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INVESTIGATION OF HIGH SPEED FLAT END MILLING PROCESS-
PREDICTION OF CHIP FORMATION, CUTTING FORCES,
TOOL STRESSES AND TEMPERATURES

D I S S E R T A T I O N

Presented in Partial Fulfillment of the Requirements for
the Degree of Doctor of Philosophy in the
Graduate School of The Ohio State University

by

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* * * * *

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1998

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ABSTRACT

End milling of tool steels is a highly demanding operation because of the temperatures and stresses generated on the cutting tool due to high workpiece hardness. Modeling and simulation of cutting processes have the potential for improving cutting tool designs and selecting optimum conditions, especially in advanced applications such as high speed milling.

The main objective of this work was to develop a methodology for simulating cutting process in flat end milling operation and predicting chip flow, cutting forces, tool stresses and temperatures using finite element analysis (FEA). As an application, machining of P20 AISI 4130 mold steel at 30 HRC hardness using uncoated carbide tooling was selected.

In order to model and simulate the cutting process using FEA, a methodology to determine flow stress of the workpiece material at high deformation rates and temperatures, and friction at the chip-tool contact was developed. The unknown parameters in workpiece flow stress and friction models were estimated using the results of process simulations for orthogonal cutting and the measurements from high speed orthogonal cutting experiments.

Two dimensional process simulation models were developed to predict chip formation and cutting forces in flat end milling process. An experimental set-up was built for slot milling operation using single insert flat end mills with a straight cutting edge (i.e. null helix angle) in order to validate simulated chip formation for milling process. A limited number of experiments were conducted on a horizontal milling center at the high speed milling conditions using the referred workpiece and tool material pair.
Predictions from the process simulations were compared with the experimental results. Comparison of predicted cutting forces with the measured forces showed reasonable agreements to validate developed process model for further use of prediction in tool stresses and temperatures.

An approximation to model the chip flow around the nose radius of the cutting tools was developed. A modular representation of undeformed chip geometry was introduced using plane strain and axisymmetric workpiece deformation models in the process simulations. A procedure for segmenting undeformed chip area into several elements to compute cutting conditions was also outlined.

Highest tool temperatures (>1000°C) were predicted at the primary (side) cutting edge of the flat end mill insert regardless of cutting conditions. Those temperatures increase wear development at the primary cutting edge. However, highest tool stresses were predicted at the secondary (around nose radius) cutting edge. Premature failure of the cutting edge is more likely at nose radius due to high stresses.
DEDICATION

