DEVELOPMENT OF MESO-SCALE FLEXIBLE CUTTING TOOL FOR MICRO-GROOVE CUTTING IN STEEL

BY

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THESIS

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Abstract

One of the key challenges in product miniaturization technologies is the creation of micro-grooves in devices for mechanical, electronic, photonic and bio-medical applications, such as production of hot embossing molds, micro-heat exchangers, optical lithography masks, micro-forming dies, engineered surface textures, etc. Processes such as micro-endmilling, laser scribing, μ-EDM, micro-fly cutting, AFM microscribing, photolithography, etc. have shown capability in producing micro-scale grooves in a range of materials. However, they were found to have limitations in producing grooves in hard materials that are less than a few microns wide and a few microns deep, several millimeters long, and that have arbitrary cross-sectional shapes with high relative accuracy, good material removal rates and superior surface finish at reasonable machining cost.

To meet these requirements, a strain-gauge sensor-integrated meso-scale CBN flexible cutting tool is developed that is capable of machining micro-grooves in steel. A MEMS process-based approach has been employed to fabricate the meso-scale flexible cutting tool with the integrated stain-gauge sensors. The strain-gauge sensor integrated on the meso-scale flexible cutting tool is responsible for controlling the tool deflection during the cutting process. Novel approaches for mounting and modifying the single CBN crystal on the flexible cutting tool blank and a suitable method for packaging the tool are demonstrated. The process was implemented on a 5-axis mMT with a retrofitted micro-groove cutting assembly.
The initial study of the meso-scale CBN flexible cutting tool has demonstrated the ability of the new tool to cut well-defined rectangular micro-grooves up to 1 μm deep, in stainless steel samples using a single tool pass, cutting at a speed of 100mm/min. Micro-groove characterization studies have been carried out to evaluate the outcome of the machined micro-grooves based on its expected geometry, floor surface profile, repeatability and consistency. Side burr formed during the cutting process were assessed through a series of experiments to arrive at the most suitable machining parameters for burr minimization. Multiple tool passes and high cutting speed yielded the lowest side burrs. Tool wear studies revealed that low applied loads and low cutting speeds gave the longest tool life. The ability of the flexible cutting tool to machine various micro-groove patterns has also been demonstrated.

To understand the process mechanics of micro-groove cutting in steel, the 3D finite element model for aluminum groove machining developed previously has been enhanced to include a thermo-elastic-plastic model of steel workpiece, suitable remeshing scheme, damage governing laws and different cutting edge geometries. Various inherent process mechanics including the stress and strain distribution, chip formation and development, cutting force predictions, side burr formation, and temperature distributions at the size scale involved were studied. Validation with experimental results in AISI 4340 steel showed that the model predicted side burr height to within 1.8% and chip thickness to within 28% error. The influence of cutting edge geometry and machining conditions on the outcome of the process mechanics were observed thorough a series of planned simulation trials. It was found that the cutting geometry with a small edge radius experienced lower stresses and cutting forces on its rake face, while the geometry with a larger edge radius enabled better heat conduction through the workpiece.
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Chapter 1

Introduction

1.1 Background and Motivation

Ever since the advent of transistors and the Morse’s Law, micro-machining processes have been playing an important strategic role in the miniaturization industries that challenge the existing boundaries of possibilities. Further, the increasing need to create micro-scale features on macro-scale objects as well as on micro-scale objects, micro-scale manipulations, etc. in many industrial and commercial sectors has been an important driver for change in the size of machines and cutting tools that are used to achieve these features. One important micro-scale feature in product miniaturization technologies that is on high demand today is micro-grooves. The need of devices with micro-grooves comes from a diverse spectrum of fields such as micro-electronics, optics, automotive, photonics, thermal, bio-medical, etc.

There are many critical requirements to produce micro-grooves that directly affect the performance of the micro-devices and reliability for various intended applications. For example, micro-grooves used in micro-heat exchangers need to be highly straight, long, tightly-packed, produced in hard, difficult-to-machine, and corrosion resistive materials such as steel or ceramics [113]. Micro-grooves required in diffraction gratings slits, lithography masks and LED backlight guides need to be extremely flat, ultra-smooth,
closely-spaced with complex cross-section and produced in metal surfaces [114-115]. Micro-fluidic applications require micro-grooves of varying geometries, depths and widths on flat and curved surfaces [117]. Some of them (pumps, filters) also require certain degree of groove floor roughness to be able to tune the frictional properties and flow dynamics of particles, released into the groove channels [5]. Enhanced surface coating applications, such as the sharklet skin textures need series of intersecting zig-zag grooves with widths and depths of few microns, repeatable over a large surface area [116].

Several processes exist that are suitable to produce micro-grooves as required by various applications. Additive micro-electro-mechanical system (MEMS) deposition processes such as photo lithography and etching techniques have provided an initial breakthrough in micro-groove formation. However, they are expensive, size and surface area limited, and focused on silicon-based and other semi-conductive polymer materials [105, 106, 118]. Mechanical-energy based material removal processes, such as micro-endmilling, micro-fly cutting, micro-scale planing and atomic force microscope (AFM) probe-based microscribing are capable of machining micro-grooves. They are restricted by the size of the micro-cutting tools used, minimum feature size achievable, which is tens of microns, surface finish and tolerance that are possible to achieve or rate of material removal [25-30, 38-45]. Thermal energy-based processes such as laser micro-cutting induce heat affected zones that produce debris and plume particles that affect the geometry and overall groove quality [63-68]. Electro-thermal-based process, such as micro-EDM is characterized by a very poor material removal rate, electrode wear and operates only on conductive workpieces [20, 32]. A chemical energy-based removal process such as micro-
ECM doesn’t offer dimensional control when used to machine tiny features in the order of few microns.

Recently, Bourne et al. [106] developed a novel high-performance, high-speed, high-precision, AFM probe-based microscribing process that was integrated to a 5-axis micro-scale machine tool. An external confocal laser displacement sensor was used in tandem to control the cantilever deflection during the cutting process via a feedback loop. The process improved the maneuverability, repeatability and ability to cut programmable patterns over a large surface area at a good material removal rate. Diamond tip-based AFM flexible cutting tools were used with customized micro-groove geometry to machine rectangular micro-grooves in aluminum 0.5-1.3µm wide, 0.250-0.700 µm deep, using cutting speeds up to 50mm/min. However, the current AFM flexible cutting tools used in the process are not suitable for machining micro-grooves in ferrous materials, such as steel, since the diamond tips were prone to accelerated wear-related issues due to carbon graphitization. Also, the flexible cutting tools lack the strength and stiffness to machine grooves that are deeper than 1µm. Moreover, the confocal laser displacement sensor has limited bandwidth; drift stabilization issue and demands long set-up time, before it can be used for the micro-groove cutting process. In order to address the gap in the current micro-machining capabilities of the flexible cutting tool, there is a need for the development of new flexible cutting tool system and this provided the motivation for this research.
1.2 Research Objectives, Scope, and Tasks

1.2.1 Research Objective and Scope

The main objective of this research is to develop a cost-effective and robust meso-scale flexible cutting tool that is capable of machining micro-grooves in hard materials such as steel. Specifically, the meso-scale flexible cutting tool should be able to cut repeatable linear, curvilinear and zig-zag micro-groove patterns over a large surface area that are a few micron wide, a few micron deep and between a few hundred microns to millimeters long. The grooves should be produced with a good material removal and appreciable tool wear and with high relative accuracy and groove depth consistency.

To achieve this objective, it is necessary to design and manufacture a meso-scale flexible cutting with an in-built, high-bandwidth sensor system that is capable of controlling the deflection of the flexible cutting tool and support process monitoring capabilities during the groove cutting process. A suitable cutting tool tip material will have to be accommodated in the tooling system that is capable of cutting grooves in hard surfaces. A thorough understanding of the micro-groove cutting process using the new flexible cutting tool must be developed in order to achieve good performance and enable efficient process planning.

Both experimental and modeling approaches will be employed to gain the basic understanding of the micro-groove cutting process. The experimental work will be limited to producing linear and curvilinear micro-grooves cut on metallographically polished and highly flat steel material. Only dry cutting conditions will be considered for all the experiments performed. All the micro-grooves machined in the experiments will have
rectangular cross-sections. Also, all the micro-grooves under consideration will be cut using the same cutting tool tip material. The process modeling work will focus on development of the steel workpiece model in order to accommodate all the necessary mechanical and thermal properties. A homogenous workpiece condition will be assumed for the simulation trials. A decoupled-adiabatic heat generation model will be used for this study. Simulation trials will also be limited to studying underlying process mechanics on the workpiece material at the tool-chip interface and will not include analysis of any cutting tool deformations. Specifically, scope of the discussion from the simulation results include steady-state chip formation, stress-strain distribution, side burr formation, and force prediction.

1.2.2 Research Tasks

To realize the goals of this research, the following tasks will be carried out.

Task 1: Meso-Scale Flexible Cutting Tool Development

- A meso-scale flexible cutting tool will be designed and developed that is capable of producing micro-grooves in hard materials such as low-carbon steel and stainless steel. A micro-electro-mechanical systems (MEMS)-based process fabrication approach will be used to make the new flexible cutting tool.
- A Strain-gauge sensor-based feedback control system will be developed and integrated onto the meso-scale flexible cutting tool.
- Single Cubic Boron Nitride (CBN) crystal will be used as the cutting tool tip material. The CBN crystal will be accordingly be modified by laser-milling and focused ion
beam (FIB) and provided with the micro-groove cutting geometry capable of meeting the micro-groove geometry requirements.

- A suitable compact packaging method will be developed that can assemble the meso-scale CBN flexible cutting tool for micro-groove cutting. The package will provide portability, ease in handling of the tooling system and a simple process to engage electrical connectivity between the strain-gauge sensor and the external power supply.

**Task 2: Design Evaluation of the Flexible Cutting Tool**

- The manufactured meso-scale flexible cutting tool will be evaluated for its design objectives. The stiffness, resonant frequency and maximum load carrying capabilities will be evaluated.
- The integrated strain-gauge sensor will be statically tested for its nominal resistance, conductivity, gauge factor and non-linearity.
- The performance of the strain-gauge sensor will be assessed and compared with the established confocal laser displacement sensor used in conjunction with the diamond tip-based AFM sapphire cantilevers developed by Bourne et al. [106].

**Task 3: Performance Evaluation of the Flexible Cutting Tool**

- The capability of the meso-scale CBN flexible cutting tool to machine micro-groove in steel will be assessed. In particular, the maximum depth of cut achievable in a single tool pass condition will be established.
- Experiments will be carried out to understand the micro-groove characteristics machined using the meso-scale flexible cutting tool. In particular the consistency and repeatability of the micro-grooves cut over long range distance will be covered.
through the study of tool wear, nature of floor surface finish obtained and side burr produced during the cutting process.

- The applicability of the meso-scale CBN flexible cutting tool will be demonstrated through the fabrication of the various types of features including straight, zig-zag and curvilinear grooves, rectangular grooves with different widths and depth, very closely-spaced grooves, surface patterns composed of multiple arrays of grooves, single and multiple intersecting grooves.

**Task 4: 3D Process Modeling Development**

- There are inherent restrictions in experiments performed during the micro-groove cutting in steel that limit the scope of understanding the process. This will be studied better through the development of a 3D finite element model. The 3D FEA model developed by Bourne et al. [106] will be adopted and modified accordingly to include suitable workpiece material input properties, formulation schemes, adaptive meshing techniques and tool geometries.

- Simulation trials will be conducted for various machining conditions to study the process mechanics that include chip formation and development at steady-state, cutting and thrust force predictions, stress and strain distribution, tool-chip interfacial temperature gradients and side burr formation.

**1.3 Thesis Overview**

The organization of the thesis is as follows. Chapter 2 reviews existing literature on the micro-cutting tools that can machine micro-grooves with depths under 200 μm and widths under 50 μm. The micro-cutting tools covered include cutting tools shearing
materials mechanically such as micro-endmills, single-point micro-turning and grooving tools; thermal energy-based micro-cutting tools such as CO$_2$ and Nd: YAG lasers; ultrasonic-vibration-assisted micro-cutting tools and load-based flexible cutting tools such as Atomic Force Microscope (AFM) probe tips operating under different energy sources. A summary of the relevant type of cutting tool material considered, geometry considerations of cutting edge and the operating mechanism of the micro-cutting tools are reviewed. Different sensor-integrated micro-cutting tools are also studied with emphasis on the tool-sensor integration methods. A 3D FEA model for micro-groove cutting of aluminum is also reviewed. Finally, the gaps in literature are identified and the need to develop a new flexible cutting tool that can meet the micro-groove specifications mentioned in section 1.2.1 is described.

Chapter 3 begins with the preliminary testing of the single CBN crystal tool tip mounted on a commercial AFM sapphire cantilever to machine micro-grooves on low-carbon steel 1018. The test results provide the motivation towards developing a new meso-scale CBN flexible cutting tool that can overcome the short-comings found in the AFM sapphire cantilever. Next section discusses the design and development of the strain-gauge sensor-integrated meso-scale CBN flexible cutting tool. The MEMS-based fabrication process that operates on a bottom-up dry surface micromachining approach to build the flexible cutting tool layer-by-layer is discussed. In the following section, the fabricated flexible cutting tools are tested for their stiffness, strength and load carrying capabilities, while the strain-gauge sensors integrated on them are tested for nominal resistance, gauge factor and non-linearity in the measurement regime. The single CBN crystal tool tip mounting, tip modification and edge definition; and packaging of the
flexible cutting tool in order for it to be attached into the 5-axis mMT are discussed in the last section.

Chapter 4 commences with the discussion of the cutting procedure and an experiment to test the ability of the flexible cutting tool to machine one micron deep micro-grooves in stainless steel. In the next section, the machined micro-grooves are characterized based on expected dimensions, floor surface finish, consistency and repeatability for different machining conditions. In the following section, a detailed analysis of the side burr formed during the groove cutting process is carried out. A design of experiments is performed to study the influence of groove depth and other machining parameters on the formation of side burrs. Next, tool wear study is carried out to assess the performance of the flexible cutting tool is to machine repeatable and consistent micro-grooves for long range of cut lengths. In the last section, the applicability of the flexible cutting tool to machine various complex micro-groove patterns for the intended commercial applications is demonstrated.

Chapter 5 discusses the enhancements made to the 3D FEA model [106] in order to simulate the micro-groove cutting in steel. The required workpiece and tool properties are described, following which the contact algorithm and additional boundary conditions to complete the input conditions are explained. In the next section, the results of the simulations are analyzed and compared with experimental results for validation. The influence of cutting edge geometry, cutting speed and tool rake angle conditions on the outcome of the process mechanics is described that include chip formation and development, cutting and thrust force predictions, stress and strain distribution, and tool-chip temperature gradients.
Chapter 6 provides a brief summary of the work described in this thesis and gives conclusions that are based on the work accomplished. The recommended areas of future work are briefly discussed to further develop the advanced versions of sensor-integrated flexible cutting tools.
Chapter 2

Literature Review

2.1 Introduction

This chapter provides an in-depth knowledge and reviews the existing literature available on the micro-scale cutting tools, especially micro-milling tools, process involved in fabricating them, relevant geometry considerations at the size scale, suitable process monitoring conditions and summarizes the challenges that lies ahead. The chapter is broadly classified into two sections. The first section talks about the various micro-scale tools used in open-air micro-scale machining approaches, which includes mechanical micro-scale cutting tools such as micro-end mills, micro-turning and threading tools that cut in 20-200 µm domain; AFM-based flexible cutting tools that are driven a load-based cutting approach and operate at a sub µm domain; ultrasonic-vibration assisted micro cutting tools that are essentially a non-thermal, non-chemical and non-electrical process used for difficult to machine materials; laser-based micro cutting tools that relies on non-contact type thermal principles machining in 10-90 µm domain and lastly, sensor-assisted micro cutting tools, where the process monitoring capabilities of the sensors that control and influence the performance of the micro-cutting tool is reviewed. This study is relevant in understanding the need for designing a cutting tool that operates in the meso-scale combining features of both micro and nano-scale levels. In the last section, the finite
element analysis modeling approach developed by Bourne et al. is reviewed in relevance to its applicability for performing micro-groove cutting process simulations for the experiments discussed in Chapter 4 of the thesis.

2.2 Micro-Milling Cutting Tools

Micro-milling is one of the most emerging fabrication technologies with a promising future. It is envisaged as technology of choice to create complex three-dimensional shapes in hard engineering materials, especially for biomedical applications and injection moulds. It is characterized by mechanical interaction of a sharp tool with the workpiece material, causing breakage inside the material along defined paths, and eventually leading to removal of the unwanted part of the workpiece in the form of chips. The quality of the micro-machined parts depends on the cutting parameters introduced, the milling strategy, the nature of the workpiece material, and to a large extent on the micro-milling cutting tool, itself.

The outcome of size and quality of the micro-scale products machined depend on the stiffness, accuracy, thermal stability, dynamic performance of the micro-milling tools especially when working with tight geometric tolerances of 0.1-0.01 µm. Table 2.1 gives a summary of the generic micro-milling machine error sources that contribute to the overall quality of the micro-machined parts. Note that the tool-related errors dominate a significant amount of the error sources.

Manufacturing of micro-milling cutting tools is a significant challenge in micromachining. Up until 2004, the smallest commercially available micro-endmills had a diameter of 50 µm [2], and today the smallest viable commercially available micro-
endmills are in the range of 15 - 25 \( \mu \text{m} \) in diameter [3,4]. The smallest size of the available micro-end mill for commercial purpose is 5 \( \mu \text{m} \) in diameter [3]. It is to be noted here that imprecise geometry and the irregularity of micro-milling tools often negate the advantages of ultra precision process control, state of the art machine tools, and ultra fine tuning of process parameters. Therefore it is important to optimize the process of fabrication of these micro-scale tools.

The types of tools used for micro-milling operations are endmills, ball-nose, drills and engraving tools. The most commonly used material for making the micro-end mills are Tungsten Carbide (WC) and Diamond. Diamond material is preferred when the workpiece object is non-ferrous in nature. Figure 2.1 shows the commercially available micro-end mill
tools – (A) shows the conventional end mill with two helical flutes, (B) shows the ball-nose end milling cutter and (C) shows a micro-engraving tool.

![Figure 2.1: Various types of micro-scale endmills [5, 6, and 3]](image)

The following sub-sections discuss the classification of micro-milling tools based on; (1) type of material used to fabricate them; (2) different flute and end cutter geometry and (3) the fabrication approach implemented in getting final tool geometry.

### 2.2.1 Material

1. Diamond Cutting Tools

Diamond cutting tools represent with the unique capability of achieving surface finishes in the range of Ra ~ 10 nm with an almost atomic sharpness of the cutting edge [7]. These tools are available down to sizes of 0.1 mm in diameter [6]. However, diamond cutting tools are restricted to machine workpiece materials that don’t include ferrous elements in them, due to the high affinity of iron to carbon. Above 700ºC, diamond degenerates into graphite leading to devastating wear and final tool breakage [8].
Several approaches have been used to avoid the graphitization problem associated with diamond cutting tools. These include the reduction of the contact time between tool-workpiece by Elliptical Vibration Cutting [9], application of a nitrating coating [10], or use of other cutting tool materials such as cemented carbide, ceramics or different monocrystalline materials [11]. Recently, nano-crystalline diamond (NCD) coatings on tungsten carbide micro-endmills have been investigated with excellent results as an alternative to diamond cutting tools [12]. The NCD coating have significantly reduced the side burr formation and shown to have improved the surface finish of machined 6061 Aluminium as shown in Fig. 2.2(B). The coatings reduced the amount of smearing and adhesion when compared to using uncoated carbide micro-end mills as seen in Fig. 2.2(A). In most cases the applications of diamond micro-cutting tools are restricted to ultra-precision machining as they have a limited ability to machine ferrous materials.

Figure 2.2: Groove Channels cut on 6061-Aluminum using uncoated (A) and NCD (B) micro-end mill [11]
2. Tungsten Carbide (WC) Micro-Milling Tools

Tungsten carbide cutting tools are generally used for the micro-mechanical cutting process due to their hardness over a broad range of temperatures and materials. As pointed out earlier, diamond tools are limited to machine mostly non-ferrous materials. Therefore, micro-tools such as micro-end mills and micro-drills are generally made from tungsten carbide (WC), which has high hardness and strength at high temperatures, as shown in Fig. 2.3 [13, 26].

![Figure 2.3: Hardness of cutting tool materials as a function of temperature [26]](image)

To improve the wear resistance characteristics of micro-tools, very small grain size tungsten carbide (i.e. 600 nm) is fused together to form the tool. Researchers have used Cobalt, but its content influences tool hardness. [7, 9-10]. Smaller cobalt content makes the carbide harder, but at the expense of higher brittleness. The size of commercially available micro-end mills can be as small as 5 μm in diameter, with their helix angle fabricated by grinding. However, researchers have pointed out that these tools suffer from a limited operational life and have difficulty in machining adhesive metals such as aluminum and
copper [14, 15]. Also, the maximum achievable surface finishes ranges between $R_a$ values of 0.1-0.3μm depending on the workpiece material and other parameters [16].

2.2.2 Tool Geometry

The most commonly available micro-end mill geometry is shown in Fig. 2.4(A), which is the scaled down geometry version of a conventional macro-scale endmills with two helical flutes that are fabricated using precision diamond grinding of tungsten carbide. Figure 2.4(B) shows a schematic of a testbed used in the fabrication of micro-endmill.

Figure 2.4: (A) Typical 2-fluted micro-end mill; (B) micro-endmill fabrication set-up [18]

Viable endmills with this geometry are commercially available at sizes as small as 20 μm in diameter [3]. However, subsurface damage, which is a direct outcome of the grinding process, lowers the strength of ground endmills [20]. Figure 2.5(A) shows a single-flute endmill with a single cutting edge, which is fabricated using fewer cuts. Figures 2.5(B) shows a similar single-flute design, but with two cutting edges. Figure 2.5(C) shows an extension to the previous design with a side relief angle of 50°. Figure 2.5(D) shows a two-flute design, which is more suitable for plunging due to the end relief on the
tool. The design shown in Fig. 2.5(E) affords the most cutting edge relief out of the two-flute design [18].

Figure 2.5: Various flute types in micro-scale endmills [18]

As the breakages of the micro end-mills are a serious issue, an extensive investigation on tool geometry is essential for the size scale involved. Researchers[ 18 – 22] have shown that micro-milling tools of diameter less than 50 μm need a zero helix angle in order to improve their rigidity [3, 53] and mitigate the limitations of fabrication techniques. To further develop an understanding of the micro-milling process, Fang et al. [21] has investigated various micro-carbide tool geometries (i.e. triangular and semi-circular bases) using finite element method and experimentally verified their predictions. Figure 2.6 shows various types of tool geometry studied – (A) two flute; (B) Δ-type with straight body; (C) D-type with straight body; (D) Δ-type with taper body and (E) D-type with taper body. They found the semi-circular-based endmills are better than triangular or the conventional two fluted endmills and the two-flute end-mills are 8 to 12 times weaker than the Δ-type (Fig. 2.6(D)) and D-type (Fig. 2.6(E)) end-mills with tapered body. They also concluded that when there is no helix angle on the micro-tools, poor chip evacuation may result in a poor surface finish. Additionally, it was found that Δ-type end-mills and D-type end-mills with straight tool body are not suitable for micromachining. In practice, end-mills with a tapered body are usually required to form 3D structures or form slopes for micro molds /
dies applications. In addition, it is easy to fabricate tapered body for Δ-type end-mills and D-type end-mills, but is rather difficult to fabricate two flutes on tapers.

Fig. 2.6: Various flute end-mills in micromachining [21]
(A) Two-flute end-mills, (B) Δ-type end-mills with a straight body, (C) D-type end-mills with a straight body, (D) Δ-type end-mills with a tapered body and (E) D-type end-mills with a tapered body.

2.2.3 Tool Edge Preparation

1. Diamond Grinding

The most commonly used tool edge preparation reported in literature is the diamond grinding process [22, 23]. It is possible to machine micro-endmills with a dovetail profile and micro-ball mills with different helix angles. The selection of tool material is of high importance for grinding efficiency, milling performance and tool life. The micro-grinding machine set-up and schematic of the process showing different steps involved in the micro-endmill cutting edge fabrication is shown in Fig. 2.7(A) and 2.7(B), respectively. The grinding machine is built on a vibration isolated granite base, with two grinding spindles mounted on the horizontal x-y table. The process allows the user to achieve cutting edge radius smaller than 0.1 μm and the process time is around 10 mins. for a micro-milling tool with diameter of 10 μm.
Figure 2.7: (A) Tool grinding machine and (B) The steps involved in grinding [27]

The disadvantages associated with diamond grinding include subsurface damage, potential for tool breakage [17, 29-30], and limitations on the smallest feature sizes that can be readily fabricated [30]. At the sub-millimeter level, the grinding forces may cause the bending and breaking of the end mills and the damages caused by grinding lowers their strength [20]. Figure 2.8 shows a micro-endmill (D = 20 μm) produced by the grinding process. Schaller et al. [17] fabricated micro-tungsten carbide tools using diamond-grinding disks to cut grooves as wide as 50 μm and 180 μm deep in Brass; and over 100 μm wide and deep in Stainless Steel as shown in Fig. 2.9(A-C), respectively. As seen in figure, the process produced significant amount of side burrs that varied between 10-30 μm in height.

Figure 2.8: Micro-endmill prepared by Diamond Grinding [27]
Figure 2.9: (A) Micro-groove channels cut in brass, (B) Intersecting grooves in brass and (C) Micro-grooves in stainless steel [17]

2. Focused Ion Beam Machining

In FIB machining, Gallium ion beam with specific intensity and diameter is directed to substrate material for producing a feature on the micro-end milling tool. The micro-endmills are made with non-planar facets by ion sputtering process as shown in Fig. 2.10 [29]. The sputtered facet edge closest to the ion source is rounded and is reported to having a radius of curvature on the order of 1.0 μm due to the Gaussian beam profile [24, 28-30]. This inclination of facet is intended to provide a clearance angle during machining.

Figure 2.10: Fabrication of micro-endmills using FIB principle [29]
Sandia Labs has developed a 25 µm diameter carbide end mill tools with four, five and six cutting edges using focused ion beam machining, as shown in Fig. 2.11(A-C), respectively [29]. These micro-endmills were used to fabricate micro-groove trenches on Al-6061 alloy with that are 15-25 µm deep, 25-40 µm wide and with surface finish roughly around ~ 400 nm as shown in Fig. 2.11(D) and 2.11(E), respectively [30]. The focused ion beam machining method used for fabricating micro-endmills has a material removal rate that is very low, leading to a very high production cost.

![Figure 2.11: Micro-Endmills prepared using FIB- (A) Four cutting edges, (B) Five cutting edges and (C) Six cutting edges [29], (D) and (E) Micro-Grooves Cut in Al-6061 using FIB-prepared micro-end mills [30]](image-url)

3. Electrode Discharge Machining (EDM)

The machining force in EDM is extremely small, thus the problems of tool bending, breaking, and decrease of strength due to subsurface damages can be solved effectively[29]. Researchers have used two methods namely- Block electrode and Wire-EDM, also known
as Wire-Electrode Discharge Grinding (WEDG) to shape the micro-endmill tools for micromachining purposes [31, 32-34] as shown in Fig. 2.12(A) and 2.12(B), respectively. The former method is suitable for fabricating cylindrical and conical tools having circular cross-sections. Micro-WEDG is preferred for making non-circular tools, since the wire is fed continuously and the region contacting with the tool is always new as seen in Fig. 2.12B. Therefore, a highly precise tool can be fabricated, provided the EDM machine has a high repetitive accuracy and a high step resolution [35].

![Figure 2.12: (A) Block electrode method and (B) Wire-Electrode Discharge grinding method [35]](image)

In micro-WEDG, which takes place at a low discharging energy level, the evaporation and micro explosions of the oil (dielectric) are very weak. As a result, the removed material may get attached to the micro-endmill and roughen the surface finish as shown in Fig. 2.13A. While using a mobile oil flow system, the melted material is prevented from adhering to the micro-endmill as shown in Fig. 2.13B. The micro-end mill fabricated using this method was found to be between 30-120 µm in diameter [31-35].
Figure 2.13: Micro-Endmills prepared using WEDG method: (A) Without oil flow system; and (B) With oil flow system [35]

The micro-grooves that are 50 µm deep cut on Steel using tools made from micro-WEDG have found to be smooth and flat without significant burr formation and chip adhesion as shown in Fig. 2.14(A-B). This effect might be indebted to the advantage of surface micro asperities as seen in Fig. 2.14(C). It is presumed that the micro surface asperities of the tool improve the tribological properties of the tool–workpiece interface and asperities also decrease the effective contact area between the tool and the chip, leading to a low friction coefficient.

Figure 2.14: (A and B) Micrograph showing micro-grooves machined using WEDG-fabricated micro-endmill; and (C) Schematic of the micro-end milling process showing the micro-asperities [36]
2.3 Micro-Turning & Threading Tools

Limited literature exists for micro-turning tools that have been developed for ultra-precision lathes. Miniaturization of the single cutting point of the tool remains a big challenge till date for their use in micro-machining. Zinan Lu et al. [95] developed micro-turning tools via ultra-precision grinding process for 300 x 200 mm sized micro-lathe, as shown in Fig. 2.15A, while Fig. 2.15B shows a SUS 303 Steel turned using the tool. Kitahara et al. [96] developed single point diamond micro-turning tools for a micro-lathe 32 mm in length that were able to make features 25 μm in size.

![Figure 2.15: (A) Single point micro-turning Tool [94] and Machined SUS 303 steel (B) [96]](image)

Recently, most of the micro-turning and threading tools have been developed using FIB machining principle similar to the micro-end mill fabrication. FIB sputtering is attractive for fabricating micron-size tools or instruments, because this technique can precisely remove or add material providing 10-nA currents, 10-nm spot sizes, and 10-nm pixel spacings. Adams et al. [30] fabricated Tungsten carbide micro-grooving and threading tools are used to machine 13-μm wide, 4-μm deep, helical grooves in PMMA and Al 6061 cylindrical workpieces. Micro-grooving tools were also used to fabricate
sinusoidal cross-sectional features in planar metal samples. The measured cutting edge radii of curvature were 40 nm for diamond and tungsten carbide micro-grooving tool [30-31]. Figure 2.16(A) shows a single point diamond micro-cutting tool that is brazed into a tapered tungsten carbide mandrel. The width of the tool after ion-shaping was 23 µm. The circular groove feature machined on Aluminum is shown in Fig. 2.16(B). Using the same method of fabrication, the authors also fabricated M42 high-speed steel micro-threading tools, which machined a pair of closely spaced triangular grooves into the surface of a 3-mm diameter PEEK rod using an Optimum 2000 micro-lathe, as shown in Fig. 2.16(C-D).

Figure 2.16: (A) Single point Diamond micro-turning tool; (B) A series of concentric grooves cut on aluminum, (C) Two-tip micro-threading cutting tool and (D) Portion of machined PMMK cylindrical surface [31].
2.4 AFM-Based Micro-Cutting Tools

For creating features that are in the range of 500nm to 5µm, many researchers have considered using load-based cutting process through using the Atomic Force Microscope (AFM)-based tips as micro-cutting tools. AFM-based micro-cutting tools have a much broader potential and range of applications compared to scanning tunneling microscopy (STM)-based probes because STM is normally limited to conducting substrates in a high vacuum environment. Researchers have chosen to use load-based control approach instead of tool trajectory control in AFM as it is extremely difficult to control tool position compared to tiny load at the size scale involved. Figure 2.17 shows the schematic of the process. In such cases, the depth of cut is a feature of tool geometry, workpiece material properties, applied load, and cutting conditions.

![Figure 2.17: Schematic of material removal using AFM [39]](image)

The most common method of producing the AFM cantilevers is using photolithography-based micro-fabrication approach. This is most established method in
both the industry and academic research. A step-by-step sequence of fabricating a Silicon probe tip integrated monolithic AFM cantilever is shown in Fig. 2.18 starting out with an undoped silicon blank wafer. Upon release, the Si cantilevers are either coated with silicon nitride or diamond via plasma-assisted deposition methods for improving wear resistance properties. In the following sections different types of AFM-based tips used in micro-nano machining applications based on the type of force (energy source) applied on the probe tips are reviewed in detail.

![Step-by-step sequence of fabricating a Silicon probe tip integrated monolithic AFM cantilever](image)

**Figure 2.18:** Schematic of AFM cantilever fabrication [38]

2.4.1 AFM Tips under Mechanical Loads

The AFM-based tips that are loaded with mechanical force or energy, act as a micro-cutting tools to remove material via direct contact or tapping contact with the substrates. This mechanical scribing process is known as AFM scratching or nano-scratching. Mechanical scribing process is normally operated under contact mode. Although tapping and dynamic modes have been used for material removal, they suffer from the inherent lower tip force than that of the contact mode, which limits the depth of
the scribed grooves to a few nanometers, even for soft materials [40]. The most commonly used probes for mechanical scribing are silicon nitride (Si$_3$N$_4$) and diamond coated tips with apex radius ranging between 15-25 nm. [39-45].

A wide range of experiments have been conducted using different mechanical loads, cutting speeds, cut lengths, and number of tip passes. Grooves with depths from a few to tens of nanometers and widths from tens to hundreds of nanometers have been scratched on many hard surfaces of metals such as aluminum, gold, gold-palladium, nickel, oxides, and semiconductors like silicon, as well as various soft materials, such as polymers (PMMA, polyamide) [37, 39, 41-45]. A load graph (Stiffness of 42N/m) relating depth of cut and surface roughness for mechanical scribing of aluminum coated on silicon substrate is shown in Fig. 2.19(A), while 2.19(B) shows the cross-section of the groove cut on aluminum with an overlapping outline of the unworn AFM probe tip.

![Figure 2.19](image)

**Figure 2.19:** (A) Load vs depth of cut in machining Aluminum; and (B) Cross-sectional profile of groove cut using AFM-based tips under mechanical loading [41]
In other experiments, loads that range from 100 nN have been used, when cutting 2 nm deep and 20 nm wide trenches in softer materials such as polyamides [44], to the levels of 100 μN when cutting in single crystal silicon [46]. Reported cutting speeds tend to vary between 0.006 mm/min [41] and 3 mm/min [44], while the typical length of the cuts reported is between a few microns [43, 45-46] and tens of microns [47]. However, lengths of the cuts are limited by the full range of motion of an AFM scanner.

