. This time the geometry had a micro-crack modeled on the Flank face of the cutting insert. The overall dimensions of the cutting insert – chip – workpiece were maintained. The geometry exhibiting the micro-crack can be seen in Figure 9 while the geometric characteristics of the micro-crack are presented in Figure 10.

Figure 10. Geometric Model of the Cutting insert containing a micro-crack and the Workpiece
Figure 11. Geometric Model of the Cutting insert containing a micro-crack and the Workpiece

Finite Element Discretization

The geometric model was parted into several surfaces connected together at their edges. All those surfaces were discretized into 2D triangular and quadrilateral finite elements. The area of interest was at the tip of the Cutting Insert, were high temperatures and high stresses were expected to develop. Consequently, this was the region where the highest mesh density was used. In order to generate a high-density mesh, the region was separated from the rest by means of small surfaces. The organization of the surfaces on the region of the tool tip is shown in Figure 12.

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The surfaces were separated on two property groups. The surfaces forming the Cutting Insert were assigned the PCBN material properties while the surfaces on the work-piece area were assigned the RBSN material properties. The material properties are listed in the Table I. Both property groups were comprised of 2D four-noded plane stress elements, triangular and quadrilateral, with the thickness of 10mm.

The initial geometry that did not contain a micro-crack (as seen in Figure 8) was discretized into a mesh of 202,089 elements and 97,196 nodes. The mesh density at the tool tip area is presented in Figure 13. It can be noted that the area near the crack tip,
where high temperatures and high stresses are expected, has a finer mesh than the area away from the crack tip. Also, the areas on the workpiece have a less dense mesh, as the interest of this research was to analyze the phenomena at the tip of Cutting Insert.

![Figure 13. Distribution of the FEM on the No-Crack model (Chip area Detail)](image)

After the No-Crack model was analyzed a new geometry was constructed that included a micro-crack located on the flank face of the cutting insert (see Figure 9 and 10): the Crack Model. The location of the micro-crack was determined based on the conclusions drawn after the analysis of the No-Crack model.
One node located one element away from the edge representing the Flank Face of the Cutting insert, and along the stress line defined in Figure 23, was transformed into the tip of the micro-crack by releasing the connections between the two adjacent elements (see Figure 14). The analysis was performed one more time and the results were extracted. This case had one element on each of the edges of the micro-crack, as can be seen in Figure 14.

After this analysis, the number of nodes at both edges of the crack was increased to 10, with more elements towards the crack tip, and the analyses were re-run. The stresses at the crack tip were found to be ~32 times larger than in the previous case. Figure 15 shows the organization of the elements in this mesh in this case.
Later, one more iteration was performed, in which the sides of the crack were discretized into 20 elements each. In this case, the stress at the tip of the micro-crack increased ~8 times compared to the previous case and 268 times larger than the original case where only one element per side were considered.
Figure 16. Distribution of the FEM on the No-Crack model (Chip area Detail) – 20 nodes per micro-crack side

Figure 16 shows the organization of the mesh in this case. This improvement in results due to the mesh refining was considered satisfactory as a compromise between accuracy of the results and computational time.

As a summary, the mesh used in the No-Crack analysis is shown in Figure 13 and used 97196 nodes. The Crack analysis used 98,176 nodes and the representation of the mesh discretization is presented in Figure 16.

A summary of the geometrical surfaces, finite elements and nodes used on the model is presented in Table IV.
## TABLE IV  Finite Elements Model settings

<table>
<thead>
<tr>
<th>Number of Surfaces:</th>
<th>29</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of elements:</td>
<td>202254 (crack model)</td>
</tr>
<tr>
<td></td>
<td>202089 (no-crack model)</td>
</tr>
<tr>
<td>Number of nodes:</td>
<td>98176 (crack model)</td>
</tr>
<tr>
<td></td>
<td>97196 (no-crack model)</td>
</tr>
</tbody>
</table>

### Loads

There were two different types of loads considered, according to each type of analysis. The Thermal Analysis used the heat sources at the shear plane of the chip and at the rake face. The Structural Analysis used as loads the cutting forces and the temperatures obtained from the Thermal Analysis.

### Forces

Rajurkar, Wang and Murugappan (1996) experimentally obtained the values of the forces acting on the cutting insert in the process of hybrid machining of RBSN using a PCBN cutting insert. The experiment was performed at the Machine Tool Agile Manufacturing Research Institute at the University of Nebraska, Lincoln, NE and was described in detail in Chapter 2.