To:
Pelin, Müge and Kaya Sadriye Özel
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<th>Units</th>
<th>Description</th>
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<tbody>
<tr>
<td>$a_e$</td>
<td>(mm)</td>
<td>Radial depth of cut or step over distance</td>
</tr>
<tr>
<td>$a_n$</td>
<td>(mm)</td>
<td>Normal (axial) depth of cut</td>
</tr>
<tr>
<td>$A_c$</td>
<td>(mm$^2$)</td>
<td>Area of cross section of cut chip</td>
</tr>
<tr>
<td>$A_{nose,chip}$</td>
<td>(mm$^2$)</td>
<td>Area of cross section of undeformed chip around the nose radius</td>
</tr>
<tr>
<td>$A_s$</td>
<td>(mm$^2$)</td>
<td>Area of cross section of the shear plane</td>
</tr>
<tr>
<td>$A_u$</td>
<td>(mm$^2$)</td>
<td>Area of cross section of undeformed chip</td>
</tr>
<tr>
<td>$d$</td>
<td>(mm)</td>
<td>Depth of cut</td>
</tr>
<tr>
<td>$D$</td>
<td>(mm)</td>
<td>Nominal cutting tool diameter</td>
</tr>
<tr>
<td>$D_{eff}$</td>
<td>(mm)</td>
<td>Effective diameter</td>
</tr>
<tr>
<td>$C_s$</td>
<td>(deg)</td>
<td>Side cutting edge angle</td>
</tr>
<tr>
<td>$f_z$</td>
<td>(mm/tooth)</td>
<td>Feed per tooth</td>
</tr>
<tr>
<td>$\vec{F}_f$</td>
<td>(N/mm)</td>
<td>Friction force (per unit width) vector</td>
</tr>
<tr>
<td>$\vec{F}_r$</td>
<td>(N/mm)</td>
<td>Tool cutting force (per unit width) vector</td>
</tr>
<tr>
<td>$\vec{F}_s$</td>
<td>(N/mm)</td>
<td>Shear force (per unit width) vector at the shear plane</td>
</tr>
<tr>
<td>$F_p$</td>
<td>(N/mm)</td>
<td>Plowing forces (per unit width) due to rubbing on the flank contact</td>
</tr>
<tr>
<td>$F_R$</td>
<td>(N/mm)</td>
<td>Resultant tool force (per unit width) in orthogonal cutting</td>
</tr>
<tr>
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<td>(N/mm)</td>
<td>Tool cutting force (per unit width) in orthogonal cutting</td>
</tr>
<tr>
<td>$F_r$</td>
<td>(N/mm)</td>
<td>Tool radial force (per unit width) in oblique cutting</td>
</tr>
<tr>
<td>$F_{rad}$</td>
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<td>Radial force (per unit depth of cut) in flat end milling</td>
</tr>
<tr>
<td>$F_t$</td>
<td>(N/mm)</td>
<td>Tool thrust force (per unit width) in orthogonal cutting</td>
</tr>
<tr>
<td>Symbol</td>
<td>Unit</td>
<td>Description</td>
</tr>
<tr>
<td>--------</td>
<td>------</td>
<td>-------------</td>
</tr>
<tr>
<td>$F_{_{\text{tan}}}$</td>
<td>(N/mm)</td>
<td>Tangential force (per unit depth of cut) in flat end milling</td>
</tr>
<tr>
<td>$F_n$</td>
<td>(N/mm)</td>
<td>Normal force (per unit width) on the tool rake face in orthogonal cutting</td>
</tr>
<tr>
<td>$F_f$</td>
<td>(N/mm)</td>
<td>Friction force (per unit width) on the tool rake face in orthogonal cutting</td>
</tr>
<tr>
<td>$F_s$</td>
<td>(N/mm)</td>
<td>Shear force (per unit width) on the shear plane in orthogonal cutting</td>
</tr>
<tr>
<td>$F_{_{\text{nas}}}$</td>
<td>(N/mm)</td>
<td>Normal force (per unit width) applying to the shear plane in orthogonal cutting</td>
</tr>
<tr>
<td>$F_x$</td>
<td>(N/mm)</td>
<td>Cutting force (per unit width) in x direction</td>
</tr>
<tr>
<td>$F_y$</td>
<td>(N/mm)</td>
<td>Cutting force (per unit width) in y direction</td>
</tr>
<tr>
<td>$F_z$</td>
<td>(N/mm)</td>
<td>Cutting force (per unit width) in z direction</td>
</tr>
<tr>
<td>$h$</td>
<td>(mm)</td>
<td>Instantaneous undeformed chip thickness</td>
</tr>
<tr>
<td>$h_{\text{equivalent}}$</td>
<td>(mm)</td>
<td>Average equivalent thickness of cross section of undeformed chip around nose radius</td>
</tr>
<tr>
<td>$h_{\text{max}}$</td>
<td>(mm)</td>
<td>Maximum chip thickness</td>
</tr>
<tr>
<td>$H_{\text{ref}}$</td>
<td>(kg/mm$^2$)</td>
<td>Brinell hardness of reference material</td>
</tr>
<tr>
<td>$i$</td>
<td>(deg)</td>
<td>Inclination angle in oblique cutting</td>
</tr>
<tr>
<td>$k$</td>
<td>(MPa)</td>
<td>Shear stress</td>
</tr>
<tr>
<td>$k_{\text{primary}}$</td>
<td>(MPa)</td>
<td>Average shear flow stress at the primary deformation zone</td>
</tr>
<tr>
<td>$k_{\text{chip}}$</td>
<td>(MPa)</td>
<td>Average shear flow stress at chip-tool interface</td>
</tr>
<tr>
<td>$K_c$</td>
<td>(N/mm$^2$)</td>
<td>Specific cutting force</td>
</tr>
<tr>
<td>$K_t$</td>
<td>(N/mm$^2$)</td>
<td>Specific thrust force</td>
</tr>
<tr>
<td>$K_{\text{mean}}$</td>
<td>(N/mm$^2$)</td>
<td>Mean specific cutting force</td>
</tr>
<tr>
<td>$K_{\text{rad}}$</td>
<td>(N/mm$^2$)</td>
<td>Specific cutting force for radial force component in flat end milling</td>
</tr>
<tr>
<td>$K_{\text{tan}}$</td>
<td>(N/mm$^2$)</td>
<td>Specific cutting pressure for tangential force component in flat end milling</td>
</tr>
<tr>
<td>$l_c$</td>
<td>(mm)</td>
<td>Chip-tool contact length on the tool rake face</td>
</tr>
<tr>
<td>$l_e$</td>
<td>(mm)</td>
<td>Chip-tool contact length on the sliding region of the tool rake face</td>
</tr>
<tr>
<td>$l_p$</td>
<td>(mm)</td>
<td>Chip-tool contact length on the sticking region of the tool rake face</td>
</tr>
</tbody>
</table>
Shear friction factor
Spindle speed
Tool nose radius
Effective tool nose radius
Tool nominal radius
Uncut/undeformed chip thickness
Cut/deformed chip thickness
Average temperature in the deformation zones
Cutting speed/velocity
Maximum cutting speed/velocity
Chip velocity in orthogonal cutting
Feed rate
Shear velocity
Rake angle
Normal rake angle in oblique cutting
Friction angle in orthogonal cutting
Normal friction angle in oblique cutting
Clearance angle
Shear strain
Shear strain rate
Strain
Ultimate strain of workpiece material
Strain rate
Average strain rate in the deformation zones
Lead angle
Tilt angle
Clearance angle
Tool rotation angle
Rotation angle at the end of tool engagement in flat end milling
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Unit</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\phi_{h\text{max}}$</td>
<td>(deg)</td>
<td>Rotation angle of maximum chip thickness in flat end milling</td>
</tr>
<tr>
<td>$\phi_{\text{entry}}$</td>
<td>(deg)</td>
<td>Rotation angle of initial tool-workpiece contact in flat end milling</td>
</tr>
<tr>
<td>$\phi_s$</td>
<td>(deg)</td>
<td>Shear angle at the primary deformation zone</td>
</tr>
<tr>
<td>$\phi_n$</td>
<td>(deg)</td>
<td>Normal shear angle in oblique cutting</td>
</tr>
<tr>
<td>$\kappa_{\text{nose,chip}}$</td>
<td>(deg)</td>
<td>Angle of the engagement between nose radius and undeformed chip</td>
</tr>
<tr>
<td>$\lambda$</td>
<td>(deg)</td>
<td>Helix Angle</td>
</tr>
<tr>
<td>$\mu$</td>
<td>(-)</td>
<td>Coulomb friction coefficient</td>
</tr>
<tr>
<td>$\rho$</td>
<td>(deg)</td>
<td>Tool cutting edge hone radius</td>
</tr>
<tr>
<td>$\sigma_n$</td>
<td>(MPa)</td>
<td>Normal stress on the tool rake face</td>
</tr>
<tr>
<td>$\overline{\sigma}_{\text{primary}}$</td>
<td>(MPa)</td>
<td>Average flow stress of work material at the primary shear zone</td>
</tr>
<tr>
<td>$\overline{\sigma}_{\text{chip}}$</td>
<td>(MPa)</td>
<td>Average flow stress of work material at the chip-tool interface</td>
</tr>
<tr>
<td>$\tau_p$</td>
<td>(MPa)</td>
<td>Frictional stress in sticking region at the tool rake face</td>
</tr>
<tr>
<td>$\tau_s$</td>
<td>(MPa)</td>
<td>Shear stress on the shear plane</td>
</tr>
<tr>
<td>$\tau_f$</td>
<td>(MPa)</td>
<td>Frictional stress on the tool rake face cutting force ratio in flat end milling</td>
</tr>
<tr>
<td>$\eta_c$</td>
<td>(deg)</td>
<td>Chip flow angle in oblique cutting</td>
</tr>
<tr>
<td>$\eta_{\text{mean}}$</td>
<td>(-)</td>
<td>Cutting force ratio including plowing effects in flat end milling</td>
</tr>
</tbody>
</table>
GLOSSARY

**Chip Geometry**
Volume of material removed in one tool revolution. The deformed chip geometry (after material removal) is not considered in this work. Therefore, all references to chip geometry are associated with undeformed chip geometry (before material removal).

**Climb (Down) Milling**
Milling cutter enters to workpiece with zero chip thickness and exits with maximum chip thickness.

**Conventional (Up) Milling**
Milling cutter enters to workpiece with maximum chip thickness and exits with zero chip thickness.

**ERC/NSM**
Engineering Research Center for Net Shape Manufacturing (established in 1987 at The Ohio State University).

**FEM**
Finite Element Method

**HRC**
Hardness indication using Rockwell measurement technique with C scale.

**Saw-tooth Shape Chip**
A type of chip form so that back of the chip resembles the teeth on a saw.

**Specific Cutting Force**
Cutting force divided by width of cut and undeformed chip thickness, in other words the unit force applied to the chip load area.

**Flat End Milling Parameters**
Spindle speed and feed rate.

**Flat End Milling Strategy**
The type of motion for the cutting tool; e.g. (climb milling) clockwise rotation, left stepover and feed forward direction or (conventional) clockwise rotation, right stepover and feed forward.
<table>
<thead>
<tr>
<th><strong>Premature Tool Failure</strong></th>
<th>Chipping or fracture of cutting edge due to instantaneous high stress concentration at a particular location.</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>WC</strong></td>
<td>Cemented tungsten carbide. WC is the most common material for cutting tools. This cutting tool material is sometimes coated to increase tool life.</td>
</tr>
</tbody>
</table>
CHAPTER 1

INTRODUCTION

As a result of the advances in machine tools and cutting tool technology end milling at high rotation speeds, "High Speed Milling/Machining (HSM)”, became a cost-effective manufacturing process to produce parts with high precision and surface quality. Until recently, high speed milling was applied to machining of aluminum alloys for manufacturing complicated parts used in the aircraft industry. Recently, with the advance of machine tools and cutting tool technologies, HSM has been used for machining tool steels (usually hardness > 30 HRC) for making molds and dies used in the production of a wide range of automotive and electronic components, as well as plastic molding parts [Dewes; 1997].