The number of AFM tip passes can be as low as one, but Gnecco et al. investigated the abrasive wear of mechanical scribing of KBr [001] by using multiple scratches with 5120 scratch cycles to study morphology of scratched debris [48]. Minimum chip thickness estimation studies were also investigated by Ahn et al. [49], while machining in silicon (001). At a shallow scratching depth (4 nm), only pile-up formation was observed. As the depth increased (18 nm), however, machining chips started forming, and then were generated continuously, which indicates the mode transition from ploughing to cutting, as shown in Fig. 2.20

![AFM-3D scans of Grooves cut at two different depths of cut](image)

**Figure 2.20:** AFM-3D scans of Grooves cut at two different depths of cut [49]
2.4.2 AFM Tips under Thermal Loads

AFM tips have been loaded with thermal energy to locally melt or soften the contacted substrate surfaces to increase the material removal rate. Material removing behavior by a heated tip is more complex than that with mechanical loading, and it is no longer dominated by the magnitude of the force and the number of scratches applied. The heat supplied to the tip is used to soften the substrate in the contact region, at which point a very low tip force in the order of 100 nN could create a pit or a hole on the substrate. A schematic of AFM probes under thermal loads is shown in Fig. 2.21.

![Schematic of thermal loading on AFM probe tip](image)

**Figure 2.21**: Schematic of thermal loading on AFM probe tip [51]

Recently, researchers have used the heated AFM tip to provide thermal energy to activate certain chemical reactions to break the intermolecular bonds or to modify the material structures of organic substrates such as thin polymer films, which is known as thermo-chemical nanolithography (TCNL) [50]. Such an approach is appealing as it is known that the thermal profile in the vicinity of a heated AFM tip can give rise to sharp thermal gradients, which can affect the chemical reaction rates [51]. Most of the heat in tips is generated by electrical energy, most commonly that from a resistive heating element that is embedded in the cantilever [50-53]. The speed, temperature, and duration of the forces applied also have major influences on the scratched geometries. Normally, the heated tips
used to perform the TCNL processes (either Si or Si$_3$N$_4$) do not require to be loaded with high mechanical forces. Thus, the tip size can be similar to those used in imaging applications to ensure its precision sensibility or guarantee its high resolution [50, 53]. Mamin et al. [54] used input power of 35mW to heat an AFM tip to temperatures of up to 170 °C for 4 ms to drill an array of sub-100-nm pits on PC substrates. The same authors also used a diode laser (30mW, 0.55NA) to heat the AFM probe tip at 120 °C to scribe on PMMA surface [55]. The set up for the process is shown in Fig. 2.22 (A) and a typical load vs. depth of cut achieved in thermal-based scribing process with cantilever stiffness of 0.06N/m is shown in Fig. 2.22 (B) and a series of nano-grooves created by the thermal-loading of AFM-based tips are shown in Fig. 2.22(C) [52].

![Figure 2.22](image)

**Figure 2.22:** (A) Set-up for thermal-based AFM scribing using Laser source; (B) Load-displacement curve for thermal loading of AFM probe tip; and (C) Nano-grooves created at temperatures varying from 240 to 360 °C on PPV [59]
2.4.3 Multiple AFM Tips Arrays

One of the major challenges faced in the development of AFM-based scribing tool tips is to increase their throughput for making the process viable for mass-manufacturing. Efforts have been made to use multiple probe arrays for parallel processing of nanostructures. Approaches ranging from individual multifunction probes to independently activated array probes have been developed by Wang et al. [55, 56]. Researchers at IBM have applied the multiple-probe concept, called Millipede, for data-storage applications; an array of heated tips was used to drill (write), image (read), and melt (erase) nano-scale holes (data) on very thin polymer films. A $64 \times 64$ cantilever/tip array for write/read/erase functionality was developed to demonstrate the potential of Millipede for ultrahigh storage density as shown in Fig. 2.23. The challenges in using multiple probe concepts is that during material removing process, the approach angle for a case of a multiple-probe arrangement becomes critical in the plane parallel to the surface to workpiece surface. The alignment of the cantilevers and the approach of the tip-array determine which tips come in contact with the surface first.

**Figure 2.23**: The Millipede Concept showing multiple arrays of AFM cantilever probe systems scribing and storing data simultaneously [56]
2.4.4 AFM Tips based on Electric Bias

To induce a local electric field for the enhancement of the material removing activities, an AFM conducting tip can be charged with an electric bias with respect to a conducting substrate. Lyuksyutov et al. [58, 60] studied the tip-induced electric field and found that a strong non-uniform electric field on the order of $10^9 - 10^{10}$ V/m between a conductive AFM tip and a dielectric polymer film spin-coated onto a conductive Au–Pd substrate could be achieved. A set of holes of 4 nm depth and 37 nm width patterned in PMMA using an AFM tip with a bias of $-30$ V and an exposure time of 0.5 s is achieved as shown in Fig. 2.24.

![AFM Tip Image](image)

**Figure 2.24:** Set of holes of 4 nm deep and 37 nm wide patterned in PMMA using electro-induced AFM probe tips

Application of a bias voltage between 5–50 V between on a conductive AFM-based tip (or the arrays of the tips) and the substrate, results in charge transport (due to the breakdown) and causes localized joule heating in order of nano-amounts in the substrate. Electrostatic attraction of the softened polymer film toward the AFM tips removes nanostructures with dimensions 5–100 nm width and 0.1–100 nm height. Due to joule heating, a temperature could rise above the glass transition temperature and could cause
significant thermal expansion or dimension changes [61]. Based on the applied voltage and electric current there could be either raised nano features or material removed (ablation). Figure 2.25 shows the schematic of the AFM-based tip electrostatic nanolithography, wherein the case of nano-structure being raised from the material is shown. Figure 2.25(B) represents the Joule heating effect from amplified current flow, which increases temperature within the polymer film (isotherms shown in red solid lines) and the highly non-uniform electric field generates a step electric field gradient (shown in arrows). In Figure 2.25(C), large non-uniform electric field gradient that surrounds the AFM tip produces an electrostatic pressure on the polarizable, softened polymer creating raised features. Figure 2.26 shows nano-machined grooves using electro-induced AFM probe tips. The dependence on tip-to-sample bias was explored over the range 5.0–6.5V. The full width at half-maximum depth was in the range 20–40 nm.

**Figure 2.25**: Schematic representation of the AFM-based tip under influence of electric bias [60]
As mentioned earlier, material removal using AFM-based tips under influence of electrostatics have to be conductive or semi conductive. The tip conductivity in addition to the tip geometry can influence the performance of the AFM probe tip-assisted machining processes [62]. To make the probe conductive, the tip side of the cantilever is frequently coated with a highly conductive metal. The backside can also be coated by a highly reflective metal to improve the reflection of the PSD laser beam as shown in Fig. 2.27. The reflective coating is typically made of Pt or Au with an additional interface thin film, such as iridium or chromium, to improve its adhesion to a Si or Si$_3$N$_4$–based probe. [62] Tips made of high-conductivity and high-hardness materials, such as diamond and CNT (carbon nanotubes) may also be used for conductive AFM.

**Figure 2.26**: Nano-machined grooves showing the tip to sample bias dependence [61]

**Figure 2.27**: Platinum mirror micro-machined on V-shaped cantilever [62]
2.4.5 Rotation-based AFM Cutting Tools

AFM probes have been traditionally used as a scribing tool, where either the tool or the workbench platforms translates to create micro- and nano-scale features. Arda et al. [97-98] developed a rotating tip-based AFM tool (nanotool) that is rotated at high speeds by out-of-phase motions of the axes of a three-axis piezoelectric actuator. The nanotool is directly attached onto an acrylic post in a reverse configuration, where the tip is directly supported by the nanotool post. Since the compliant nanotool is not used as the connection element, a high stiffness rotary nanotool assembly is attained. The AFM tool was capable of two configurations that included in-plane and out-of-plane elliptical nano-motions as shown in Fig. 2.28A and 2.28B, respectively. The two instants of the cross-section of the nanotool are indicated as 1 and 2. Continued tool rotation is shown in shade of grey.

![Figure 2.28: Two Fundamental modes of rotation of the AFM tool tip (nanotool)[98]](image)

Arda et al. were successful in creating complex features on PMMA that were up to 150 nm deep dominated by formation of curled or snarled chips, through a direct prescription of groove depth and shape unlike the traditional AFM tool relying on load control[125]. The set-up with the reversed AFM tool mounted on the testbed platform is shown in Fig. 2.29.
Figure 2.29: Rotating AFM Tool along with the Testbed [98]

First, the sample surface is located through a sample contact algorithm using the AFM cantilever. The piezo-shaker placed under the sample is vibrated at the first resonant frequency of the AFM cantilever with sub-nanometer vibration amplitude. The sample is then slowly approached to the tool using the nano-positioning stage, while measuring the vibrations of the tool cantilever using the LDV. A contact between the tool and the sample surface causes a significant (an order of magnitude or more) increase in cantilever vibrations at the first resonant frequency and thus, detected. The AFM tool is then rotated with selected frequency using the piezoelectric actuator and the nanopositioning stage used to prescribe the depth and feeding motions to create a particular feature. The process capabilities are demonstrated in Fig. 2.30(A-C) for different shapes of trenches that vary between 5nm-100nm in depth.
Figure 2.30: Different geometries machined using a rotating AFM tool tip [98]

2.5 Ultrasonic Vibration-assisted Micro-Cutting Tools

At the micromachining level, inherent material property characteristics of hard and brittle materials such as silicon, glass, quartz crystal and ceramic make it difficult to achieve the desired part quality. One of the efforts directed towards machining these difficult to cut materials is using an ultrasonic vibration-based cutting method with a diamond tool at a frequency range of 20-100 kHz and amplitude of 25 μm or less. It is a non-thermal, non-chemical and non-electrical process, where the tool is vibrated at ultrasonic frequency via a piezoelectric actuator to generate breakage on the work piece surface [127]. Based on the nature of oscillation of the diamond tool, bringing it into and out of contact with the material in rapid succession, it can either be 1-dimensional (1D), oscillation in a single direction, or 2-dimensional (2D), a summation of two independent tools motions. While in 1D motion the tool tip is moved in a reciprocating manner, in 2D motion-based cutting there is a generation of an elliptical path.
2.5.1 1D Vibration-based Micro-Cutting Tools

The tool is driven harmonically in a linear path that is super-imposed on the up feed motion of the workpiece. For a given vibration frequency $f$, there exists a critical up feed velocity below which the rake face of the tool will periodically break contact with the uncut material surface. Figure 2.31 shows the idealized kinematics of the 1D vibration–based micro-cutting tool.

![Diagram of 1D vibration-based cutting](image)

Figure 2.31: Kinematics of 1D vibration-based cutting [132]

In 1D resonant system—an ultrasonic generator is used to create high-frequency linear tool motion. An ultrasonic generator uses a piezoelectric or magneto restrictive actuator to create reciprocating harmonic motion of high frequency but low amplitude. A acoustical- shaped waveguide booster and horn amplifies this ultrasonic motion. A cutting tool is attached at the end of the horn and aligned in such a way that the rake face is normal to the direction of vibratory motion [132]. Figure 2.32 shows the 1D cutting tool system that tends to operate at discrete frequencies of approximately 20 or 40 kHz which are achievable by commercial ultrasonic generators. The amplitudes are typically 3–20µm. This arrangement results in a ± 5µm sinusoidal displacement of the diamond tool [133].

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In the areas of non-resonant 1D tooling systems, Han et al. [134] made a system in which the tool was mounted to a flexure driven by a piezoelectric actuator. A sinusoidal voltage signal applied to the piezo stack caused it to expand and contract along its length. This actuator motion was amplified by the flexure to produce amplitude of 10 µm at ultrasonic frequencies. Cuttino et al. [135] developed a non-resonant 1D tool system that can be operated over a wide range of ultrasonic frequencies. Piezoelectric actuators cause transverse deflection of a notched flexure that holds the tool. To achieve high frequencies without the need to actively cool the piezo stacks, multiple actuators could be used [133]. Compared to conventional machining approaches, 1D vibration motion-based cutting tools experience lower thrust and cutting forces for the same load conditions [133]. The minimum achievable feature size using 1D tool is 2-3 µm deep, with a tool entering width of 100-150 µm. For more precise operations and complex micro-structure machining, motions in two directions are necessary that is provided by Elliptical Vibration-based cutting tools.
2.5.2 Ultrasonic Elliptical Vibration-based Micro-Cutting Tools (2D)

Ultrasonic Elliptical Vibration-assisted cutting (UEVC) systems create a circular or elliptical tool motion by causing the supporting structure to vibrate at its resonant frequency, in one or two dimensions. The generation of the elliptical cutting tool path is performed by superimposing two orthogonal modes. However, it is practically challenging to have the elliptical cutting tool path coplanar with the plane containing the cutting and chip-flow direction. Furthermore, these systems have several limitations including zero bandwidth, i.e. fixed excitation frequency, difficulty in synchronizing two resonant frequencies of the spatially orthogonal bending modes, compliance of the support, and lack of methodologies to design the optimum shape of horns [130]. Figure 2.33 represents a schematic of 2D UEVC cutting mechanism

![Figure 2.33: Schematic of 2D UEVC-based Cutting Operation [130]](image)

Brinksmeier et al. [138] built a simple resonant 2D UEVC system by mounting the center of mass of the diamond tool away from the centerline of a 1D ultrasonic system. Moriwaki et al. developed the resonant 2D system as shown in Fig. 2.34 [130,136 and 137].
Piezoelectric actuators attached to the side faces of the beam structure are activated in opposing pairs to induce bending in the up-feed and vertical directions at the third resonant frequency. A phase difference exists between the two pairs of actuators. The diamond tool is mounted on the end of the beam and the combinations of the two bending vibrations at right angles make it move in an elliptical path. This system has been used at discrete frequencies in the range 20–40 kHz.

**Figure 2.34**: Resonant 2D UEVC Tool developed by Moriwaki et al. [136]

In non-resonant 2D UEVC systems, sinusoidal voltage signals are applied to piezoelectric actuators causing them to extend and contract but at a frequency below the first natural frequency of the system. Ahn et al. [139] developed a tool system where the piezoelectric actuators are oriented at right angles to one another and aligned along the up feed and vertical directions, as shown in Fig. 2.35. The linear motion of the piezo stacks is converted into elliptical tool motion by a mechanical linkage. The tool is mounted on the flexure opposite to the actuators. The tool tip is driven in an elliptical path when the stacks
are activated by sinusoidal voltage signals with a phase difference between the two channels. The system operates at 1 kHz with ellipse dimensions of 5 µm × 5 µm.

Figure 2.35: 2D UEVC Tool system with orthogonal piezoelectric actuators [139]

Negishi et al. [140] developed a design as shown in Fig. 2.36 A. Sinusoidal voltage signals are supplied to the two parallel actuators. The tool holder serves as a mechanical linkage to convert the linear motion of the actuators into elliptical tool motion. Advantages of this system are its abilities to operate over a range of frequencies and for the tool path size, aspect ratio, and orientation to be varied by changing the amplitude and phase difference of the voltage signals to the actuators. Figure 2.36B shows the low-frequency design that had air-cooled piezo stacks and operated at 200 Hz with ellipse amplitudes of 20 µm × 4 µm.
Figure 2.36: Non-resonant 2D UEVC Cutting System with Parallel actuators (A) Machining Concept and (B) Prototype [140, 141]

UEVC is ideal for patterning micro V-grooves that have been widely used for optical devices, which is free of burrs. Lee et al. [142] studied the characteristics of micro V-grooves produced on a planar light wave circuit and glass by the UEVC method. Ma et al. [143] evaluated side burr formation in turning processes for conventional cutting, 1D-resonant-based cutting and 2D UEVC. A surface finish of 8–9 nm RMS was achieved. With a carbide tool and aluminum workpiece it was found that for a wide range of HSR, burr heights in 1D VAM were approximately 60–80% smaller than for conventional machining. 2D UEVC suppressed burr formation by an even greater degree and resulted in virtually no burr for HSR < 0.17. In 2D UEVC, the geometry of the cutting operation suppresses burr formation. When the depth of cut is equal to or less than the vertical vibration amplitude (dB ≤ 1), the tool exits the work-piece on each cycle, forming discrete chips [128]. The functioning of the UEVC is characterized by the elliptical tool paths it generates with superposition of two orthogonal resonant modes, which puts constraints on its industrial implementation for micro V-groove machining. Figure 2.37(A) is an SEM.
image of a trihedral feature 8µm tall in copper, which also shows virtually no burr at high magnification. Figure 2.37(B) shows an array of features with concave sculpted surfaces along the up feed direction. The feature radius of curvature on the trailing side is 250µm as compared to 50µm on the approach side [141]. Figure 2.37(C) shows precision steel micro-molds produced using the resonant 2D UEVC system depicted in Fig. 2.34.

![Figure 2.37: (A) 8 µm tall regular trihedral structure; (B) Non-planar microstructures with concave surfaces in direction of tool motion [141] and (C) Ultra-precision Steel Micro-molds machined using 2D UEVC method [144]](image)

2.5.3 Vibration-based AFM Micro-Cutting Tools

The AFM scribing described in the previous section was based on an AFM tip that smoothly moved over a surface under some load. However, there are some advantages to introducing low-amplitude ultrasonic movements between the AFM tip and the workpiece [145, 146]. A contact-mode AFM cantilever when brought to the sample surface for imaging exerts a repulsive force ranging over several nano-Newton ranges. For imaging mode, the feedback gain is set at a proper value within a non-oscillation range. If the feedback gain is further increased, the piezoscanner and, as a result, the cantilever begin to
oscillate in the z direction around the natural frequency of the feedback circuit. When the amplitude of the oscillation reaches several tens of nanometers, the sharp AFM tip scratches the sample surface. The main factor in pattern formation is the magnitude of the force applied to the sample combined with the cantilever oscillation arising from the feedback circuit. Hyon et al. [147] used an oscillating AFM micro cutting tool to make micro-and nano groove scratches on GaAs as shown in Fig. 2.38A, where an air-operated, commercial SiNx AFM cantilever was used. The force constant of the cantilever is 0.5 N/m that lead to a maximum applicable force of about 1 µN. Figure 2.38B-C shows the AFM image of the patterned nano-grooves with varying scan speed. Both the width and the depth exhibited an exponential increase with a decrease of scan speed.

**Figure 2.38:** Schematic of the AFM tool set-up (A); AFM image of nano-groove machined (B) and Scan speed dependence of width and depth (C)

Another way in which ultrasonic movements have been achieved is by depositing a film of a desired workpiece material onto a quartz crystal resonator [145, 146 and 148]. Once the quartz crystal is mounted on an AFM scanner, as shown in Fig. 2.35A, high frequency lateral oscillations can be introduced by applying alternating voltages of a
suitable frequency to the quartz crystal resonator. Figure 2.39A shows a schematic diagram of the combined AFM micro-cutting tool and QCR scratching instrument developed by Iwata et al. [148]. The oscillating surface is scratched with the AFM’s diamond tip to generate an effective cutting force. Scratching without a QCR oscillation forms bumps on the polymer surface, which is well known phenomenon of scratched polymer surfaces. However, ultrasonic scratching results in the carving of the thin films without the formation of bumps. The direction of the crystal oscillation depends on its crystallographic orientation. The resonance frequency of the QCR is 6.5 MHz. The wafer plate of the QCR was placed in the AFM sample holder located on a piezoelectric tube scanner. The laterally oscillating surface was scratched with the AFM tip as schematically explained in Fig. 2.39(B-C). The commercial cantilever which has a diamond tip was employed to avoid wearing of the tip and stable modification.

Figure 2.39: AFM micro-machining with QCR (A); Schematic understanding of Conventional AFM scribing (B) and Ultrasonic-based AFM Scribing (C) [148]
The major advantages to ultrasonic excitation is the increase in depth of cut when cutting both metals and polymers, and improvement in surface finish when cutting polymers [146]. For instance, when cutting aluminum using 4 AFM tool tip passes using a cutting load of 18.9\(\mu\)N and a speed of 0.18mm/min, the use of 5 MHz ultrasonic excitation can cause an increase in depth of cut from about 20 nm to 110 nm [146]. Furthermore, the amount of increased depth rises with increasing the loads, and rises with increasing numbers of tool passes, and decreases with increasing cutting speed [146]. Improvements in surface finish due to ultrasonic excitation can be seen when cutting some polymers such as polystyrene, polycarbonate, and PMMA that display poor cutting behavior during conventional AFM scribing. The effect of using ultrasonic-based AFM cutting compared to the conventional is explained in Fig. 2.40(A-B). During conventional AFM scribing, if the tip load is not sufficiently high, highly distorted raised surfaces are formed instead of a groove or pocket. When AFM scribing with a sufficiently high load, a raised surface will still result during the first tool pass, and by about the third tool pass this raised surface will distort and form ridges. By contrast, if ultrasonic excitation is used, a groove or pocket can be formed even during the first AFM tip pass as seen in Fig. 2.40 (B), and the resultant surface will be much smoother as shown in Fig. 2.40(C) [148].
Figure 2.40: Ultrasonic Vibration-based AFM scribing vs. Conventional AFM scribing:

Wear Height Variations (A) and Mean surface roughness (B)

2.6 Laser-Based Micro-Cutting Tools

Laser beam as micro-cutting tools is a thermal energy based non-contact type micro-machining technique, which can be applied to a wide range of materials. Direct write laser processing using a laser beam as a micro-cutting tool and where the material removal occurs at high power densities during the laser ablation process, has been demonstrated as a powerful technique for micro-machining processes, especially in micro-scale feature creation in glass and other fiber-optics [64]. One of the major advantages of laser scribing is the ability to quickly cut grooves in a wide range of materials, with different mechanical properties, that do not need to be electrically conductive. Materials reported to have been cut in the literature include polymers, glasses, metals, ceramics, crystals, and amorphous materials. A schematic of a laser tool set-up and the heat front generated by the laser beam on interaction with the work piece is shown in Fig. 2.41(A) and 2.41(B), respectively.
Figure 2.41: (A) Schematic of Laser beam micro-machining [64] and (B) Description of the Laser beam heat front [65]

Laser beam is focused for melting and vaporizing the unwanted material from the parent material or through a photolithic process involving localized breaking of chemical bonds in the workpiece material. A photolithic process usually occurs when cutting polymers with ultraviolet wavelength laser and a pyrolithic process occurs when laser cutting most other materials. It is suitable for geometrically complex profile cutting and making miniature holes in sheet metal. Lasers can be broadly divided into two types: continuous and pulsed. Continuous lasers generate a beam with time-invariant power and are unsuitable for micro-scale machining because thermal damage will occur around laser processed regions and prohibit the formation of sharp structures [65]. Pulsed lasers rapidly generate a series of brief laser pulses that tend to have a higher peak power with the constant power of a comparable continuous laser. When pulsed lasers are used for cutting, the length and power of the laser pulses have a significant effect on the cutting action. Among various type of laser tools used for micro-machining in industries, CO₂, Excimer
and Nd: YAG-based laser tools are the most established [63]. A description of different laser sources used in micromachining applications is shown in Fig. 2.42.

**Figure 2.42:** Laser Sources used in micromachining applications [69]

Lasers used for micromachining are characterized by short pulse length from millisecond range for micro-welding to pico- and femto-second for micro-scale ablation of materials. The beam characteristic is characterized by the half divergence angle (Θ) and radius of the beam waist (w). For an ideal beam,

\[
\text{M}^2 \propto \Theta \cdot w
\]

where \( \lambda \) is the wavelength of the emitting source. This quantity is invariant, implying for ideal optics characteristics, this relation holds true over the entire beam trajectory. A more significant expression number for beam quality is the \( \text{M}^2 \) number, ratio between \( \Theta \cdot w \) product for the real beam and ideal Gaussian beam. For a real beam, this product becomes,
where $M^2 \geq 1$. With resulting focusing optics, the divergence angle $\Theta$, after passing the lens becomes $\Theta_f = D/2f$ that results in a minimum spot diameter $\delta$ given by,

$$\begin{align*}
\text{---} \quad \text{---},
\end{align*}$$

where $f$ and $D$ are the focal length and diameter of the external optical lens system. For micromachining purposes a laser tool must possess a small spot size, which can be obtained with a high beam quality, $M^2 \approx 1$. Some of the important properties essential in the micromachining domain for the three types of laser tools are summarized in Table 2.2.

**Table 2.2: Laser Beam Properties for Micromachining [63-68]**

<table>
<thead>
<tr>
<th>Tool Type</th>
<th>Wavelength ($\lambda$) (µm)</th>
<th>Power (P) (W)</th>
<th>w. $\Theta$ (mm.mrad)</th>
<th>Beam Quality ($M^2$)</th>
<th>Spot Diameter ($\delta$) (µm)</th>
<th>Pulse Duration (µs)</th>
</tr>
</thead>
<tbody>
<tr>
<td>CO$_2$</td>
<td>10.6</td>
<td>1000</td>
<td>10</td>
<td>1.5</td>
<td>80</td>
<td>200</td>
</tr>
<tr>
<td>Nd: YAG</td>
<td>1.06</td>
<td>100</td>
<td>6</td>
<td>10</td>
<td>50</td>
<td>100</td>
</tr>
<tr>
<td></td>
<td>1000</td>
<td>25</td>
<td></td>
<td>80</td>
<td>500</td>
<td></td>
</tr>
<tr>
<td>Excimer</td>
<td>0.193-0.351</td>
<td>100</td>
<td>20</td>
<td>200</td>
<td>-</td>
<td>0.020</td>
</tr>
</tbody>
</table>

---

(2.2)

(2.3)
2.6.1 CO$_2$ Laser

CO$_2$ lasers have wavelength of 10 mm in infrared region. It has high average beam power, better efficiency and good beam quality. It is suitable for fine cutting of sheet metal at high speed. Unlike the original glass tube style gas lasers, the modern CO$_2$ lasers are used for materials processing are of a hard sealed waveguide construction that use extruded aluminum RF driven electrodes to excite a CO$_2$/N$_2$/He gas mixture [70-72]. The lasing transitions are from asymmetric to symmetric stretch modes at 10.6 μm, or from asymmetric stretch to bending modes at 9.4 μm of the CO$_2$ molecule CO$_2$-laser emits radiation continuously. Wherever the focused laser beam meets the workpiece surface, the temperature of the irradiated spot will rise so rapidly that the material will first melt and then decompose, leaving a void in the workpiece [71]. Typical examples of the groove channels ablated by the CO$_2$ laser beam on PMMA polymeric substrate is shown in Fig. 2.43, which follows a Gaussian profile averaged over 0.5ms of irradiation time. The cross section of the channels depends on the thermal diffusivity of the workpiece material and since the thermal diffusivity in polymeric materials such as PMMA is very low, it is the intensity distribution that mainly determines the channel cross section.

![Image](image.png)

**Figure 2.43**: Cross-sections of the micro-groove channels machined using CO$_2$ laser beam at (A) 1.9W, (B) 6.6W, (C) 18W and (D) 31W laser power [72]
CO$_2$-based laser tools have proven to be a very good method for creating micro-groove channels on polymeric substances for micro-fluidics. Researchers have investigated the CO$_2$ laser cutting of different polymeric plastics such as polyethylene (PE), polypropylene (PP) and polycarbonate (PC) [74]. Klank et al. [70] and Snakenborg et al. [72] investigated the use of a commercial CO$_2$ laser system for the fabrication of polymethyl methacrylate (PMMA) micro-fluidic systems. The laser power employed in their work ranged from 10 to 60 W and the scanning speed ranged from 80 to 400 mm.s$^{-1}$. Figure 2.44 shows the micro-groove channels crossing each other that are cut on the PMMA surface. As seen in Figure at the channel rims and at T-junction, resolidified melted polymer material seems to have been ejected out.

![50 μm](image)

**Figure 2.44:** Micro-Groove channels crossing each other in PMMA surface [72]

Yuan et al. [76] have reported experimental and theoretical analyses of direct-write laser micromachining of PMMA with a low laser power (0.45–1.35 W) and low scanning speed (2–14 mm.s$^{-1}$) by CO$_2$ laser. Atanasov and Baeva [75] and Berrie and Birkett [71] have developed models for CO$_2$ laser cutting and CO$_2$ laser drilling of PMMA with even higher laser power, respectively. Niami et al. [73] investigated the effects of change of molecular weight of the polymeric material while using CO$_2$ laser for different power and
speed in the micro-machining of groove channels. Figure 2.45 (A) and (B) shows the groove channel width and depth variation with respect to laser speed and power, respectively. As expected, the depth increases with increase in laser power and decreases with increase in speed. Figure 2.45(C) shows the dependence of width and size of the channel on weight of PMMA for different laser speeds. It is observed that with increase in the weight, there is a decrease in width and depth of the micro-channel features.

Figure 2.45: Micro-groove channel characteristics with variation in Laser Beam parameters- (A) Speed, (B) Power, and (C) With Variation in Molecular weight of machined polymer

Figure 2.46 illustrates the laser micro-machined grooves on the glass surface under a number of focused and defocused conditions. The central grooves in Fig. 2.46(A) and (B), marked by arrows, represent the optimum machining with the best focusing conditions. The large cracks in the glass were produced when the high intensity laser beam was directly focused on to the surface. Using careful selection of experimental parameters such as power, intensity, scanning rate is therefore necessary while using CO$_2$ laser tool when micromachining the polymeric work pieces [64].
2.6.2 Nd: YAG Laser

Nd: YAG are solid-state lasers that have low beam power, but when operating in pulsed mode, high peak and focusing powers enable it to machine through thicker materials. Although the mean beam power is relatively low, the beam intensity can be relatively high due to smaller pulse duration, which is ideally suited for micro-machining of thinner materials as well. Due to their shorter wavelength of 1µm, they are readily absorbed by high reflective materials that are difficult to machine by CO₂ lasers [77].

Smaller kerf width, micro-size holes, narrower heat affected zone (HAZ) and better cut edge kerf profile can be obtained in Nd: YAG laser beam micro-machining. The smaller thermal load by Nd: YAG laser allows the machining of some brittle materials such as SiC ceramics that cannot be machined by CO₂ laser without crack damage [78]. Due to shorter wavelength, Nd: YAG laser is highly absorbed when falling even on a reflective
surface. The enhanced transmission through plasma, wider choice of optical materials and flexibility in handling with the advent of fiber optic beam delivery is also interesting characteristics of the Nd: YAG laser [78]. A schematic of Nd: YAG Laser tool system in shown in Fig. 2.47.

Figure 2.47: A schematic representation of Nd: YAG laser tool set-up [78]

Micromachining of materials with laser beam requires micrometer size spot and very short interaction time [79]. Q-switched Nd: YAG lasers with nanosecond and pico-second pulses are better suited for this purpose. Nd: YAG lasers with doubled and tripled frequency have been applied in micromachining of different metals, non-metals and composites [79-80]. Tripled frequency Q-switch Nd: YAG laser with 50 ns pulse duration was used to investigate the drilling depth and hole size during micromachining of copper [81]. It was found that the depth and diameter of the machined hole both increase with laser intensity (W/cm²) and become constant after a higher value (1010 W/cm²). Depth of hole also increases with increase in number of pulses. The experimental investigations by
Yousef et al. [82] during micromachining of three metals copper, aluminum and brass show that the crater depth and diameter increases with pulse energy. A 400 µm thick ceramic NdFeB (a magnetic material) was cut using a sub-microsecond-pulsed Nd: YAG laser in air and water [83]. Cutting in water provided best cut quality, narrower cut width and elimination of the debris compared to cutting in air as shown in Fig. 2.48 (A-B).

![Figure 2.48: SEM micrographs of micro-slots cut using Nd: YAG Laser in (A) Air and (B) Water [83]](image)

Nd: YAG laser with dual-prism optical system was used to fabricate spiral microstructures in 50–190µm thick silicon wafer [84]. The aspect ratio up to 10 was found during experimentation. Parametric study on frequency tripled Nd: YAG laser micromachining of sapphire (381 µm) and silicon (533 µm) wafers show that focus position and micromachining speed are the most influential parameters for depth of micromachining [85]. Figure 2.49A shows column-like microstructures and debris formation on the cutting sidewall of Silicon. Figure 2.49B depicts the typical laser micromachining cavities in sapphire using 355nm Nd: YAG laser tool. In the same study it was found that micromachining rate increases with fluence (J/cm²) but micromachining speed has no significant effect. It was also found that the kerf width and shape was greatly influenced by focal plane position.
Figure 2.49: The SEM image of Nd: YAG 355nm laser micro-machining on: (A) Silicon and (B) Sapphire [84].

Figure 2.50(A) shows the variation in ablation rate of the Nd: YAG laser tool used at different wavelengths for micro-machining in Sapphire. The depth variation with laser speed at a constant wavelength of 355nm is shown in Fig. 2.50(B), while Fig. 2.50(C) shows the plot of laser micromachining precision as a function of laser fluence using 266nm and 355nm lasers for different materials. It is observed that in general the Nd: YAG laser micromachining provides high level of precision at lower wavelengths and it provided better precision in the order of sapphire, silicon and then on Pyrex.

Figure 2.50: Study of Ablation Rates for different wavelengths (A) and Study of Groove depth with variation in Speed (B) for sapphire and Laser-micro-machining precision for various materials and (C) Micromachining precision as function of fluence [86].
2.7 Sensor-Integrated Micro-Cutting Tools

Sensors apply a major role in process control and optimization. The sensor measuring techniques for the monitoring of micro-cutting tools are categorized into two approaches: direct and indirect [87]. In the direct approach the sensor is integrated to the body of the cutting tool, while in the indirect approach, which is the most widely used method; the sensor system is externally mounted away from the cutting tool.

The sensor-integrated micro-cutting tools in literature are predominantly found to either acquire force-related or temperature-related information during the cutting process that enhances the understanding regarding tool wear / life of the cutting edge. Kim et al. [88] developed a combined tool dynamometer utilizing the best features of strain gauge and piezoelectric sensor types- (1) Strain sensing for static force measurements, and a (2) piezoelectric thin film accelerometer for dynamic force measurements. The total cutting force could be obtained by the summation of the static and dynamic forces. Figure 2.51 shows the schematic diagram of the experimental set-up.