Referring to Figure 17, the following forces were determined:
- the friction force $F_f$ between the cutting insert and the newly formed chip, acting along the line AB at the interface between the cutting insert and the chip; this force was found to be (on average) $F_f = 180 \text{ N}$;

- the normal force $N$ on the cutting insert, acting distributed along the line AB at the interface between the cutting insert and the newly formed chip; this force was found to be (on average) $N = 100 \text{ N}$.

Figure 17. Friction forces $F_f$ and Normal forces $N$ acting on the PCBN Cutting Insert

The friction Force $F_f$ was decomposed along Ox and Oy axes in order to be properly input in the FEA code:
\[ F_{rx} = F_r \sin(10^\circ) = 31.3 \text{ N} \]

\[ F_{ry} = F_r \cos(10^\circ) = 177.6 \text{ N} \]

The normal force was decomposed along Ox and Oy axes in order to be properly input in the FEA code:

\[ N_x = N \cdot \cos(6^\circ) = 99.5 \text{ N} \]

\[ N_y = -N \cdot \sin(6^\circ) = -10.4 \text{ N} \]

**Heat Sources**

The heat developed in the orthogonal cutting process and acting at the tool – workpiece interface is generated by three heat sources. Figure 18 presents the location of those sources in the orthogonal cutting process.

The heat sources have the following origins:

1. the shear effect on the chip \( (Q_1) \), along the line BC in the Figure 18:

\[ Q_1 = Q_{11} + Q_{12} \]

2. the friction between the rake face of the tool and the newly formed chip \( (Q_2) \), at the interface between the cutting insert and the newly formed chip, along the line AB in Figure 18
Figure 18. Heat sources considered in orthogonal cutting of RBSN with PCBN Cutting Insert

3. the friction between the cutting insert and the newly formed surface (Q₃), at the point B; this source was neglected, as there are no theoretical representations available to describe it.

A complete derivation of the values for the heat sources is found in Appendix I. The values considered are:

\[ Q_{11} = 2000 \text{ W} \]
\[ Q_{12} = 12000 \text{ W} \]
\[ Q_2 = 7000 \text{ W} \]

Here, \( Q_1 \) was split into two sources due to the different meshing in the area BC of the geometry: \( Q_{11} \) and \( Q_{12} \).

Since the relationship between the workpiece speed and the heat sources is linear, it was possible to scale the values for the heat forces in order to obtain the values for heat sources corresponding to different cutting conditions.

**Boundary Conditions**

*Structural Boundary Conditions*

For the structural analysis, the boundary conditions consisted of translation and rotation restrictions on all the axes on the outside edges of the model. The inside edges of the model were free to rotate and translate in the plane of the model. The settings are illustrated in Figure 19.

The cutting forces developed in the orthogonal cutting process are presented in Figure 17.
Thermal Boundary Conditions

The thermal analysis used the boundary conditions set as in Figure 20. They assumed thermal insulation on the edges of the model. The temperatures on those edges were set at the level of the room temperature, 25°C and it was assumed that the temperature of the outside environment does not require insulation properties to the other free edges. Subsequent analyses performed showed that the temperature gradient was gradually deceasing as moving away from the center of the geometrical model (where the heat sources reside) and towards the edges of the tool and of the workpiece, and all of the outside edges were at 25°C. The temperature distribution obtained showed that the edges of the model are far enough from the heat sources, and they do not cause the temperature
at the edges to be away from the thermal boundary condition of 25°C. This proved the accuracy of the boundary conditions selection and values.

Figure 20. Boundary conditions used for the thermal analysis

The heat sources involved in the orthogonal cutting process are presented in Figure 18.
CHAPTER 5

FEA OF THE CUTTING INSERT – ANALYSIS

The analysis began with a geometrical model of the cutting insert cutting the workpiece, assuming no micro-crack was present on the rake face of the cutting insert. After analyzing the results, an worst-case location of a micro-crack was chosen and the analysis was performed again. The results were analyzed once more. The analysis used MSC.Patran/Nastran with SOL101 solver for the stress analysis and SOL153 solver for the thermal analysis.

No-Crack Model

The No-Crack model consisted of the geometry presented in Figure 8, with detail of the chip and cutting tool tip presented in Figure 11. The set of boundary conditions considered was presented in the Figure 19 for the stress analysis and in Figure 20 for the thermal analysis.