Major advantages of high speed machining are reported as: high material removal rates, the reduction in lead times, low cutting forces, less workpiece distortion and increased precision of the part. However, problems related to the application of high speed machining differ depending on the work material and desired product geometry. The common disadvantages of high speed machining are claimed to be: excessive tool wear, need for special and expensive machine tools with advanced spindles and numerical controllers, fixturing, balancing the tool holder, and lastly but most importantly the need for advanced cutting tools (see Figure 1.1) [Schulz; 1992].
Figure 1.1: Conventional and high speed cutting ranges for machining metals [Schulz; 1992]
Parallel to the increasing number of high speed machining applications, there has been also an increase in the number of research efforts toward the development of new exotic cutting tool materials, the design of cutting tool inserts and the improvement of cutting process conditions. Accordingly, the modeling and the analysis of the high speed machining process are becoming more vital in order to improve the cutting technology. Likewise, computer-aided simulation techniques to predict cutting process variables are also emerging to serve technological needs.

In the past, the fundamental modeling of machining processes and the mechanics of chip formation have been investigated by a large number of researchers. Similarly, prediction of process performance with finite element method (FEM) simulations of metal forming process has been very well developed for industrial applications [Altan; 1996]. Hence, the following observations are essential for this dissertation research:

- Machining is not only a material removal process, but also a deformation process [Thomsen; 1967] where very high strains, strain-rates and temperatures are generated and where deformation is highly concentrated in a small, thin zone. Thus, there are many similarities between the mechanics of chip formation and those of bulk forming processes. Finite element analysis tools can be effectively used to predict process parameters in high speed machining as has been done in metal forming.

- In machining, the interactions between the tool and the workpiece are extremely important, especially when using modern machining techniques such as high speed dry machining and precision machining to obtain an extremely fine surface finish. Therefore, a realistic simulation of cutting processes using FEM-based techniques would greatly advance the high speed machining technology.
• There have been several attempts to simulate the cutting process using FEM techniques. Consequently, the earlier FEM analyses of machining processes, with very few exceptions [Marusich; 1995] [Strenkowski; 1990], had many shortcomings. This was partly due to the limitations in the formulation used or the computation power, and partly because it was difficult to obtain material properties and friction under machining conditions [Usui; 1982] as discussed in Chapter 2 of this dissertation.

1.1 Rationale and Motivation

Investigating the machining process offers a great challenge to researchers. The modeling and analysis of high speed machining technology made great advances in machine tool control and development, fixturing, analysis of dynamics of machine tools, cutting tool and workpiece interactions, process monitoring, sensing, and statistical evaluation of tool life. However, there is still considerable work to be done for:

• Predicting cutting forces, tool stresses and cutting temperatures with FEM-based numerical analysis supported by experiments and analytical modeling for high speed milling operations.

• Designing tool inserts (rake and flank angles, chip breaker configurations, increased fatigue life, reduced wear) in an optimum manner for the specific application, i.e. tool/workpiece pair and cutting conditions.

• Estimating the tool failure (wear and chipping) based on physical entities that are obtained from reliable FEM simulations for given machining conditions.

A majority of the studies in modeling machining processes focus on force and torque prediction by using mathematical and empirical analysis. Most of the cutting force models for those processes consider only the force kinematics and are not usually sufficient in predicting other cutting process variables such as tool stresses and cutting temperatures for further analysis of tool wear and failure.
Besides, those models require some refinement coefficients and/or constants that can only be obtained by curve fitting the results of classic and practical cutting experiments at a great cost. It is believed that modeling these processes with FEM-based simulation techniques to predict not only cutting forces but also related tool stresses and temperatures would be very beneficial to optimize tool geometry and cutting processes for a more productive manufacturing environment.

The methodology reflecting the physical relationship between cutting tool geometry, tool and workpiece materials, cutting conditions and process variables should be formulated and, together with the analysis tool, made available for practical use in the manufacturing industry.

At this time, in most machining operations, prediction of cutting process variables such as cutting forces tool stresses and temperatures depends largely on empirical and experimental analysis.

The most recent literature for modeling machining operations reveals that FEM models are still limited by the capabilities of the models used, although there have been great improvements in last decade [Armerago; 1996]. For many years, single-edge orthogonal and recently oblique cutting have been studied to a certain extent with those numerical FEM-based models. However, there is no effective FEM modeling technique to study chip formation in milling operations. Several comprehensive analytical 2-D cutting models are also presented in the literature [Oxley; 1989] and some of them are utilized to predict cutting forces in 3-D flat and ball end milling operations [Armarego; 1983, Feng; 1994, Budak; 1996]. Most of those models are aimed to develop a mechanistic model to predict global tool load to prevent tool breakage, or to minimize the geometrical surface errors associated with tool and/or workpiece elastic deflections. However, the tool stresses caused by workpiece deformation, local temperatures, and tool chipping, which are important in HSM, cannot be predicted using the existing mechanistic models. Therefore, FEM simulation of plastic deformation in cutting
process offers significant potential for understanding the process mechanics for high speed milling.

The machining model of this study is expected to yield information on following machining process parameters:

- continuous (type II) chip formation
- actual chip geometry (chip thickness, chip-tool contact length etc.)
- forces, (cutting force, thrust force, friction and normal force, plowing)
- stresses and temperatures at the cutting tool
- tool geometry (micro and macro level)

It should be noted that in this work, the focus will be on high speed, flat end milling with straight and single cutting edge inserts. However, using similar FEM techniques, high speed flat end milling with helical multi-cutting edge solid tools can also be investigated as a future work.

1.2 Research Objectives

The overall objective of this dissertation research is to develop a methodology for understanding and predicting the physical phenomena that occur during the cutting process in flat end milling, i.e. the detailed mechanics of the process, with a minimal amount of experimentation.

FEM-based simulation techniques will be used to obtain a process model, and the required simulation parameters will be determined through modeling as well as by using experimental data.

The experiments will be conducted by both a) end turning of disks for orthogonal cutting and b) slot milling of thin plates using single insert indexable flat end milling with a straight cutting edge (i.e. null helix angle).
For given process input parameters (workpiece and tool insert materials, geometry of the tool, cutting conditions, i.e. feed, speed, and depth/width of cut), the specific objectives of this work are to:

- Investigate the contact conditions at chip-tool interface and develop a reliable FEM-based process model for orthogonal cutting. Improve the process model by calibrating process parameters and validating the simulation results with experimental data,

- Develop a methodology for determining both: the material properties (flow stress as a function of strain, strain rate and temperatures) and the friction at the chip-tool interface under the high speed cutting conditions,

- Develop a method for predicting in flat end milling a) the chip flow, b) the stresses and temperatures in the workpiece and tool insert, and c) the cutting forces,

- Develop a FEM-based process model for single-edge, flat end milling, and predict chip formation and process variables such as tool stresses, forces and temperatures, and verify with the experimental data,

- Expand the FEM based process model of 2-D flat end milling to the 3-D flat end milling process model with the aid of modular deformation modeling,

- Predict process parameters in end milling integrating FEM-based process models and analytical force models,

1.3 Dissertation Overview

A short description of each chapter in this dissertation is given below.