![Diagram of sensor integration](image)

**Figure 2.51**: Strain-Gauge Sensors for Micro-Cutting Tools [88]
Yoshioka et al. [89] investigated a thermometry-type in-process MEMS-based sensor for monitoring thermal behavior of turning inserts at cutting point. They developed a platinum resistance type micro-sensor that is mounted directly onto the surface of a tool tip at a distance of 0.8mm from the cutting edge, as shown in Fig. 2.52(A-B). It consisted of two gauge elements and three electrodes provided quick response and high resolution at the ultra-precision cutting scale. The fabrication sequence for sensor-integration onto the cutting edge is shown in Fig. 2.52(C). This sensing method represents the direct approach, as discussed at the start of this section.

**Figure 2.52:** (A) Schematic of the MEMS-based sensor integrated on Cutting tool; and (B) Actual Sensor Fabricated on top of tool surface using MEMS process; and (C) Fabrication procedure for sensor-integrated cutting tool [89]
Santochi et al. [90] used strain-gauges integrated to the tool shank surface for force measurements during ultra-precision lathe operations. The integration of the sensor within the tool shank, created a system which was easy to use and capable of transmitting data to the CNC through wireless equipment, as shown in Fig. 4.53. In particular, the output signal of the measurement bridge is amplified and sent to an external data acquisition system by infra-red transmission. The amplifying and transmitting circuit has been miniaturized and directly placed within the tool shank.

![Schematic view of the sensor-integrated tool system](image)

**Figure 4.53:** Schematic view of the sensor-integrated tool system [90]

Ranc et al. [91] measured the temperature field in the tool-chip interface using the principle of pyrometry in the visible spectral range, in a specialized sophisticated ballistic device set-up as shown in Fig. 2.54. The mechanical device developed could reproduce orthogonal cutting conditions that reach very high cutting speeds up to 120 m/s. The intensified CCD camera is used for thermal measurements and visualize the chip geometry is fixed directly over the cutting process. The workpiece is propelled by an air gun in a launch tube. Two symmetrical cutting tools are fixed at the entry of a second tube. The two tubes are co-axial. The workpiece is machined when it leaves the launch tube and just
before to enter the second tube. A load sensor supporting the cutting tools measures the cutting forces during the process.

**Figure 2.54:** Schematic description of the experimental set-up (left) and thermal measurement system (right) [91]

Until now, the sensor-integrated rigid cutting tools available in literature were reviews. In this section, sensor integrated into AFM probe-based flexible cutting tools will be discussed. Recall that the AFM probe-based flexible cutting tools consist of a cantilever, which is mounted with a cutting tool tip at its end. The forces on the AFM probe tip due to contact with the workpiece cause a slight deflection of the cantilever. This deflection is measured by shining a laser beam onto the back of the cantilever and measuring the angle it is reflected at via four Position-sensitive detectors positioned in the path of the reflected beam [40-47]. The sample typically sits on a piezoelectric scanning stage capable of adjusting the lateral x-y position of the sample and sometimes the z height of the sample. Alternatively, the scanning stage may only adjust the x-y position of the sample and a separate z-stage may adjust the height of the AFM probe [92]. This is schematically represented in Fig. 2.55
For creating curved surfaces by material removing, AFM piezo scanners or drivers have been integrated with commercially available multi-axis stages to enhance the manipulability of workpiece movements. Thus, the workpiece could move and/or rotate in 3D and a curvilinear pattern of grooves could be singly scratched or scratched in an overlapped manner. Lee et al. [93] coupled an AFM with a closed-loop piezo-driven stage to perform material removing. The coupled system was equipped with a capacitive-based feedback sensor to improve the accuracy of the stage movements, as shown in Fig. 2.56.

**Figure 2.55:** Process Monitoring Set-up for AFM-based micro-machining process [92]

**Figure 2.56:** Micro/nano-CNC control of AFM-based cutting system [93]
Bourne et al. [105, 106] integrated the AFM flexible cutting tool with a five-axis micro-machine tool (mMT) that was retrofitted with a scribing assembly for high-speed, high-precision chip formation-based micro-groove cutting, as shown in Fig. 2.57. The z-stage that houses the scribing assembly could hold the cutting tool at varying angles relative to a workpiece. The workpiece could be traversed in x-y plane perpendicular to the direction of approach of the scribing assembly.

Figure 2.57: 5-axis mMT attached with the schematic of the scribing assembly [105]

The scribing assembly permitted deflections of the AFM cantilever to be measured via a Keyence confocal laser displacement sensor as shown in Fig. 2.58(A-B) [107]. Unlike traditional AF-based cutting tools that measures tool position by means of cantilever deflection on the photo detector with picometer resolution, they used the confocal laser displacement sensors with a built-in microscope for deflection measurement up to 10nm resolution at a sampling rate of 1562Hz. The sensor could be set to continuously measure the displacement of a single spot on a tool (displacement mode) or to continuously sweep back and forth (scanning mode).
2.8 Process Simulation for Micro-Groove Cutting

2.8.1 Modeling Methods

Continuum-based numerical modeling is essential to investigate physics-related process field variables such as temperature, stress and strain fields, heat generation at the cutting zone and frictional characteristics that influence the chip formation and ultimately the groove quality at the micro-scale. Micro-scale machining is influenced by the size of the edge radius of the cutting tool relative to the uncut chip thickness and by the size of the uncut chip thickness relative to the workpiece microstructure. Since the micro-groove cutting under consideration is a chip-based process, only micro-scale machining processes that use mechanical means to remove material are reviewed.

Different types of models exist for analyzing micro-scale machining processes such as analytical models, mechanistic models, molecular dynamics (MD) models, and finite element models. Some of the commonly used analytical methods include slip-line field model [99], shear plane model [100] and parallel-sided shear zone theory [101] as well as
mechanistic models [102]. Though these models provide adequate information on predicting surface profile generation and tool-chip contact forces, they fail to deal with stress-strain and temperature fields throughout the workpiece. MD models provide accurate representation of molecular interactions between those that make up the tool-workpiece contact zone. Many researchers have used MD to study AFM-based nano-scribing processes [103-104]. The model restricted to focusing on a very small volume and becomes computationally exhaustive when a workpiece area exceeds several thousand nanometers. Finite element models have been used to study many aspects of machining both at the micro-scale and at the macro-scale, which offers a better computational efficiency when analyzing features at the micro-scale. Using 2D and 3D formulation schemes, the models have been used to study the effects of changing process parameters such as cutting speed, rake angle, and coefficient of friction on process characteristics including shear angle, chip thickness, strain, strain rate, stress, temperature, chip curl, tool-chip contact length, and cutting forces.

A more detailed review on various finite element models using in micro-machining can be found in Bourne et al. [106]. This section reviews the 3D finite element model developed by Bourne et al [106] for micro-groove cutting of aluminum deposited on a silicon substrate. Specifically, the review includes a discussion on the motivation for 3D numerical approach, element model characteristics such as formulation and integration schemes, chip formation and separation concepts and salient features, process variables predicted and limitations.
2.8.2 Finite Element Model for Micro-Groove Cutting

A. Motivation for 3D Modeling Approach

The 2D approach developed in literature assumes the plane strain approximation, which is valid provided that the width of the cut is much greater than the depth of cut [106, 110,111]. The model also assumes all strain tensor components orientated out of the cutting plane small enough to be treated as zero. In the micro-groove cutting process under consideration, a tool with a width of 1.05 μm was used to cut grooves with depths of approximately 0.25 to 1.0 μm using orthogonal cutting [112]. Thus, the width of the cut grooves is at least 1.05 - 4.2 times the depth of cut, which invalidates the plane strain assumption. Furthermore, the micro-groove cutting process involves 3D stress-strain fields and hence a model that will capture the 3D micro-groove cutting process is needed. Moreover, the side burrs and exist burrs observed during the cutting process that significantly affect the tolerance of the features can only be evaluated in a 3D work space.

B. Integration and Formulation Scheme

Two element solution methods exist for finite element models - implicit dynamic analysis and explicit dynamic analysis. Each solver has its own advantages and drawbacks as pointed in [106]. For this study, Bourne et al. used the explicit dynamics procedure using Abaqus Explicit 6.9 software, where the solver calculates dynamic quantities during a current time step using only quantities that were already calculated in a previous time step via the use of a central-difference time integration rule. This enables accommodation of complex contact and boundary conditions, large strain-rate deformations and material failure.
Three finite element formulations are currently employed when modeling micro-machining processes: the Lagrangian formulation, the Eulerian formulation, or the adaptive Lagrangian-Eulerian (ALE) formulation. Using Eulerian formulation, the configuration of the finite element mesh never changes. Rather, material flows into and out of the elements. The ALE formulation is a hybrid of the Lagrangian and Eulerian formulations. More details on existing cutting models using the described formulation methods can be found in [106]. The 3D model developed by Bourne et al. [106] used the Lagrangian-based formulation to investigate and understand the process mechanics of micro-groove cutting process. In the Lagrangian formulation, elements that make up the finite element mesh, each corresponding to a piece of material that never leaves the element. Mesh deformation corresponds to deformation of underlying material. It is ideal for simulating transient conditions such as tool entry and exit from a workpiece.

C. Element Selection & Modeling Scheme

The workpiece material used by Bourne et al for the experimental work consisted of a 3.1 μm thick film of thermally evaporated aluminum deposited on a flat silicon substrate. The workpiece showed evidence that thermal softening during machining is not very significant in the size scale of interest [106]. For modeling of the workpiece, 8-node linear reduced integration elements (C3D8R) with “relax stiffness” hourglass control was used. The aluminum film was modeled as an isotropic plastic-elastic material with strain hardening and strain rate hardening. Johnson-Cook failure model for damage criterion was adopted. Thermal softening and stress-triaxiality effects were not considered due to the high thermal conductivity of the workpiece, small uncut chip thickness and relatively low
cutting speeds employed for the study [109]. The tool was constructed from rigid 3D planar elements (R3D4) with 0° rake angle, a 20° end clearance angle, a 5° side clearance angle, and a perfectly sharp cutting edge. The tool was assumed to be perfectly rigid and experienced no deformations during the cutting process. A mass-scaling factor of 5000 was used to increase the material density of simulation to reduce the large number of time increments during simulation. The overall model geometry was developed on half-symmetry to save computational time. The overall mesh is shown in Fig. 2.59(A), the rigid tool surface mesh is shown in Fig. 2.59(B) and the initial crack with a opening angle was introduced to encourage steady state chip formation as shown in Fig. 2.59(C).

Figure 2.59: (A) Overall Mesh Geometry of 3D Model, (B) Tool surface and (C) Crack Initiation for chip formation [106]

D. Chip Separation & Contact Algorithm

One of the key aspects of development of the finite element model is to accurately model and mesh the chip formation and removal during the cutting process. Several
approaches have been used in literature that can be broadly categorized as indentation-based, nodal separation-based, or element failure-based. Several remeshing and solution mapping algorithms exist in literature to handle element distortions during chip formation [106]. The Lagrangian formulation developed must be able to handle the chip formation and separation phenomena. Separation of a chip form the workpiece for the micro-groove cutting process was accomplished via failure and deletion of elements around the chip during the chip formation event. This allows the workpiece mesh to essentially be cut. Contacts between the tool and workpiece and between the workpiece and itself are modeled using a general contact algorithm. This is automatically performed by the Abaqus 6.9 software [108]; and frictional effects were modeled using extended coulomb frictional model, an approach followed by many researchers [106].

2.9 Chapter Summary

This chapter gives an overview of the literature relevant to several different micro-scale cutting tools that are capable of cutting in range of 20nm – 400 nm and 20 µm - 200µm, respectively. Different types of mechanical micro-cutting tools that include micro-end mills, micro-turning and threading tools were studied. Fabrication methods involved in making intricate modifications to the cutting edge were also discussed. Different kinds of features on wide range of materials that are possible using these tools were also mentioned. Factors such as minimum chip thickness, burr formation and instability in micro-cutting forces, machine tool dynamics play a significant role in the nature of the features that are created. Exponential tool edge wear and sub-surface damage are significant issues when fabricating micro-end mill using FIB and WEDG techniques.
Based on type of energy (force) applied on the probe tip, the AFM-based flexible cutting tools were capable of machining different size features with depth of penetration relating to the applied energy source. Nature of workpiece material played a major role in limiting the type of tool utilized. Different improvements in the process control of the AFM-based flexible cutting to improve its productivity and manupalibility were discussed. For catering to demands of micro-electronics and advanced space and communication industries, it was necessary to create micro-scale patterns on difficult to machine materials such as SiC, BK-7 Glass and Fiber optics materials. Ultrasonic-vibration assisted micro-cutting tools offered reduced tool forces, minimization of burr formation and extended tool life when cutting difficult to machine materials. This was evident in both ultrasonic vibration-assisted rigid and flexible cutting tools. It was also possible to machine any out-of-plane complex micro-geometry using these tools. Laser-beam based micro-tool had the ability to cut very fast on almost any material with no tool wear-out issues. Beam intensity and delivery mode played a significant role in determining the performance and tolerance achieved using laser-based cutting tools. The Gaussian beam profile was a common feature in the tools discussed in this section that was evident on all the micro-features cut. Different types of sensor-based micro-cutting tools have been discussed where the sensors attached to the workpiece or tool platform influence the process control during cutting operation and also enable tool monitoring capabilities, especially during the ductile to brittle mode and ploughing to shearing mode transitions, a feature in micro-machining processes.
Gaps in Literature

Several micro-scale cutting tools have been reviewed that are capable of cutting repeatable micro-scale patterns, esp. micro-groove channels, and each of these tools have their own merits and drawbacks. Mechanical micro-cutting tools such as Micro-End Mills, turning and threading tools, though capable of creating 2D and 3D micro features on most polymeric and metal surface, are not ideal for creating high-aspect ratio features with deep and narrow features. They cannot operate sub 20 µm domain and those that operate due to FIB-based tool tip modification face significant wear-out issues due to machine tool dynamics involved. Also, at smaller depths of cut, size effect and material microstructure may affect the surface integrity, roughness and cutting forces. Conventional AFM-based cutting tools are capable of producing high-resolution features in the 10-200 nm range. Also, they cannot cut features in the mm range and without modifying their set-up, cannot cut features on curved topographies. The most significant drawback with AFM-based cutting tool tip is that they are predominantly pyramidal shaped, which is either SiNx or diamond coated and therefore, not applicable to cutting all materials without wear-effects coming into play. For the point of view of mass-productivity, they are the slowest technology available.

Ultrasonic vibration-assisted micro-cutting tool require a complex test bed set up with the provision and maintenance of an ultrasonic generator. Due to nature of tool configurations while machining, minimum obtainable surface finish and the smallest feature size possible is limited. Lastly, laser-based micro-cutting tools have a few limitations as well. One of the major feature and disadvantage in these tools is the complexity of setting beam parameters to suit the requirement. There are no ideal
conditions that are reported in literature, most of them are to be set empirically. Also as pointed out in the earlier sections, another flaw in laser-based micro-cutting tools is the formation of dense ridges on the side of feature due to ejection and re-solidification of melted polymers/metal that significantly affects the outcome when the tolerance are less than 5 µm.

There are several critical issues associated with micro-machining and machine tools that come mainly from the miniaturization of the components, tools and processes. As the scale of features and machined parts decreases, the resolution of techniques used to measure and quantify these parts increases. With the increase in complexity at the micro- and nano-scale machining, there is a need for intelligent and robust in-situ sensors working in proximity with the cutting tool that can handle and modify tool performance based on changes in the environment and therefore, influence the characteristics of the resultant micro-scale features machined. The challenges that lie in this regard include packaging capabilities of sensor with the micro-scale cutting tool, abilities for having a self-calibration system that is not very tedious and time-consuming, self-diagnostic capabilities and cogent decision making capabilities that can seamlessly interface with the machine tool controller. Meeting these demands by creating a small and compact sensor-integrated flexible cutting tool system that are simple in design and economical to produce and which can readily integrate with the micro-scale machine tool provides the motivation for this thesis.
Chapter 3
Development of Meso-Scale Flexible Cutting Tool

3.1 Introduction

A meso-scale CBN flexible cutting tool is developed using a Micro-Electro-Mechanical-Systems (MEMS)-based fabrication approach. The cutting tool is integrated with a strain-gauge sensor that is responsible for in-situ process monitoring and deflection control of the flexible cutting tool. The process presents a simple and cost-effective way to batch fabricate of sensor-integrated flexible cutting tools for machining micro-grooves in steel that are repeatable over a large surface area, few micron wide, few micron deep and between a few hundred microns to millimeters long and with high relative accuracy and depth consistency.

The first section of the chapter explores the suitability of using a CBN tool tip on a commercial AFM sapphire cantilever for machining micro-groove channels on low-carbon steel. The design requirements for the meso-scale cantilever and strain-gauge sensor are discussed next. The MEMS-based fabrication steps for meso-scale flexible cutting tool are also described. It is followed by the tool characterization and strain-gauge sensor performance. Lastly, the CBN tool tip mounting strategies on the fabricated meso-scale flexible cutting tool are discussed.
3.2 Preliminary Micro-Groove Cutting on Low Carbon Steel

As described in chapter 1, one of the main research objectives of this thesis work is to successfully machine micro-groove in hard materials. To this end, micro-grooves - both rectangular and compound v-shaped have been cut up to several millimeters in length, around 1 µm wide and less than 1 µm deep in pure aluminum, sputter coated on silicon wafers. Commercial AFM sapphire cantilevers [149] mounted with diamond tool tip were used to aid the process of groove cutting. The diamond tool tips were provided with a micro-groove cutting geometry using the focused ion beam (FIB) machining technique [106].

The next logical step in that direction was to investigate micro-groove cutting on low-carbon steel samples for which, the current diamond tool tips is needed to be replaced with an alternative cutting tool material. This was because diamond is not effective in machining ferrous materials as the carbon in the tool will diffuse into the workpiece as a result of graphitization and cause unacceptable tool wear at the size scale involved. This challenge was tackled using single CBN crystal tool tips in place of diamond. CBN is an ideal choice for difficult to machine materials and possesses various characteristics such as extreme hardness at room temperature, excellent wear durability and tool life, good thermal resistance and stability, and it is unlikely to generate any kind of chemical reactions with the workpiece material at elevated temperatures. A typical single CBN crystal is shown in Fig. 3.1, whose size varies between 450 µm - 600µm. However, the superior mechanical properties of CBN also make it challenging to modify
its geometry at the micro-nano scale via FIB machining for creation of exact micro-groove geometry.

Figure 3.1: Single cubic boron nitride (CBN) crystal

3.2.1. Metallographic Sample Preparation

A very highly polished, scratch-free steel surface is essential to perform the micro-groove experiments. A series of specimen preparation steps have been followed to obtain a target value of 25-40 nm average surface roughness (Ra) finish on low-carbon 1018 steel. The process sequence started with rough and fine grinding using different grades SiC abrasive grit papers (P180 → P320 → P600 → P800), followed by rough polishing using sequential diamond lapping compound (10 µm → 6 µm → 1 µm) spread on different Texmet medium-nap micro-polishing cloth. A fine polishing step was then carried out on a 0.25 µm low-nap micro-polishing cloth using 0.05 µm and 0.03 µm suspended alumina slurries. Between each of the polishing steps, the specimen was washed thoroughly under high stream tap water and sonicated in an Isopropyl Alcohol (IPA) bath. The final step was done in a Syntron Vibratory Polisher, where the steel samples were placed face down in 20nm colloidal Silica suspensions and induced to
vibrations for 12 hours. Figure 3.2 shows the microstructure of the polished 1018 steel sample that was etched with 4% Nital reagent, revealing the pearlite granules (black) in the ferrite (α-Fe) matrix.

![Microstructure of polished 1018 steel sample](image)

**Figure 3.2**: 1018 low-carbon steel microstructure

### 3.2.2. Flexible Cutting Tool

The flexible cutting tool used for cutting on 1018 low-carbon steel is a commercially available AFM sapphire cantilever as shown in Fig. 3.3. The sapphire beam with its high modulus is bonded to a sapphire substrate and the single CBN crystal is glued to the tip of the cantilever using an adhesive. The factory measured dimensions of the AFM sapphire cantilever are tabulated in Table 3.1. Using the provided values, the fracture strength and failure load of the tool was calculated. Applying cutting loads that exceeded the failure load values results in catastrophic failure of the AFM sapphire cantilever. Therefore, it was necessary to maintain the applied cutting load at the tool tip below the failure values.
Figure 3.3: AFM-sapphire Cantilever – (A) Schematic and (B) SEM image

Table 3.1: AFM Sapphire Cantilever Properties

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cantilever Cross-Section</td>
<td>Circular</td>
</tr>
<tr>
<td>Thickness (µm)</td>
<td>48</td>
</tr>
<tr>
<td>Length (µm)</td>
<td>930</td>
</tr>
<tr>
<td>Radius (µm)</td>
<td>37</td>
</tr>
<tr>
<td>Edge Height (µm)</td>
<td>141</td>
</tr>
<tr>
<td>Tool Set Back Length (µm)</td>
<td>75</td>
</tr>
<tr>
<td>Stiffness (N/m)</td>
<td>643</td>
</tr>
<tr>
<td>Fracture strength (MPa)</td>
<td>483</td>
</tr>
<tr>
<td>Failure Load (mN)</td>
<td>7.6</td>
</tr>
</tbody>
</table>

The prescribed cutting geometry was provided on a single CBN crystal using the FEI Dual Beam 235 FIB machine. The machine uses Gallium ions to selectively sputter on target areas that follow a Gaussian distribution. The details of the FIB process planning can be found in [106]. Figure 3.4 shows the micro-groove geometry machined
on the CBN tip of the AFM sapphire cantilever. The side view of the micro-groove cutting tool is shown in Fig. 3.4(A), where the zoom-in image on the right shows the cutting edge. Figures 3.4(B) and 3.4(C) show the rake face profile as viewed from the back-end of the cantilever and the clearance faces as viewed top-down using the SEM, respectively. Table 3.2 summarizes the micro-groove cutting tool geometry parameters used during the FIB machining process.

**Figure 3.4:** (A) Side view of the cutting tool showing the cutting edge; (B) Rake face view of the tool seen from back-end of the cantilever and (C) Clearance faces seen top-down

**Table 3.2:** Cutting tool geometry parameters

<table>
<thead>
<tr>
<th>Description</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tool Height</td>
<td>100 µm</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>120 nm</td>
</tr>
<tr>
<td>Rake Profile</td>
<td>Rectangular</td>
</tr>
<tr>
<td>Tool Width</td>
<td>1.85 µm</td>
</tr>
<tr>
<td>End Clearance angle</td>
<td>20 deg</td>
</tr>
<tr>
<td>Side Clearance angles</td>
<td>4 deg</td>
</tr>
<tr>
<td>Side Rake Angle</td>
<td>7 deg</td>
</tr>
<tr>
<td>Max Depth of Cut</td>
<td>4.6 µm</td>
</tr>
</tbody>
</table>
3.2.3. Groove Cutting Experiments in 1018 Low Carbon Steel

A series of micro-grooves were cut using 3 tool pass condition at a cutting speed of 100mm/min. Grooves for 4.0mm length using the single tool pass, followed by second tool pass for a length of 3.5 mm on top of the first set of grooves and for a length of 3.0 mm on the third tool pass over the same set of grooves were cut. Six different cutting loads between 1.0 and 5.0 mN were used keeping the maximum stress experienced by the tool under the failure stress. The load values were chosen based on knowledge of machining grooves on aluminum and considering the difference in hardness of low carbon steel compared to that of aluminum. Figures 3.5 shows the micro-grooves cut in 1018 steel using three tool pass condition and different load conditions for a single tool pass, respectively. The figure shows three parallel grooves, where the top groove (Groove #1) is machined using a single tool pass for the length of the solid arrow, while the middle one (Groove #2) is machined using two tool passes for the length of the dashed arrow and the bottom groove (Groove # 3) is machined using three tool passes for the length of the dotted arrow. As seen from the SEM figures, well-defined linear micro-grooves with rectangular side walls, constant width and depth were machined successfully over the length of the cuts. The figure also shows the chips formed during the groove cutting process scattered on to the side or lying on the groove path.
Figures 3.6(A-C) and 3.7(A-C) show the 2D and 3D AFM scans of the micro-grooves cut in 1018 steel using the lowest (1.0 mN) and highest (5.0 mN) load condition. The 3D images use units of microns and are used to create the corresponding median 2D cross-sections shown to the right of every 3D AFM image. It was found that all conditions produced rectangular groove cross-sections as observed from the 2D AFM plots. Moreover, groove depth increased with increased load and with an increased number of tool passes. Also, all grooves had side burr formation along the groove borders with sizes dependent on the cutting conditions. Further, it was noted that at high loading conditions, the maximum groove depth was achieved during the first tool pass. At low load conditions (1 mN), there was a combination of elastic and plastic deformation, especially during the first tool pass. This can be seen in Fig. 3.6(A), where ploughing action mainly dominated. For the same load, at higher number of tool passes, a completely plastic deformation was observed.
Figure 3.6: (A) AFM plots of the micro-grooves cut at 1mN load using (A) Single tool pass; (B) Two tool passes and (C) Three tool passes
Figure 3.7: (A) AFM plots of the micro-grooves cut at 5mN load using (A) Single tool pass; (B) Two tool passes and (C) Three tool passes

3.3 Motivation for Meso-Scale Flexible Cutting Tool

From the preliminary micro-groove cutting of low-carbon steel it was clear that maximum achievable feature depth was around 750 nm using multiple tool passes. It is understood that the depth of cut was a function of the stiffness of the AFM sapphire
cantilever. In order to make deeper groove channels using the current tool, multiple tool passes were necessary, which would increase the tool wear, burr-formation along the feature edges and the machining time. The current micro-grooving module uses a Keyence LT-9010M confocal laser displacement sensor (~$20,000), which is a separate system from the cantilever. Precisely aligning the laser on the back of the cantilever is difficult and time-consuming process; also, the laser sensor being used has a fixed average scanning frequency of 776Hz, thus limiting the system bandwidth of the micro-grooving process and leading to slower than achievable machining speeds and increased cycle-times, especially, over complicated contoured surfaces. Furthermore, the sensor requires a long set-up time to stabilize due to drifting before it can be used for feedback. These factors pointed towards the need for developing a stronger and stiffer flexible cutting tools with a cost-effective and robust tool deflection sensing capability, and specifically designed for micro-groove cutting using the single CBN crystal tool tip. The following sections discuss the design and development of the meso-scale CBN flexible cutting tool with a higher strength and stiffness and with an in-built strain-gauge sensor for tool deflection control.

The discussion on the meso-scale flexible cutting tool evolves as follows. In Section 3.4, the design procedures for the meso-scale cantilever and the strain-gauge sensor are explained. A MEMS process-based fabrication procedure employed to fabricate the meso-scale cantilever, which forms the base of the flexible cutting tool, integrated with the strain-gauge sensor is discussed in Section 3.5. The design characterization of the fabricated meso-scale cantilever and the performance evaluation of the strain-gauge sensor are evaluated in the following two sections. The last section,
presents the discussion on the single CBN crystal mounting on the meso-scale flexible cutting tool, groove cutting geometry definition and tool packaging.

3.4 Meso-Scale Cantilever Fabrication

The meso-scale CBN flexible cutting tool consists of: (a) the meso-scale cantilever; (b) in-built strain-gauge sensor; (c) substrate for tool handling; and (d) tool tip post that accommodates a single CBN crystal with the micro-groove cutting geometry produced via a MEMS-based fabrication process. Figure 3.8 shows a sketch of the meso-scale cantilever, whose length is in the millimeters range, and width and thickness are several hundred micrometers, respectively. In comparison, the AFM sapphire cantilever, shown in Fig.3.2 is 800 µm in length, 30 µm wide and 30 µm thick.

Figure 3.8: Sketch of the meso-scale CBN flexible cutting tool
3.4.1 Meso-Scale Cantilever Design

The objective of the meso-scale cantilever design is to increase the overall load carrying capability of the cantilever. The material for the cantilever is selected as single-crystal silicon. In order for the meso-scale cantilevers to exceed the maximum loading condition of 8 mN (or stiffness of approximately 850 N/m) that could be obtained with the AFM sapphire cantilevers, the stiffness values in this study are chosen as 1070 N/m, 1140 N/m and 1390 N/m, respectively. Furthermore, these stiffness values guarantee that the stress experienced by the cantilevers when loading the tool will be within the failure limits of the etched single-crystal silicon [119]. Each batch is designed to make 20 cantilevers of 3 different stiffness values. The dimensions of the cantilever, specifically, the length and width are dictated by the aspect ratio of the strain-gauge sensor design. The width of the meso-scale cantilever was chosen such that there is a sufficient surface area to place the strain-gauge sensor. The width was set to a base value of 1mm, proportional to the length and then fine-tuned for the three different types of cantilevers to match the design stiffness. The thickness of the cantilever was kept constant at 140 µm based on the total base wafer thickness available for etching. Based on the chosen width and thickness values and the desired stiffness K, the length of the meso-scale cantilever (L) was calculated using the force-deflection formula for a concentrated free-end loading on a cantilever beam:

\[
L = \sqrt[3]{\frac{3EI}{K}},
\]  

(3.1)
where, $E$ is the Young’s modulus for Silicon, $I$ is the moment of Inertia and $K$ is the design stiffness of the meso-scale cantilever. Table 3.3 lists the dimensions of the three cantilevers with varying stiffness values.

**Table 3.3: Meso-scale cantilever dimensions**

<table>
<thead>
<tr>
<th>Type</th>
<th>Tool Stiffness (N/m)</th>
<th>Length (mm)</th>
<th>Width (µm)</th>
<th>Thickness (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>1070</td>
<td>5.0</td>
<td>900</td>
<td>140</td>
</tr>
<tr>
<td>B</td>
<td>1140</td>
<td>4.9</td>
<td>950</td>
<td>140</td>
</tr>
<tr>
<td>C</td>
<td>1390</td>
<td>4.8</td>
<td>1050</td>
<td>140</td>
</tr>
</tbody>
</table>

In order to understand the behavior of the designed meso-scale cantilever under the varying load conditions, stress analysis studies were carried out based on the Euler-Bernoulli beam theory [120]. A uniform, inelastic, static beam loading condition was used in the calculations for comparing a commercially available AFM sapphire and the designed meso-scale cantilever. The maximum surface stress, $(\sigma_{\text{max}})$ experienced by the cantilever for end-point loading is given by:

$$\sigma = \frac{3E.y.t_c}{2L^2},$$

(3.2)

where $E$ is the Young’s modulus of elasticity, $y$ is the deflection for a given end-point load, and $t_c$ and $L$ are the thickness and length of the cantilever respectively. The deflection of the cantilevers subjected to end-point loading, $P$ is given by:

$$y = \frac{P.L^3}{3E.I},$$

(3.3)
Figure 3.9 shows the maximum surface stresses calculated using incremental cutting loads that both AFM sapphire and new meso-scale cantilevers experience during the groove cutting process. The graph also shows the failure stresses values for both of these cantilevers. It can be seen from figure, the AFM sapphire cantilever experiences greater stress as compared to the meso-scale cantilever at a given load condition. In fact, for the meso-scale cantilever the maximum stresses are uniformly distributed over a wide range of cantilever deflection and are well below the failure stress level of etched silicon, i.e., 300 MPa.

**Figure 3.9:** Tool failure stress analysis

3.4.2 Strain-Gauge Sensor Design

Several options for strain-gauge-based sensor have been investigated. In particular, piezoresistive strain-gauges, metallic foil strain-gauges including both the bonded types and thin-film-based types were considered. A metallic planer foil-type resistive pattern
was chosen for the strain-gauge sensor for its simple design, repeatability, low-hysteresis effects, minimal drift changes and linearity over the measurement range [120-121].

Due to its high fatigue life, thermal stability, high resistivity and adjustable strain sensitivity, Ni-Cr was chosen to be suitable for the strain-gauge sensor material. It has a Gauge Factor of 2.2 and has one of the lowest thermal coefficients among other strain-gauge materials and a high electrical resistivity, which makes it an ideal material for the strain-gauge sensor. The basic circuit considerations requires the nominal gauge resistance of the foil grid design to be high enough to minimize the self-heating due to the gauge current and desensitization due to the output lead wires being in series with the foil grid unit.