Forces Load Analysis, No-Crack Model

This analysis sought to find the contribution of the cutting forces only to the stresses developed in the cutting insert. The stress distribution due to the force loads on the No-Crack Model is shown in Figure 21. Figure 22 shows a detail near the tip of the
cutting insert. The maximum stress is located at the area tip of the cutting insert and has a value of $7.72 \times 10^6 \text{N/mm}^2$, with the stresses decreasing towards the tool-holder area.

![Stress Distribution](image)

Figure 21. Stress Distribution caused by the Forces loads acting on the No-Crack Model, (tool tip detail)

An interesting point can be made here: on the Flank face, away from the area with highest stress levels (see the dotted line in Figure 22), the stresses have opposite orientations, suggesting a state of tension in the material. A delimitation line between the stresses starting from the flank face and directed away from the Flank face and towards
the inside of the body of the cutting insert can be easily imagined. Even at this stage where the simulation is not very accurate, as the loads due to the temperatures involved in the machining process are missing, the presence of two opposite stresses is obvious. The value of the stress at this area is 1.54E6 N/mm². This area is referred to as the “virtual crack area”.

Figure 22. Stress Distribution and Orientation (compressive) due to the Forces loads acting on the No-Crack Model, (virtual crack detail)
Temperature Distribution Analysis, No-Crack Model

In this case, the analysis looked for the distribution of the temperature in the cutting insert due only to the heat sources developed in the orthogonal cutting insert considered.

As mentioned before, two heat sources were considered in this process:

- The heat generated at the shear plane of the chip, \( Q_1 \).
- The heat due to the friction between the newly formed chip and the rake face of the tool, \( Q_2 \).

The boundary conditions consisted of outside edges of the geometric model being at a room temperature, 25°C, as seen in Figure 20. The location of the heat sources can be seen in Figure 18.

The thermal analysis revealed a temperature field due to this Loads/Boundary Conditions setting that is presented in Figure 23.

As expected, the maximum temperature is located on the rake face of the tool tip and on the tool area. The temperature value in this area is \(~1060°C\). The temperatures gradually decrease towards the edges of the tool and working piece, where they are at 25°C, the room temperature, proving the accuracy of the boundary conditions selected in Figure 20.

The temperature field obtained from this analysis was used as additional load in the structural analysis.
In this case the object of the analysis was to determine the contribution of the heat sources developed in the cutting process to the stress distribution in the cutting insert. The load consisted only of the temperature field determined in previous analysis while the boundary conditions are the ones set for Force Load Analysis and shown in Figure 19. A coupled analysis was performed to obtain the stress distribution field. Figure 24 shows the stress distribution due to the temperature loads at the future location of the micro-crack.
As expected, the maximum stress obtained has a value of 383E+06 N/mm² and is located at the tip of the cutting insert, where the temperatures are higher. The same antagonistic behavior of the stresses in the area where this phenomenon was noted in the Forces Load Analysis can be seen. The maximum stress in the area of the antagonistic stresses observed in the case of force load analysis has a value of 25.5E6 N/mm². The values of the stresses are more than 150% higher than the stresses developed due to the
cutting forces only. This shows that the heat developed in this orthogonal cutting process has a more malignant effect on the cutting insert than the cutting forces alone.

*Temperature and Forces Analysis, No-Crack Model*

The analysis in this case was aimed at determining stress distribution in the cutting insert due to both the cutting forces and the heat sources developed in the process. The loads considered in this case consisted of both the cutting forces determined experimentally and the temperature loads determined in the previous analysis, while the boundary conditions were the ones presented in Figure 19. The combined loads of cutting forces and field of temperatures results in a stress distribution described in the Figure 25.

It can be observed that the stress distribution is very similar to the one obtained in the case of the temperatures loads and shown in Figure 24. However, the stress values are slightly different, due to the additional loads considered (the cutting forces). The maximum stress value in this case 377E+06N/mm² is lower than in the previous case. This is due to the antagonistic actions of the force load and temperature load. The stresses due to the force loads tend to compress the material, while the forces due to the temperature field load tend to expand the material, thus balancing to a small degree the force loads stresses.