Chapter 1. INTRODUCTION. This chapter gives a brief introduction, rationale and motivation for the research problem and also defines the objectives.
Chapter 2. HIGH SPEED MACHINING OF HARD STEELS. This chapter discusses the benefits of high speed cutting for manufacturing dies and molds, the workpiece materials, cutting tools, milling strategies and gives an introduction to flat end milling process.

Chapter 3. MECHANICS AND MODELING OF METAL CUTTING PROCESS. This chapter contains background information about mechanics of orthogonal and oblique cutting with emphasis on modeling of flat end milling process. This chapter also contains a literature review regarding FEM modeling of cutting process.

Chapter 4. MODELING OF HIGH SPEED ORTHOGONAL CUTTING PROCESS. This chapter outlines the proposed concept for modeling of orthogonal cutting process using FEM simulation technique. Methodologies developed to obtain flow stress behavior of the workpiece material and tool-chip interface friction are also given.

Chapter 5. MODELING OF HIGH SPEED, FLAT END MILLING PROCESS-2-D CHIP FLOW. This chapter deals with the development of process model to predict chip flow, cutting forces, cutting temperatures and tool stresses in slotting operation for variable undeformed chip geometry.

Chapter 6. MODELING OF CUTTING PROCESS IN FLAT END MILLING-3-D CHIP FLOW. This chapter concentrates on the development of the piecewise-approximated deformation models for process simulation of 3-D chip flow in high speed, flat end milling.

Chapter 7. PREDICTION OF TOOL STRESSES AND TEMPERATURES IN 2-D FLAT END MILLING. This chapter presents the predictions for
process variables and discusses the effect of cutting conditions on the chip formation and premature tool failure

Chapter 8. CONCLUSIONS AND FUTURE WORK. Here, a summary of the motivation and research objectives is shown. A list of contributions and proposed future work is presented.
CHAPTER 2

HIGH SPEED MACHINING OF HARD STEELS

2.1 High Speed Cutting (HSC)

The advent of new cutting tools has made possible the machining of hard steels (usually hardness >20 HRC). Previously, the workpiece was used to be machined near-net shape in the annealed condition, heat treated to desired hardness, then machined to final dimensions and tolerances by grinding. Using advanced cutting tools it is possible to turn and mill steels in their hardened state to the final shape, thereby eliminating further grinding operations and the problems associated with workpiece distortion.

Machining of alloy steels in hardened conditions, especially at high cutting speeds, offers several advantages. Those advantages are: reduction of finishing operations, elimination of distortion if the part is finish-machined prior to heat treatment, achievement of high metal removal rates, lower machining costs and improved surface integrity [Tönshoff; 1986] [König; 1993]. HSC of hard steels, however, result in high temperatures and stresses at the workpiece-tool interface. Consequently, cost effective application of this technology requires a fundamental understanding of the relationships between process variables on one hand and tool life and machined surface integrity on the other hand. Thus, it is necessary to understand how temperatures and stresses developed during HSC, influence tool wear and premature tool failure (or chipping) as well as residual stresses on machined surfaces.
2.1.1 Characteristics of High Speed Cutting of Hard Steels

2.1.1.1 Chip Formation

Experimental data show that when machining hardened steels workpiece material microstructure (not only the hardness) and thermal properties affect the chip flow. It is common to observe higher cutting forces with higher workpiece hardness. However, it is also observed that different thermal properties of the tool material may result in lower cutting forces [Ng; 1997] [Elbestawi; 1997]. Therefore, in order to understand the process better and improve the performance of cutting tools, the use of deformation theory and advanced numerical techniques is recommended [Stephenson; 1997a].

In machining hard materials, continuous (Type II) chip formation is observed at conventional to high cutting speeds and low to moderate feed rates (see Figure 2.1a). However, at higher feed rates "saw tooth" or "shear localized" (Type IV) chips are produced, (see Figure 2.1b) [Nakayama; 1974]. The latter type of chip formation can cause cyclic variations of both cutting and thrust forces and can result in high frequency vibrations that affect tool life and failure [Davies; 1996].

There are two possible explanations for the formation of saw tooth (shear localized) chips. According to “thermal theory” in high speed/ high feed cutting the heat dissipation is highly concentrated and results in a shear band [Zhen-Bin; 1995]. The “shear theory” assumes that fracture on the surface of the current workpiece propagates inside the chip until the stress state is altered from a low to high compressive stress region [Elbestawi; 1996] [Vyas; 1997].

Throughout the scope of this work, only the cutting conditions that result in continuous chip formation have been used. Therefore, the mechanics and simulation of saw tooth (shear localized) chip formation is not discussed in the work presented.
Low undeformed chip thickness (feed), (< 0.1 mm)

![Continuous chip flow](a)

Continuous chip flow

Higher undeformed chip thickness and higher cutting speed

![Saw-tooth shape shear localized chip](b)

Fracture

Saw-tooth shape shear localized chip

Periodic fracture

Case 2

Figure 2.1: Schematic diagram of chip formation during machining of hard steels [Konig; 1993]
2.1.1.2 Cutting Mechanism

Fundamental knowledge of the cutting process is essential for high speed cutting. Nevertheless, differences in the machining of soft steels and hard steels also need to be understood in order to implement this process successfully. As mentioned earlier, common chip types observed in hard part machining are continuous chips at low undeformed chip thicknesses and saw-tooth shape at high undeformed chip thickness (usually > 0.1 mm). According to recent observation, the frequency of shear localized saw-tooth shape chips is very high [Davis; 1996]. The cutting edge is subjected to a high frequency force variation. The influence of chip formation on tool wear and surface integrity has not been clarified yet. However, the chip formation certainly affects the cutting forces. The mechanics of the cutting process are further discussed in Chapter 3 of this dissertation study.

2.2 High Speed Machining of Dies and Molds

Dies and molds are composed of functional and support components that generally are cavity and core inserts in injection molding and die casting, die cavities in forging, and punch and die in stamping. Cavity and core inserts are usually machined out of solid blocks of die steel. However, large stamping dies and punches are often cast to near-final geometry with machining allowances included. Most of the time support components are standard parts and assure the overall functionality of the tooling assembly in such areas as alignment, part ejection, and heating or cooling. By using standard die and mold components, the time necessary for manufacturing a die is reduced, and machining is mainly devoted to producing the core and cavity, or the punch and the die.

2.2.1 Surface Quality

The surface finish of die and mold functional components varies depending on the application. From the types of dies and molds mentioned only some types of
forging and stamping dies are delivered to the customer without any grinding or polishing after the finish machining phase.