For most typical industrial applications, the nominal gauge resistance of strain-gauge sensors varies between 100Ω - 5kΩ. For this application, a nominal gauge resistance value of 10kΩ was chosen. A large resistance value guaranties that there is a sufficient effective gauge length ‘Le’, over which the cantilever deflection occurs. A higher effective gauge length increases the strain resolution and ensures a high frequency response of the measured voltage signal. The rectangular cross-section of the grid ensures effective strain transmission from the cantilever to the foil. The width of the foil grid was chosen as 30 µm to ensure that (a) the aspect ratio (L_e/w) is greater than 100 and (b) it is greater than the maximum resolution achievable in the optical photolithography process. A high aspect ratio also improves the sensitivity of the sensor that is necessary to measure small changes in resistance per unit resistance (∆R/R) for very small changes in strain observed during the micro-groove cutting process. The thickness of the foil grid was set to 140 nm to accommodate the effective gauge length. Due to design constraints,
the width of the foil grid, being two orders of magnitude greater than the thickness, can
induce transverse strain in the gauge resistor. However, the end-loop provisions
compensate for this behavior. The complete design of the metallic foil-type resistive
strain-gauge sensor is shown in Fig. 3.10(A). The effective gauge length, ‘\(L_e\)’, is
determined by nominal gauge and cross-sectional area using the following relationship:

\[
L_e = \frac{R_w t}{\rho},
\]

(3.4)

where \(R\) is the nominal gauge resistance, \(\rho\) is the resistivity of grid material and \(t\) and \(w\)
represent the thickness and width of the foil grid, respectively. Using Eq. (3.4), the
effective gauge length was calculated as 30 mm. The metallic foil-type strain-gauge
sensor is arranged in a serpentine grid fashion as shown in Fig. 3.10 (B). The serpentine
grid arrangement increases the surface contact area of the gauge resistor and
permits heating due to the current flowing through the grid to be dissipated more readily.
As seen in Fig. 3.10(B), the planer serpentine arrangements introduces opposing current
directions in adjacent lines that reduces mutual inductance, while geometry-related inter-
line capacitances in series reduces overall capacitance. Both inductance and
capacitance produce reactance proportional to the operating frequency that changes
the effective resistance and the phase between the current and voltage in the circuit
[123].
In order to determine the number of serpentines, which has to be an integer number, the active grid length, ‘b’ and grid spacing ‘g’ were to be chosen. The active grid length is defined as the total center line length of the serpentine resistor. Based on the calculated effective gauge length value and the available surface area of the meso-scale cantilever, the active grid length was set to 3mm. The minimum grid spacing, “g”, was chosen such that the spacing between the running grid lines ensured there is no pattern distortion during the subsequent fabrication steps. The grid spacing value was set to 25 μm to satisfy the requirements mentioned above. For strain-gauge sensors with a large aspect ratio (L/w>100), the centerline approximation formula gives a reasonably accurate estimation of the number of serpentines, n:

\[ n = \frac{L_e + g}{b + g}, \]  

(3.5)

where \( L_e \) is the effective gauge length, g is the minimum grid spacing and b is the active grid length. The number of serpentines was calculated using Eq. (3.5) to be 10. The overall design parameters of the strain-gauge sensor are summarized in Table 3.4.
Table 3.4: Strain-gauge sensor design parameters

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Center Line Length (b)</td>
<td>3 mm</td>
</tr>
<tr>
<td>Active Length (Le)</td>
<td>30 mm</td>
</tr>
<tr>
<td>Grid Width</td>
<td>30 μm</td>
</tr>
<tr>
<td>Grid Space (g)</td>
<td>25 μm</td>
</tr>
<tr>
<td>Overall Length</td>
<td>7.5 mm</td>
</tr>
<tr>
<td>Gold Contact pads</td>
<td>2.5 mm²</td>
</tr>
<tr>
<td>Serpentines (n)</td>
<td>10</td>
</tr>
<tr>
<td>Nominal Resistance</td>
<td>10 kΩ</td>
</tr>
</tbody>
</table>

3.5 MEMS-based Fabrication Process

A Micro-Electro-Mechanical-Systems (MEMS)-based fabrication process was implemented to fabricate the new flexible cutting tool blanks. The MEMS-based fabrication process was implemented to fabricate the new flexible cutting tool blanks. The fabrication process integrates an active strain-gauge sensor on the top side of the cantilever beam, provides a rectangular substrate base to handle the entire tooling system and also to house the passive strain-gauge sensor. The passive strain-gauge sensor positioned on the base of the tool substrate is for temperature compensation.

3.5.1 Fabrication Consideration

A highly doped (p-type, <100>, DSP) single Silicon crystal wafer was chosen as the device base platform. The nominal thickness of the base wafer was 300±25 μm. The design parameters derived in Section 3.4.1 and 3.4.2 were used to develop the chrome masks that defined the pattern scale of the strain-gauge sensors for the optical photolithography step of the fabrication process. The resistor material under
consideration, Ni-Cr with a Gauge Factor (G.F) of 2.2 offers a high resistance value over a short gauge length, as used over the cantilever surface. Since the fabrication platform was a highly doped Si surface, a commonly used dielectric layer Silicon Nitride (Si$_3$N$_4$) is introduced to isolate the current paths flowing through the resistors of the strain-gauge sensor. Si$_3$N$_4$ is also used as a protective film over the top of the sensor region to ensure the chips generated during the machining did not affect the output from the sensor. The process was planned to fabricate three types of meso-scale cantilevers with different tool stiffness values. This was achieved by modifying the length and width of the cantilever boundaries across the mask pattern definition as explained earlier. The dry etching procedure was preferred over wet etching, as it provided better etch rates (quicker production times); anisotropic etch profile (vertical walls) on Silicon, high uniformity to maintain dimensional accuracy of the device and lower loading effects on the chrome pattern masks.

3.5.2 Process Outline

The major steps involved in the MEMS-based fabrication process include [124]:

1. Creation of Chrome Photomasks;
2. Plasma-Enhanced Chemical Vapor Deposition (PECVD);
3. Photolithography;
4. Sputtering (Physical Vapor Deposition(PVD));
5. Lift-off principles for Material Removal;
6. Reactive Ion Etch (RIE);
A. Design of Chrome Photomasks

Definition of feature pattern and mask fabrication is the first step towards realization of the strain-gauge sensor and cantilever patterns on Si wafer substrate. Using CAD tools, the photomasks were generated on a chrome-based flat glass plate to be used in optical photolithography. Each photomask shown in Fig. 3.11 represents an image of subsequent processing layer of the process. All the masks used in the process were a 5”x5” square chrome masks. Each mask also had alignment marks included at the corner, to help in alignment of multiple pattern features on top of one another during the course of fabrication. The features of interest in Figs. 3.11(A) and (B) appear in white (light) shade, while the features in Figs. 3.11(C) and (D) appear in black (dark) shade. The opaque region (dark area) represents the chrome on the glass plate that doesn’t allow light to pass through. The inverse polarity as seen between the two set of masks is based on the type of photoresist material employed during the exposure of each of these masks. The most important design considerations for the mask fabrication included a high degree of optical transparency at the exposed wavelength, a highly flat and polished surface that reduces scattering effects of light, and lastly, the critical dimension (CD) - the size of the smallest feature size on the mask pattern, which also defines the mask resolution. The CD possible during the photolithography process was 25 µm and this was reflected in the minimum grid spacing value chosen for the strain-gauge sensor design.
Figure 3.11: (A) Strain-gauge sensor pattern; (B) Gold contact pad pattern; (C) Front side of the meso-scale cantilever; and (D) Back-side of the meso-scale cantilever

The image of chrome mask pattern is projected onto the wafer surface via a coated thin photosensitive material known as photoresist (PR). A positive tone photoresist, SPR 220, was used to pattern the strain-gauge sensor and gold contact pads seen as seen in pattern design in Figs. 3.11(A-B). The dark areas that were not exposed to ultra-violet (UV) radiation retained the PR layer, while the white shades (transparent) that were exposed to the UV radiation developed, releasing the PR. The exposed region makes the resist soluble, which is removed by the developer. The selectively removed areas form the openings on the wafer to be deposited with material of choice.
A negative tone PR, KMPR 1010 used as an etch mask defined the meso-scale cantilever and tool tip mounting boundaries as Figs. 3.11(C-D). Similar to the positive tone design, the black shade represents opaque, unexposed areas, while the white portion represents the exposed areas. Based on the chemical nature of the negative photoresist material, white areas – that were exposed to UV are insoluble in the developer and retained the PR while the areas that were not exposed (dark regions) dissolved the PR, to allow the etch channel to be established.

**B. Photolithography**

As outlined in the fabrication consideration section, a layer of Si$_3$N$_4$ was first deposited on the Si surface using PECVD technique with the STS Mixed-Frequency Nitride PECVD equipment at 300 C. This provided a dielectric base for the remaining steps of the fabrication, which is followed by the photolithography process. Photolithography is the most recurring step in the fabrication process and used whenever there is a new pattern to be built on the substrate base. The steps involved in photolithography include (1) Wafer Surface Preparation; (2) Photoresist Application; (3) Soft bake of the coated wafer; (4) Mask alignment and UV Exposure; (5) Development of the patterned wafer; (6) Hard Bake; and (7) Resist Removal.

Wafer surface preparation, included degreasing of wafer by thorough rinsing of wafer with Acetone, IPA and de-ionized water sprayed on either side of wafer, in that order. It was followed by nitrogen blow-off done at an angle, to dry the wafer surface. Degreasing was followed by descumming, which was done using low vacuum plasma for about 1 min at 100W power, to remove any other liquid contaminants present the surface.
A positive tone PR, SPR 220 was spin-coated using a high-speed spinner bench. Prior to spinning the positive tone resist an adhesion promoter, AP8000 was spun on the wafer to improve the adhesion between the PR and Si. The coated wafer was then soft baked for 2 min at 60°C and at 100°C for 1min to improve adhesion and partial evaporation of resist solvent. This step also optimized line-width control of the photosensitive material. Then, the coated wafer was placed in the contact mask aligner system loaded with the Chrome mask to be selectively exposed to 405nm near UV light (H-line) for 12s. Figure 3.12(A) shows the schematic of the photolithography process and 3.12(B) shows the double-sided mask aligner equipment used during the photolithography step. The mask and the wafer were set to a predetermined separation of 50 µm between them to enable a complete transfer of the pattern. The exposed Si wafer was then developed for 2min in the AZ400K developer (5:1 /Developer: DI water) to dissolve the unwanted PR material. The hard bake step done was done at 110°C for 2mins to improve the uniformity and adhesion of the remaining PR on the substrate.

**Figure 3.12:** (A): Schematic overview of Photolithography process; 9(B): The H-Line-based Double- Sided Mask Aligner
C. Sputtering Deposition (PVD) and Lift-Off

The patterned Si wafer was deposited with Ni-Cr (and Au in the next sequence of photolithography steps) by the AJA ATC Orion Series Sputtering system, based on Physical Vapor Deposition (PVD) principle that worked as a parallel-plate plasma reactor set up in a vacuum chamber in the presence of a magnetic field. At low pressure, a voltage is applied to two parallel electrodes resulting in a plasma discharge. The accelerated gas ions impinge onto the cathode (target) and metal atoms are emitted from the target, which deposit on the wafer leading to layer growth as shown in Fig. 3.13(A). The Si wafer material formed the anode that was bombarded with the target species (Ni-Cr) accelerating in the presence of Ar\(^+\) ions towards the substrate. The operating vacuum pressure was set to \(10^{-5}\) T and the rate of deposition of Ni-Cr to 140 A/min. As calculated from the required design parameters, a 140nm thick strain-gauge sensor pattern was desired to achieve the required nominal resistance. Prior to the deposition of gold contact pads (Au), a 30s Chromium deposition was done on the target substrate (Si Wafer) to improve the adhesion property of gold on silicon. A 3 min deposition process was followed for the gold contact pads at the rate of Au 240 A/min. Figure 3.13(B) shows the Sputtering Chamber set up used for the deposition process.

Lift off refers to the process of selective removal of sacrificial layer for patterning materials that cannot be etched without affecting underlying materials on the substrate. The process takes advantage of the fact that step coverage of thin film deposition method is limited and material from the edges of the pattern (steep or undercut steps) can be easily removed. The photoresist (PR) and unwanted metal deposition was removed using this principle. The sputtered wafer was placed in 1165 PR stripper bath and was severely
sonicated in an ultrasound for 15 min until all the metal ions and PR was completely dissolved away from the wafer. The wafer was immediately degreased, descummed and ready for the next step.

![Figure 3.13: (A) Schematic of sputtering; (B) AJA sputtering equipment](image)

**Figure 3.13:** (A) Schematic of sputtering; (B) AJA sputtering equipment

**D. Reactive Ion Etching of Nitride**

Following the PVD process, the patterned device was coated with a 50nm layer of Si₃N₄ dielectric layer to provide a protection for the underlying sensor. The nitride was then selectively etched using the AXIC Reactive-Ion Etching system (RIE) equipment, where the wafer is placed in an enclosed low vacuum plasma chamber and etched anisotropically under the assistance of fluorinated plasma gas of CF₄ and O₂ at Radio Frequency (RF) power of 100 W and operating pressure of 100mT for 5-6 min. A change in color of the plasma, when viewed through the chamber window indicated the completion of nitride removal process on the patterned Si wafer.
E. Deep Reactive Ion Etching (DRIE) of Silicon

Dry etching of Si was preferred over the conventional chemical wet etching as it yields better selectivity, higher etch rates and an anisotropic etch as pointed out in section 3.5.1. These factors make the plasma-assisted dry etching more repeatable than wet etching principles. The devices were coated with a negative tone PR, KMPR 1010, which act as an etch mask for the etching process. The negative tone PR defines the etching channel along the design dimensions of the tool blank. For the purpose of Si etching, inductive-coupled plasma (ICP)-assisted DRIE procedure was implemented. Figures 3.14(A) and (B) shows the schematic configuration of the ICP-DRIE process and the DRIE equipment used for the process, respectively. The ICP source was made with an antenna connected to an RF power supply and wrapped around an alumina cylinder. RF power was then coupled to the plasma through an inductive mode. The oscillating current in the antenna at 13.56 MHz induced an electromagnetic field in the alumina cylinder, which now provided the system to control the plasma, both electrically and magnetically.

The ICP-DRIE also called as the “Bosch process” alternates repeatedly between these two modes to achieve the required vertical structures. The first step was the etch step, done using etch gas SF\textsubscript{6} to etch the silicon for a limited depth. The second step was a deposition step done using C\textsubscript{4}F\textsubscript{8} to form chains of a Teflon-like polymer, which is deposited on the surface of the wafer that doesn’t react with the etch plasma. The removal of the polymer coating before the start of the next etch step was done using the ions in the plasma accelerated by the electric field. By switching back and forth between etch and deposition plasma, the silicon is etched in an anisotropic fashion to the desired etch depth.
The etch rates were calibrated for the defined etch channel width (100µm) whose dimensions were dictated by the design on the chrome photomask in the photolithography stage. The rate of etching obtained was 0.4-0.6 µm/min. Both sides of the Si wafer were subjected to the DRIE in order to achieve the required thickness. The top side etch was done to the depth equal to the required cantilever beam thickness, while the back side etch was done till the entire tool blank was released from silicon wafer.

Figure 3.14: (A): Process configuration of ICP-DRIE mechanism; (B) Plasmatherm ICP-DRIE equipment

The released devices were then transferred to an 1165 PR stripper bath for a period 10-12 hours to strip away the KMPR from the meso-scale cantilever tool blank surface and remove any other form of contamination, before testing and characterization steps are carried out. The entire process as described in this section is outlined in the Figs. 3.15(a-h) and the SEM image of the fabricated meso-scale cantilever is shown in Fig. 3.16.
Figure 3.15: Process sequence for MEMS-based cantilever fabrication
3.6 Meso-Scale Cantilever Characterization

The meso-scale cantilevers are tested for obtaining the maximum loading condition at mechanical failure using the test bed set-up as shown in Fig. 3.17. The meso-scale cantilever is mounted on a 3-axis Microlution 310S mMT and deflected progressively at its tip by a known amount, as indicated by an optical encoder, using a micro-end mill along the z-axis as shown in Fig. 3.18. The encoder position is set to zero, at the point where the endmill makes contact with the cantilever surface, which is known as touch-off point, shown as Fig 3.18(A). The progressive loading beyond touch-off point causes cantilever to deflect as shown in the following Fig 3.18(B-D), where the amount of deflection is stated as a function of the z-axis encoder count. The theoretical maximum load is calculated from the calculated deflection and stiffness values using the spring-force equation. Table 3.5 summarizes the results and lists the deflections found at
failure load with an experimental error of $\pm 5\mu m$, which is the encoder resolution of the mMT. As can be seen from table, the new meso-scale cantilevers ($K = 1070 N/m$) can withstand cutting loads up to 500 mN until failure, which is more than 50 times greater than failure cutting loads for the AFM sapphire cantilevers used earlier (Refer Table 3.2).

![Figure 3.17: Experimental set-up for testing of meso-scale cantilever](image)

**Figure 3.17:** Experimental set-up for testing of meso-scale cantilever

![Figure 3.18: Progressive loading at the tip of the meso-scale cantilever (Type C):](image)

(A) at $z = 0 \mu m$ (Touch-off); (B) at $z = 350 \mu m$; (3) at $z = 500 \mu m$; and (4) at maximum loading condition, $z = 770 \mu m$. 

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Table 3.5: Meso-scale cantilever characterization parameters

<table>
<thead>
<tr>
<th>Type</th>
<th>Tool</th>
<th>Stiffness (N/m)</th>
<th>Max. deflection (µm)</th>
<th>Max. Loading (mN)</th>
<th>Resonant Frequency (KHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td></td>
<td>1070</td>
<td>470</td>
<td>502</td>
<td>6.85</td>
</tr>
<tr>
<td>B</td>
<td></td>
<td>1140</td>
<td>680</td>
<td>727</td>
<td>7.14</td>
</tr>
<tr>
<td>C</td>
<td></td>
<td>1390</td>
<td>770</td>
<td>1007</td>
<td>11.76</td>
</tr>
</tbody>
</table>

In order to ensure dynamic stability and to avoid undesirable vibrations while cutting micro-grooves, the resonant frequency of the meso-scale cantilever needs to be at a different frequency regime compared to the natural frequency of a micro-Machine Tool (mMT). The resonant frequency, ‘f₀’, of the fabricated meso-scale cantilevers is calculated using the relationship:

\[
f₀ = \frac{1}{2\pi} \sqrt{\frac{3EI}{mL^2}},
\]

where \(E\) is the young’s modulus of Si, \(I\) is the moment of Inertia, \(m\) and \(L\) are the mass and length of the cantilever, respectively. The values of the resonant frequencies are summarized in Table 3.5. As can be seen from table, these frequencies are well above the natural frequency of a typical mMT. (Eg., for the 5-axis mMT, the natural frequency is 114 Hz [125])
3.7 Strain-Gauge Sensor Performance Evaluation

The performance of the strain-gauge sensor was evaluated to measure: (a) Electrical conductivity; (b) Gauge factor and (c) Non-linearity. Electrical conductivity tests of the strain-gauge sensors were performed using a digital multimeter on a probe testing station, to verify the design nominal gauge resistance value of 10kΩ - for each of the active and passive sensor present on every fabricated meso-scale cantilever. In a batch of 20 cantilevers, the fabricated sensors were found to have an average nominal resistance of 9.7kΩ, with a standard deviation of ±400Ω.

Gauge factor (G.F) is the function of the basic strain sensitivity of the alloy (Ni-Cr) from which the foil grid is manufactured and is mathematically defined as:

\[
G.F = \frac{R_\varepsilon - R_0}{R_0 / \varepsilon}, \quad (3.7)
\]

where \( R_\varepsilon \) and \( R_0 \) denote the resistance of the strain-gauge sensor at strain \( \varepsilon \) and at zero strain, respectively. In order to calculate G.F, the theoretical change in strain (\( \varepsilon \)) on the meso-scale cantilever due to the applied load is calculated using:

\[
\varepsilon = \frac{1}{x} \int_0^x P \left( \frac{L - x}{E.I} \right) \, dx, \quad (3.8)
\]

where \( P \) is the load applied on the tip of cantilever, \( L \) and \( I \) is the length and moment of inertia of the cantilever, \( E \) is the young modulus of elasticity, \( \bar{y} \) is the distance of the neutral axis from the base of the cantilever, \( x \) is the distance between the midpoint of the strain-gauge sensor from the point of application of force. The resistance of the
strain-gauge sensor was measured at zero strain ($R_0$) and at an arbitrary value of 10.2 µm deflection ($R_\varepsilon$) of the meso-scale cantilever applied by pushing against an alumina artifact using a 5-axis mMT. Meso-scale cantilever deflection was converted to strain using a conversion factor of 7.5 µε per µm derived from Eq. 3.8. The gauge factor for the fabricated strain-gauge sensor was measured to be 1.95 at room temperature. The value followed closely to the Ni-Cr design G.F of 2.2.

Non-linearity is a dimensionless measure of the deviation of a single measured output from the strain-gauge sensor from the expected output based on a linear approximation of the collection of measured data points across multiple strain levels [126]. It is computed by taking the difference between the measured output and the linear approximation at that strain level, and dividing it by the full-scale range of the linear approximation. It can be computed as:

$$\text{Non linearity} = \frac{R_2 - R_1}{R_{\text{max}} - R_{\text{min}}} \times 100\%,$$

(3.9)

where $R_1$ is the actual resistance at strain $\varepsilon_1$ and $R_2$ is the resistance from the straight line that is fitted with the actual upper value $R_{\text{max}}$ and lower value $R_{\text{min}}$ at strain $\varepsilon_1$. To measure maximum nonlinearity, the strain-gauge sensor was tested using the same experimental setup as used to measure G.F. Using the 5-axis mMT, the cantilever was deflected against an alumina artifact in various amounts over a range of approximately 0-10 µm (corresponding to 0-75 µε). Maximum non-linearity over the range of measurements was computed to be 1.48%, which lies in the range of most of the metal strain-gauge sensors. The variation of non-linearity over the range of meso-scale cantilever deflection is shown in Fig. 3.19. It can be seen from the figure that the non-
linearity effects to be under 0.5% beyond 8 μm, which is within the operating range of the meso-scale cantilever.

![Graph showing non-linearity as function of meso-scale cantilever deflection](image)

**Figure 3.19:** Non-linearity as function of meso-scale cantilever deflection

A Wheatstone bridge circuit was employed to measure the change in resistance values on the strain-gauge sensor for the micro-groove cutting experiments. As mentioned in Section 3.5.1, the MEMS-based fabrication process also accommodated a passive strain-gauge sensor on the substrate side of the meso-scale cantilever for temperature compensation. During its typical operation, the strain-gauge sensor might undergo changes in its resistance due to changes in temperature. This affects the linearity of the balanced Wheatstone bridge, causing an unbalanced condition and thus, inducing a measurement error. To mitigate this effect and maintain the bridge in linear operating condition, a passive strain-gauge sensor was introduced in the design. Figure 3.20(A) and (B) shows the circuit diagram for the Wheatstone bridge configuration implemented for this study and the CAD representation of the two sensor system used in the meso-scale cantilever, respectively.
Figure 3.20: (A) Half-bridge Wheatstone configuration and (B) CAD representation of the active and passive strain-gauge sensors

3.8 Tool Tip Mounting & Geometry Definition

The meso-scale cantilever fabricated provides the flexible tool blank upon which the cutting tool tip is to be mounted and the groove cutting geometry created. Due to large size of the single CBN crystal, whose size varies between 450-600 µm as shown in Fig. 3.1, the geometry definition is carried out in two steps. In the first step, known as the coarse tool tip modification step, the irregularly shaped single CBN crystal is modified such that a suitable tool tip post is created in the size range of 50 x 100 x 50µm³. The modification provides an ideal working volume for the next step - the fine tool tip modification, for creating the exact micro-groove cutting geometry via focused ion beam (FIB) machining process.

3.8.1 Coarse Tool Geometry Modification

In this section, two specific techniques to coarsely modify the mounted CBN crystal are discussed – (1) Laser milling and (2) Wire-EDM.
Laser Milling

Laser milling involves applying laser energy to remove material through ablation in a layer-by-layer fashion. The single CBN crystal is attached to the tip of the meso-scale cantilever by means of Aquabond thermal adhesive. To scan the CBN crystal the laser spot is moved in the slice plane by a pair of computer-controlled mirrors and focusing is achieved through a movable lens. A continuous Nd:YAG (neodymium-doped yttrium aluminum garnet) solid-state laser producing a collimated coherent beam in the near-infrared region of wavelength 1064 nm is used for ablating the material. The minimum beam spot size achievable is 30 µm. The schematic of the process is shown in Fig. 3.21

![Schematic of the laser-milling process](image)

**Figure 3.21:** Schematic of the laser-milling process

The CAD diagram defines the path for the laser beam. For this application, a series of concentric circles in the size range of the spot size is fed into the controller, which is used to ablate material based on the run-cycles (execution time for one program). Typically 5-10 run cycles resulted in close to 100 µm depth (z-axis, along the direction of the beam) of material removal. Figure 3.22 (A) shows the mounted CBN crystal on the meso-scale flexible cutting tool. Figure 3.22 (B) shows the intermediate stages of the CBN crystal being milled by the laser beam leading to a tool tip post being created at its
center as shown by the boxed region in Fig. 3.22(C), which provides the target site for exact groove geometry definition performed by the fine modification step.

Figure 3.22: (A) CBN crystal attached on tip meso-scale cantilever, (B) Laser-milling spots on the crystal and (C) Tool tip post creation for FIB

Wire-EDM Process

In the wire-EDM approach, the single CBN crystal is modified using the electric spark created by the 100 μm single-strand brass wire. The single CBN crystal is placed in a pocket of an aluminum stub that acts as the carrier for the crystal. All the Wire-EDM machining is done on the Al stub after which it is attached to the cantilever tip. First, the CBN crystal is placed in the aluminum stub pocket, filled with a conductive silver epoxy and cured at 70 °C for over 5 hours as shown in Figs. 3.23(A) and (B), respectively. This creates a conductive path for the wire travel. To find the exact location of the crystal inside the epoxy matrix and estimate the wire-EDM machining coordinates, the Al stub was scanned using X-ray microtomography (Micro-CT). Micro-CT scans constructed 2D cross-sections of the 3D matrix-crystal object at 1 μm resolution in X-Y, Y-Z, Z-X planes. Figures 3.24(A) and (B) show the top and side views from the selected CT slice scan,
respectively. Next, the Al stub is placed in the wire-EDM machine on a rotating spindle as shown in Fig. 3.24(C). The wire is fed into the job in the X-Y plane and machined accordingly. Figures 3.24 (D-F) shows the intermediate cutting sequence and the resultant mounting of the Al stub carrying the modified CBN crystal tool tip post attached to the tip of the meso-scale cantilever. The modified CBN tool tip post serves as the target site required for the fine geometry modification step.

Figure 3.23: (A) CBN-Ag matrix inside Al stub pocket and (B) Curing process

Figure 3.24: Tomograph Slice showing :(A) Top view and (B) Side view; (C) Wire-EDM process; (D) Intermediate stage in the Al stub machining process.
The laser-milling process has very high turn-over time due to the high beam intensity of the laser and high material removal rate. However, the thermal-based laser interaction with CBN and heat diffusion into the surrounding material regions tends to induce mechanical stresses and micro-cracks in the nearby region. There is also debris and plumes released near the heat affected zones, which needs further cleaning. The wire-EDM method is more controlled in material removal than the laser-Milling process. While it doesn’t introduce local heat affected zones as in laser-milling process, there is a greater lead time associated with the process before the CBN tool tip post is ready for further FIB machining. Furthermore, the total tool tip post area available for FIB-based fine geometry definition is limited by the amount of material lying above the aluminum stub. Therefore, there is limited scope of reshaping and reusing the tool tip when using the wire-EDM approach. For this research study, the laser-milling method is chosen for the coarse geometry modifications.
3.8.2 Fine Tool Geometry Modification

Following the coarse tool tip modifications, the required micro-groove cutting geometry is created using fine tool geometry modification step, achieved using the FIB machining technique. Figure 3.25(A-B) shows the cutting edge and side clearance faces of the machined groove cutting geometry. The details of the FIB machining steps can be found in [106]. Table 3.6 summarizes the characteristics of the cutting tool tip.

![Figure 3.25](image)

**Figure 3.25:** (A) Cutting edge of the tool Rake face profile (inlet); (B) Top view showing the clearance faces

**Table 3.6:** Cutting tool tip properties

<table>
<thead>
<tr>
<th>Description</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tip Width</td>
<td>2.45 µm</td>
</tr>
<tr>
<td>Tip Length</td>
<td>12 µm</td>
</tr>
<tr>
<td>Rake Profile</td>
<td>Rectangle</td>
</tr>
<tr>
<td>Tip Height</td>
<td>100 µm</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>75 nm</td>
</tr>
<tr>
<td>Max Depth of Cut</td>
<td>5.16 µm</td>
</tr>
<tr>
<td>Back Rake angle</td>
<td>20 deg</td>
</tr>
<tr>
<td>Side Relief Angle</td>
<td>14 deg</td>
</tr>
</tbody>
</table>
3.9 Tool Packaging

The meso-scale cantilever is packaged on a UV-curable photopolymer resin (Phototherm 12120) base that was built using Stereo Lithography Apparatus (SLA). The SLA is a rapid prototyping technique that is used to create high-temperature tolerant and insulating base for mounting the meso-scale flexible cutting tool substrate. The package provides portability; ease in handling of the tooling system during set-up, experimentation and post-experimental analysis. An aluminum lead frame was glued along on the perimeter of the meso-scale cutting tool that served as a channel for electrical connectivity between the signal conditioner and the strain-gauge sensor. The schematic of the package is shown in Fig. 3.26(A). A 30 µm diameter gold wire was used to connect the gold contact pads to the Al lead frame using the Kulicke & Soffa 4524AD Ball Bonder. This completed the packaging of the meso-scale CBN flexible cutting tool, as shown in Fig. 3.26(B).

![Image](image_url)

**Figure 3.26:** (A) Meso-scale CBN flexible cutting tool package, and (B) Gold wire-bonding
3.10 Hardware Interface

The meso-scale cutting tool package was mounted on to a tool holder bar through screws. 4-pin connectors are used to attach the channels coming out of the aluminum lead frame as shown in Fig. 3.27(A). The strain-gauge sensors integrated on the meso-scale CBN flexible cutting tool is powered by the VMM 2310A signal conditioning amplifier. The signal conditioning amplifier provides the source voltage and completes the Wheatstone circuit for the strain-gauge sensor unit. The output from the amplifier was connected to a Delta Tau ACC-28E 16-bit analog to digital converter board housed in the Delta Tau Turbo UMAC machine controller (PMAC 2 Turbo) that runs on a fully programmable open architecture. The change in voltage of the strain-gauge sensor during the calibration, workpiece registration and the micro-groove cutting operation is monitored via a digital oscilloscope. Specifically, the UMAC controller reads three analog outputs from the strain-gauge sensor using two 16-bit analog to digital (A2D) converter channels: an output voltage proportional the measured displacement and an output voltage flag indicating a new reading. The sample rate is 5000 Hz, which is the servo cycle update frequency of the controller. The experimental set-up used for micro-groove cutting is shown in Fig. 3.27(B). A digital 4th order Butterworth low pass filter with a cutoff frequency of 50 Hz is used for removing noise from the sensor output reading. A 100 nm threshold value was set in the program as a benchmark for detection of the flexible cutting tool deflection. The other feedback and electronics settings were consistent with that used in [106]. A secondary sensor in the form of an existing confocal laser displacement sensor is used to calibrate the strain-gauge sensor.
Figure 3.27: (A) Tool holder bar with the meso-scale flexible CBN cutting tool package, and (B) Experimental set-up for micro-groove cutting

3.11 Summary

This chapter described the design and fabrication of the meso-scale flexible cutting tool capable of machining micro-grooves in steel. The designed tool was significantly stiffer and rigid than the existing AFM sapphire cantilevers and was essential in order to cut deeper micro-grooves. A micro-electro-mechanical system (MEMS)-based fabrication process approach was chosen to fabricate the tools. The new flexible cutting tool was integrated with a strain-gauge sensor that provided the dynamic feedback control to maintain the cantilever deflection during the micro-groove cutting process. A new tool tip material, single cubic boron nitride (CBN) crystal was chosen to replace the single diamond crystal, which was unsuitable for machining steel due to the graphitization issues. Novel approaches for coarse and fine tool tip modification of the CBN crystal were discussed that were needed to introduce the required micro-groove
cutting geometry. A compact packaging platform was built to handle the portability of the strain-gauge-integrated meso-scale CBN flexible cutting tool. Preliminary testing for the cantilever design and characterization of the strain-gauge sensor was carried out to verify the design and performance.

The next chapter deals with the cutting performance evaluation of the meso-scale CBN flexible cutting tool. A series of experiments are carried out that evaluates the effectiveness of the strain-gauge sensor-integrated flexible cutting tool to machine high-quality micro-grooves in steel.
Chapter 4

Performance Evaluation of Meso-Scale CBN Flexible Cutting Tool

4.1 Introduction

The desire to create highly accurate and consistent micro-grooves in hard and heterogeneous materials such as steel led to design and development of the meso-scale CBN flexible cutting tool. Specifically, the newly developed flexible cutting tools are intended to address the limitations in existing flexible AFM cantilever-based cutting tools, including the ability to produce micro-grooves deeper than 1 µm on hard surfaces, replicate them on a large area while maintaining the required tolerance and relative accuracy, create curvilinear and other arbitrarily-shaped groove patterns on flat and curved topographies and produce them in an inexpensive and economical manner for mass-scale pattern fabrications. This chapter aims at evaluating the performance of the newly developed tools to meet those defined objectives.

The chapter commences with the demonstration of the micro-groove cutting ability of the meso-scale CBN flexible cutting tool in stainless steel. Next, the overall micro-groove quality machined using the new flexible cutting tool is studied through a set of defined criteria including expected profile, dimensions, consistency and repeatability. In the following sub-section, the side burr formation that affects the tolerance and surface
integrity of the machined micro-grooves is analyzed through a series of experiments. Next, the tool wear analysis is carried out to understand the performance of the CBN tool tip to machine micro-grooves efficiently over large distances. In the last section, the ability of the new meso-scale CBN flexible cutting tool to create patterns with complex groove geometries, which otherwise is not possible using the AFM sapphire cantilevers is demonstrated.

4.2 Micro-Groove Cutting in Stainless Steel

In order to assess the ability of the meso-scale CBN flexible cutting tool, micro-grooves were cut in austenitic stainless steel 303 (SS 303). The SS303 samples for all the experiments were metallographically prepared to an average surface roughness (Ra) of 20-25nm. The procedure for polishing the sample to the required surface finish is provided in Section 3.2.1. The primary objective of this study was to experimentally evaluate the depth of cut that can be achieved using a fixed cutting load on the tip of the newly-developed cutting tool.

Experimental Procedure

The micro-groove cutting experiments are carried out on a 5-axis mMT with a retrofitted scribing assembly that holds the meso-scale CBN flexible cutting tool. The tool is mounted to the tool holder bar such that the axis of the cantilever nearly aligned with the x-axis of the mMT. From the beginning of every experiment, the meso-scale CBN flexible cutting tool is packaged on a SLA base along with FIB-machined cutting edge geometry, as explained in Chapter 3. The experimental procedure begins with the
cutting edge detection of the flexible cutting tool, which essentially registers the cutting edge of the tool to a known location on the workpiece surface. This is followed by the generation of a calibration curve relating the tip displacement to a strain-gauge voltage output. Mathematically, it relates the deflection of the cutting tool to the cutting load, which in turn enables precise control of the required depth of cut. The desired cutting load, rake angle and thus, the cutting edge orientation is achieved by first, empirically setting the amount of cantilever displacement via the calibration curve and next, by setting the required tool mounting angle by suitable rotation of the b-stage of the mMT. The cantilever displacement control is maintained via a feedback loop mechanism provided by the strain-gauge sensor integrated into the meso-scale CBN flexible cutting tool unit. The strain-gauge sensor is powered by the VMM 2310 signal conditioning amplifier. The schematic of the micro-groove cutting process is shown in Fig. 4.1. During these cuts, the applied cutting load is ramped up from zero to a desired steady state load over a fixed portion of the cut, to minimize tool breakage due to sudden loading.