The situation at the location pointed out by the dotted line in Figure 25 is also very similar to the one where only the temperatures field was used as a load (Figure 24). The only noticeable difference between the stress distributions is in terms of the values obtained. Here, the maximum value in the area shown by the line is 25.5E+06N/mm².
This is again slightly lower than in case shown in Figure 24, for the same reasons explained above.

Figure 25. Stress Distribution and Orientation (compressive) due to the Force and Temperature loads acting on the No-Crack Model, (virtual crack detail)

Crack Model

It is well known that the cutting inserts wear in two modes:

- Crater Wear: it consists of an appearance of a concave section on the rake face of the tool caused by the action of the chip sliding against the surface.
- Flank Wear: it results from rubbing between the newly generated workpiece surface and the flank face adjacent to the cutting edge.

The most important is the Flank Wear since it makes the insert become duller and affects the dimension of the resulting workpiece. It was hypothesized that both wearing modes are caused by pre-existing micro-cracks in the geometry of the tool. In the following, the presence of a micro-crack was assumed at an worst-case location (see Figure 26) and the stresses at the crack tip were discussed. Figure 10 shows the dimensional values assumed for the micro-crack.

The model presented previously shows an antagonistic behavior of the stresses near the flank face of the tool, at a certain distance from the tool tip and on normal cutting conditions. According to the wearing theories explained above, if a micro-crack would exist at this location, there would be a good chance that conditions will become favorable for it to propagate inside the tool material.

In the following we will consider a micro-crack located at the location where the stresses are already acting against each other. Three analyses were performed, as in the No-Crack Model section, and the results will be analyzed.
The aim of this analysis was to find the influence of the cutting forces on the stresses developed in the cutting insert, when a micro-crack is present on the rake face of the cutting insert. As stated before, in this case the load consisted of only the forces acting on the tool tip that were experimentally determined by Rajurkar, Wang and Murugappan (1996).

The boundary conditions consisted of the fixed outside edges of the cutting tool, as shown in Figure 19.
The stress distribution obtained at the crack tip with the stress distribution is shown in Figure 27. The maximum stress is located on the rake face, very near the tool tip and has a value of 7.73E+06N/mm². It can be noted a clear stress concentration at the crack tip, as expected, with a maximum value of 3.09E+06N/mm². The stresses on the crack faces are oriented from the crack towards the material, as expected, showing that the crack has a tendency to open. The stresses at the crack tip are oriented from the material towards the crack opening, which denotes a tendency of displacement of the crack surfaces and suggests an advancement of the crack tip.

Figure 27. Stress Distribution and Orientation (compressive) due to the Forces Loads acting on the Crack Model, (crack detail)
Also, a stress concentration area was observed at the area of beginning of the chip formation. This stress causes the chip to be separated from the workpiece material.

*Temperature Distribution Analysis, Crack Model*

The analysis was performed to obtain the temperature field in the cutting insert due to the heat source developed during the orthogonal cutting process considered, on the geometry with a micro-crack present.

The heat generated at the shear plane of the chip \((Q_1)\) and the heat due to the friction between the newly formed chip and the rake face of the tool \((Q_2)\) were the loads used in this model. The boundary conditions consisted of the outside edges of the geometric model being at a room temperature, 25°C (as in Figure 20).

This Loads/Boundary Conditions setting causes a development of a temperature field as shown in Figure 28.

As expected, the maximum temperature is located on the rake face of the tool tip and on the tool area. The temperature value in this area is 1060°C. The temperatures gradually decrease towards the edges of the tool and working piece, where they were set to 25°C, the room temperature.

By comparing the temperatures obtained in both the No-Crack and the Crack models it can be seen that the differences are small. It can be inferred that the influence of the crack on the temperature field is minimal.
The temperature field obtained from this analysis was used as an additional load in the coupled structural analysis.

*Temperature Load Analysis, Crack Model*

The analysis in this case sought to determine the influence of the heat sources on the distribution of the stresses in the cutting insert, when a micro-crack was present on the Flank face.

In this case, the load consisted of only the temperature field calculated at the previous analysis while the boundary conditions are the ones set for Force Load analysis.
(as in Figure 19). Figure 29 shows the stress distribution that results from those loads at the location of the micro-crack.

Figure 29. Stress Distribution and Orientation (compressive) due to the Temperatures loads acting on the Crack Model (micro-crack detail)

The maximum stress obtained has the value of 382E+06N/mm$^2$ and is located at the tip of the tool. As expected, a stress concentration area is observed at the crack tip, with the value of 76.2E+06N/mm$^2$. The stresses on the micro-crack faces are oriented
away from the material, while the stress at the crack tip is oriented towards the material. This shows an opening tendency of the crack associated with a displacement of the crack tip inside the material (a crack expansion is suggested).