A survey conducted by the Engineering Research Center for Net Shape Manufacturing (ERC/NSM) shows that finish machining requires the largest share of time for injection molds, die-casting dies and forging dies (25 to 30 % of total lead time). In the specific case of large automotive draw dies, finish machining is also a significant portion of the total production time [Fallböhmer; 1996].

Finish machining also has an indirect impact on benching time (grinding and polishing) which is about 15% for injection molds and die-casting dies and about 20% is sheet metal forming dies [Fallböhmer; 1996]. By finish machining with smaller step over distances, the scallop height is reduced, allowing in turn a reduction of the benching time [Nakamura; 1992].

Surface requirements for injection molds are higher than for forging and stamping dies. The average values for dimensional and form error are given in Table 2.1.

<table>
<thead>
<tr>
<th></th>
<th>Average Dimensional Error [mm]</th>
<th>Average Form Error [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Injection Molds</td>
<td>0.020</td>
<td>0.015</td>
</tr>
<tr>
<td>Die casting dies</td>
<td>0.046</td>
<td>0.041</td>
</tr>
<tr>
<td>Stamping dies</td>
<td>0.061</td>
<td>0.043</td>
</tr>
<tr>
<td>Forging dies</td>
<td>0.028</td>
<td>0.023</td>
</tr>
</tbody>
</table>

Table 2.1: Tolerance requirements for dies and molds [Fallböhmer; 1996]

2.2.2 High Speed Milling

With the advent of HSM the application of the milling process in die/mold production is expanding. HSM offers the possibility of cutting down on lead time by diminishing the effort for grinding and polishing operations. The technology is rather new and a lack of process knowledge and appropriate cutting tools have impeded a broad application of HSM thus far [Dewes; 1997]. In forging dies, the surface finish is not as critical as in dies/molds for the other applications. In die
casting and injection molding, the dies/molds have to be polished anyway in order to achieve the required surface finish.

<table>
<thead>
<tr>
<th>STOCK ALLOWANCE</th>
<th>for Semi-Finishing</th>
<th>for Finishing</th>
</tr>
</thead>
<tbody>
<tr>
<td>Injection molds</td>
<td>0.25 - 0.75</td>
<td>0.125 to 0.25</td>
</tr>
<tr>
<td>Stamping dies</td>
<td>0.25 - 0.75</td>
<td>0.125 to 0.25</td>
</tr>
<tr>
<td>Die-casting dies</td>
<td>0.5 - 1.5</td>
<td>0.125 to 0.25</td>
</tr>
<tr>
<td>Forging dies</td>
<td>0.5 - 2.5</td>
<td>~ 0.75</td>
</tr>
</tbody>
</table>

Table 2.2: Machining allowances in die and mold machining
[Fallböhmer; 1996] [Dewes; 1997]

2.2.3 Die and Mold Materials

According to the same survey [Fallböhmer; 1996], 50% of the surveyed die and mold manufacturers are involved in manufacturing injection molds. In mold materials, the most common material is P20 mold steel in prehardened conditions of 30 HRC. Forging dies as well as die-casting dies mainly consist of H13 at a hardness range from 45HRC to 55HRC for forging dies and 46HRC to 50HRC for die-casting dies. The most common materials are listed in Table 2.3.

<table>
<thead>
<tr>
<th>APPLICATION</th>
<th>MATERIAL</th>
</tr>
</thead>
<tbody>
<tr>
<td>Injection molds</td>
<td>Mainly P-20, S-7, H-13, P-2 and A-2</td>
</tr>
<tr>
<td>Stamping dies</td>
<td>Mainly cast iron, D-2 and A-2</td>
</tr>
<tr>
<td>Die-casting dies</td>
<td>Mainly H-13</td>
</tr>
<tr>
<td>Forging dies</td>
<td>H-11, H-12, H-13 and H26</td>
</tr>
</tbody>
</table>

Table 2.3 Most commonly used die and mold materials [Budinski; 1980] [Dewes; 1996]

2.3 Cutting Tools and Machine Tools for End Milling

Among the cutting tools used for die and mold machining, carbide is the most common cutting tool material. Carbide tools have a high degree of toughness but poor hardness compared to exotic materials such as cubic boron nitrite (CBN) and ceramics. According to Stephenson [1997a], ideal cutting tool material
should have the highest toughness and hardness. In order to improve the hardness and surface conditions, carbide tools are coated with hard coatings such as TiN, TiAlN and TiCN. Other cutting tool materials used are; ceramics (AlO, SiN), cermet and polycrystalline diamond (PCD) [Stephenson; 1997a]. In this study, the attention is given only to uncoated tungsten carbide (WC) cutting tools.

In the die and mold industry, various configurations of machine tools are being used. However, 3-axis horizontal and vertical milling centers are the most common configuration. There are many high-performance (10,000-30,000 rpm spindle speed, 7.5-40 kW spindle power and 10-60 m/min feed rate) machining centers available on the market and details are given in a recent work [Dewes; 1997]. This study used an 4-axis horizontal machining center (Makino A55 Delta), located at the Net Shape Manufacturing Laboratory, with capabilities including 14,000 rpm spindle speed, 22 kW spindle power and 40 m/min feed rate.

2.3.1 Mechanical and Thermal Properties of Carbide Cutting Tools

The basic particles of tungsten carbide (WC) are bonded with metallic cobalt. This composition type is still considered to have greatest resistance to abrasive wear and therefore has many applications in machining [Stephenson; 1997a]. The chemical, thermal and mechanical properties of cutting tool materials are given in Table 2.4. Generally, there are two parameters considered to describe the carbide tools:

- Tungsten Carbide (WC) grain size
- Cobalt content

The relationship between these two parameters and their influence on the properties of carbide tools can be explained as follows:
2.3.1.1  Hardness and Wear Resistance

One of the most important properties of a carbide tool is extreme hardness at room temperature. For maximum hardness, the tungsten carbide grains are recommended to be as small as possible (<1 µm). Hardness and abrasion resistance increase as the cobalt content is lowered. Wear resistance is denoted by the number of disc revolutions for a predetermined volume loss of carbide when a carbide pin or block is applied to an abrasive disc. Typical values of hardness for tungsten carbides are between 800 to 2400 HV, or 83 to 94.5 HRA.

2.3.1.2  Tensile and Fatigue Strength

It is very difficult to measure tensile strength of hard materials because of difficulties in gripping and maintaining precise alignment. Thus, it is determined by calculation as a percentage of transverse rupture strength (45 to 55 percent). Fatigue strength gives the resistance to repeated stress cycles and is determined by the rotating bend test or pulsating compressive stress. The endurance limit is usually $10^8$ cycles (reverse bending) 600 to 850 N/mm².

2.3.1.3  Toughness, Impact Strength and Transverse Rapture Strength

The toughness of a solid body can be best described by resistance to shock loading called impact strength. Although many manufacturers seem to regard transverse rupture strength as a measure of shock resistance, in practice the correlation is extremely limited, except with grades of basically similar composition and structure. Compared to other cutting tool materials such as high speed steel (HSS) and CBN the toughness of tungsten carbide is higher. A typical range of values is 0.4 to 6 Joules (Carboloy, Charpy test), 0.3 to 5 Joules/cm² (Kennametal, drop test with 4.76 mm square bar).