**Figure 4.1:** Schematic of the micro-groove cutting process
Two sets of experiments were conducted to evaluate the depth of cut achieved during the cutting process, while varying the number of tool passes used. In the first set of experiments, a series of four parallel grooves were machined in annealed SS 303, each of which was 2mm in length using a single tool pass. During the second set of experiments, another four set of grooves of length 1.5 mm each were cut over the initially cut grooves, creating the two tool passes condition. The cutting load at the tip of the meso-scale cantilever, cutting speed and mounting angle were kept to a constant value. Table 4.1 and 4.2 summarize the experimental and cutting tool conditions used for the cutting process, respectively.

**Table 4.1: Cutting tool properties**

<table>
<thead>
<tr>
<th>Description</th>
<th>Values</th>
</tr>
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<tbody>
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<td>Tool Stiffness</td>
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<tr>
<td>Tool Tip Width</td>
<td>2.45 µm</td>
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<tr>
<td>Rake Profile</td>
<td>Rectangle</td>
</tr>
<tr>
<td>Total Tool Tip Height</td>
<td>100 µm</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>74 nm</td>
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<tr>
<td>Back Rake angle</td>
<td>20 deg</td>
</tr>
<tr>
<td>Side Relief Angle</td>
<td>14 deg</td>
</tr>
<tr>
<td>Rake Angle</td>
<td>7 deg</td>
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<tr>
<td>Max. Depth of Cut</td>
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</table>
Table 4.2: Experimental Cutting Conditions

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<th>Parameters</th>
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</thead>
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<td>Run No.</td>
<td>A</td>
</tr>
<tr>
<td>Tool Passes</td>
<td>1</td>
</tr>
<tr>
<td>Cut Length</td>
<td>8 mm</td>
</tr>
<tr>
<td>Cutting Load</td>
<td>15 mN</td>
</tr>
<tr>
<td>Desired Deflection</td>
<td>10.254 µm</td>
</tr>
<tr>
<td>Feedback Mechanism</td>
<td>Strain-Gauge Sensor</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>100 mm/sec</td>
</tr>
</tbody>
</table>

The grooves were examined with a Scanning Electron Microscope (SEM) and were found to possess a rectangular profile with well-defined vertical walls and floors. The section of the micro-grooves cut using one and two tool passes can be seen in Fig. 4.2 (A) and (B), respectively. The 2D AFM images of the cross-sections of the test sample representing each of the tool pass condition are shown in Fig. 4.3 and 3D plots of the same data are shown in Fig. 4.4.

Figure 4.2: SEM images of micro-grooves formed after: (A) one tool pass; (B) after two tool passes
As seen from the Figs. 4.3 and 4.4, the depth of the micro-grooves increased following each of the tool pass condition. It was also observed that depth of cut of 1.02 µm and 1.48 µm were achieved using one and two tool passes, respectively. When comparing with the micro-grooves cut in one tool pass using the CBN tip-based AFM sapphire cantilevers on 1018 low-carbon steel (Section 3.2), the depths of grooves achieved were
less than 700nm. In addition, the stress experienced by the meso-scale cantilever when removing over 1µm of material was less than 100MPa, compared to the stress levels of over 350 MPa with the CBN tip-based AFM sapphire cantilevers.

4.3 Micro-Groove Characterization

The quality of the micro-grooves machined by the meso-scale CBN flexible cutting tool in stainless steel is to be characterized for their: (1) consistency in width; (2) uniformity in depth throughout the length of cut; (3) fluctuations in the machined groove floor profile; and (4) quantity of side burr formation along the borders of the groove channels. These parameters affect the performance of the micro-grooves that are driven by the surface finish and tolerance requirements as needed by various applications. The discussed features are shown in a 3D AFM outline of a micro-groove in Fig. 4.5.

**Figure 4.5:** Characterization measures for a micro-groove
4.3.1 Groove Profile Analysis

The characterization study is carried out in two sections. In the first section, the machined micro-grooves are analyzed for the overall profile in terms of expected dimensions and geometry. In particular, the width, depth and floor profiles are evaluated; in the next section the side burr formation is studied for various tool geometry and machining conditions. To achieve the objective of the first section, an experiment is conducted where a series of five linear, straight micro-grooves are machined in polished austenitic SS 303 samples for a length of 2mm each, using the flexible cutting tool at a cutting speed of 100mm/min and cutting load of 8mN. The other machining conditions used in the experiment are summarized in Table 4.3.

<table>
<thead>
<tr>
<th>Tool Type</th>
<th>CBN tip-based AFM sapphire cantilever</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tool Type</td>
<td>Cutting Parameters</td>
</tr>
<tr>
<td>Tool Tip Width</td>
<td>2.28 µm</td>
</tr>
<tr>
<td>Tool Stiffness</td>
<td>1140 N/m</td>
</tr>
<tr>
<td>Rake Profile</td>
<td>Rectangle</td>
</tr>
<tr>
<td>Edge Radius</td>
<td>108 nm</td>
</tr>
<tr>
<td>Back Rake angle</td>
<td>20 deg</td>
</tr>
<tr>
<td>Side Relief Angle</td>
<td>14 deg</td>
</tr>
<tr>
<td>Rake Angle</td>
<td>7 deg</td>
</tr>
<tr>
<td>Cut Length</td>
<td>2 mm</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>100 mm/sec</td>
</tr>
<tr>
<td>Cutting Load</td>
<td>8 mN</td>
</tr>
<tr>
<td>Feedback Mechanism</td>
<td>MEMS Stain-Gauge Sensor</td>
</tr>
<tr>
<td>Resonant Frequency</td>
<td>3.6 kHz</td>
</tr>
</tbody>
</table>
The SEM images of the micro-grooves and the 2D AFM cross-sectional plots captured from each of the micro-groove channel are shown in Fig. 4.6(A) and 4.6(B), respectively. It was seen that the groove depth and width appeared fairly consistent over the few micron shown in the SEM images. The AFM plots showed the repeatability of the rectangular micro-groove cross-sectional profiles achieved by the new flexible cutting tool over the entire 10mm range of the groove cut length. The rectangular profile is shown to have a V-shaped outline in Fig. 4.6(B) due to the limitation of the high-aspect ratio AFM tips used for the metrology [151].

**Figure 4.6:** (A) Micro-grooves in stainless steel for characterization study and ;(B) AFM cross-sections of micro-grooves

In order to assess the micro-groove floor profile variations, the AFM scans were used to generate groove cross-sections at points at randomly chosen regions along length of the machined grooves. The median heights of the groove floors in the cross-sections were then used to assemble curves describing the change in height of the floor depth along each micro-groove. Line profiles describing the curves corrected for tilt-related errors. Multiple scans of the same micro-grooves were performed keeping a fixed scan rate and speed, while varying the scan length between 5 μm and 60 μm. Shorter scans provided high
frequency information, while longer scans provided low frequency information. Due to the scanning size limitations of the AFM equipment, the longest scan length that was possible was 60µm. Figures 4.7 (A) and 4.7 (B) show the curves describing the variation in the elevation of the micro-groove floor over the 5 µm and 60 µm scanned length, respectively. A summary of the geometry characterization results of the machined micro-groove floor surface finish is given in Table 4.4.

![Figure 4.7](image)

**Figure 4.7**: Height variation of the micro-groove floors scanned over a length: (A) 5 µm; and (B) 60 µm
### Table 4.4: Summary of groove characterization results

<table>
<thead>
<tr>
<th>Groove No.</th>
<th>Mean Groove Floor Profile (nm)</th>
<th>Mean Groove Width (μm)</th>
<th>Mean Groove Depth (μm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Over 5 μm</td>
<td>Over 60 μm</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Average Roughness</td>
<td>Peak-to-peak</td>
<td>Average Roughness</td>
</tr>
<tr>
<td>1</td>
<td>50.123</td>
<td>70.625</td>
<td>118.345</td>
</tr>
<tr>
<td>2</td>
<td>61.678</td>
<td>98.122</td>
<td>121.150</td>
</tr>
<tr>
<td>3</td>
<td>56.654</td>
<td>88.364</td>
<td>126.424</td>
</tr>
<tr>
<td>4</td>
<td>58.782</td>
<td>91.454</td>
<td>128.922</td>
</tr>
<tr>
<td>5</td>
<td>57.188</td>
<td>96.987</td>
<td>127.098</td>
</tr>
</tbody>
</table>

As seen from the figures and Table 4.4, the average peak-to-peak variations were around 80 nm and 250 nm for the five different floor height profiles measured at 5 μm and 60 μm scan lengths, respectively. The average surface height variations were found to be consistently less than 60 nm during 5 μm scans and under 130 nm for 60 μm scans. The nature of the resultant undulations observed on the groove floor profiles can be attributed to the combined dynamic effects of rate of sensor response to changes in the steady-state tool deflections endured during the cutting process and overall servo-update frequency of the machine controller that operates the feedback loop.

It is observed that most of the variations of the groove floor profile occurred over longer distances as seen from the scan results. From the statistical analysis, it can be seen that, while the groove floor is not completely flat, the acceptable amount of roughness (or serrations) and sharpness is largely governed by targeted requirements. Also, in some of the applications such as micro-fluidics or micro-heat exchangers, the serrations might enhance the performance of the groove channels.
4.3.2 Side Burr Analysis

Burr formation in machining is an inevitable event that impacts the quality and performance of machined features. It is problematic especially, in micro-scale cutting where the burr size is comparable to the feature size and the burrs could alter the required size and tolerance of the machined micro-grooves. The two types of burr formation seen in micro-groove cutting are side and exit burrs. The side burr is formed due to an undesired lateral extension of material on either side of the groove channel past the theoretical intersection of tool and workpiece. This is similar to poisson burr formation at macro-scale machining [34, 152]. Exit burr formation is a hump of unsheared material left on the edges of intersecting micro-grooves. The side and exit burr formed in machining micro-grooves are illustrated in Fig. 4.8.

![Side and Exit Burr Illustration](image)

**Figure 4.8:** Side and exit burr formation during micro-groove cutting

Two separate experiments are conducted to study and evaluate the side burr formation when machining micro-grooves in steel using the flexible cutting tool. Specifically, the impact of higher load carrying capacity of the flexible cutting tool on burr formation is evaluated. The influence of combination of machining parameters is studied using factorial design of experiments to assess the nature of the side burrs formed.
EXPT 1: Deep Cut Micro-Grooves

A series of three linear grooves were cut with the flexible cutting tool using incremental cutting load condition in austenitic stainless steel. The tool geometry and machining conditions of the flexible cutting tool is given in Table 4.5. A new cutting edge was implemented following each load condition. After the experimental trials, measurement of side burrs was carried out using a commercial Asylum 310S AFM that was used to image cross-section of each groove cut. The data from these images were examined and used to calculate the mean side burr height, as shown in Fig. 4.9. The machined micro-grooves using the three load conditions are shown through a SEM image in Fig 4.10. Table 4.6 summarizes the results of the experiment.

**Table 4.5: Tool geometry and cutting parameters**

<table>
<thead>
<tr>
<th>Tool Type Parameters</th>
<th>Meso-Scale CBN flexible cutting tool</th>
</tr>
</thead>
<tbody>
<tr>
<td>EXPT 1</td>
<td></td>
</tr>
<tr>
<td>Tool Tip Width</td>
<td>2.15 µm</td>
</tr>
<tr>
<td>Avg. Cutting Edge Length</td>
<td>4.75 µm</td>
</tr>
<tr>
<td>Avg. Cutting Edge Radius</td>
<td>90 nm</td>
</tr>
<tr>
<td>Cutting Load (mN)</td>
<td>9, 14, 18</td>
</tr>
<tr>
<td>Cut Length</td>
<td>1.5 mm</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>100 mm/min</td>
</tr>
</tbody>
</table>
Examinations of the results reveal that at higher depth of cuts, the mean side burr height formed on the borders of the micro-groove increased. This was further evident from the AFM cross-sectional and 3D profile analysis as shown in Fig. 4.11 (A) and (B), respectively. This effect was more significant when transitioning from 14mN to 18mN, where the side burr height increase was nearly 40%. As the depth of cut increased, the growth of side burrs along the edges became more longitudinal than lateral. The crowning effect of the side burrs at higher depths of cut could be explained by the compression effect.
of the deformed workpiece material traversing ahead of the tool path [112]. This effect was evident as there is a slight decrease in expected groove width as shown in the Table 4.6.

![AFM plots of the micro-grooves](image)

**Figure 4.11**: AFM plots of the micro-grooves : (A) 2D and; (B) 3D

**Table 4.6**: Summary of results

<table>
<thead>
<tr>
<th>Load (mN)</th>
<th>Depth of Cut (µm)</th>
<th>Mean Side Burr Height (nm)</th>
<th>Groove Width (µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>9</td>
<td>0.86</td>
<td>186</td>
<td>2.15</td>
</tr>
<tr>
<td>14</td>
<td>1.30</td>
<td>267</td>
<td>2.14</td>
</tr>
<tr>
<td>18</td>
<td>1.85</td>
<td>423</td>
<td>2.12</td>
</tr>
</tbody>
</table>
EXPT 2: Influence of Machining Parameters

From the previous experiment it was clear that higher loading conditions yielded in a deeper micro-groove with a pronounced side burr formation. This is particularly not desirable when machining micro-grooves for tight tolerance requirements, like photolithography [114] and surface texture coatings [150]. Therefore, it is necessary to choose appropriate machining parameters to minimize side burr height without compromising on micro-groove specifications as dictated by the feature application. To achieve this, a $2^k$ half-fractional factorial design scheme was planned to analyze the effect of five machining parameters on side burr formation that included the cutting load, cutting speed, tool rake angle, number of tool passes and inter-groove spacing. The experimental values used are given in Table 4.7. A 1.3 μm wide FIB-machined tool tip was used for the experiment, while other tool geometry-related conditions were the same as listed in Table 4.1. A series of straight, linear and non-intersecting micro-grooves were cut in austenitic stainless steel 303. The AFM 2D and 3D plots for the selected experiments are shown in Figs. 4.12 and 4.13(A-B), respectively.

Table 4.7: Experimental parameters

<table>
<thead>
<tr>
<th>Expt 2</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting Load (mN)</td>
<td>8, 16</td>
</tr>
<tr>
<td>Tool Rake Angles</td>
<td>0, 10</td>
</tr>
<tr>
<td>Cutting Speed (mm/min)</td>
<td>100, 400</td>
</tr>
<tr>
<td>Inter-Groove Spacing (μm)</td>
<td>1, 4</td>
</tr>
<tr>
<td>Tool Passes</td>
<td>1, 2</td>
</tr>
<tr>
<td>Tool Width(μm)</td>
<td>1.3</td>
</tr>
<tr>
<td>Cutting Edge Radius (nm)</td>
<td>108</td>
</tr>
</tbody>
</table>
Figure 4.12: 2D AFM plots from Run # 16, # 3, #6 and # 8 (L to R)

Figure 4.13(A): 3D AFM plots from Run # 16, # 3, #6 and # 8 (L to R)

Figure 4.13(B): 3D AFM plots from Run # 4, # 11, #5 and # 13 (L to R)
The results of the experiments are summarized in Table 4.8. In total 16 runs were performed as per the half-factorial design and result of each experimental run is tabulated column-wise in the table. In all the cases considered, the side burr height increases with increase in depth of cut. The spacing between the micro-grooves also played a major role in burr formation, where shorter groove-spacing increased the amount of side burr formation as shown by Run nos. 4 and 9, respectively. This is also shown by the AFM groove cross-sectional plot in Fig. 4.12.

Cutting speeds alone didn’t significantly influence change in side burr formation. Increase in rake angle impacts the burr formation process due to less material support of the cutting edge during chip removal. Number of tool passes had an influence on reducing the side burr formation for a particular groove depth when compared to a single tool pass used to achieve the same depth. Groove machined with low load and two tool pass condition (Run no. 11) had a lower burr height when compared to micro-grooves of similar depths machined with high load and single tool pass condition (Run No 10). The best micro-groove channels with regard to minimal burr formation were achieved when cutting at lower loads and hence lower depth of cut (Run No 6). The highest quality of micro-grooves in terms of lowest side burr formed is achieved when cutting at high-load condition, as shown by Run No. 13.

It was clear that that there is a trade-off between side burr height and micro-groove depth. As the flexible cutting tool cuts deeper into the workpiece, there is undesirable extension of material in the form of burr on either side of the chip formation. The extent of the burr formation can be controlled by controlling the amount of material removed by the tool tip. It was also understood that side burrs cannot be prevented by changes in load,
Table 4.8: Results of side burr analysis

<table>
<thead>
<tr>
<th>Run No.</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>10</th>
<th>11</th>
<th>12</th>
<th>13</th>
<th>14</th>
<th>15</th>
<th>16</th>
</tr>
</thead>
<tbody>
<tr>
<td>Load (mN)</td>
<td>8</td>
<td>16</td>
<td>8</td>
<td>8</td>
<td>16</td>
<td>8</td>
<td>16</td>
<td>8</td>
<td>16</td>
<td>16</td>
<td>8</td>
<td>8</td>
<td>16</td>
<td>16</td>
<td>16</td>
<td>8</td>
</tr>
<tr>
<td>Speed (mm/min)</td>
<td>100</td>
<td>100</td>
<td>400</td>
<td>400</td>
<td>400</td>
<td>100</td>
<td>100</td>
<td>400</td>
<td>400</td>
<td>100</td>
<td>100</td>
<td>400</td>
<td>400</td>
<td>400</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>Rake Angle (deg)</td>
<td>0</td>
<td>10</td>
<td>10</td>
<td>0</td>
<td>10</td>
<td>0</td>
<td>10</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>10</td>
<td>10</td>
<td>0</td>
<td>10</td>
<td>0</td>
<td>10</td>
</tr>
<tr>
<td>Tool Pass</td>
<td>2</td>
<td>1</td>
<td>2</td>
<td>2</td>
<td>1</td>
<td>1</td>
<td>2</td>
<td>1</td>
<td>2</td>
<td>1</td>
<td>2</td>
<td>1</td>
<td>1</td>
<td>2</td>
<td>2</td>
<td>1</td>
</tr>
<tr>
<td>Grv. Spacing (µm)</td>
<td>1</td>
<td>4</td>
<td>1</td>
<td>4</td>
<td>1</td>
<td>4</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>4</td>
<td>1</td>
</tr>
<tr>
<td>Mean Groove Depth (µm)</td>
<td>1.21</td>
<td>1.41</td>
<td>1.29</td>
<td>1.25</td>
<td>1.42</td>
<td>0.77</td>
<td>1.81</td>
<td>0.79</td>
<td>1.80</td>
<td>1.36</td>
<td>1.24</td>
<td>0.78</td>
<td>1.41</td>
<td>1.89</td>
<td>1.86</td>
<td>0.78</td>
</tr>
<tr>
<td>Mean Side Burr Height (nm)</td>
<td>288</td>
<td>370</td>
<td>319</td>
<td>311</td>
<td>333</td>
<td>190</td>
<td>416</td>
<td>215</td>
<td>410</td>
<td>388</td>
<td>267</td>
<td>197</td>
<td>304</td>
<td>331</td>
<td>389</td>
<td>214</td>
</tr>
</tbody>
</table>
speed, or tool geometry alone. Minimization of side burr can be achieved by choosing appropriate set of machining parameters keeping the overall objective (i.e. feature application) in mind. If the goal of the process is to achieve high-tolerances, like for micro-molds, micro-electronics or hot-embossing dies, then multiple tool passes will be the best-suited route to create better quality micro-grooves. Likewise, high aspect-ratios micro-grooves with relaxed tolerances and high-productivity rates are possible using high-load and high-speed conditions. Such types of grooves are in demand for applications where a large number of groove features are required over a surface area such as micro-fins, exchangers, fluidic channels, etc.

Another approach to reduce the burr formation is to implement a post-machining deburring step. A deburring strategy was implemented for the machined micro-grooves discussed by using an alternating sequence of ultrasonic cleaning step (sonicator at 40-50 kHz) in an IPA solvent bath, followed by the vibratory polish step. A FMC Syntron vibratory polishing technique was implemented for the purpose of the second step. The sample was placed face down, on a low-nap polishing cloth immersed in a 20nm colloidal silica compound suspension. Inertia from the sample weight induced movement around the periphery of the cloth in the presence of colloidal suspensions.

Vibrations in both the vertical and horizontal directions were made to produce on the sample by electromagnetic forces inside the Syntron. The results of the deburring process on the machined micro-grooves are shown in Fig. 4.14(A-C), where each step shows a reduction in burr height on the top ridges of the micro-grooves. The samples were then, cured in a hot bath (150°C) in the presence of a surfactant, to reduce the surface tension and wettability of the colloidal silica compounds that were found to adhere to the
floors of the micro-grooves during the vibratory steps, as shown in Fig. 4.14(D) and the resultant grooves after their removal is shown in Fig. 4.14(E). Figure 4.15 shows the AFM cross-section of the deburred micro-grooves that showed an improved well-defined feature with nearly flat adjacent micro-groove shoulders.

Figure 4.14: (A) Micro-grooves before deburring process, (B) After 10mins. of vibratory polish, and (C) 20 min vibratory polish and surfactant hot bath (D): Micro-grooves after 20 min. of deburring process without surfactant and (E) after using the surfactant
4.4 Tool Wear Study

Until now, the performance of the meso-scale CBN flexible cutting tool was evaluated in terms of the features of the machined micro-groove it creates. Tool wear is another aspect that plays a significant role in the overall outcome of the machined micro-groove characteristics, as demonstrated in Fig. 4.16, where micro-grooves on the left are cut using a new tool tip and the right with a completely worn-out tool tip. For a well-designed micro-machining operation, it is imperative to understand the relationship between tool wear, tool geometry and process conditions for improving tool life without compromising on resultant micro-groove quality and therefore, maximizing productivity. In the present study, the effects of cutting speed and load on tool wear are analyzed. A $2^2$ factorial scheme was used in designing the experiments for machining straight micro-grooves in a highly-polished (Ra ~ 20-30nm) austenitic SS 303 sample. Two cutting speeds of 100mm/min and 400 mm/min; and two cutting load of 8mN and 16mN were used. A zero rake angle condition was observed during the experiments. The tool geometry parameters used are provided in Table 4.1.
The tool wear examination is performed based on the measurement of cutting edge radius (R) of the tool taken periodically from an SEM image as shown in Fig. 4.17. The tool life criterion is set as the mechanical failure of the tool via fracture of the tool cutting edge, which occurs at $R > 700\text{nm}$ and consequently, the tool life is defined as the total groove length (L) cut by the tool till it reaches the tool life criterion. Therefore, the units of tool life are in mm. The failure of the cutting edge is detected by the strain-gauge sensor, when there is an unexpected spike on the feedback signal indicating change in tool tip load.

![Figure 4.17: Cutting edge wear measurement](image)

Table 4.9 contains the tabulated tool life and cutting edge wear data. A factorial effect analysis was performed using cutting edge wear as the response. It is observed from the plot that the cutting load has a dramatic effect on the cutting edge wear at both low and
high speed conditions. Furthermore, it also interesting to note that there are no interaction effects between the cutting load and cutting speed, which means the response of edge wear acted independently either due to changes in cutting load or cutting speed. The SEM images revealed that significant amount of tool failure tended to occur at high loads. The progression of cutting edge wear of the tool from low-speed and low-load condition measured periodically after cutting a fixed groove cutting length is shown in Fig. 4.18, with the tool rake face profiles shown in the inlet of each image.

**Table 4.9: Experimental results for tool life study**

<table>
<thead>
<tr>
<th>Run No.</th>
<th>Start Radius (nm)</th>
<th>Parameters</th>
<th>Tool life (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>75</td>
<td>8mN, 100 mm/min</td>
<td>234.50</td>
</tr>
<tr>
<td>2.</td>
<td>82</td>
<td>16mN, 100 mm/min</td>
<td>128.50</td>
</tr>
<tr>
<td>3.</td>
<td>76</td>
<td>8 mN, 400 mm/min</td>
<td>190.00</td>
</tr>
<tr>
<td>4.</td>
<td>80</td>
<td>16mN, 400 mm/min</td>
<td>102.50</td>
</tr>
</tbody>
</table>
Figure 4.18: Progressive analysis of cutting edge wear on tool at (A) Start of cut, R = 80nm, L = 0 mm, (B) R = 220 nm, (C) R = 456 nm, L = 100 mm (D) Failure point, L = 234 mm

Mechanical loading on the rake face seemed to be significant in accelerating damage of the cutting edge of the meso-scale CBN flexible cutting tool. This was demonstrated through the tool wear pattern as shown in Fig. 4.19. Further examination of the wear patterns indicated that it followed two main regimes- steady state and accelerating. The two regimes observed at 8mN load and 100 mm/min are marked in Fig. 4.19, where the steady state regime starts at the beginning of the cut till the tool life reaches a value of around 100 mm, followed by the accelerating regime that shows the wear to progress at an accelerated pace till it reaches the tool life criterion. The duration of these regimes were found to vary with the machining conditions. Specifically, the rate of cutting edge wear showed an accelerated rise in both regimes when high-speed and high-load conditions were
implemented, as shown in the figure and consequently, achieved the lowest tool life among along the other conditions.

![Tool Wear Pattern](image)

**Figure 4.19:** Tool wear pattern

It is seen from the Fig. 4.19 that the longer tool life can be achieved at cutting speed of 100 mm/min and cutting load of 8mN. The thermo-mechanical effects due to the micro-structural interactions of the austenitic stainless steel 303 with the CBN tool material could contribute to the observed accelerated wear patterns [153]. At lower depths of cut, the chip load at the tool tip is lesser and therefore the normal stresses and localized temperature fields at the tool tip is lower that enhances the tool life. The contrasting effects at higher depths of cut contribute to shorter tool life as observed in the wear pattern.

In order to draw a comparison between the meso-scale CBN flexible cutting tool and CBN tip-based AFM sapphire cantilever, an experiment was performed on 1018 low carbon steel sample using both the tools under similar machining conditions, to assess their
respective tool life. The histogram shown in Figure 4.20 confirmed that the meso-scale flexible cutting tool yielded a better tool life compared to the AFM sapphire cantilever. The results showed that the flexible cutting tool with higher stiffness tend to provide a longer tool life. The higher stiffness of the meso-scale flexible cutting tool provided higher rigidity and more stability over for a longer groove cut length, compared to the less stiff AFM sapphire cantilevers.

![Tool life Comparison Study](image)

**Figure 4.20:** Tool life comparison study on 1018 low carbon steel

### 4.5 Pattern Fabrication Capabilities

One of the demands of micro-groove fabrication is to produce different pattern geometries. There is a tremendous application of micro-groove features in the industry that require specific configuration, spacings and repetitions of such patterns over a large surface area. The commercial surface features demand closely spaced grooves with high relative accuracy such as in micro-heat exchanger systems, curvilinear groove patterns of varying cornering radii for micro-fluidic transportation systems, radial grooves for miniature motor
and blade systems, zigzag intersecting grooves for surface coating application such as sharklet pattern, etc.

Some of the discussed features, such as curvilinear and radial micro-groove formations require the flexible cutting tool to possess tremendous maneuverability and resistance to withstand normal and lateral forces. Such feature realizations also entail the flexible cutting tools need to handle twisting loads and transverse shear deformations at the cutting edge, while moving in asymmetrical trajectories with respect to the cantilever axis. While traditional AFM cantilevers systems are prone to failure during such cases, the meso-scale flexible cutting tools are better equipped to tackle the demands of such operations due to its increased stiffness, wider supporting base geometry and high torsional rigidity.

The main focus of the following sub-sections is to provide a detailed kinematic analysis to understand the fabrication of high-accuracy curvilinear and zig-zag micro-groove patterns. To this end, the meso-scale CBN flexible cutting tool has been developed to cut well when the rake face of the tool is mostly perpendicular to the direction of cut. In order to perform the curvilinear cuts, the tool and workpiece must be rotated as needed and therefore it is necessary to know the kinematic relationship between the tool and workpiece with a high degree of accuracy. Additionally, a tool used during such a cut must have adequate relief angles to insure that rubbing does not occur between the sides of the tool and newly cut groove sidewalls, since, such rubbing could generate lateral forces on the tool that could damage it. The following subsections: (1) Provide the relationship between the tool and workpiece position when cutting using the 5-axis mMT; (2) Explain how these equations are used to simulate groove cutting in the presence of uncertainty in the machine
tool configuration; (3) Provide simulation results that indicate limits on cut trajectories and tool geometry; (4) Provide a calibration procedure required to characterize tool position so that the kinematic equations can be used; and (5) Demonstrate the creation of curvilinear micro-groove patterns based on the developed kinematics.

4.5.1 Kinematic Equations

In this work, groove cutting was accomplished using a machine tool where the workpiece can translate and rotate relative to a tool that can approach or be retracted from the workpiece. Therefore, in order to rotate the workpiece relative to the tool without having the tool translate relative to the workpiece, a compensating translation must be simultaneously applied. This is accomplished via implementation of a set of inverse kinematics equations in the CNC controller controlling the machine, which gave the required stage positions in order to achieve a given tool position and angular orientation in a workpiece fixed coordinate system. These calculations are as follows:

**Inverse Kinematics Equations**

A set of coordinate systems are defined, as shown in Fig. 4.21 to describe the machine tool.
Let coordinate system H be parallel to the x-y stages of the machine tool and let point H be the origin of the coordinate system. Also, let point H be located such that when the machine x-y encoders read zero, the center of rotation of the workpiece rotary stage will coincide with point H. Let coordinate system T be parallel to coordinate system H, and assume the origin of the coordinate system, point, T coincides with the center of the cutting edge of the tool. Also, assume that the normal vector of the rake face of the tool is coincident with the x-direction of this coordinate system. Let coordinate system C be parallel to both coordinate system H and T, and let its origin point C be coincident with the center of rotation of the workpiece rotary stage. Lastly, let coordinate system W be a workpiece fixed coordinate system with an origin point that is coincident with point C. Now, define a coordinate notation in the form $x_{A/B}$ to mean x-coordinate of point A relative to coordinate system B. Similarly let $y_{A/B}$ to mean y-coordinate of point A relative to coordinate system B.

Then,
\[ x_{C/H} = x_{\text{encoder}}, \quad y_{C/H} = y_{\text{encoder}}. \quad (4.1) \]

Also, since the position of T relative to coordinate system H is found via calibration, as described in a subsequent subsection,

\[ x_{T/H} = x_{\text{cal}}, \quad y_{T/H} = y_{\text{cal}}. \quad (4.2) \]

It then can be found that in order for a cut to occur along a trajectory described by the position of point P in the workpiece fixed coordinate system, the required encoder position at each point along the trajectory is

\[
\begin{align*}
    x_{\text{encoder}} &= x_{\text{cal}} - (x_{P/W} \cos \theta - y_{P/W} \sin \theta), \\
    y_{\text{encoder}} &= y_{\text{cal}} - (x_{P/W} \sin \theta + y_{P/W} \cos \theta). \quad (4.3, 4.4)
\end{align*}
\]

These equations were programmed into the CNC controller that controls the groove cutting machine. However, when the CNC controller is commanded to perform a move, it must first perform forward kinematic calculations in order to find the start point of the move. Therefore, the forward kinematics equations are required.

**Forward Kinematics for Calibration of Cut Trajectory**

Assuming perfect knowledge of the system, the forward kinematics equations providing the x-y position of the point T on the tool relative to coordinate system W are as follows:

\[
\begin{align*}
    x_{T/W} &= (x_{\text{cal}} - x_{\text{encoder}}) \cos \theta + (y_{\text{cal}} - y_{\text{encoder}}) \sin \theta, \quad (4.5) \\
    y_{T/W} &= -(x_{\text{cal}} - x_{\text{encoder}}) \sin \theta + (y_{\text{cal}} - y_{\text{encoder}}) \cos \theta. \quad (4.6)
\end{align*}
\]
Equations describing Required Tool Orientation

Additionally, it is required that the rake face of the tool always be orientated such that the normal vector of the face is in the direction of machined cut. Given a tool that is orientated as shown in Fig. 4.24, the required workpiece rotary orientations during a cut are given as follows:

\[ \text{if } (\dot{x}_{P/W} < 0), \quad \theta = -\arctan\left(\frac{\dot{y}_{P/W}}{\dot{x}_{P/W}}\right), \quad (4.7) \]

\[ \text{if } (\dot{x}_{P/W} = 0 \text{ and } \dot{y}_{P/W} > 0), \quad \theta = +90^\circ, \quad (4.8) \]

\[ \text{if } (\dot{x}_{P/W} = 0 \text{ and } \dot{y}_{P/W} < 0), \quad \theta = -90^\circ, \quad (4.9) \]

\[ \text{if } (\dot{x}_{P/W} > 0 \text{ and } \dot{y}_{P/W} > 0), \quad \theta = +180^\circ - \arctan\left(\frac{\dot{y}_{P/W}}{\dot{x}_{P/W}}\right), \quad (4.10) \]

\[ \text{if } (\dot{x}_{P/W} > 0 \text{ and } \dot{y}_{P/W} < 0), \quad \theta = -180^\circ - \arctan\left(\frac{\dot{y}_{P/W}}{\dot{x}_{P/W}}\right), \quad (4.11) \]

These equations are used during the planning phase of cuts, but are not directly implemented on a CNC controller. Also, these equations above and the equations in the previous subsection are all that is necessary to plan and perform the CNC moves required to make a cut.