Temperature and Force Load Analysis, Crack Model

In this case the scope of the analysis was to find the stress distribution in the cutting insert at the tip of the micro-crack, when both the force loads and temperature loads were considered. The loads consisted of both the cutting forces and the temperature loads. The combined loads of the cutting forces and of the temperatures field resulted in a stress distribution at the micro-crack location shown in Figure 30.

One can note that the stress distribution is very similar to the one obtained in the case of the temperatures loads. However, the stress values are slightly different. The maximum stress value in this case 375E+06N/mm², lower than in the previous case. This is due to the antagonistic actions of the force load (that has the tendency of compressing the tool) and temperature load (that has the tendency of expanding the tool).

The situation at the crack tip is also very similar to the one where only the temperature field was used as a load. The stress distributions differ only in terms of the values obtained. Here, the maximum value in the area is at the crack tip, as expected, and has a value of 75.1E+06N/mm². Once again, this value is slightly lower than in the case where only the temperature field was used a load, for the same reasons explained above.

The stresses on the crack faces have very similar orientations to the case where only the temperature field was the loads. Again, it is suggested that the crack has an
opening tendency, associated with a displacement of the crack tip inside the material (a crack expansion is implied).

Figure 30. Stress Distribution and Orientation (compressive) due to the Temperatures loads acting on the Crack Model (micro-crack detail)

Comparison of the Results and Discussion

The finite element analysis showed expected results in terms of the distribution of stresses. If the cutting insert exhibits a micro-crack on its flank face, a stress
concentration area at the crack tip appears. The overall maximum stresses are slightly lower in the Crack case than in the No-Crack case since the material is more flexible due to the very existence of the crack. Moreover, after the comparison of those two cases, it can be noticed that the stresses at the tip of the micro-crack drastically increase, when comparing with the stress at the same location but on the No-Crack Model.

**Stress Orientations and Values - No-Crack Model**

The structural analysis of the No-Crack model revealed the existence of stresses of opposite directions at a location close to the Flank Face when both the heat sources ($Q_1$ and $Q_2$) and cutting forces in orthogonal cutting conditions acted on the cutting tool. The values of the stresses were in the range of $25.2E+06 N/mm^2$. Those stresses were located along a line that starts at the flank face, as seen in Figure 25. It was inferred that a pre-existing micro-crack located at this position on the cutting insert would represent a worst-case scenario for a micro-crack to appear. Therefore, a crack was modeled in the cutting tool at that location, as can be seen in Figure 9.

**Stress Orientations and Values - Crack Model**

With the FEA model set as described above, another structural analysis was performed. The overall stress distributions were similar to those obtained in the No-Crack model. However, one could notice that the stresses at the crack location and along the line defined in Figure 25 had the same opposite orientations. This tendency of the stresses was the same as on the crack faces, showing the predisposition of the crack to open and advance. Also, different stress orientations can be seen all the way along the stress line defined in Figure 25.
However, there were differences, as expected. There was a significant increase in the stress values at the crack tip, when compared to the No-Crack model.
CHAPTER 6

GENERALIZATION AND ANALYSIS OF RESULTS

Hybrid Cutting Conditions Analysis

The above Finite Element Analysis was repeated by using varying cutting conditions as input. Due to hybrid cutting conditions the heat sources were modified to simulate the heating and cooling of the cutting area. The heat sources determined in Appendix I were multiplied with factors of 0.6, 0.8, 1.2, 1.4, 1.6 and 1.8 in order to simulate the temperatures in the cutting area of as low as 650°C and as high as 1740°C. This way cutting regimes representing hybrid cutting conditions were reproduced. The heat sources were increased no further due to the fact that the maximum stress developed in the cutting tool at 1740°C is close to the compressive yield point of PCBN of 650MPa and a cutting regime with these parameters would be impractical. At the same time the geometrical characteristics of the crack were maintained the same. The meshing used for both the No-Crack Model and Crack-Model were kept the same as well.