2.3.1.4  Thermal Conductivity

The Thermal resistance and stability of a cutting tool material are mainly influenced by thermal conductivity. Because, the thermal conductivity of tungsten
carbide (WC) ranges from 80 to 120 W/m K, it is evident that the value is extremely dependent on the cobalt content.

<table>
<thead>
<tr>
<th>CARBIDE GRADE</th>
<th>M20-M30 (F)</th>
<th>K10-K20 (G)</th>
<th>P20-P30</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chemical Content</td>
<td>82%WC 5%TiC 5%Ta(Nb)C</td>
<td>92%WC - 2%Ta(Nb)C 6%Co</td>
<td>79%WC 8%TiC 5%Ta(Nb)C 8%Co</td>
</tr>
<tr>
<td>Melting points [°C]</td>
<td>2800 for WC</td>
<td>3100 for TiC</td>
<td>1495 for Co</td>
</tr>
<tr>
<td>ELASTIC PROPERTIES</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Young's Modulus [MPa]</td>
<td>558,000</td>
<td>600,000</td>
<td>558,000</td>
</tr>
<tr>
<td>Poisson's Ratio [-]</td>
<td>0.22</td>
<td>0.21</td>
<td>0.22</td>
</tr>
<tr>
<td>Thermal Expansion Coefficient ([10^{-5}/°K])</td>
<td>5.8-6.8</td>
<td>4.2-5.2</td>
<td>5.8-6.8</td>
</tr>
<tr>
<td>THERMAL PROPERTIES</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Thermal Conductivity ([W/(m°K)])</td>
<td>50</td>
<td>80-100</td>
<td>60</td>
</tr>
<tr>
<td>Specific Heat ([\text{Cal/g}°C])</td>
<td>0.055</td>
<td>0.050</td>
<td>0.055</td>
</tr>
<tr>
<td>Heat Capacity ([\text{N/mm}^2/°C])</td>
<td>3.62</td>
<td>3.12</td>
<td>2.79</td>
</tr>
<tr>
<td>Emissivity [-]</td>
<td>0.5</td>
<td>0.5</td>
<td>0.5</td>
</tr>
<tr>
<td>MECHANICAL PROPERTIES</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Density ([\text{g/cm}^3])</td>
<td>13.3-13.6</td>
<td>14.9</td>
<td>12.1-12.9</td>
</tr>
<tr>
<td>Hardness [HV]</td>
<td>1440-1540</td>
<td>1730</td>
<td>1580</td>
</tr>
<tr>
<td>Modulus of Rigidity [MPa]</td>
<td>230,000-245,000</td>
<td>240,000-260,000</td>
<td>230,000-245,000</td>
</tr>
<tr>
<td>Relative Abrasion Resistance [-]</td>
<td>10-13</td>
<td>8-12</td>
<td>12-14</td>
</tr>
<tr>
<td>Transverse Rapture Strength [MPa]</td>
<td>1900-2000</td>
<td>1700</td>
<td>1600</td>
</tr>
<tr>
<td>Charpy Impact Values [N.mm]</td>
<td>1250-1750</td>
<td>1250-1750</td>
<td>1250-1750</td>
</tr>
<tr>
<td>Fatigue Strength Endurance Limit [MPa] ((10^8\text{ stress cycles}))</td>
<td>650-805</td>
<td>625-800</td>
<td>650-805</td>
</tr>
<tr>
<td>Tensile Strength [MPa]</td>
<td>1300-1700</td>
<td>1100-1500</td>
<td>1300-1700</td>
</tr>
<tr>
<td>Compressive Strength [MPa]</td>
<td>3800-5170</td>
<td>4100-4600</td>
<td>3800-5170</td>
</tr>
</tbody>
</table>

Table 2.4: Properties of Sintered Tungsten Carbides [ASM; 1997]

2.4 Flat End Milling

The most common nose geometry for cutting tools is the ball-nose end mill or the flat-end mill depending on the number of axes when machining dies and molds on multi-axis milling centers. Solid and indexable cutting tools are used in machining of dies and molds surfaces. Indexable end mills for larger tool diameters (>10 mm) and solid end mills for smaller tool diameters are usually preferred.
2.4.1 Undeformed Chip Geometry in Flat End Milling

Since this work is focused on flat end milling, the undeformed chip geometry produced is presented in Figure 2.2 during the peripheral milling operation with a flat end mill and a straight cutting edge (zero helix angle). The chip cross section is taken from a plane normal to the tool axis. It is noted that chip geometry does not change on the planes at different heights along the tool axis. The chip thickness, which is a function of the rotation angle, is measured in the radial direction with respect to the tool center at the current revolution.

The engagement of the cutter is defined with the entrance angle at the initial contact ($\phi_{\text{enter}}$) and exit angle at the end of contact ($\phi_{\text{exit}}$) for one rotation. The maximum uncut chip thickness ($h_{\text{max}}$) is reached after initial contact in down (climb) milling mode (see Figure 2.3). A fully developed chip is usually defined from the rotation angle of maximum chip thickness ($h_{\text{max}}$) up to the rotation angle that corresponds to (see Figure 2.3). The definitions of chip thickness ($h$) and chip length ($l_c$) for the chip geometry generated in flat end milling are shown in Figure 2.4. The chip thickness ($h$) is measured in a radial direction from the tool axis of the current revolution. The chip length ($l_c$) is measured from the rotation angle $\phi_{\text{enter}}$ to $\phi_{\text{exit}}$ (as shown in Figure 2.3). The cutting tool and workpiece materials are the main factors that determine the appropriate cutting conditions for a certain application. The milling parameters (spindle speed and feed rate) are selected according to the tool engagement conditions.
Figure 2.2: Peripheral milling operation with flat end mill and the representation of 3-D undeformed chip geometry
Figure 2.3: Peripheral milling operation with flat end mill – nomenclature: entry and exit angles in climb milling mode
CHAPTER 3

MECHANICS AND MODELING OF METAL CUTTING PROCESSES

3.1 Background in Metal Cutting

There are three major cutting geometries for classification; orthogonal, oblique and three-dimensional (3-D) cutting. Orthogonal and oblique cutting, as shown in Figure 3.1, are not commonly used for practical machining operations. However, they are utilized in development of mechanic models associated with deformation theory and they are useful to analyze 3-D practical machining operations.