Forward Kinematics for Evaluation of Errors

However, all of the previous equations assume that the center of the cutting edge of the tool lies on point T and the normal vector of the rake face of the tool is coincident with the x-direction of coordinate system T. In practice, that will not be the case due to position calibration errors and alignment errors. A more realistic scenario is shown in Fig. 4.22, where point A lies on the center of the cutting edge of the tool.
In Fig. 4.25, let points E1, E2, E3, and E4 describe the corners of the tool. Given an error in location of the center of the cutting edge relative to point T, and given tool misalignment, it can be shown the coordinates of each of these points in coordinate system T is as follows:

\[ x_{E(1)T} = x_{A/T} - 0.5W \sin \phi, \]  
\[ y_{E(1)T} = y_{A/T} + 0.5W \cos \phi, \]  
\[ x_{E(2)T} = x_{A/T} + 0.5W \sin \phi, \]  
\[ y_{E(2)T} = y_{A/T} - 0.5W \cos \phi, \]  
\[ x_{E(3)T} = x_{A/T} + D \cos \phi - (0.5W - D \tan \beta) \sin \phi, \]  
\[ y_{E(3)T} = y_{A/T} + D \sin \phi + (0.5W - D \tan \beta) \cos \phi, \]  
\[ x_{E(j)W} = \left( x_{E(j)T} + x_{\text{cal}} - x_{\text{encoder}} \right) \cos \theta + \left( y_{E(j)T} + y_{\text{cal}} - y_{\text{encoder}} \right) \sin \theta, \]  
\[ y_{E(j)W} = -\left( x_{E(j)T} + x_{\text{cal}} - x_{\text{encoder}} \right) \sin \theta + \left( y_{E(j)T} + y_{\text{cal}} - y_{\text{encoder}} \right) \cos \theta. \]
Then, given these coordinate the machine x-y encoder values and the rotation of the workpiece rotary stage, the actual position of the corners of the tool in the workpiece fixed coordinate system during a cut can be found to be

\[ x_{E(j)W} = (x_{E(j)T} + x_{\text{cal}} - x_{\text{encoder}}) \cos \theta + (y_{E(j)T} + y_{\text{cal}} - y_{\text{encoder}}) \sin \theta, \]  
\[ y_{E(j)W} = -(x_{E(j)T} + x_{\text{cal}} - x_{\text{encoder}}) \sin \theta + (y_{E(j)T} + y_{\text{cal}} - y_{\text{encoder}}) \cos \theta. \]  

where \( j = 1,2,3,4. \)

4.5.2 Simulation of Curvilinear Groove Cutting

A Matlab program that makes use of the previously presented equations was used to simulate cutting. The program accepts the desired cutting trajectory in the workpiece fixed coordinate system and uses this information to calculate the stage rotations and encoder positions that must be achieved to cut the trajectory with the assumption that the location of the tool is perfectly known. This model does what the CNC controller would be doing during an actual cut. Next, the program uses the calculated values to determine where the tool actually ends up cutting, relative to the workpiece fixed coordinate system, given user provided errors in tool placement and alignment. This enables the program to evaluate the ability to cut the zig-zag portion of an intersecting pattern, such as a sharklet surface as shown in Fig. 4.23, given tool shape, tool location error, and tool misalignment.
In Fig. 4.2, the actual zig-zag portion of the Sharklet pattern is shown with the ideal zig-zag cutting trajectory drawn on top of it. However, when running simulations it quickly becomes apparent that it is not possible to cut perfectly sharp corners without causing the sides of the tool to touch and attempt to pass through the sidewalls of the newly formed micro-grooves. Therefore, the corners of the cut trajectory must be rounded somewhat. Figure 4.24 shows an ideal zig-zag trajectory with sharp corners and it shows the same trajectory with 10 μm fillets introduced at the corners. In order to prevent any distortions to the curvilinear groove pattern, the use of the minimum amount of rounding is desirable. The pattern can be produced using cutting edge geometries of various shapes. For example, suppose a cut is made using a tool that is 2 μm wide, 4 μm deep, and that has a side relief angle of 12 degrees, as shown in Fig. 4.25.
Figure 4.24: Ideal and rounded cutting trajectory with a fillet radius of 10 μm

Figure 4.25: Example of a cutting edge shape

If the position of the tool is perfectly known, and it is perfectly aligned, the resultant groove is as shown in Fig. 4.26, where the centerline is the desired trajectory, the lines above and below the centerline are the sides of the resultant groove, and the trapezoids give the position and orientation of the tool during selected portions of the cut. It is to be noted that in this case there is no rubbing between the sides of the tool and the sidewalls of the grooves.
However, if some tool positioning error is introduced (e.g. 2 μm in the x-direction, 2 μm in the y-direction, and 1 degree of misalignment) the resultant groove and tool trajectory looks like the one shown in Fig. 4.27. In this case, the sides of the tool are rubbing against the side of the groove, which would twist the tool. This shows that it is necessary to determine what amount of trajectory rounding and what tool dimensions are required to tolerate what errors in how well the tool position is known. To this end, many simulations were run using different tool shapes, amounts of rounding, and positioning errors.
4.5.3 Simulation Results

Based on the simulations, combinations of conditions that result in no rubbing between the side of the tool and workpiece (desired) and some rubbing (unacceptable) were found. These conditions are indicated below in Tables 4.10 – 4.12. Table 4.10 shows the effect of tool misalignment. Table 4.11 and 4.12 show the effects of incorrectly calibrating the position of the tool relative to the center of rotation of the workpiece rotary stage. The way to read each table is as follows. The x and y-direction calibration errors and tool misalignment used in a set of simulations are given at the top of each table. Below this, the side clearance angle corresponds to each column of the table, and the “depth” of the tool (the amount of material supporting the cutting edge) corresponds to each row. At the intersection of each row and column, the minimum radius of rounding of the ideal cutting path required to avoid rubbing, in microns is provided. If rubbing cannot be avoided by adjusting the rounding radius, or the tool shape is not geometrically possible, no value is given.

Table 4.10: Acceptable conditions with tool position is perfectly known and there is up to 2° of misalignment
Table 4.11: Acceptable conditions tool position is known to within 2 μm and there is up to 2° of misalignment

Table 4.12: Acceptable conditions tool position is known to within 4 μm and there is up to 2° of misalignment

Based on the tables it was concluded that the best set of conditions would be a tool “depth” of 4 μm, a side clearance angle of 14 degrees, and a rounding radius of 12 microns. Note however, that there are combinations of calibration errors that will result in this configuration still rubbing.
4.5.4 Tool Calibration Procedure

In order to be able to perform the calculations described in the previous subsections, it is necessary to find the location of the center of the cutting edge of a tool relative to the center of rotation of the workpiece rotary stage. This is accomplished by first roughly finding the position optically, and then, performing a test cut to more precisely to find the position. During cutting, a camera is held in a fixed position behind the cutting tool. Therefore, the location of the tool in images produced by the camera is always about the same and the approximate coordinates of the tool relative to the center of rotation of the workpiece can be found as follows:

1. Without a tool loaded, the x-y stages of the machine are adjusted to position some test feature on a test workpiece directly under the location seen by the microscope where the tool would normally be located. Then the x-y encoder outputs of this point is noted;

2. The workpiece is rotated by 180° degrees using the workpiece rotary stage;

3. Once again, the x-y stages of the machine are adjusted to position some test feature on a test workpiece directly under the location seen by the microscope where the tool would normally be located;

4. Then the new x-y encoder outputs are recorded.

The location of the tool relative to the center of rotation is then approximately given by the mean of the coordinates from steps 1 and 3. However, this rough location procedure does not account for any deviations in the position or geometry of the tool, which would affect the location of its cutting edge relative to the center of rotation of the workpiece.
Therefore, once the tool is loaded a fine calibration procedure is performed by taking two test cuts in a test workpiece. This procedure is as follows:

1. A workpiece consisting of some easy to cut material, like aluminum, is mounted to the workpiece rotary stage such that it is nearly centered;
2. The start and end coordinates of a groove cut are selected such that it would produce a groove parallel to the x-axis of the machine tool that terminates at some small, but not accurately known distance below and to the left of the center of rotation of the workpiece rotary stage;
3. Cutting from right to left, a groove cut is made using the coordinates from step 2. The end of this cut is indicated by the dot in Fig. 4.28 (left);
4. The workpiece rotary stage is next rotated by 180 degrees. A second groove cut is performed from right to left using the coordinates from step 2. The end of this cut is indicated by a second dot in Fig. 4.28 (right);

**Figure 4.28:** Two test groove cuts
5. The workpiece is taken off of the machine and observed under an optical microscope. The workpiece is aligned such that the two grooves appear horizontal in the microscope. Then, the horizontal and vertical distance are measured between the ends of the two grooves. These two distances can then be used to calculate the x-y distances between the endpoint of the first groove and the center of rotation of the workpiece rotary stage as shown in Fig. 4.29.

![Figure 4.29: Cut endpoints relative to center of rotation](image)

It is to be noted that this procedure is limited by the resolution of the microscope. Therefore, it is best if the ends of the two groove cuts end up being close together so that measurement of the distance between them can be made at a high magnification. In all experiments where curved groove were cut the endpoints of the tool grooves were set to ideally be 50 μm apart in the x and y directions. In these experiments the calibration procedure was shown to perform adequately. Therefore, a system of tool design for cutting curvilinear geometry was developed. The tool had adequate side relief angles to ensure against rubbing when the direction of cut is continuously changing as shown in Fig. 4.30.
Figure 4.30: Cutting edge geometry of tool tip used for curvilinear groove cutting (A) Side view and (B) Top view

4.5.5 Pattern Results

Curvilinear Micro-Grooves

Curvilinear or wave-like patterns are used in the study of micro-scale behavior of various continuous-flow fluids, such as laminar flow, electrowetting and thermal relaxation properties, etc. A typical array of micro-fluidic transportation channel involving curvilinear grooves is shown in Fig. 4.31(A). In molecular biology, such as cell, DNA or protein transportation, curvilinear grooves are used to control shape of protein cells within the groove channel as shown in Fig. 4.31(B) [154].

Based on the kinematic tool motion development and simulation results developed in Section 4.6.1-4.6.4, curvilinear groove cutting experiment was performed using the meso-scale flexible cutting tool. The forward and inverse kinematic equations developed in the Section 4.6.1 was inducted into the numerical control program. The minimum corner radius of 12 μm was chosen for the curvilinear micro-groove patterns. In order for the
micro-machine tool (mMT) to accurately predict the center of the cutting edge to the center of c-stage rotation (Refer Fig. 4.1 for mMT orientations), the calibration procedure (Section 4.6.4) was carried out and the x-y coordinates of the cutting edge were updated into the program.

A highly polished austenitic SS 303 sample was used in order to cut the series of curvilinear micro-grooves. A low cutting load of 9mN and low cutting speed of 50 mm/min were implemented for the experiment. The machining conditions used for the curvilinear groove cutting experiment are summarized in Table 4.13. The cutting tool geometry orientations provided in the table represent one of the conditions to perform the curvilinear groove cut with minimum rubbing effects. The tool was provided with adequate side relief angles to ensure against rubbing when the direction of cut is changing continuously. The SEM image of the cutting tool geometry is shown in Fig. 4.30.

**Table 4.13: Machining conditions for curvilinear micro-groove cutting**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting Load</td>
<td>9mN</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>50mm/sec</td>
</tr>
<tr>
<td>Max. Tool Tip Depth</td>
<td>4 µm</td>
</tr>
<tr>
<td>Tip Width</td>
<td>2.1 µm</td>
</tr>
<tr>
<td>End Relief Angle</td>
<td>20 deg</td>
</tr>
<tr>
<td>Rake Angle</td>
<td>0 deg</td>
</tr>
<tr>
<td>Side Clearance Angle</td>
<td>9.5 deg</td>
</tr>
<tr>
<td>Min Curve Radius</td>
<td>12 µm</td>
</tr>
<tr>
<td>Total Cut Length</td>
<td>50 µm</td>
</tr>
</tbody>
</table>
Figure 4.31 (C) shows the SEM images of the curvilinear micro-grooves created in SS 303. It can be seen that the machined curvilinear grooves followed the planned micro-groove trajectory effectively. Figure 4.31(D) shows the magnified section of the curvilinear groove. The width of the groove channel was found be 2 µm, corner radius of 13 µm while the distance between adjacent grooves rows were 4 µm. Well-defined micro-grooves with consistent rectangular cross-sectional features can be seen from the 2D AFM plots in Fig. 4.31(E). Since a low-load condition was implemented for the experiment, minimal amount of side burrs, less than 80 nm were formed along the borders of the cut channel. This can also be seen in Figure 4.31(E), which represents a 3D plot of a section of the curvilinear grooves.

![Figure 4.31](image_url)

**Figure 4.31**: (A&B): Application of curvilinear grooves in micro-fluidic channels; (C & D): Machined curvilinear micro-grooves; and (E & F): Curvilinear micro-groove characterization using AFM plots.
Straight, Closely-spaced Micro-Grooves

One of the many applications of linear, closely-spaced micro-groove pattern is in heat-transfer applications. An example for such an application is in micro-heat exchangers as shown in Fig. 4.32 (A) & (B), which require high-aspect ratio micro-groove channels laterally displaced from each other by a fixed distance, such as the fluid is confined to the respective groove boundary. The relative accuracy of between the machined micro-grooves channels becomes critical in such applications.

An experiment was conducted on SS 303 to demonstrate the capability of the flexible cutting tool to machine extremely straight, closely spaced micro-grooves over a large area. Table 4.14 summarizes the experimental conditions. The micro-groove tool cutting geometry used for this study can be found in Table 4.1. A series of 32 micro-grooves were cut, at a relative distance of 2 µm between each other for a length of 2 mm. Figure 4.36 shows the SEM image of the machined array of micro-grooves that are closely-spaced and consistently parallel to each other.

Table 4.14: Machining conditions for curvilinear micro-groove cutting

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting Load</td>
<td>10 mN</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>100 mm/sec</td>
</tr>
<tr>
<td>Max. Tool Tip Depth</td>
<td>4 µm</td>
</tr>
<tr>
<td>Tip Width</td>
<td>1.96 µm</td>
</tr>
<tr>
<td>Cutting Edge Radius</td>
<td>90 nm</td>
</tr>
<tr>
<td>Total Cut Length</td>
<td>128 mm</td>
</tr>
<tr>
<td>Tool Passes</td>
<td>2</td>
</tr>
</tbody>
</table>
Figure 4.3: (A & B) Micro-heat transfer application for straight micro-grooves and (C) Series of linear, closely-spaced micro-groove cut with 2 µm pitch.

Intersecting Micro-Grooves

Many applications can be found that demand complex patterns where micro-grooves machined are expected to intersect each other, one such being the engineered surface textures for various biological, mechanical and electronic applications. One of the challenges of intersecting micro-groove cutting are the formation of exit burr at all intersecting points, which results in pattern distortions. This is expected when the tool exits through a side wall of an already existing groove and the amount of burr formation depends on the combination machining conditions and properties of workpiece material used.

Also, during the intersection of micro-grooves, the cutting tool enters and exits through a region where there is no material to removed. This means that as the cutting operation is performed, there is no support or back-up material on the rake face. The sudden changes in the material load on the rake face can results in unwanted side burr formations. This effect also causes a dip in the intersecting groove which may affect the result of the profile obtained [151].
In order to qualitative observe the features of intersecting grooves, an experiment was conducted on a highly-polished and flat SS 303 sample using the same tool geometry parameters and machining conditions as provided in Tables 4.1 and 4.17, respectively. However, only a single tool pass condition was observed for this study. A series of four grooves were cut in opposite directions each being 2 µm wide, 4 µm apart and 1mm long, that intersect at specific locations on the workpiece grid as shown in Fig. 4.33(A). Figure 4.33 (B) shows the magnified section of the intersecting micro-groove pattern, which depicts a reasonable amount of side and exit burr formation. Figure 4.33 (C) shows a 3D AFM plot of another section of intersecting micro-grooves. The images show certain amount of burr formation and distortions along the ridges. This could be due to the uneven loading of the flexible cutting tool when it intersects an existing groove. Figure 4.33(D) and (E) represent a case of single micro-groove intersection on a single or series of existing grooves.

Figure 4.33: (A) A matrix of machined intersecting micro-groove pattern; (B) An enlarged region of the intersecting grooves; and (C) 3D AFM plot of one of the intersecting groove pattern.
Figure 4.33 (Cont.): (D) & (E) Single and multiple micro-groove intersections.

Textural Application of Intersecting Micro-Groove

Intersecting micro-grooves find a useful application of surface property enhanced textures. An example for such an application is the sharklet skin profile that consists of zigzag micro-grooves intersected by a series of closely cut longitudinal micro-groove patterns as shown in Fig. 4.34(C) [118, 150]. This surface texture is a bimorph of the outer skin of a shark that possesses anti-microbial properties.

To machine this pattern, a cutting procedure similar to that of curvilinear groove cutting was followed. The forward kinematic transformation for tool tip motion alone was sufficient for performing the experiment. First, an outline of the overall pattern boundary was defined in order to locate the center of the pattern, or the point of origin in the workpiece coordinates system. Now, in order to find the relative distance between the point of origin and center of workpiece-mounted stage (c-stage of 5-axis mMT, Refer Fig. 4.1), a calibration procedure similar to one described in Section 4.6.4 is carried out and the cutting edge offset errors from the center of stage rotation were obtained that was to be included in the forward transformation matrix. The program is updated accordingly, before implementing it for the groove cutting experiment.
The machining conditions used for the experiment is shown in Table 4.15. A highly polished SS 303 sample was used as the workpiece. The pattern was achieved in a three-step operation sequence. In the first step, the cutting tool tip is aligned to the c-stage at 0 deg (point of origin). The workpiece stage is then rotated by +135 deg., where a series of micro-grooves are machined with 25 \( \mu \)m inter-groove spacing, for a length of 2 mm. Next, from its current location, c-stage is rotated by -270 deg and similar series of micro-grooves are cut, which diagonally cut the previously machined series. In the last step, the workpiece is rotated back to 0 deg or point of origin, where a series of micro-grooves with 4\( \mu \)m spacing between them, are cut intersecting both the previously machined micro-grooves creating the required pattern. The resultant micro-groove pattern that resembles closely to the sharklet skin texture is shown in Fig. 4.34 (A). An enlarged area of a single array of the texture is shown in Fig. 4.34(B).

**Table 4.15: Summary of machining conditions**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting Load</td>
<td>10 mN</td>
</tr>
<tr>
<td>Cutting Speed</td>
<td>100 mm/sec</td>
</tr>
<tr>
<td>Max. Tool Tip Depth</td>
<td>4 ( \mu )m</td>
</tr>
<tr>
<td>Tip Width</td>
<td>2.15 ( \mu )m</td>
</tr>
<tr>
<td>Cutting Edge Radius</td>
<td>102 nm</td>
</tr>
<tr>
<td>Total Cut Length</td>
<td>32 mm</td>
</tr>
</tbody>
</table>
4.6 Summary

Cutting evaluation of the meso-scale CBN flexible cutting tool for micro-groove machining in austenitic stainless steel 303 was the subject of this chapter. In the first part of the chapter, the capability of the new flexible cutting tool to machine micro-grooves in stainless steel sample was demonstrated. Micro-grooves were machined at cutting speeds of 100 mm/min that had well-defined rectangular cross-sections and a depth of cut of 1 μm deep was achieved in a single tool pass condition.

In the next section, the overall quality of the machined micro-grooves was assessed. The quality of the micro-grooves was evaluated particularly, for the consistency, repeatability and accuracy of the profile obtained over the duration of cutting. The repeatability and consistency of the tool to machine micro-grooves were established. The uniformity of the micro-groove floors were also studied during this test, where it was
concluded that the waviness or floor surface finish obtained was consistently under 130nm as observed in all the machined micro-grooves.

Side burr formation during micro-groove cutting was analyzed through a series of experiments that were carried to study influence of depth of cut, combination of machining parameters and type of flexible tool used. It was observed that the height of the side burr increased when cutting deeper grooves using high cutting loads. However, the discretion of choosing suitable machining parameters reduced side burr height. In particular, it was concluded that use of multiple tool passes to cut deeper groove channels controlled the side burr height and improved the groove depth consistency.

In the last section, the capabilities of the meso-scale flexible cutting tool to cut complex groove patterns in stainless steel were demonstrated. It was argued that such patterns require greater maneuverability and stringency in order to tackle the unexpected lateral stress induced on the cutting edge. The meso-scale flexible cutting tool was found to be better equipped to meet such demands due its rugged geometry base and high tool stiffness values.
Chapter 5

A 3D Finite Element Analysis (FEA) Model for Micro-Groove Cutting of Steel

5.1 Introduction

In Chapter 4, the micro-groove cutting process in steel has been studied through experimentation. These experiments provided useful information about the cutting performance of the meso-scale CBN flexible cutting tool and the characteristics of the machined micro-grooves. However, there is a limit on how much information can be gathered from the experimental results on the underlying process mechanics for the purpose of process improvements. Further, there are certain aspects of micro-groove cutting process such as chip formation, stress and temperature distribution, etc. that cannot be directly observed through empirical results. These factors can influence the outcome of the machined micro-grooves and the performance of the cutting tool tip. Therefore, it becomes imperative to study the micro-groove cutting through process modeling that can enhance the fundamental understanding and support in efficient process planning.

The objective of this chapter is to develop a 3D finite element analysis (FEA) model for simulating micro-groove cutting in steel. The developed model should be able to:
(1) Accurately account for all the necessary material input parameters that include – elastic, plastic and constituent deformation properties of the workpiece, heat partitioning and friction laws at the tool-workpiece contact regions;

(2) Provide an understanding of chip formation and progression during steady-state micro-groove cutting;

(3) Predict the 3D surface and sub-surface stress and strain fields, cutting and thrust forces and temperature distributions along the tool-chip interface;

(4) Evaluate the side burr formation events that are typical to the micro-groove cutting process.

(5) Simulate the influence of the cutting edge geometry that includes cutting edge radii and tool rake angles on the outcome of the observed process mechanics.

The outline of this chapter is as follows. The first section describes the development of the micro-groove cutting model for simulating trials in steel. Specifically the enhancements to the existing 3D FEA model are discussed. The model assumption, formulation and development of constituent workpiece model properties are discussed in the following section. Next, the chip formation and separation criteria are established that control the deformation and subsequent failure of the workpiece model. An adiabatic heat generation module that is concluded in the model is discussed in the next section. The overall 3D mesh geometry and boundary conditions necessary to perform the simulations are discussed. Following the initial model validation based on experimental results, the simulation experiments are conducted and subsequently evaluated in the last section.
5.2 3D FEA Model Development

5.2.1 Model Enhancement to the Existing 3D FEA Model [106]

Bourne et al. [106] developed a 3D model for micro-groove cutting process simulation to study the micro-groove cutting of a pure aluminum film coated on silicon using a rigid diamond tool tip. While most of the features of this model would remain unchanged in developing the new model for simulating micro-groove cutting in steel using a CBN tool tip, including the definition of the overall mesh geometry, implementation of the explicit dynamics procedure, Lagrangian formulation scheme, use of half-symmetry boundary conditions, chip separation criteria and general contact algorithm, there are a few changes that were necessary.

For example, there is a fundamental change in the material set to deform from aluminum to steel. Therefore, changes have to be made to the material property module of the 3D model including the elastic properties, instantaneous yield strength, damage criterion, etc. These changes will be explained in detail in the following sections. Further, due to the poor thermal conductivity of steel, a suitable heat generation model is needed to be included in the new model. The 3D model also considered an infinitely sharp cutting edge, hence a uniform meshing approach around the cutting edge has to be implemented and a single row of sacrificial elements below the uncut chip was sufficient for failure and chip formation. Since in the new model, different tool geometries with varying cutting edge radii are to be simulated, mesh densities around the cutting edge needs to be refined in order to resolve large strain gradients in regions of strong plastic
deformations. In addition, the layer of sacrificial elements needs to be updated accordingly such that no distorted elements flow underneath the cutting edge.

5.2.2 Model Assumptions

The workpiece material chosen for the current study is austenitized AISI 4340 steel, due to the readily available literature on its material properties. The microstructure of AISI 4340 is shown in Fig. 5.1, which revealed the martensitic grain boundary on the ferrite matrix post its heat treatment. As it can be seen, the average grain size is around 20 µm. This is greater than the length of cut implemented in the 3D FEA model, which is 10.8 µm. Therefore, the plastically deformable workpiece is assumed to be an isotropic and homogenous material for this study, i.e. micro-structural effects of steel are neglected. Also, the micro-groove cutting process involves distinctly 3D stress and strain fields that can only be studied through a 3D model. This was further strengthened by the fact that 2D plane strain assumptions being invalid in the micro-groove cutting process, as pointed out in Section 2.8 of Chapter 2. Moreover, the work material mode should represent the elastic-plastic and thermal behavior of the material deformations that closely follow the complexities of actual the micro-groove cutting process. Specifically, the temperature dependent properties of the workpiece material and associated heat generation and partition scheme are to be inducted into the 3D model. A uncoupled adiabatic heating condition is considered for observing the thermal results from the model.
5.2.3 Model Formulation

The Abaqus/ Explicit software is used in the 3D FEA model development. The explicit method is chosen for this process, as the implicit method may have difficulty in convergence due to the involved contact and material complexities. The explicit analysis improves the physical comprehension of the chip formation and provides a more accurate representation of the complexity of the domain boundaries and material failure. More specifically, an explicit dynamics procedure is implemented that evaluates a large number of small time increments using a central-difference time integration rule and lumped element mass matrices where displacement and velocities are calculated in terms of the quantities known at the beginning of an increment [155]. The accelerations calculated at time ‘t’ will be used to advance the velocity solution to time t + Δt/2 and the displacement solution to time t + Δt/2. The equations governing the motion of the body are integrated using the following equations:
where $M^{NJ}$ is the lumped mass matrix, $P^J$ is the applied load vector and $I^J$ is the internal force vector, $u$ is the nodal displacement, $\ddot{u}$, $\dot{u}$ and $u$ are the acceleration, velocity and displacement vectors, $u^N$ refers to the degree of freedom, either a displacement or rotational component and the subscript ‘i’ refers to the increment number in an explicit dynamics step. The central difference integration is explicit in that the kinematic state may be advanced by known values of $\ddot{u}^{(i-\frac{1}{2})}$ and $\ddot{u}^{(i)}$ from the previous increment. The Abaqus software uses an adaptive algorithm to determine the incrementation scheme and is fully automatic. In order to provide a reasonable number of time increments to observe the micro-groove cutting simulation, a mass scaling factor of 5000 is used that artificially increases material density. The exact estimate of suitable time increments that fits into the 3D FEA model can be found in [106].

An eight (8) node linear hexahedric element (C3D8R) with “relax stiffness” hourglass treatment and reduced integration was selected for modeling the workpiece. The chosen element type was ideal to observe the adiabatic heating effects and large deformations to be studied during the cutting process. The tool is modeled as a 3D rigid (R3D4) non-deformable element. Since the process is a dynamics event that produces large deformation within small time increments, it was necessary to encounter massive
mesh distortion that occurs during the simulation. This necessitates the use of a remeshing procedure to maintain the stability of the mesh during the simulation. An Arbitrary Lagrangian – Eulerian (ALE) adaptive remeshing technique with pure Lagrangian formulation is implemented that provides control of the mesh distortion and maintains the condition of the mesh topology throughout the simulation. The ALE formulation for a pure Lagrangian analysis is shown in Fig. 5.2. This approach doesn’t alter the elements and connectivity of the constructed mesh geometry. A kinematic penalty contact scheme is introduced for remeshing during the cut establishment step with default frequency and remeshing sweeps per increment.

Figure 5.2: Finite element model formulation
5.2.4 Constitutive Workpiece Flow Model

The constitutive flow stress model used for the cutting mechanics problem must be able to accurately describe material behavior at large strains, high strain rates, and high temperatures. The material flow stress is described as instantaneous yield stress under loading and is typically dependent to strain, strain rate, temperature and microstructural effects existing in the material. Johnson-Cook (JC) constitutive material flow stress model is most frequently used thermo-visco-plastic material model for steel when high strain rate and high temperature is involved [156-157]. JC constitutive material flow stress model reflects a particular type of Von Mises plasticity as represented by Eq. (5.4). The flow stress data for metal cutting simulations has been obtained mainly using four methods: high-speed compression tests, Split-Hopkinson’s bar tests, practical machining tests and inverse analysis using FEM techniques.

\[
\bar{\sigma} = \left( A + B\varepsilon^n \right) \left( 1 + C \ln \left( \frac{\dot{\varepsilon}}{\dot{\varepsilon}_p} \right) \right) \left( 1 - \left( \frac{T - T_r}{T_m - T_r} \right)^m \right), \tag{5.4}
\]

where \(\bar{\sigma}\) is the von mises flow stress, A is the yield strength, n is the strain hardening exponent, \(\varepsilon\) is the equivalent plastic strain, B is the strain hardening coefficient, C is the strain rate coefficient, \(\dot{\varepsilon}\) is the equivalent plastic strain rate, T is the current temperature and \(T_m\) is the melting temperature, \(\varepsilon_o\) is the reference plastic strain rate. The constants of the JC constitutive model for AISI 4340 are provided in Table 5.1[156]. The mechanical, elastic and thermal properties of AISI 4340 are summarized in Table 5.2 [159-160].
Table 5.1: JC constitutive model parameters of AISI 4340[156]

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Yield stress, $A$</td>
<td>792 MPa</td>
</tr>
<tr>
<td>Strain hardening coefficient, $B$</td>
<td>510 MPa</td>
</tr>
<tr>
<td>Strain rate factor, $C$</td>
<td>0.014</td>
</tr>
<tr>
<td>Strain exponent, $\varepsilon$</td>
<td>0.26</td>
</tr>
<tr>
<td>Temperature exponent, $m$</td>
<td>1.03</td>
</tr>
<tr>
<td>Reference plastic strain rate, $\varepsilon_0$</td>
<td>$1s^{-1}$</td>
</tr>
<tr>
<td>Current Temperature, $T$</td>
<td>20 °C</td>
</tr>
</tbody>
</table>

Table 5.2: Mechanical and thermal properties of AISI 4340 [159-160]

<table>
<thead>
<tr>
<th>Property</th>
<th>AISI 4340</th>
</tr>
</thead>
<tbody>
<tr>
<td>Modulus of Elasticity ‘E’, GPa</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Temp (˚C)</td>
</tr>
<tr>
<td></td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>150</td>
</tr>
<tr>
<td></td>
<td>250</td>
</tr>
<tr>
<td></td>
<td>350</td>
</tr>
<tr>
<td>Poisson Ratio ‘$\nu$’</td>
<td>0.3</td>
</tr>
<tr>
<td>Density, ‘$\rho$’, (kg/m$^3$)</td>
<td>7830</td>
</tr>
<tr>
<td>Heat treatment</td>
<td>Austenitized</td>
</tr>
<tr>
<td>Hardness</td>
<td>30 HRc</td>
</tr>
<tr>
<td>Thermal Conductivity, ‘$k$’, (W/m ˚C)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>100</td>
</tr>
<tr>
<td></td>
<td>200</td>
</tr>
<tr>
<td></td>
<td>400</td>
</tr>
<tr>
<td></td>
<td>600</td>
</tr>
</tbody>
</table>
Table 5.2 (Cont.): Mechanical and thermal properties of AISI 4340 [159-160]

<table>
<thead>
<tr>
<th>Property</th>
<th>AISI 4340</th>
</tr>
</thead>
<tbody>
<tr>
<td>Melting Temperature (°C)</td>
<td>1520</td>
</tr>
<tr>
<td>Linear coefficient of thermal expansion, ‘α’, (μm/m·°C)</td>
<td></td>
</tr>
<tr>
<td>100</td>
<td>12.4</td>
</tr>
<tr>
<td>300</td>
<td>13.6</td>
</tr>
<tr>
<td>500</td>
<td>14.3</td>
</tr>
<tr>
<td>Specific Heat, ‘C_p’, (W/m·°C)</td>
<td></td>
</tr>
<tr>
<td>100</td>
<td>473</td>
</tr>
<tr>
<td>300</td>
<td>519</td>
</tr>
<tr>
<td>500</td>
<td>561</td>
</tr>
</tbody>
</table>

5.2.5 Chip Separation & Failure Model

Chip formation is defined to occur along the defined narrow separation line ahead of the cutting edge of the tool. A narrow line of sacrificial elements is modeled and separates the chip region from the workpiece region. This strategy is adopted from Subbiah et al. [161]. Separation of the chip from the workpiece is accomplished via failure and deletion of the sacrificial elements since this is an approach that is readily implemented in Abaqus / Explicit. A more detailed explanation on material failure model and crack initialization can be found in [106].