The data obtained are presented in tabular form in the Appendix II. Figure 31, Figure 32 and Figure 33 show the variation of the stresses at the crack tip, as well as the variation of the maximum stresses under different cutting conditions.
Figure 31. Stress Growth for the No-Crack Case, Heat and Forces Loads

Figure 32. Stress Growth for the Crack Case, Heat and Forces Loads
Figure 33. Stress Growth for the Crack Case and No-crack Case, Micro-Crack location

Analysis of Results

The following conclusions can be inferred:

1) In the case where no crack is present, the maximum stress and the stress at the virtual crack tip grow as the temperature increases, as expected. The dependence to the temperature is very close to linear. However, the increase is more dramatic for the maximum stress than for the stress at the virtual crack tip (see Figure 31). The maximum stress grows at a rate of about 500% faster than the virtual crack tip stress does.

In practical terms this indicates that the maximum stress in the cutting tool decreases linearly with the temperatures at a rate of about five times faster than the stresses at the virtual crack tip does. This shows that the hybrid machining with cooling techniques leads to a lowering of the maximum stresses developed in the cutting insert.
2) In the case where a micro-crack pre-exists on the flank face of the cutting insert, the maximum stresses and the stresses at the crack tip grow as the temperature increases, as expected. The dependence to the temperature is also close to linear. The increase is even more dramatic for the maximum stress than for the stress at the virtual crack tip (see Figure 32). The maximum stress grows at a rate of about 1500% times faster than the crack tip stress does.

In practical terms, this means that when a micro-crack is present on the flank face of the cutting tool and when the temperature in the cutting tool is decreasing, the maximum stress decreases at a rate about 15 times faster than the stress at the tip of the crack does. This shows that the hybrid machining with cooling techniques results in lowering the maximum stresses developed in the cutting tool. This machining technique also lowers the stresses developed at a pre-existing tip of a micro-crack.

3) When comparing the stresses at the crack tip and the stresses at the virtual crack tip, it can be noticed that in the situation where a micro-crack is present on the flank face of the insert, the stresses have a growing rate with the temperature of about 300% faster than when no crack is present at the flank face of the insert (see Figure 33). The dependence to the temperature is also close to linear. In relative terms, the stresses at the crack tip are about 300% of the stress at the virtual crack location.

In practical terms, the above illustrates that when the temperature in the cutting tool decreases, the stress developed at the crack tip decreases about
three times faster than the stress developed at the same location but when no crack is present. The stresses decrease from about four times the value of the stress at the virtual crack to about three times that value.

The above three conclusions show that hybrid machining with cooling techniques results in lowering the stresses developed at the tip of a pre-existing micro-crack on the flank face of the cutting insert by a factor of three. This relates well with the experiment performed by Rajurkar et al (1996) where it was found that Hybrid Machining of RBSN ceramic slows the wearing of the PCBN Cutting Insert by about three times when compared to the Standard Turning (see Figure 4).

Since the tendency of the crack propagation in the body of the cutting insert is reduced under hybrid cutting conditions, therefore the wearing of the cutting insert is lowered. This sequence of events as an explanation for the wearing of the Cutting Insert is supported is illustrated in Figure 34.
Hybrid machining with cooling techniques

Stresses at the crack-tip are lowered

Micro-crack pre-exists on the flank face

Less favorable conditions for micro-crack growing

The wearing of the Cutting Insert is ~three times slower

Figure 34. Flow Chart of the factors influencing the Hybrid Machining on the Cutting Inserts
CHAPTER 7

CONCLUSION

An FEA model was developed that theoretically explains the observed phenomenon of wear decrease in the PCBN cutting insert during hybrid machining of RBSN ceramic. It was hypothesized that the wearing of the Cutting Insert is caused by micro-cracks that pre-exist on the Flank Face of the Cutting Insert. The analysis was performed on two separate geometries: one without a crack, representing the ideal geometry of the Cutting Insert, and the other one having a micro-crack modeled at an worst location found based on the results obtained from the analysis of the No-Crack model.

A comparison between the results obtained from analyzing both the No-Crack and the Crack models under different cutting conditions was performed. The outcome of the analyses proved to be consistent with the experimental findings that during the hybrid machining of RBSN ceramic with a PCBN cutting insert, the wear of the cutting insert is about 300% slower than in normal cutting conditions. The finite element analyses showed that a decrease in the temperature fields that develop at the interface cutting insert – chip – workpiece from 1740°C to 597°C (which corresponds to hybrid machining conditions) results in a rate of reduction in the stresses at the tips of pre-existing micro-
crack of about three times faster than the stresses at the same location but when no micro-crack pre­exists. This way, a link was found between the theory and the observed phenomenon of the decrease of the tool wearing in the hybrid cutting conditions.