Orthogonal cutting uses a single cutting edge. While the tool approaches the uncut workpiece, plastic deformation takes place around the cutting edge and the chip is formed in a shear zone. (Figure 3.2). This indicates that analysis can be done in a plane and contains two velocity or force components. End turning a tube or disk and slotting operation with a straight-edge milling cutter can be modeled as orthogonal cutting. There are some closed-form-solutions for the prediction of cutting process variables based on thin shear zone (or shear plane) assumptions [DeVries; 1992].
Figure 3.1: The basic cutting geometry a) orthogonal cutting, b) oblique cutting [Boothroyd; 1966]

Figure 3.2: Deformation zones in orthogonal cutting
Oblique cutting also uses a single cutting edge. In this case the cutting velocity is oblique to the cutting edge with an inclination angle $i$, Figure 3.1. The chip is formed in a shear zone near the cutting edge but no longer in a plane. However, the mechanics of oblique cutting can still be analyzed as a two-dimensional problem when a correct reference plane is chosen and a transformation is applied to obtain a three-dimensional force vector. Cutting with helical end mills, face mills or some single edge turning tools can be considered as oblique cutting [DeVries; 1992].

3-D cutting uses multiple edges rather than a single cutting edge, as is done orthogonal and oblique cutting. These cutting edges are usually called as major and minor cutting edges. That problem cannot be solved in two dimensions and there are no analytical closed-form solutions presented in the literature. However, several numerical approaches based on finite element analysis (FEA) have been presented. These are reviewed in section 3.5.

During cutting process, three fundamental type of chip formation may occur:

- **Type I discontinuous** - where chips form into small segments. This type of chip formation is observed when work material is unable to go under large plastic deformations and fractures.

- **Type II continuous** - where the chips form long and continuous ribbons. This is the most common chip type observed in turning operations. The mechanism for chip formation is almost entirely plastic deformation.

- **Type III continuous with built-up edge** - where work material around the cutting edge tends to weld to the tool surface and fresh chips slide over the welded zone. A built up edge occurs at relatively low cutting speeds and is not a major concern in HSM, which is the main focus of the present research.

- **Type IV continuous with “saw tooth” shape** - where the chips form by shear localization on the secondary deformation zone. As explained earlier, saw-
tooth shaped chips are dominant at high chip thickness and high cutting speeds when machining hard steels.

3.2 Mechanics of Metal Cutting Processes

In this section, conventional techniques and existing models will be briefly reviewed.

3.2.1 Orthogonal Cutting

In cutting, the tool is forced to move through the workpiece so that a chip of material is removed from the surface (see Figure 3.2). The contact conditions between the tool and the workpiece are very severe. The temperature at the tool-workpiece interface may be as high as 900 to 1300°C when cutting mild steel. In general, temperature gradient is sensitive to cutting speed. According to Shaw [1989]:

- The heat produced in the contact originates primarily from heat generated by the mechanical deformation of the chip. Secondly, heat comes from friction between the chip and the tool rake face and in small amounts from the rubbing action on the tool tip.

- A major amount of the heat is removed by the chip. Some is absorbed by the workpiece and the rest is conducted through the tool.

The normal pressure on the rake face is very high and can be 1200 MPa (172 ksi) when cutting steel [Trent; 1979]. As illustrated in Figure 3.2, there are local deformation zones where the severe deformations and high strain rates take place:

- From the tip of the tool to the intersection of chip and uncut workpiece, there is a shear zone where deformation takes place. This region is referred to the *primary deformation or shear zone*. The shear stress is constant on the tool rake face until a certain point at which the chip starts to slide on the tool rake face.


• The secondary deformation or shear zone is at the chip-tool contact interface where plastic deformation due to frictional stress occurs.
• As the cutting edge moves through the workpiece, a third shear zone occurs under and behind the leading edge.

A large number of researchers have proposed various models to describe the cutting process. The earliest models were based on the shear plane assumption of Ernst and Merchant [Ernst; 1941]. The major drawback of those models was the infinite acceleration of the chip flow on the shear plane. Some improvements to Merchant’s model were made by Lee and Schaffer [Lee; 1951] in which rigid-plastic workpiece material was assumed and slip line field analysis was used to model the shear zone. Later, cutting models that included friction, and material behavior at high strains, strain-rates and temperatures were presented [Zorev; 1963].

3.2.1.1 Tool forces in orthogonal cutting

The simplest force model was also first proposed by Ernst and Merchant [Ernst; 1941] and assumes continuous chip formation, a sharp cutting edge (no plowing and flank force), thin shear zone that is idealized as a plane and uniform shear stress on shear plane (see Figure 3.3). In practice, no cutting tool can be manufactured with a perfectly sharp cutting edge; thus a roundness always exists. Therefore, the resultant tool force can be expressed as:

\[ R = F_R + F_P \]  \hspace{1cm} (3.1)

where \( F_R \) is the component that removes the chip on a plane normal to the tool cutting edge (see Figure 3.3). The effects of the cutting edge on the cutting forces due to flank rubbing action is represented with a “plowing” force component, \( F_P \). Generally, the zero feed force is diagnosed as plowing force. At a given uncut thickness and a constant cutting speed, the cutting forces are measured and extrapolated to the zero uncut thickness to find the plowing force.
Figure 3.3: Shear plane model and force diagram in orthogonal cutting [Boothroyd; 1966]
Alternatively, zero-feed force can be determined by noting the minimum force observed after ceasing the feed motion while continuing to rotate the part against the stationary tool in a turning operation [Stevenson; 1997].

The thin shear zone model for continuous chip formation is widely accepted by the research community along with the following assumptions:

- The tool tip is round, therefore rubbing action takes place in flank contact. According to Equation 3.1 plowing forces do not contribute to remove the chip removal. Thus, deformation analysis has been done considering $F_R$ only.
- Plane-strain conditions prevail and no chip side flow occurs.
- The stresses on the shear plane are uniformly distributed.

Geometric and kinematic relations in the thin shear zone yield the following equations using velocity and strain components.

$$ \frac{V_c}{\cos(\phi_s - \alpha)} = \frac{V_{chip}}{\sin \phi_s} = \frac{V_r}{\cos \alpha} $$

(3.2)

Thus, the shear strain and strain rate can be estimated in terms of cutting speed, rake and shear angles as:

$$ \bar{\gamma} = \frac{\cos \alpha}{\sin \phi_s \cos(\phi_s - \alpha)} $$

(3.3)

$$ \dot{\gamma} = \frac{V_c \cos \alpha}{\Delta s \cos(\phi_s - \alpha)} $$

(3.4)

It should be noted that the shear strain rate is about $\dot{\gamma} = 10^4$ sec$^{-1}$ when conventional machining at a cutting speed of 30 m/min [Oxley; 1989]. Based on the material continuity assumption, the geometrical relationships allow the estimate cutting ratio as:

$$ r_i = \frac{t_u}{t_c} = \frac{\tan \phi_s}{\cos \alpha + \tan \phi_s \sin \alpha} $$

(3.5)
and the shear plane angle from the cutting ratio as:

\[
\phi_s = \tan^{-1} \left[ \frac{(t_u / t_c) \cos \alpha}{1 - (t_u / t_c) \sin \alpha} \right]
\]  

(3.6)

the length of the shear plane:

\[
l_s = \frac{t_u}{\sin \phi_s} = \frac{t_c}{\cos \left( \phi_s - \alpha \right)}
\]

(3.7)

The shear and normal shear forces, \(F_s\) and \(F_{ns}\), on the shear plane can be written as functions of the measured cutting force components, \(F_c\) and \(F_t\), and the shear plane angle:

\[
F_s = F_c \cos \phi_s - F_t \sin \phi_s
\]