The failure model for AISI 4340 steel is obtained using a combined approach of ductile criterion condition and Johnson-Cook damage equations based on shear fracture. The failure at the chip entry step of the simulation is modeled using a ductile criterion condition. It is a phenomenological model used for predicting the onset of damage due to nucleation, growth, and coalescence of voids. The model assumes that the equivalent
plastic strain at the onset of damage is a function of stress triaxiality and strain rate, given by Eq. (5.5):

$$\frac{\varepsilon_{pl}}{\varepsilon_D} = f \eta, \dot{\varepsilon}^{pl},$$

(5.5)

where is the stress triaxiality, as defined by Eq. (5.6), is the equivalent plastic strain rate, is the mean principle stress and is the hydrostatic stress

$$\eta = \frac{\sigma_m}{\sigma},$$

(5.6)

where is the mean principle stress and is the Von Mises equivalent stress. It follows from literature that stress triaxiality plays a critical role in ductile crack formation [162]. The equivalent plastic strain for fracture tends to decrease with increase in stress triaxiality [162-163]. A cut-off value of stress triaxiality equal to -1/3 was empirically determined, below which fracture never occurred in Steel [163]. However, this limit criterion is not followed by the Johnson-Cook (JC) damage model that is used in micro-groove cutting simulations. In order to compensate for the limitation in the JC damage model, user-defined values of equivalent plastic strain for fracture in the cut-off range of stress triaxiality for AISI 4340 is provided by the user [157]. The damage evolution at the start of chip propagation is entered in a tabular fashion from the values extracted from Fig. 5.3, where equivalent fracture strain at different strain rates (ER) and temperatures (C) for AISI 4340 is provided for the variations in the stress triaxiality values.
In the rest of model, crack propagation is defined such that shear fracture mechanism alone is considered for failure and the criterion for damage initiation is met when the following condition is satisfied:

\[ \omega_D = \frac{d\bar{\varepsilon}^{pl}}{\bar{\varepsilon}_D^{pl} \eta, \dot{\varepsilon}^{pl}} = 1, \quad (5.7) \]

where \( \omega_D \) is a state variable that increases monotonically with plastic deformation. At each increment during the analysis the incremental increase in \( \omega_D \) is computed as:

\[ \Delta \omega_D = \frac{\Delta \bar{\varepsilon}^{pl}}{\bar{\varepsilon}_D^{pl} \eta, \dot{\varepsilon}^{pl}} \geq 0. \quad (5.8) \]
The value of \( \bar{\varepsilon}_D \) during each step is derived using the Johnson–Cook (JC) damage model, given by Eq. (5.9) [156].

\[
\bar{\varepsilon}_D^{pl} \eta, \dot{\varepsilon} = \left[ d_1 + d_2 e^{-d_3 \eta} \right] \left[ 1 + d_4 \ln \left( \frac{\dot{\varepsilon}^{pl}}{\dot{\varepsilon}_0} \right) \right] \left[ 1 + d_5 \hat{\theta} \right], \tag{5.9}
\]

where \( d_1-d_5 \) are material constants provided in Table 5.3, \( \eta \) is the stress triaxiality factor, \( \hat{\theta} \) is the non-dimensional temperature defined by Eq. (5.10). Following the initiation of damage, the properties of the damaged material can be made to degrade, which is followed by material failure. Alternatively, the material can be assumed to immediately fail upon damage initiation, which is a commonly used approach in metal cutting that is also used here.

\[
\hat{\theta} = \begin{cases} 
0, & \text{For } \theta < \theta_{\text{transition}} \\
\frac{\theta - \theta_{\text{transition}}}{\theta_{\text{melt}} - \theta_{\text{transition}}}, & \text{For } \theta_{\text{transition}} \leq \theta \leq \theta_{\text{melt}} \\
1, & \text{For } \theta > \theta_{\text{melt}}
\end{cases} \tag{5.10}
\]

where \( \theta \) is the current temperature, \( \theta_{\text{melt}} \) is the melting temperature, and \( \theta_{\text{transition}} \) is the transition temperature defined as the one at or below which there is no temperature dependence on the expression of the damage strain \( \bar{\varepsilon}_D \). The material parameters are measured at or below the transition temperature.
Table 5.3: Johnson and Cook Damage law parameters [156]

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial failure strain, $d_1$</td>
<td>0.05</td>
</tr>
<tr>
<td>Exponential factor, $d_2$</td>
<td>3.44</td>
</tr>
<tr>
<td>Triaxiality factor, $d_3$</td>
<td>2.12</td>
</tr>
<tr>
<td>Strain rate factor, $d_4$</td>
<td>0.002</td>
</tr>
<tr>
<td>Temperature factor, $d_5$</td>
<td>0.61</td>
</tr>
<tr>
<td>Transition Temperature</td>
<td>1520 °C</td>
</tr>
</tbody>
</table>

5.2.6 Tool-Chip Interface Model

Friction at the tool-chip interface is modeled using an extended coulomb friction model, based on the Zorev’s model [166]. It considers two distinct regions of sliding ($l_c - l_p$) and sticking ($l_p$) at the tool-chip interface as shown in Fig. 5.3. In the sliding region, the contact pressure is low and the relative motion occurs between the tool and the chip, and the predicted frictional shear stress $\tau_{\text{fric}}$, is below a set limit, $\tau_{\text{crit}}$ and is given by a constant coefficient of friction, $\mu$. In the sticking region, there is high contact pressure on the rake face of the tool tip as shown in Fig. 5.4 and predicted frictional shear stress exceeds the limit and is given by the limit itself. Equation 5.11 explains the two regions as:

$$
\tau_{\text{fric}} = \begin{cases} 
\mu p_{\text{contact}} & \text{if } \mu p_{\text{contact}} < \tau_{\text{crit}} \quad \text{sliding region, } 0 < l_p \\
\tau_{\text{crit}} & \text{if } \mu p_{\text{contact}} \geq \tau_{\text{crit}} \quad \text{sticking region, } l_p < l_c 
\end{cases} \quad (5.11)
$$
The basis for the approach is that the pressure on the rake face of the tool tip during micro-groove cutting machining can become so high that the frictional shear stress exceeds the shear strength of the material at the tool-chip interface. This effect causes seizing of a thin contact layer on the chip and localized plastic flow of the underlying chip material. For this study, the threshold for shear frictional stress is set as 549MPa [159-160, 167] and coefficient of friction value, $\mu$ is set to 0.5 [167].

5.2.7 Heat Generation Model

In micro-groove cutting, heat is generated in the primary shear zone due to plastic shearing and in the secondary shear zone, due to both plastic deformation and sliding frictional effects. The main regions of heat generation during micro-groove cutting process are shown in Fig. 5.5 [168]. Firstly, heat is generated in the primary deformation zone due to plastic work done at the shear plane. The local heating and thermal softening effects of 4340 steel workpiece allows greater deformation. Secondly, heat is generated in
the secondary deformation zone due to work done in deforming the chip and in overcoming the sliding friction at the tool-chip interface zone. The heat generated in the tertiary deformation zone, at the tool workpiece interface, is due to the work done to overcome friction, which occurs at the rubbing contact between the tool flank face.

![Diagram of heat generation](image)

**Figure 5.5:** Heat generation during micro-groove cutting

Heat generation for this study is modeled using uncoupled adiabatic analysis. Many researchers that have chosen to address heat generation using the same assumption [158-159, 161, 169] where heat generation due to local energy dissipation occurs so quickly that it doesn’t have the time to diffuse away and local heating will occur in the active primary and secondary deformation zones. Hence, adiabatic heating is assumed where each integration point is treated as if they are thermally insulated from its neighbors and all thermal energy within an element remains in that element. This approach simplifies computation and reduces required computer time. Heat generated considered for analysis is the result of (a) plastic deformation and (b) friction. Let $\Delta T_p$ be the local temperature rise in the workpiece and chip induced by plastic work in a time...
interval. Under adiabatic conditions, the heat generation rate due to inelastic work is given by Eq. (5.12):

\[ \dot{q}_p = \rho C_p \frac{\Delta T_p}{\Delta t} = \eta_p \sigma \dot{\varepsilon}_p, \]  

(5.12)

where \( \rho \) is mass density, \( C_p \) is the specific heat of AISI 4340, \( \eta_p \) is the inelastic heat fraction, given by Taylor-Quinney equation [169], \( \sigma \) is effective mises stress and \( \dot{\varepsilon}_p \) is effective plastic strain rate. The inelastic heat fraction is set to 0.90 for all the calculations.

Similarly, heat generation due to friction, is given by Eq. (5.13):

\[ \dot{q}_p = \rho C_p \frac{\Delta T_f}{\Delta t} = \eta_f J \tau_{fric} \dot{\gamma}, \]  

(5.13)

where \( \Delta T_f \) is the local temperature rise caused by friction under adiabatic conditions, \( \dot{\gamma} \) is shear strain rate, \( \eta_f \) is the fraction of dissipated heat due to friction, \( \tau_{fric} \) is the frictional shear stress provided by the extended coulomb’s model and \( J \) is equivalent heat conversion factor that provides the amount of frictional heat transferred to the tool tip.

The fraction of friction-induced heat along the tool-chip interface that goes into the chip is taken to be 0.5 in this study [159, 161, and 170]. Consequently, the overall thermal energy equation defining the temperature fields is given by Eq. (5.14):

\[ k \nabla^2 T - \rho C_p \frac{\Delta T}{\Delta t} + \dot{q} = 0, \]  

(5.14)

where \( \dot{q} = q_f + q_p \) and \( k \) is thermal conductivity of AISI 4340 and remaining variable are defined in earlier equations.
5.3 FEA Mesh Geometry

The selected type of workpiece mesh elements used to construct the geometry are same as used in [106]. The overall model is shown in Fig. 5.6, which makes use of half-symmetry. This reduces the element count and nodal solutions that help improve the computational efficiency. The direction of cut is in the negative x direction. The workpiece portion of the model consists of five parts that are meshed separately, and then joined using surface-based mesh tie constraints [106]. Additionally, two sets of AISI 4340 properties were used – one that allows elastic behavior under flow stress properties and the other for element failure and deletion, in order to ensure chip separation from workpiece. The cutting tool tip is modeled using 3D planar elements, which will be discussed in more detail in the coming sections of the chapter.

![Figure 5.6: Overall model geometry](image-url)
Also, in order for separation of the chip to occur, a region of elements in line with the cutting edge was made to fail. This configuration insures that the bottom of the chip separates from the workpiece exactly at the cutting edge of the tool tip. Moreover, the height of the sacrificial layer elements was always kept higher than the cutting edge radius of the tool tip as shown in Figs. 5.7(A) and (B) for a sharp tool and chamfer tool, respectively. This was necessary for different tool edge radii considered in the simulation experiments. If the height of the sacrificial layer elements is less than the cutting edge radii of the tool, then, upon element failure when the damage criterion is met, the nodes on the top of the surface of the sacrificial elements may tend to slip underneath the flank face of the tool instead of climbing up the rake face. Hence, the sacrificial layer height is always maintained higher than the cutting edge radii of the tool tip. Further, a separate rigid chip guide part is placed such that it extends from the cutting edge of the tool tip and prevents nodes from moving in physically unrealistic manner [106]. Since this part is not physically real, contact between the chip guide and the workpiece is taken to be frictionless.

**Figure 5.7:** Description of the sacrificial layer for: (A) Sharp tool and (B) Chamfer tool
5.4 Boundary Conditions

The full simulation is broken into five time steps: the initial step (0.0 msec), the cut entry step (0.242 msec), the cut established step (1.32 msec), the cut exit step (0.6 msec), and the cut clear step (0.25msec), where the number is the brackets represent the duration of the step. These steps correspond establishment of initial conditions, initial entry of the tool into the workpiece, steady-state cutting, exit of the tool from the workpiece, and moving the tool past the workpiece. During each step, boundary conditions are applied, maintained, or modified as provided by [106]. In addition, a predefined tool temperature of 20 °C was initialized at the cut entry step and a temperature boundary condition was included at the faces, where the modeled cut workpiece joint the uncut section for adiabatic analysis. The contact between the tool and workpiece is modeled using a general contact algorithm as provided by the Abaqus software/ Explicit 6.9.

5.5 Design of Simulation Experiments

The focus of the modeling study is to investigate the effect of different depth of cuts, cutting speeds, cutting edge radii and rake angles on the outcome of the resultant 3D stress/strain fields, temperature distributions, thrust and cutting force exertion and side burr formation. The systematic numerical approach helps develop numerical model for better predictions of the quality of micro-grooves produced. Especially, the burr formations are of concern because they affect the ability to produce groove cross-sections with a specified tolerance. Hence, side burr study has been performed for a comprehensive understanding of the phenomenon. The organization of the simulation
study is as follows. In the first section, two different depths of cut (cutting load) are considered at two cutting speeds for a fixed cutting tool edge radius and rake angle. The measurable parameters from the experiments are then compared with the simulated results to validate the accuracy of the model. In the final section, the depth of cut and cutting speed are maintained constant, while different combinations of cutting edge radii and rake angle conditions are imposed on the micro-groove cutting simulations to predict the intensity variations of the observed process mechanics.

The two load conditions and cutting speed considered are 4.6mN and 9.5 mN; and 100 mm/min and 300 mm/min, respectively. The resultant depth of cut from the two load conditions were 0.501 µm and 0.964 µm, respectively at a cutting speed of 100mm/min and 0.506 µm and 0.978 µm, respectively at a cutting speed of 300 mm/min. For simulating the micro-groove cutting process, a predetermined depth of cut is initialized, which are 0.500 µm and 0.960 µm for the respective experimental load conditions. Likewise, to study the influence of tool geometry, a constant depth of cut of 0.500 µm and cutting speed of 300 mm/min were maintained, and four different cutting edge combination of the CBN tool tip were considered, namely - R0, R60, R120 and C50, where ‘R’ represents cutting edge radius and ‘C’ represents chamfer length and the units of the numbers are in nm. The other tool angles that support the cutting edge radius were fixed to a constant value. The three rake angle conditions considered include - 0°, 10° and -5°. A schematic of the round tool and chamfer tool is provided in Fig. 5.8, where α, the rake angle and γ, the end clearance angle for both the tools are maintained the same throughout the simulations as explained in Table 5.4.
Furthermore, all the tools had to be modeled with rounded bottom radius of 945 nm and side non-cutting edge radius of 60 nm to account for the features produced as a result of FIB machining during the actual cutting process. The rake face and cutting edge profiles of the FIB-machined tool are shown in Figs. 5.9(I) and 5.10(II), respectively. The width of the cutting tool tip was measured to be 1.25 µm. This condition was reflected on the half-symmetry rake face profiles and cutting edge geometries tried out in the simulation trials as shown in Figs. 5.9 (A-D) and 5.10(A-D), respectively. The details of the simulation experiments to be performed are summarized in Table 5.5.

**Table 5.4:** Cutting tool geometry parameters

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Round Tool</th>
<th>Chamfer Tool</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tool Rake Angle (α)</td>
<td>0°</td>
<td>0°</td>
</tr>
<tr>
<td>End Clearance Angle (γ)</td>
<td>20°</td>
<td>20°</td>
</tr>
<tr>
<td>Side Clearance Angle</td>
<td>5°</td>
<td>5°</td>
</tr>
<tr>
<td>Non-Cutting Edge Radius(nm)</td>
<td>60</td>
<td>60</td>
</tr>
<tr>
<td>Cutting Edge (nm)</td>
<td>R0, R60, R120</td>
<td>C 50</td>
</tr>
<tr>
<td>Chamfer Angle(β)</td>
<td>-</td>
<td>15°</td>
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**Figure 5.9:** Rake face profile of various CBN tool tip geometries: (I) FIB-machined; (A) Sharp Tool with infinite edge radius (R0); (B) Round tool with 60nm edge radius; (C) Round tool with 120 nm edge radius; and (D) Chamfer tool with 50nm chamfer length.

**Figure 5.10:** Side profile of various CBN tool tip geometries: (II) FIB-machined; (A) Sharp Tool (R0); (B) Round tool with 60nm edge radius; (C) Round tool with 120 nm edge radius; and (D) Chamfer tool.
Table 5.5: Schedule of micro-groove cutting simulation trials

<table>
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<tr>
<th>Schedule</th>
<th>Rake Angle (deg)</th>
<th>Cutting Speed (mm/min)</th>
<th>Friction coefficient</th>
<th>Cut Depth (nm)</th>
<th>Cutting Edge Geometry (nm)</th>
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</thead>
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<tr>
<td>Simulation Trial 1</td>
<td>0</td>
<td>300</td>
<td></td>
<td>500</td>
<td>R0</td>
</tr>
<tr>
<td>Simulation Trial 2</td>
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<td>0.5</td>
<td>500</td>
<td>960</td>
</tr>
<tr>
<td>Simulation Trial 3</td>
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<td></td>
<td>500</td>
<td>R0</td>
</tr>
<tr>
<td></td>
<td>0</td>
<td></td>
<td></td>
<td>960</td>
<td>R60</td>
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<td></td>
<td>-5</td>
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<td></td>
<td></td>
<td>R120</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>C50</td>
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<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>R0</td>
</tr>
</tbody>
</table>

5.6 Experiments in AISI 4340 Steel

In order to validate the simulation trials of micro-groove cutting of AISI 4340 steel, the cutting experiments were performed using the exact machining parameters that are to be applied in the 3D FEA model. As stated in the previous section, the two cutting loads considered were 4.6 mN and 9.5 mN; and two cutting speeds were 100 mm/min and 300 mm/min, respectively. A FIB-machined single CBN crystal tool tip, 1.13µm wide, was used during both the experiments, which was similar to the half-symmetry cutting tool tip used for the simulation trials. The AFM cross-sectional results of the machined micro-grooves are shown in Fig. 5.11, where a load of 4.6 mN and 9.5 mN and cutting speed of 100 mm/min yielded a depth of cut of 0.501 µm and 0.964 µm, respectively and 0.506 µm and 0.978 µm for loads of 4.6 mN and 9.5 mN at a cutting speed of 300 mm/min, respectively. The chip from both the load conditions and cutting speeds are shown via SEM images in Figs. 5.12(A) to (D), respectively. Table 5.6 summarizes the results of the experiments that include the chip formation and side burr
height information. Procedure described earlier in Section 4.3.2 was used for the side burr height measurement.

![Graph showing Groove Width vs Groove Depth](image)

**Figure 5.11:** Micro-Grooves cut on AISI 4340

![SEM images of chips formed](image)

**Figure 5.12:** Chip formed at: (A) 4.6mN load and (B) 9.5mN load at 300mm/min; (C) 4.6 mN load and (D) 9.5mN load at 100 mm/min
Table 5.6: Micro-groove and chip characteristics of AISI 4340 steel

<table>
<thead>
<tr>
<th>Conditions</th>
<th>Load (mN)</th>
<th>Speed (mm / min)</th>
<th>Tool Tip Width (µm)</th>
<th>Groove Shape</th>
<th>Mean Depth (µm)</th>
<th>Mean Width (µm)</th>
<th>Left Burr Height (nm)</th>
<th>Right Burr Height (nm)</th>
<th>Chip Characteristics</th>
<th>Mean Chip Thick (µm)</th>
<th>Chip Thick Std (µm)</th>
<th>Mean Chip Width (µm)</th>
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<tr>
<td></td>
<td>4.6</td>
<td>100</td>
<td>1.14</td>
<td>Mean Depth</td>
<td>0.501</td>
<td>1.140</td>
<td>87</td>
<td>108</td>
<td>Mean Chip Thick</td>
<td>0.951</td>
<td>0.015</td>
<td>1.076</td>
</tr>
<tr>
<td></td>
<td>9.5</td>
<td>100</td>
<td>1.08</td>
<td>Mean Width</td>
<td>0.964</td>
<td>1.086</td>
<td>205</td>
<td>180</td>
<td>Chip Thick Std</td>
<td>0.011</td>
<td>0.015</td>
<td>1.342</td>
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<tr>
<td></td>
<td>4.6</td>
<td>300</td>
<td>1.16</td>
<td>Left Burr</td>
<td>0.506</td>
<td>1.170</td>
<td>77</td>
<td>91</td>
<td>Mean Chip Width</td>
<td>1.116</td>
<td>0.012</td>
<td>1.116</td>
</tr>
<tr>
<td></td>
<td>9.5</td>
<td>300</td>
<td>1.10</td>
<td>Right Burr</td>
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<td>1.096</td>
<td>188</td>
<td>199</td>
<td></td>
<td></td>
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</tr>
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</table>

5.7 Simulation Results & Discussion

In the following three sub-sections, the simulated results are studied for two different depths of cuts performed at two levels of cutting speed. The ability of the simulated model for steady-state chip formation and cutting force prediction capabilities are studied at both the depths of cut for a fixed cutting speed condition. This is followed by the model validation with the experimental data. The experimental side burr formation obtained by AFM scans and deformed chip thickness measurements from SEM images are used to aid the 3D FEA model validation. The influence of cutting tool geometry and tool rake angle conditions are then systematically analyzed for their impact on the outcome of the process mechanics – mainly on the surface and sub-surface stress-strain distribution, side burr formation, temperature effects and resultant cutting forces experienced by the tool.

5.7.1 Steady-State Cutting Analysis

The simulation results for steady-state chip formation process at depth of cut of 0.500 µm and 0.960 µm using a sharp tool and 0° rake angle at a cutting speed of 300
mm/min are shown in Fig. 5.13 and 5.14, respectively. The figure shows four plots of the chip flow geometry at different instances of the cutting process. In each of these simulations the cutting speed was 300 mm/min and cutting was allowed to progress until steady-state chip formation occurred. The last two plots show the instance at an elapse time of 0.65 ms and 1.0 ms since the cutting begins, where the tool-chip contact length maintains a constant steady-state value. In all cases, it can be seen that a chip separates from the workpiece, flows up the rake face, and separates from the rake face. Side burrs are formed on the side of each groove. Notice that the chip contact length at steady-state increases with increase in depth of cut, as shown in the respective figures. The contact lengths for a 0.500 µm and 0.960 µm depth of cut were 1.1 µm and 1.6 µm, respectively. The steady state chip formation process didn’t show any difference in behavior while modeling at a lower cutting speed of 100 mm/min. Therefore, for the subsequent part of this section, involving stress and strain distribution studies, the cutting speed of 300 mm/min alone is considered for discussion.

![Figure 5.13: Chip formation process for a depth of cut of 0.500 µm and speed of 300 mm/min at: (A) Chip Initialization at t = 0.22ms; (B) Chip Growth at t = 0.48ms; (C) Steady-state chip formation at t = 0.65 ms; and (D) Steady-state chip formation at 1.0 ms](image-url)
Figure 5.14: Chip formation process for a depth of cut of 0.960 µm and speed of 300mm/min at: (A) Chip Initialization at $t = 0.22\text{ms}$; (B) Chip Growth at $t = 0.45\text{ms}$; (C) Steady-state chip formation at $t = 0.62\text{ms}$; and (D) Steady-state chip formation at 1.0 ms.

Figures 5.15 (A) and 5.16(A) show the Von Mises Stress (MPa) at the symmetry plane and on the cut groove surface for a depth of cut of 0.500 µm and 0.960 µm machined at 300 mm/min cutting speed, respectively. It can be seen that the stress gradients translate smoothly over the mesh discontinuities where the fine mesh is attached to a coarser mesh. The highest stresses are observed at the primary shear zone forming a distinct dense band of contours in a region where the deformed chip separates the unreformed chip region, as shown in the magnified region of Fig. 5.15(A). The primary shear zone is formed at an angle with the lateral direction of the performed cut, providing the shear angle. The size of this band tends to increase with increase in depth of cut, while the maximum Von Mises yield stress remains constant, as it is a material dependent failure property.
Figure 5.15: (A) Symmetry Von Mises Stress (MPa) and (B) Equivalent plastic strain at a depth of cut of 0.500 µm

Figures 5.15 (B) and 5.16(B) show the plastic equivalent strain (PEEQ) plots at steady-state cutting condition for a depth of cut of 0.500 µm and 0.960 µm, respectively. As can be seen in the both the images, the maximum plastic strain was seen to occur in two distinct regions one at the top side edge of the deformed chip and maximum values occurred in a band-like region formed at the secondary shear zone at the tool-chip interface, and second at the primary shear zone. This can be seen in the magnified region of the too-chip interface shown in Fig. 5.15(B) and 5.16(B). The highest PEEQ value reached in the range of 9.33-11.53 during the last stages of the cut indicating that the material is being highly distorted. The highest plastic strain on the workpiece seemed to occur at the secondary shear zone region at the tool-chip interface.
As pointed out in Section 5.2, an FEA model for simulating metal cutting process at any scale involves high strain rates in the order of $10^{-4}$ sec$^{-1}$ and above. In order to check the applicability of the developed model, it was essential to examine cutting dynamics predicted by the 3D FEA model at high strain rates. Figures 5.17(A) and (B) show the maximum principle strain rate line contours during the micro-groove cutting simulation using a sharp tool at 500 µm and 960 µm, respectively machined at a cutting speed of 300 mm/min. As can be seen in the Fig. 5.17, the maximum strain rate was 42749 sec$^{-1}$, mostly at the rake face of the cutting tool tip. More importantly, it can be observed that most of the material experienced a strain rate of 20,000 sec$^{-1}$ or less. This indicates that the workpiece material model must be valid up to these very high strain rates.
rates. Furthermore, it extends the use of the JC flow stress model parameters for 3D FEA analysis for AISI 4340 steel.

Figure 5.17: Maximum principle strain rate contours (sec$^{-1}$) predictions for a depth of cut of :(A) 0.500 µm, and (B) 0.960 µm, at cutting speed of 300mm/min

5.7.2 Model Validation

The 3D FEA model is validated by comparing the simulated side burr height information at a given depth of cut to the data obtained from the actual micro-groove cutting experiments. The cross-sectional 2D AFM plots from the experiments are superimposed on the simulated groove cross-section of a deformed mesh obtained from a fixed frame of reference in the steady-state cutting regime, while maintaining the same aspect ratio for the both cross-sections. The mean side burr height is calculated as the vertical displacement of highest point on the 2D AFM curve from the horizontal reference line that represents the undeformed surface (at zero depth of cut); similarly for
the simulations, the vertical height from one side of the deformed mesh is calculated as burr height, while the other side is mirrored along the axis of symmetry. Figure 5.18(A) shows the 2D AFM line profile of the experimental micro-groove machined at a depth of 0.964 μm (9.6mN) using cutting speed of 100 mm/min superimposed on the simulated micro-groove cross-section (mesh) under the same conditions extracted from a chosen y-z reference frame at steady-state. Figure 5.18(B) shows the 2D AFM line profile of the experimental micro-groove machined at a depth of 0.964 μm using cutting speed of 300 mm/min superimposed on the simulated micro-groove cross-section (mesh) under the same conditions. Similarly, Fig. 5.18(C) and (D) represent the experimental and simulated plots for the depth of cut of 0.500 μm machined at 100mm/min and 300 mm/min., respectively. Burr height predictions also captured the trend of increase in side burr height with increase in depth of cut, as seen in all the figures. Note that the left side of the experimental cross-sections found via AFM measurement does not line up as well with the predicted groove cross-sections. This is due to tilt-based distortion caused by the high-aspect ratio AFM probes.

**Figure 5.18(A):** Experimental and simulated micro-groove cross-sections at depth of cut of 0.960 μm and cutting speed of 100 mm /min
Figure 5.18 (B): Experimental and simulated micro-groove cross-sections at depth of cut of 0.960 µm and cutting speed of 300 mm /min

Figure 5.18(C): Experimental and simulated micro-groove cross-sections at depth of cut of 0.500 µm and cutting speed of 100 mm /min
**Figure 5.18(D):** Experimental and simulated micro-groove cross-sections at depth of cut of 0.500 µm and cutting speed of 300 mm/min

The deformed chip thickness from the simulations is also compared with respective values obtained from the experiments. The schematic of simulated chip thickness measurement from the symmetry plane at the center of the tool-chip contact length is shown in Fig. 5.19, where \( t_c \) is the deformed chip thickness. The SEM images of the chip roots obtained from micro-groove cutting tests shown earlier in Figs. 5.12(A-D) are used to calculate the experimental deformed chip thickness. The experimental measurements of the deformed chip thickness obtained from SEM image data are plotted with a corresponding \( \pm 1\sigma \) from the expected mean value.

**Figure 5.19:** Schematic of Chip formation at 0.500 µm depth of cut
Figure 5.20 shows the validation results for chip thickness measurement between the experimental and simulated trials. Table 5.7 summarizes the groove characteristics and chip thickness information from both experimental and simulated results. It can be seen in Table 5.7 and Fig. 20 that chip thickness prediction errors are within 7.0 – 28%. The side burr height prediction errors are within 1– 5%. The predictions are more accurate at lower depths of cut, irrespective of the value of the cutting speed implemented. The deformed chip thickness increased with increase in depth of cut and decreased with increase in cutting speed for both experimental and simulated results. However, the predicted chip thickness was larger than the experimentally observed chip thickness in all cases due to uncertainties in the assumed plastic flow in the simulations and due to the fact that there are variations in the coefficient of friction between the tool-chip interface and the limiting shear stress controlling the chip formation considered in the simulations compared to the actual experimental conditions. While error between experimental and simulated values were under 10% at higher depths of cut for both high and low cutting speeds considered for the study, the error percentage at higher cutting speed increased from 18.2% to 27.7% with the increase in depth of cut. These errors are found to be larger than side burr prediction errors. The possible reason for this trend is the scale of the model being under discussion. While the chip formation measurements from the experiments reflect a behavior observed over thousands of microns, the simulated model predicts the chip formation as a large ductile plastic flow over less than 10 μm. In light of the model’s intended use, the ability to predict chip flow and burr height are considered important measures for assessing its accuracy.
Figure 5.20: Simulated and experimentally calculated chip thickness

Table 5.7: Summary of validation results

<table>
<thead>
<tr>
<th>Exp. Depth of Cut (µm)</th>
<th>Sim. Depth of Cut (µm)</th>
<th>Cutting Speed (mm/min)</th>
<th>Characteristic</th>
<th>Exp. Value</th>
<th>Sim. Value</th>
<th>Error</th>
</tr>
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<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Chip Thickness (µm)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>501</td>
<td>500</td>
<td>100</td>
<td></td>
<td>0.95</td>
<td>1.162</td>
<td>18.23%</td>
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<td>964</td>
<td>960</td>
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<td>506</td>
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<td>300</td>
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<td>0.76</td>
<td>1.198</td>
<td>27.7%</td>
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<tr>
<td>978</td>
<td>960</td>
<td>300</td>
<td></td>
<td>1.3</td>
<td>1.358</td>
<td>6.54%</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Side Burr Height (nm)</td>
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<td>100</td>
<td></td>
<td>87,108</td>
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<td>77,91</td>
<td>89</td>
<td>1.8%</td>
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<tr>
<td>978</td>
<td>960</td>
<td>300</td>
<td></td>
<td>188,199</td>
<td>195</td>
<td>0.89%</td>
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5.7.3 Force Prediction

The process reached steady-state cutting around 0.5ms after the initiation of the cut, as shown by the respective force plots shown in Fig. 5.21(A) and (B) for the two load conditions. Note that these values reflect the values at half-symmetry plane conditions.
As seen from the figures, the cutting and thrust force ramp up at the initialization of the cut and settle at a constant value that indicates steady-state cutting. The direction of the forces shown in the figures is based on the simulation model analysis, extracted along the negative y-axis (Refer Fig. 5.6). The time interval for reaching steady-state condition is similar to what was predicted by the steady-state chip formation plots (Refer Figs. 5.13 and 5.14). Beyond a certain chip contact length on the rake face, the cutting and thrust forces experienced by the tool tip does not increase with increase in chip length. The oscillations in forces seen in the graphs are due to discrete finite element failure in the 3D FEA model during chip separation sequence and are not expected in the actual cutting process.

The average cutting force experienced by the tool tip was 0.587 mN and 1.298 mN for a depth of cut of 0.500 µm and 0.960 µm, respectively. It is noticed that the cutting force plots reached steady-state faster during the 0.500 µm depth of cut than 0.960 µm. It is also clear that at steady-state cutting, the tool tip experiences more force fluctuations while cutting at a higher depth of cut. Further, it can be seen from Fig. 5.21 that average thrust force predicted by the mode was 0.1 mN and 0.35 mN, respectively for a depth of cut of 0.500 µm and 0.960 µm. These values were less than the actual thrust force applied during the experiment, which was 4.5mN and 9.6 mN, respectively for a depth of cut of 0.506 µm and 0.968 µm. Therefore, the efficiency of the 3D FEA models in thrust force predictions is not accurate. This is typically due to the fact that the simulation model is based on macro-scale orthogonal cutting model where thrust forces are negligible in the direction of cutting. Table 5.8 summarizes the results for he stress-strain distributions and the cutting forces for the two depths of cut and cutting speed.
conditions discussed thus far. It can be seen from the summarized results that the average values of the observed responses tend to decrease with increase in cutting speed, albeit marginally and tend to show a considerable increase with the increase in depth of cut.

\[ \text{Figure 5.21: Half-symmetry thrust and cutting forces for a depth of cut of: (A) 0.500 \, \mu m and (B) 0.960 \, \mu m} \]

\[ \text{Table 5.8: Observed steady-state values of the process mechanics} \]

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Values</th>
</tr>
</thead>
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<tr>
<td>Speed (mm/min.)</td>
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<tr>
<td>Depth of Cut (\mu m)</td>
<td>0.500</td>
</tr>
<tr>
<td></td>
<td>0.960</td>
</tr>
<tr>
<td></td>
<td>0.500</td>
</tr>
<tr>
<td></td>
<td>0.960</td>
</tr>
<tr>
<td>Von Mises Stress (MPa)</td>
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<tr>
<td>Plastic Equivalent Strain</td>
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<tr>
<td>Cutting Force (mN)</td>
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<td>1.37</td>
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<td></td>
<td>0.471</td>
</tr>
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<td>1.108</td>
</tr>
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</table>
5.7.4 Influence of Cutting Tool Geometry

To this end, the 3D FEA model was established to study the micro-groove cutting process in steel at two different depths of cut and two different cutting speeds. The objective of this section is to study the influence of the cutting tool geometry for a given set of machining conditions (groove depth, cutting speed) on the stress-strain distributions, side burr formation, temperature gradients along the tool-chip interface and predicted cutting forces. This study is carried out at two levels – (1) Variations in the cutting edge radius of the tool tip for a fixed 0 deg. rake angle; and (2) Variations in the tool rake angle for a fixed infinitely sharp cutting edge radius.

(A) Cutting Edge Radius

In order to understand the influence of cutting tool edge geometry, four different cutting edge geometries were considered as shown in Figs. 5.9 and 5.10. Recollect from Chapter 4, Section 4.5 that the most suitable machining parameters in order to achieve the desired micro-groove features with minimal burr formation and required tolerance were found to be at lower depths of cut, zero degree rake angle and high cutting speeds. Therefore, for this study the depth of cut, rake angle and the cutting speed are maintained at 0.500 µm, 0° and 300 mm/min, respectively. In each of these simulations the cutting was allowed to progress until steady-state chip formation occurred.

The variations of the Von Mises stress (MPa) for different cutting edge geometry are shown in Figs. 5.22. Drawing comparisons from the earlier study, it was found that the maximum area of the shear band formation at the primary shear zone tends to increase with increase in the edge radius. This effect is shown by the magnified sub-plots
from selected regions for each of the cutting edge radius condition. The increase in area was a logical conclusion as the number of nodes, supporting the cutting edge radius or chamfer length responsible for the deformation of the workpiece, increases with increase in edge radius. Due to the rounding of the cutting edge, there is an increase in bluntness, which requires larger forces for material shearing and ploughing hence, the normal and shear forces are higher in magnitude at the primary shearing zone. Adding on, the contact area with the shearing plane increases with increase in edge radius, causing both contact normal and shear stresses along the rake face and the edge radius to rise. It was also observed that a sharper tool induces chip curling effect as shown in Fig. 5.22(A) compared to a round edge tool as seen Fig. 5.22(B) and (C). Consequently, there is a rise in the tool-chip contact length with increase in cutting edge radius.