The good correlation between the experimental data and the theoretical data proved that a pre-existing micro-crack on the Flank face of the PCBN Cutting insert decreases the likeliness of the tool wearing under hybrid cutting conditions by a factor of three.
CHAPTER 8

FUTURE WORK

This work provides a clear direction for the investigation of the influence of the pre-existing micro-cracks on the Flank Face on the wearing of the Cutting Insert. More accurate results can be obtained if a finer mesh is used, if plasticity at the tip of the micro-crack is considered and if different locations and orientations of a micro-crack are considered. Multiple micro-cracks can also be considered.

Future work can be developed in four main areas:

1. Once a theoretical model that represents the heat source coming from the friction between the Cutting Insert and the newly formed surface on the workpiece is developed ($Q_3$), it can be included in the analysis in order to obtain more accurate results.

2. Future work can be done to analyze the influence of the shape of the crack tip on the propagation of the crack into the body of the Cutting Insert and to determine the severity of crack types; once this is accomplished, research can be done in order to determine better manufacturing techniques that result in micro-cracks less prone to propagation in the body of the PCBN cutting insert.
3. Further work has to be performed to correlate the wear on the Rake Face of the tool with pre-existence of the micro-cracks in that area, where similar FEA set-up can be employed.

4. A generalization of the model presented in this research can be made to include ceramic cutting inserts as a class, as well as cutting inserts made out of other materials.

On a separate note, a special attention should be given to developing better tool materials and material technologies that produce materials with less micro-cracks.
APPENDIX I

HEAT SOURCES IN THE CUTTING PROCESS

The turning process of RBSN ceramic using PCBN cutting insert was assumed to be accurately represented by the orthogonal cutting theory.

There are two main heat sources that come into play: the shear effect on the chip (Q₁) and the friction between the rake face of the tool and the newly formed chip (Q₂).

The geometrical characteristics of the cutting process considered are:

- Undeformed Chip Thickness (feed rate): \( t = 0.0005 \text{m} \)
- Width of Cut: \( b = 0.001 \text{m} \)
- Length of cut in the direction of motion: \( a = 0.0001 \text{m} \)
- Rake angle: \( \alpha = 10 \cdot \frac{\pi}{180} \text{ rad} \)
- Flank Angle: \( \phi = 10 \cdot \frac{\pi}{180} \text{ rad} \)
- Workpiece velocity: \( n = 385 \text{ rpm} \)
- Workpiece diameter: \( D = 71.12 \text{ mm} \)
The cutting force is comprised of a friction force $F_f$ along the cutting edge and away from the tool tip and a normal force $N$, normal to the cutting edge and away from the cutting edge. Their values were found experimentally (Murugappan, 1995) to be:

$$F_f = 180 \text{ N}$$

$$N = 100 \text{ N}$$

The horizontal components of those forces are:

$$F_{f\alpha} = F_f \cdot \sin(\alpha) = 31.3 \text{ N}$$

$$F_{f\beta} = F_f \cdot \cos(\alpha) = 177.6 \text{ N}$$

$$N_x = N \cdot \cos(\phi) = 99.5 \text{ N}$$

$$N_y = N \cdot \sin(\phi) = -10.5 \text{ N}$$

The summation of the forces along $O_x$ and $O_y$ axes will give us the horizontal ($F_p$) and vertical ($F_q$) components of the forces:

$$F_p = F_{f\alpha} + N_x = 130.8 \text{ N}$$

$$F_q = F_{f\beta} + N_y = 167.2 \text{ N}$$

The force along the shear plane is given by:

$$F_s = F_p \cdot \cos(\phi) - F_q \cdot \sin(\alpha) = 147.6 \text{ N}$$

The linear velocity of the workpiece is given by the relation:

$$v = \frac{\pi \cdot D \cdot n}{60} = 1.434 \cdot 10^3 \frac{\text{m}}{\text{s}}$$

The chip velocity is given by the relation:

$$v_c = \frac{\sin(\phi)}{|\cos(\phi - \alpha)|} \cdot v = 155.9 \frac{\text{m}}{\text{s}}$$
The shear velocity is the velocity of the chip relative to the tool and directed along the shear plane and is given by the Merchant equation:

\[ v_s = v_c \cdot \cos \left(45 \cdot \frac{\pi}{180} + \frac{\alpha - \phi}{2} \right) = 93.8 \frac{m}{s} \]

The heat source from the chip shear zone is given by:

\[ q_1 = \frac{F_t \cdot v_s}{t \cdot b \cdot \cos(\epsilon \cdot \phi)} = 4.796 \cdot 10^{10} \frac{W}{m^2} \]

The heat source from the friction between the tool and the workpiece is given by:

\[ q_2 = \frac{F_t \cdot v_c}{a \cdot b} = 2.812 \cdot 10^{11} \frac{W}{m^2} \]

By converting the values of the heat sources to the drawing units, we will get:

\[ q_1 = \frac{q_1}{10^6} = 4.796 \cdot 10^4 \frac{W}{\text{DrawingUnit}^2} \]
\[ q_2 = \frac{q_2}{10^6} = 2.812 \cdot 10^5 \frac{W}{\text{DrawingUnit}^2} \]

Considering the thickness of the cutting tool, we will obtain:

\[ Q_1 = \frac{q_1}{0.01} = 4.796 \cdot 10^6 W \]
\[ Q_2 = \frac{q_2}{0.01} = 2.812 \cdot 10^7 W \]

The heat sources will be applied to the geometry as distributed applied heat along lines. The region where the heat source coming from the shear of the chip will be applied will be split into two lines for meshing purposes:

\[ Q_1 = Q_{11} + Q_{12} \]
$Q_{11}$ will be applied to a line of 0.034mm in length with a mesh seed of 20 nodes, while $Q_{12}$ will be applied to a line of 0.169 mm in length, with a mesh seed of 10 nodes.

The heat source $Q_{2}$ will be applied to a line of 0.1mm in length and a mesh seed of 400 nodes.

The heat sources will give the following numerical values:

$$Q_{11} = \frac{Q_{1} \cdot 0.0085}{20} = 2.038 \cdot 10^{3} \text{W}$$

$$Q_{12} = \frac{Q_{1} \cdot 0.025}{10} = 1.199 \cdot 10^{4} \text{W}$$

$$Q_{2} = \frac{Q_{2} \cdot 0.1}{400} = 7.031 \cdot 10^{3} \text{W}$$

Finally, the applied heat sources used in the model will be rounded to:

$$Q_{11} = 2000 \text{W}$$
$$Q_{12} = 12000 \text{W}$$
$$Q_{2} = 7000 \text{W}$$
APPENDIX II

TABULAR RESULTS OF THE INSERT STRESS VARIATION

The following shows the maximum stresses and the stresses at the crack tip and virtual crack tip obtained at different cutting conditions: 636°C, 848°C, 1060°C, 1272°C, 1484°C, 1696°C, 1908°C. The stresses are presented in the cases when only the cutting forces were considered, when only the heat sources were considered and when both cutting forces and heat sources were considered.
### TABLE V  FEA Results for the 636°C

<table>
<thead>
<tr>
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<th>Forces + Heat</th>
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<tbody>
<tr>
<td></td>
<td>Maximum Stress</td>
<td>Stresses at Crack Tip</td>
<td>Maximum Stress</td>
</tr>
<tr>
<td>Without Crack</td>
<td>7.72</td>
<td>1.03</td>
<td>214</td>
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<tr>
<td>With Crack</td>
<td>7.73</td>
<td>3.09</td>
<td>213</td>
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<td>Ratio Flan Crack / N Crack</td>
<td>1.001</td>
<td>3</td>
<td>0.99</td>
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<tr>
<td>Ratio Crac Stress/ Ma Stress</td>
<td>Without Crack</td>
<td>With Crack</td>
<td>Without Crack</td>
</tr>
<tr>
<td></td>
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### TABLE VI  FEA Results for the 848°C

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<td>Ratio Crac Stress/ Ma Stress</td>
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<td>With Crack</td>
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TABLE VII  FEA Results for the nominal cutting speed of 1060°C

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<td>Ratio Crack Stress/ Max Stress</td>
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<td>With Crack</td>
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TABLE VIII  FEA Results for the 1272°C

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<td>Ratio Crack Stress/ Max Stress</td>
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### TABLE IX  FEA Results for the 1484°C

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### TABLE X  FEA Results for the 1696°C

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**TABLE XI  FEA Results for the 1908°C**

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