Figure 5.22: Von Mises plane stress (MPa) analysis at steady state cutting for: (A) Sharp Tool; (B) Round tool of edge radius 60 nm;
Figure 5.22: Von Mises plane stress (MPa) analysis at steady state cutting for: (C) Round tool of edge radius 120 nm; and (D) Chamfer tool of length 50 nm

It is well known that high temperatures are generated during metal cutting at the macro-scale. Similarly, heat generation and transportation during the micro-groove cutting process due to plastic deformation can affect the chip formation, tool wear and the overall quality of a micro-groove. Through the simulation results, complexity of heat distribution, maximum temperature observed and heat evacuation in the region after the departure of the tool can be systematically observed. The distribution of temperature rise induced by energy dissipation, local heating and thermal softening of workpiece material for various cutting edge geometries is shown in Fig. 5.23(A-D) through the form of isothermal line contours. This provides a qualitative representation of the thermal gradient densities.
Since rise in temperature due to the localized adiabatic heating effects are directly proportional to the effective plastic strain rate as pointed out by Eqs. (7.12-7.14), the maximum values of the converging isothermal lines are found at the tool chip interface as shown by the red isothermal lines in the images. This is almost identical to the plastic equivalent strain distributions discussed in Section 5.7.1. The maximum bands of severe plastic straining regions are at the secondary shear zones, and hence the maximum temperatures are found in those regions for all the considered cutting edge geometry conditions. Initially, the material moves from the undeformed regime towards the cutting edge of the tool tip, causing severe strain and temperature rise in the primary shear zone. As the material goes past the primary shear zone, the accumulated strain and
temperatures are carried into the chip, where further plastic straining and local heating effects are experienced. There is a very little temperature rise in the workpiece ahead of the tool tip as indicated. Also, the magnitude of maximum observed temperatures reached in the simulations near the tool tip (~1300 °C) is not affected by the size of edge radius; rather the average temperature distribution monotonically increases with increase in edge radius.

It can be seen that the degree of plastic deformation in the secondary shear zone and on the machined micro-groove surface (i.e. density of the contour lines) increases considerably as the edge radius increases. Therefore, more heat is generated due to plastic work near the tool tip for a tool with a higher edge radius and is transmitted into the chip or the tool. On the other hand, the dissipated heat can be more readily dispersed over a larger surface area owing to the larger edge radius at the shearing plane and subsequent reduction in geometrical restrictions. These two opposing effects provide a balance of the rise and distribution of the thermal load over a greater region of the cutting edge. The isothermal lines are more densely populated at the sharp tool R0, as seen in Fig. 5.23(A) as compared to R120 in Fig. 5.23(C). This is observed by the fact the line are more crowded at the too-chip interface for R0 (Fig. 5.23(A)), while the contours are spaced out in R60, R120 and C50. (Fig. 5.23(B-D)). Also the increase in edge radius promotes greater sub-surface temperature distribution, beneath the workpiece due to increase contact area as seen in Fig. 5.23(B-D) compared to the sharp tool, R0.

For the purpose of study of side burr formations, an instantaneous frame of the deformed workpiece was considered at a function of time at steady-state as the tool passes through a fixed frame for different cutting edge geometries. Figure 5.24 (A-D)
shows the side burr formation event at steady-state groove cutting for the cutting edge geometries of R0, R60, R120 and C50, respectively. The y-z plane half symmetry-based micro-grooves (Refer Fig. 5.6, Section 5.3) are mirrored along the x-y axis of the defined mesh orientation and a single row of deformed workpiece elements representing the outline of the groove are extracted from each of the cutting edge geometry condition. The method of calculating side bur height has been explained earlier in Section 5.7.2. The mean burr height achieved by the deformed mesh elements increase marginally with increase in edge radius, while the highest burr height is attained using a chamfer tool tip design. The maximum burr height reached was 89 nm, 91 nm, 99nm and 101 nm for R0, R60, R120 and C50, respectively. These values match with burr height of 91nm that is obtained from the cutting experiment, performed at cutting speed of 300 mm/min and 0.536 μm depth of cut.

**Figure 5.24:** Side burr formation at steady-state condition using : (A) Sharp Tool (R0); (B) Round tool with edge radius 60nm (R60); (C) Round tool with edge radius of 120 nm (R120); and (D) Chamfer tool with length 50nm (C50)
Since the objective of this study is burr height comparisons, the workpiece material deformation, chip separation, stress and strain states of the sliced mesh are not shown in these plots. The trend observed in the plots seemed to agree with the actual cutting experiments, where there was increase in burr height with increase in edge radius. Since a sharp tool with infinite edge radius is a hypothetical scenario, the edge radius between R60 – R120 yields good control on the formation of side burr, while cutting at a depth of 500µm. Since the sacrificial layer of element for failure and rigidity of the cutting tool tip is maintained throughout the length of simulation, the groove bottom profile remains identical in all the cases.

Figure 5.25 shows the cutting forces experienced by the tool tip in the direction of cut for various cutting tool edge geometries considered. Conversely, finite element simulations tend to not do a good job of predicting thrust forces; hence they are not considered for this study. The R0 curve that represents the predicted cutting force in the direction of cut for a sharp tool gave the lowest average value of the cutting force accumulated through the length of cut considered. The C50 curve that represents chamfered edge geometry had the most fluctuating force curve at steady-state cutting. This indicated alternating increased loading and unloading of deformed mesh elements contacting the cutting edge of the tool. The average cutting force values increased with the increase in the edge radius of the tool tip. A constant value of the cutting force indicative of the steady-state cutting was reached at shorter instant of time interval for R0 (~ 86 x E⁻³ ms) compared to R120 (~102 x E⁻³ ms). Hence, the average cutting forces were more uniform while cutting with a sharper tool, which was indicative of a better representation of steady-state cutting, with a good discretization balance between
deformed workpiece mesh elements and subsequent deletion of failed elements from the workpiece.

The contact between the tool tip and workpiece material moving along the edge radius and upward along the rake face of the tool increase with increase in edge radius, contributing to higher contact stress on the tool tip. This significantly affected the characteristics of the cutting forces obtained for different edge geometry simulations. The dashed ‘grey’ colored curve in the Fig. 5.25 shows the cutting force predicted for R0 tool edge geometry condition, where thermal softening, temperature-related properties and adiabatic heat generation at the workpiece are neglected. The average value is seen to be much less than that obtained at R0 in the couple adiabatic heat generation model (green colored curve). This indicates that thermal components had a major role to play in determining the cutting force experienced and subsequently in the other discussed process dynamics during the micro-groove cutting simulations.

Figure 5.25: Cutting force analysis for different cutting tool edge geometries
(B) Tool Rake Angle

The geometry of the tool rake angle determines the contact area between the cutting edge of tool tip and deformed chip. The objective of this study is to determine the impact of rake angle on the performance of the cutting tool tip. Three different rake angle conditions are studied, which are $-5^\circ$, $0^\circ$ and $10^\circ$ using a sharp cutting tool, R0, simulated at depth of cut of 0.500 µm and cutting speed of 300 mm/min. The chip formation results are shown in Figs. 5.26(A-F). The first column of images in Fig. 5.26(A-C) show the Von Mises Stresses along the symmetry plane captured during steady-state cutting for $-5^\circ$, $0^\circ$ and $10^\circ$ rake angle, respectively, The second column of images in Fig. 5.26 (D-F) shows the respective plastic equivalent strains for the respective set of rake angles considered. The maximum values of the stress and strain distribution patterns are found in the primary and secondary shear zone, respectively.

While the pattern of distribution remains the same in all cases, the magnitudes of maximum stress and strain increase with decrease in rake angle from $10^\circ$ to $-5^\circ$. By contrast, recollect that these values decreased with increase in depth of cut. It is also observed that under the effect of lower rake angles, there is higher distortion of meshed element cut by the tool tip. While cutting with a smaller rake angle, the area of high effective Von Mises stresses tends to move towards the tool tip and also tends to penetrate deeper into the workpiece as shown in Fig. 5.26(B) as compared to Fig. 5.26C). The peaks of the equivalent plastic strain were found to occur in the end of the sliding friction regions, where the mesh elements begin to lose contact with the cutting tool tip. While observing the chip flow characteristics, the deformed chip thickness, the tool-chip contact length and chip curl radius decrease with increase in rake angle. The amount of
workpiece material that is supported by the rake face of the tool tip decreases with the increase in the rake angle, causing for the observed chip characteristics.

**Figure 5.26:** Von Mises stress analysis at symmetry plane for 0.500 µm depth of cut at rake angles of (A) -5° (B) 0° (C) 10° and Plastic equivalent strain analysis at rake angles of (D) -5° (E) 0° (F) 10°
The temperature distribution at the tool-chip interface is shown in Fig. 5.27(A-C) for the rake angles of -5°, 0° and 10°, respectively. Cutting with a smaller rake angle tends to exhibit a higher temperature gradient at the tool-chip interface. Particularly, smaller rake angles cause larger contact area of the tool tip with the deformed workpiece that causes higher chip volume and subsequently increased heat generation. Since it is known that the maximum percentage heat flux is dissipated into the chip during deformation, the observed trends seem acceptable. The highest temperatures reached in each of the sub-plots shown are around 1520 °C for -5°, 1433 °C for 0° and 1382 °C for 10° rake angle.

The results of the maximum temperature distribution and dissipation contrast with that of the previous section when different cutting edge radii geometries are analyzed.

![Figure 5.27: Temperature distributions at symmetry plane for 0.500 μm depth of cut at rake angles of (A) -5° (B) 0° (C) 10°](image)

5.8 Summary

In this chapter, the development of a 3D finite element analysis (FEA) model of micro-groove cutting of steel was described. The AISI 4340 steel was chosen as the
principle workpiece surface as it easy to acquire the necessary material properties for this particular grade. The model was specifically aimed to study the stress and strain distributions, chip flow and evolution, analysis of cutting forces in the direction of cut, side burr formation and temperature gradients along the tool-chip interface that took place during micro-groove machining.

An enhanced 3D FEA model to include a decoupled adiabatic heat generation component to assess the temperature distributions during the micro-groove cutting and multiple cutting geometry combinations was developed. The model aimed to study the role of machining parameters such as cutting tool geometry, tool rake angle, depth of cut, and cutting speed on the outcome of the process dynamics. The model was validated using side burr height and deformed chip thickness measurements obtained from the cutting experiments, where the errors were found to be lower than 4 % and 28 %, respectively. The accuracy of the Johnson-Cook constituent flow stress model parameters were justified by observing the strain rate contours developed during steady-state cutting that was above 20,000 s\(^{-1}\).

The steady-state chip formation process was observed for during all the simulated experiments. The chip contact length increased with increase in depth of cut. The existence of the primary and secondary shear zone was noticed in all the simulations for the different machining conditions considered. It was also noted that the highest stresses were present at the primary shear zone neat the cutting edge and the highest strains were present in the second shear zone. The highest temperature contour density was found the tool-chip interface, where the sliding frictional effects were dominant. Also, the depth of cut and rake angle had strong effects on chip thickness, chip curl radius, the amount of
plastic strain in a chip, and the distribution of plastic strain in a chip. The cutting forces and side bur height increased with depth of cut, while the changes in cutting speeds didn’t affect the outcome significantly. Increase in rake angle of the cutting edge decreased the cutting forces, temperature distributions and stresses, while increase in cutting edge radius of the tool tip increased those responses. The penetration of temperature fields beneath the workpiece increased with increase in cutting edge radius.
Chapter 6

Conclusions and Recommendations

6.1 Overview of Thesis

The objective of this work was to design and develop a robust, cost-effective flexible cutting tool that is capable of machining micro-grooves in hard materials, such as steel. The flexible cutting tool accommodated an in-built strain-gauge sensor system to: measure and control the deflection of the cutting tool during the micro-groove cutting process. CBN material was used as the cutting tool tip and modified to the required geometry for micro-groove cutting in steel samples. The flexible cutting tool was able to machine different patterns of programmable micro-grooves that are several millimeters long, several microns deep and several microns wide, repeatable over a large surface area.

6.2 Conclusions

The following conclusions can be drawn from the work presented in this thesis.

6.2.1 Development of Flexible Cutting Tool

- A strain-gauge sensor-integrated meso-scale CBN flexible cutting tool has been designed and built using the MEMS-based fabrication method. The in-built strain-gauge sensor enabled closed-loop robust feedback control by measuring the flexible tool deflection during the groove cutting process. The MEMS-based fabrication process
enabled production of three types of flexible cutting tools based on their inherent tool stiffness - 1040 N/m, 1140 N/m and 1280 N/m.

- The maximum stress levels for the designed meso-scale cantilevers are uniformly distributed over a wide range of cantilever deflection, and are well below the failure stress level of etched silicon, i.e., 300 MPa. The MEMS-based strain-gauge sensor also showed similar behavior in terms of the measured electrical conductivity, gauge factor and non-linearity to the industrial resistive metal strain-gauge sensors.

- Single CBN crystal material was used as the cutting tool tip material. The mounting and novel crystal modification methods for coarse and fine changes to create the required geometry on the CBN crystal for groove cutting have been established. Further, the complete tool was compactly packaged on a SLA platform that eased the handling and portability of the flexible cutting tool. Further, the package also provided the electrical connectivity for the integrated strain-gauge sensor with the external signal conditioning amplifier.

6.2.2 Evaluation of Flexible Cutting Tool

Results on Design Evaluation

- The stiffness, resonant frequency and load carrying capacity of the meso-scale CBN flexible cutting tool were evaluated to verify the design objectives. The new flexible cutting tools (stiffness = 1040 N/m) were found to endure cutting loads up to 500mN, which was about 50 times more than the capacity of the AFM sapphire cantilevers.
• Under similar loading conditions, the meso-scale cantilever were theoretically found to operate under stresses of 100 MPa, while the AFM sapphire cantilevers experienced stresses as high as 470 MPa, which was close to their failure stress value of 483 MPa.

• The strain-gauge sensor integrated on the meso-scale CBN flexible cutting tool was evaluated in terms of electrical conductivity, gauge-factor and non-linearity. Under the operational excitation voltage of 5V, the sensor resolution was 7.5με per μm. Their gauge factor was 1.92 and non-linearity was measured to be 1.48%, which was similar to the behavior of the industrial strain-gauges.

Results on Performance Evaluation

• The micro-grooves of more than 1μm depth have been machined in a single tool pass condition using the meso-scale CBN flexible cutting tool, which was greater than depth achieved when using the CBN tip-based AFM sapphire cantilevers.

• The micro-grooves machined using the flexible cutting tool was found to be consistent and repeatable. This was verified by an experiment where the groove widths and depths at different section of the grooves cut over a 10 mm length were extracted and compared.

• The groove floor profiles were studied using 2D AFM scans. The measurement of peak-to-valley deviations were consistently found to be less than 120 nm, when scanned for a 60 μm length and around 50 nm for a short scan length of 5 μm.

• Micro-groove quality machined by the flexible cutting tool was found to be significantly affected by the side-burr formed during the cutting process. Under similar machining conditions, the side burr height achieved using the meso-scale CBN cutting tool was
found to be lesser than that formed while using the CBN tip-based AFM sapphire cantilevers.

- The height of side burr increased with increase in depth of cut of grooves machined with new flexible cutting tool using a single tool pass. A mean side burr height of 423 nm was obtained when cutting groove with depth of 1.8μm and at cutting speed of 100 mm/min.

- In order to achieve high-quality micro-grooves with minimal burr formation, a combination of machining parameters such high cutting speeds of 400 mm/min, positive rake angle and multiple tool pass conditions were necessary. A reduced mean burr height of 330 nm was achieved at the same groove depth machined using two tool passes, 10 deg. rake angle and 400 mm/min cutting speed.

- Based on the tool wear study, suitable machining conditions for the meso-scale CBN flexible cutting tools were established. Cutting load of 8mN and cutting speed of 100 mm/min provided the best tool life for machining grooves that were nearly one micron deep and two microns wide. Repeatable and consistent micro-grooves as long as 234 mm in length under these conditions, without CBN tool tip fracture.

- The tool life of the meso-scale CBN flexible cutting tool was found to be higher when cutting micro-grooves on 1018 low-carbon steel, compared to the CBN tip-based AFM sapphire cantilever under similar machining conditions.

6.2.3 Modeling of the Micro-Groove Cutting Process in AISI 430 Steel

- A 3D finite element analysis (FEA) model has been developed to provide a fundamental understanding of the micro-groove cutting process in AISI 4340 steel. The 3D FEA
model built for this process was enhanced from an earlier work [106] that was used to study micro-groove cutting in aluminum.

- A thermo-elastic-plastic model of steel workpiece was modeled using Lagrangian formulation with arbitrary Lagrangian Euler (ALE) adaptive remeshing technique for simulating the micro-groove cutting process. A surface-to-surface contact-based algorithm based on an explicit dynamics procedure using a central integration operator was adopted through the use of Abaqus 6.9 software.

- The model was designed to simulate steady-state micro-groove cutting for a length of 10.4 μm using a rigid CBN tool tip. Since cut length was in the range of a single grain size of AISI 4340 steel, a homogenous assumption of the material was made. Johnson-Cook Flow stress model with thermal softening and work hardening components was incorporated to model the AISI 4340 steel material. An adiabatic heat generation model was implemented for performing the thermal calculations.

- An extended coulomb’s friction model was used for modeling friction at tool-chip interface. The chip separation criteria from the workpiece were handled through the use of element failure in specified sacrificial regions below the chip such that the height of the deletion layer was higher than the cutting edge radii of the CBN tool tip. The failure of the workpiece was defined by a ductile failure criterion based on stress triaxiality and plastic strain rate at the start of chip formation process and Johnson-Cook failure criterion at steady-state chip formation process.

- Model validation was performed by comparing the side burr height and deformed chip thickness values obtained from experimental results. The prediction errors were within
1.8 – 4.6% for side burr height measurements and 6.7 - 28% for deformed chip thickness measurements.

6.2.4 Simulation Results of the Micro-Groove Cutting Model Study

- The 3D FEA model developed was used to study the process mechanics of micro-groove cutting at steady-state cutting that included surface and sub-surface stress and strain analysis, temperature distributions, cutting force predictions in the direction of cut, chip development and side and exit burr formation events. The simulation was carried for two different cutting speeds – 100 and 400 mm/min and two different depths of cut – 0.500 μm and 0.960 μm. The influence of cutting edge of the tool tip and the tool rake angles on the process mechanics outcome was also investigated. Three different edge radii considered include a sharp tool with infinite radius, a 60nm and a 120 nm edge radius, while rake angles were varied from -5 to 10 deg.

- The steady-state tool-chip contact length was established for both 0.500 μm and 0.960 μm depth of cut. The tool-chip contact length increased with increase in depth of cut. The contact was not affected by change in cutting speed of the tool tip. During steady-state cutting a primary and secondary shear zone was distinctly formed. For both the depths of cut, the highest Von Mises principle stresses were found at the primary shear zone while the highest strains occurred in the secondary shear zone. As depth of cut increased, the sub-surface stresses were also found to increase below the cutting edge of the tool. The stresses decreased in increase in tool rake angle and increased with increase in cutting edge radius of the tool tip.

- The cutting forces in the direction were found to increase with the depth of cut, increase with increase in cutting edge radius and decrease with increase in rake angle. The 3D
model didn’t predict the thrust forces accurately as the model was developed based on the macro-scale orthogonal cutting model where thrust forces are not dominant. However, in micro-groove machining, which is essentially a load-based cutting process, thrust forces balance the cutting load applied on the tool tip.

- The depth of cut and rake angle have strong effects on steady-state chip thickness, chip curl radius, the amount of plastic strain in a chip, and the distribution of plastic strain in a chip. The chip curling radius seemed to decrease with decrease in edge radius of tool tip and increase in tool rake angle. The side burr height increased with increase in depth of cut, which is consistent with the experimental results. Change in edge radius didn’t significantly affect the size of the side burr height.

- The temperature evolution patterns were observed to match the trends of the equivalent plastic strains in all the simulation experiments. The temperature contours indicate that highest temperature spots are found along the tool-chip interfacial regions where siding frictional effects were most dominant. The density of the contours and the highest temperatures attained increased with increase in cutting edge radius. Cutting with smaller tool rake angles exhibited higher temperature gradient near the tool tip.

### 6.3 Recommendations

The research described in this work resulted in the development of a meso-scale CBN flexible cutting tool capable of machining high-quality, high accuracy micro-grooves in steel surfaces. Through the conduction of several empirical and numerical investigations, a useful knowledge of the flexible cutting tool and its interaction with the workpiece has been acquired. However, there is scope for further improvements both experimentally and numerically that can
expand and refine the understanding of the micro-groove cutting process better. The recommendations for future research are as follows.

6.3.1 Monolithic Flexible Cutting Tool

- The meso-scale CBN flexible cutting tool developed for this work through the MEMS-based fabrication process provided the tool blank upon which the CBN tool tip is externally mounted using a thermal adhesive. This is a very delicate, time consuming process. Precise handling and manipulation of the CBN crystal for its placement at the end of the cantilever base is essential for further course and fine CBN crystal modifications. For future research, a monolithic flexible cutting tool concept should be explored that avoids the intermediate tool tip mounting issue completely.

- A direction towards achieving this goal is the creation of flexible cutting tool in which the tool blank and the tool tip is made of the same material, such as silicon carbide (SiC) or synthetic sapphire. Using SiC or sapphire-based monolithic tool design also provides an alternative to CBN tool tips for performing micro-groove cutting on hard surfaces. Refer Appendix A for SiC monolithic flexible cutting tool design.

- Alternatively, a suitable MEMS-based process fabrication technique can be developed which builds the monolithic flexible cutting tool layer-by-layer using appropriate functional materials for each part of the tooling system. This process could provide better control on the chosen design parameters for the tool blank and tool tip.

6.3.2 Sensor-Based Feedback Control

- The meso-scale CBN flexible cutting tool has an integrated strain-gauge sensor that maintains and controls the deflection of the flexible cutting tool during the groove
cutting process. The current strain-gauge sensor is based on a metallic foil-type resistor design that is very sensitive to heating effects – both due to flowing current within the resistor and environmental changes. This sensitivity affects the accuracy of the measurements that needs to be counter-balanced and accounted for during the output voltage reading from the sensor. This also induces hysteresis and non-linearity in measurements. Alternative sensor systems such as tool-integrated piezoresistive or piezoelectric sensors should be investigated that provide highly linear, low compliance and high-frequency response. This approach can also save on the tool blank space that is used for a integrating of a passive strain-gauge sensor for temperature compensation purposes.

- The sensor system currently works on a Wheatstone bridge whose excitation voltage is supplied externally through a conditioning amplifier. For the future work, an independent self-powered sensor system unit could be designed on the tool platform using the MEMS-based fabrication technique with an in-built power, signal amplifier and filter system that can drive the sensor unit more precisely and reduce thermal environmental noise.

6.3.3 Experimental Investigation on Machining Advanced Materials

- In the present work, the workpiece materials considered were limited to low-carbon steel 1018 and stainless steel 303. Stainless steel was chosen as it was one of most important engineering materials that were bio-compatible, a requirement in many micro-groove applications. The experimental efforts using meso-scale CBN flexible cutting tool to
evaluate machinability of high-strength, tougher and bio-compatible materials should be carried out. Some of the materials include – bulk metallic glass, titanium, zirconium, etc.

- Experimental observations and comparisons of the cutting dynamics while machining through the heterogeneous grain boundaries of various surfaces should be performed in order to establish analytical relationship between the tool stiffness, depth of cut and grain properties.

6.3.4 Cutting Fluid-assisted Micro-Groove Cutting

- A method for using cutting fluid to assist in the micro-groove cutting of harder materials should be developed in order to monitor and reduce tool wear and promote better surface finish. The novel method to be developed for delivering the cutting fluid at the size scale involved where traditional macro-scale techniques such as flooding / coolant spray impingements at the cutting edge may not be applicable

- Cutting fluid application can be implemented by spin-coating the workpiece with a thin film of suitable cutting fluid or through the use of atomized cutting fluid mist that have been recently studied for use in micro-scale machining applications. Results from preliminary testing for spin-coating techniques can be found in Appendix B.

6.3.5 Improvements on the 3D FEA Micro-Groove Cutting Model

- The deformable workpiece material in the 3D model is treated as a homogenous isotropic surface due to the assumptions of comparable grain size and simulated cut length. However, in order to effectively understand the process mechanics of micro-groove cutting in steel, a multi-grain heterogeneous workpiece surface must be modeled
to handle the different grain structure orientations, their individual properties and relevant anisotropic behavior, if present.

- The current 3D FEA study developed uses a decoupled adiabatic heat generation model to study thermal effect at the tool-chip interface. This isolated the thermal contours of the workpiece from the cutting edge of the tool. In reality, there is a certain amount of heat transfer from the workpiece to the cutting edge of the tool. In order to study the heat conduction effects along the cutting edge of the tool, a fully-coupled thermo-mechanical finite element model must be incorporated into the current one that includes thermal conduction, convection and radiation effects at the tool-workpiece boundaries.

- All simulations run in this study assume a discrete, perfectly rigid tool based on shell elements. This meant deformation was confined to the workpiece material during the simulated groove cutting. This was adopted in order to make the simulations run in a computationally effective manner and yield quicker results. However, in order to observe the of tool wear through simulations a deformable 3D cutting edge model needs to be developed that should include appropriate mechanical and thermal tool material properties. Also, different tool shape geometries should be studied that aims at varying the clearance and flank faces, amount of edge rounding on faces not in direct contact with workpiece. This study would provide a deeper understanding of tool geometry related burr formation and reduction analysis.

- In the current set-up, a case of continuous chip formation approach is modeled. However, while cutting harder surfaces different types of chip formation conditions such as discontinuous, saw-tooth shaped, shear banded chips, etc. might be in action. Examination of chip morphology that reflects the chip structure, chip thickness and
related material microstructural properties is necessary for improving the simulated chip formation. Therefore, research into the nature of chip formation might be necessary to improve the predictive capabilities of the 3D FEA model.

- The present Johnson-Cook (JC) failure criterion for element damage used in the input model development assumed the plastic failure strain to be a monotonic function of stress triaxiality and this function is extrapolated into small and negative triaxialities that may lead to substantial errors while predicting failure [163-164]. Contrary to the JC failure model where fracture occurs at all points on triaxiality space, a negative cut-off value of stress triaxiality needs to be incorporated for AISI 4340 steel into the fracture envelope to ensure better calibration of the 3D model.
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Appendix A

Monolithic Flexible Cutting Tool Concept

A.1 Tool Fabrication

This section describes fabrication concept of a Si-C-based monolithic flexible cutting tool via laser-milling method. Figure A.1 shows the steps involved in the Si-C-based monolithic flexible cutting tool development using laser-milling approach achieved using a continuous Nd: YAG laser beam, with drive current of 22 mA. The fabrication is achieved in a two-step orientation-based milling. In the first step, the blank is oriented top-side up to the direction of laser beam (orientation 1), where outline of the designed cantilever is milled out. In the next step, the blank is oriented with sideways to the direction of the laser (orientation 2), rotating 90 deg from the previous orientation, to machine a course cutting edge that needs further refinement.

Figure A.1: Schematic of Si-C monolithic flexible cutting tool Fabrication
A.2 Fabricated Tool

The monolithic flexible cutting developed using the laser-milling method is shown in this section. Figure A.2 shows the top and side views; and the crude cutting edge of the Si-C monolithic flexible cutting tool achieved using the laser-milling approach. It can be seen from the figure that the cutting edge is only coarsely defined. Further geometry modifications and processing is needed to produce the required tool geometry. Furthermore, this concept describes the fabrication without the integration of the sensor unit. A suitable method to integrate the sensors onto the monolithic flexible cutting tools has to be explored.

![Figure A.2: (A) Overall definition of tool outline, post step 1; (B) Side view of laser-milled tool, post step 2;](image-url)
Figure A.2: (C) Top view of the coarsely defined cutting edge; and (D) Side view of the coarsely defined cutting edge
Appendix B

Cutting Fluid-Assisted Micro-Groove Machining

B.1 Tapping Fluids

This section deals with the experiments involving the application of a thin-film lubricant on the workpiece during the micro-groove cutting process. Various commercially-available tapping fluids were evaluated for the micro-scale groove cutting application. Table B.1 shows the properties of various biodegradable tapping fluids in the market used for ferrous metals applications that are chlorine and sulphur free. Moreover, these fluids were aqueous, non-corrosive water-based that could produce a thin, viscous coating on the surface that can last the duration of the cutting experiment.

Table B.1: Properties of various Tapping Fluids

<table>
<thead>
<tr>
<th>Name</th>
<th>Chemical Description</th>
<th>Vapor Pressure</th>
<th>Solubility in water, % by weight</th>
<th>Rate of Evaporation (Butyl Acetate = 1)</th>
</tr>
</thead>
<tbody>
<tr>
<td>ProTap</td>
<td>Hydrocarbon Mixture</td>
<td>&lt; 1 (mm of Mercury) @ 75° F</td>
<td>&lt; 1 (Insoluble)</td>
<td>&lt; 0.01</td>
</tr>
<tr>
<td>EP-Xtra</td>
<td>Petroleum Hydrocarbons and Additives</td>
<td>&lt; 5 (mm of Mercury) @ 75° F</td>
<td>&lt; 1</td>
<td>&lt; 1</td>
</tr>
<tr>
<td>Xtra-Thick</td>
<td>Petroleum Hydrocarbons and Additives</td>
<td>&lt; 5 (mm of Mercury) @ 75° F</td>
<td>&lt; 1</td>
<td>&lt; 1</td>
</tr>
<tr>
<td>Rapid Tap</td>
<td>Predominantly Chlorinated Paraffin</td>
<td>&lt; 5 (mm of Mercury) @ 75° F</td>
<td>&lt; 0.2</td>
<td>slower than water</td>
</tr>
<tr>
<td>Tapmatic #1 Gold</td>
<td>Petroleum Hydrocarbons</td>
<td>&lt; 7 Pascal @ 68°F (20°C)</td>
<td>Insoluble</td>
<td>&lt; 0.1</td>
</tr>
</tbody>
</table>
B.2 Evaluation for Thin-Film Coating

The performance of each of the commercially-available tapping fluids applicable for thin-film coating is evaluated on (1) rate of evaporation, (2) uniform distribution of film across the specimen surface and (3) nature of particle size of the fluid, with respect to the micro-groove feature. A spin-coating testbed is prepared that consisted of a vertically mounted spindle, retrofitted with a spin-coating mount that held the polished workpiece sample, in an upright position. A calibrated amount of fluid is dispensed on the specimen surface with help of a pipette and set to spin at a specified spindle rotation. Two set of trials were performed - Test 1 and Test 2; for different spin speeds and different spin times. Additionally, the cutting fluid was dispensed both at rest (static condition) and while in motion (dynamic condition). Table B.2 and B.3 summarizes the results.

**Table B.2: Spin-Coating Results for Test 1**

<table>
<thead>
<tr>
<th>Tapping Fluid</th>
<th>ProTap</th>
<th>EP-Xtra</th>
<th>Xtra-Thick</th>
<th>Rapid Tap</th>
<th>Tapmatic</th>
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</thead>
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</tr>
<tr>
<td>RPM</td>
<td>5000</td>
<td>5000</td>
<td>5000</td>
<td>5000</td>
<td>5000</td>
</tr>
<tr>
<td>Time</td>
<td>15s</td>
<td>15s</td>
<td>15s</td>
<td>15s</td>
<td>15s</td>
</tr>
<tr>
<td>Rate of Evaporation</td>
<td>On sides &gt; 2hr</td>
<td>On sides &gt; 2hr</td>
<td>No change at t=4hrs</td>
<td>On sides at t &gt; 1hr</td>
<td>On Sides at t &gt; 2 hrs</td>
</tr>
<tr>
<td>Film Distribution</td>
<td>Even</td>
<td>Even</td>
<td>Chaotic pattern</td>
<td>Even</td>
<td>Even</td>
</tr>
<tr>
<td>Drip off / Edge Bead</td>
<td>Yes &gt; 1 hr</td>
<td>Yes &gt; 2 hr</td>
<td>Yes &gt; 10 min</td>
<td>Yes &gt; 2 hr</td>
<td>Yes &gt; 1 hr</td>
</tr>
</tbody>
</table>
**Table B.3: Spin-Coating Results for Test 2**

<table>
<thead>
<tr>
<th>Tapping Fluid</th>
<th>ProTap</th>
<th>EP-Xtra</th>
<th>Xtra-Thick</th>
<th>Rapid Tap</th>
<th>Tapmatic</th>
</tr>
</thead>
<tbody>
<tr>
<td>Time</td>
<td>25s</td>
<td>25s</td>
<td>25s</td>
<td>25s</td>
<td>25s</td>
</tr>
<tr>
<td>Rate of Evaporation</td>
<td>No Change at t=3hr</td>
<td>On sides at t&gt;2hrs</td>
<td>No Change at t=3hr</td>
<td>On sides at t&gt;2hrs</td>
<td>On sides at t&gt;2hrs</td>
</tr>
<tr>
<td>Film Distribution</td>
<td>Even</td>
<td>Chaotic pattern</td>
<td>Even</td>
<td>Even</td>
<td>Chaotic pattern</td>
</tr>
<tr>
<td>Drip off</td>
<td>Yes &gt; 3 hr</td>
<td>No change at t=3hrs</td>
<td>Yes</td>
<td>No change at t=3hrs</td>
<td>Yes</td>
</tr>
</tbody>
</table>

Based on the results obtained from Table 2 and Table 3, EP-Xtra and Rapid tap it were chosen to be coated at high spin speeds for longer durations (5000 rpm, 25s). Since, the size of particles of the Rapid Tap tapping fluid, when viewed under the microscope was in the range of the groove dimensions to be machined, EP-Xtra was decided to be used for micro-groove cutting experiments.
B.3 Thin-Film-based Groove Cutting Experiments

A highly polished metallographic low-carbon 1018 steel is used for micro-groove cutting experiments assisted with a thin-film coating of EP-Xtra tapping fluid. A set of nine parallel micro-grooves were cut using a CBN tip-based AFM sapphire cantilever by varying the cutting loads between 1-5 mN and maintaining a constant cutting speed of 100 mm/sec. Figure B.1 shows the groove cutting results. Figure B.2 shows the AFM plots from the groove cross-section with the highest load.

Figure B.1: Composite image of the ends of grooves cutting into 1018 steel when cutting using a thin-film coating of a tapping fluid

Figure B.2: (A) 2D AFM plot and (B) 3D AFM plot of the micro-groove when cutting with 5.0mN load
From the AFM data, it is clear that the cross-section of the grooves were not well formed as compared to the dry cutting results in the thesis. The depth of cut achieved was lesser than the value obtained during dry cutting. This might be caused due to the chipping of the groove geometry on the tool. It is concluded that there are a number of unknown effects that may be in play when applying cutting fluids at this size scale and further study is required before additional experiments using cutting fluids can be conducted.