(3.8)

\[
F_{ns} = F_c \sin \phi_s + F_t \cos \phi_s
\]

(3.9)

Similarly, the normal and frictional forces, \(F_n\) and \(F_f\), on the tool rake face can be written as functions of measured cutting force components, \(F_c\) and \(F_t\), and tool rake angle as:

\[
F_n = F_c \cos \alpha - F_t \sin \alpha
\]

(3.10)

\[
F_f = F_c \sin \alpha + F_t \cos \alpha
\]

(3.11)

From the force balance in Figure 3.3, the cutting forces are expressed with the following equations.

\[
\vec{F}_R = \vec{F}_c + \vec{F}_t = \begin{bmatrix} F_c \\ F_t \end{bmatrix} = \begin{bmatrix} \cos(\beta - \alpha) \\ \cos(\phi_s + \beta - \alpha) \cos(\phi_s - \beta - \alpha) \end{bmatrix} \begin{bmatrix} \tau_{w} \alpha \\ t_u \end{bmatrix} \sin \phi_s
\]

(3.12)

At the chip-tool interface as shown in Figure 3.3, the friction angle is defined according to the Coulomb friction law as:

\[
\mu = \tan(\beta) = \frac{F_f}{F_n}
\]

(3.13)
Tool reaction force, $\vec{F}_r$, on the tool rake face can then be written in normal and frictional force components, $F_n$ and $F_f$, as:

$$\vec{F}_r = \vec{F}_n + \vec{F}_f = \begin{bmatrix} F_n \\ F_f \end{bmatrix} = \begin{bmatrix} \cos(\phi_s - \alpha) - \mu \sin(\phi_s - \alpha) \\ \cos(\phi_s - \alpha) - \mu \sin(\phi_s - \alpha) \end{bmatrix} \frac{\tau_{wL}}{\sin \phi_s}$$  \hspace{1cm} (3.14)

The resultant force, $\vec{F}_R$, on the shear plane can be written in shear force and normal shear force components, $F_s$ and $F_{ns}$, as:

$$\vec{F}_R = \vec{F}_s + \vec{F}_{ns} = \begin{bmatrix} F_s \\ F_{ns} \end{bmatrix} = \begin{bmatrix} \mu \cos(\phi_s - \alpha) + \sin(\phi_s - \alpha) \\ \cos(\phi_s - \alpha) - \mu \sin(\phi_s - \alpha) \end{bmatrix} \frac{\tau_{wL}}{\sin \phi_s}$$  \hspace{1cm} (3.15)

From the above equations the cutting forces can be predicted if the mean shear stress on the shear plane, $\tau_s$, shear angle, $\phi_s$ and friction angle, $\beta$ are known. Usually, $\tau_s$ and $\beta$ are determined from the material strength and friction tests respectively under the metal cutting conditions [Armarego; 1983].

3.2.1.2 Stresses on the shear plane

It is assumed that on the shear plane, shear and shear normal forces have uniform distribution over the shear area. The area of shear is simply written as:

$$A_s = \frac{A_u}{\sin \phi_s}$$  \hspace{1cm} (3.16)

Normal shear stress and shear stress on the shear plane is given by:

$$\sigma_{ns} = \frac{F_{ns}}{A_s} = \frac{[F_c \sin \phi_s + F_t \cos \phi_s] \sin \phi_i}{t_u w} = \frac{F_c \sin^2 \phi_s + F_t \cos \phi_s \sin \phi_i}{t_u w} \hspace{1cm} (3.17)$$

$$\tau_s = \frac{F_s}{A_s} = \frac{[F_c \cos \phi_s - F_t \sin \phi_s] \sin \phi_i}{A_u} = \frac{F_c \cos \phi_s \sin \phi_i - F_t \sin^2 \phi_s}{t_u w} \hspace{1cm} (3.18)$$
3.2.1.3 Stresses on the tool rake face

Zorev [1963] assumed that at the chip-tool interface two regions exist simultaneously on the tool rake face when machining under dry conditions:

- From the tip of the tool up to a point where frictional stress is constant, the chip-tool plastic contact occurs in the sticking region, (see Figure 3.4a)
- After this point shear stress decreases on the tool rake face, where the chip-tool elastic contact takes place. This region is called the sliding region and Coulomb's friction law can be applied, (see Figure 3.4a).

The normal ($\sigma_n$) and frictional shear ($\tau_i$) stress distributions at the chip-tool interface characterize the cutting temperature and further tool wear. Several shapes have been suggested for the distributions of the normal ($\sigma_n$) and frictional shear ($\tau_i$) stresses (see Figure 3.4) and a power law assumption was commonly used to model the stress distributions [Zorev; 1963]. These models have been supported with experimental studies using split-tool or composite tool approaches by measuring the stresses near the tool edge and rake face [Lee; 1995]. Later, experiments on different workpiece materials yielded that the trend for the stress curves on the tool rake face may not be represented only with Zorev's model and some refinements to that model have been made [Barrow; 1982]. As illustrated in Figure 3.4c, the normal stress ($\sigma_n$) curve is usually an exponential but can also show a leveling at some point of the curve. In frictional stress curve, a leveling that is equal to work material shear flow stress in the mid point of the curve was observed from the test results in cutting low carbon steel. That is true, because frictional shear stress ($\tau_i$) cannot be greater than nearby workpiece shear flow stress (i.e. $\tau_i \leq k_{chip}$). Furthermore, a recent model [Li; 1997] based on experimental observations, has been developed considering a uniform stress distribution on the sticking contact region (see Figure 3.4b).
Figure 3.4: Curves representing normal ($\sigma_n$) and frictional ($\tau_f$) stress distributions on the tool rake face proposed by a) [Zorev; 1963], b) [Barrow; 1982] [Li; 1997], c) [Lee; 1995]
3.2.1.4 Friction characteristics on tool-chip interface

Friction in metal cutting is extremely difficult to determine. The contact regions and the friction parameters between the chip and the tool are influenced by factors such as cutting speed, feed rate, rake angle, etc. [Childs; 1997], mainly because of the very high normal pressure at the surface. The prevalent conditions at the tool-chip interface constrain the use of the empirical values of the coefficient of friction found from ordinary sliding test conditions. While numerous works have been reported to quantitatively explain the variable and high values of friction at the rake face, none has provided reliable quantitative predictive models that are devoid of experimental testing.

In conventional machining at low cutting speeds, the friction mechanism is mostly effective at the tool flank face. However, in high speed machining, a tremendous increase in the chip velocity, the chip-tool friction contact and the temperatures are encountered at the tool rake face [Schulz; 1992]. As a result, the increasing sliding velocity and frictional stress cause significant wear on the tool rake face. Therefore, the rate of the tool wear heavily depends on the frictional conditions in high speed machining.

3.2.2 Oblique Cutting Models

In oblique cutting, the cutting velocity vector and single, straight cutting edge forms the inclination angle ($i$) (see Figure 3.5). That is usually selected as a cutting condition. Chip flow on the rake face is described with the chip flow angle ($\eta_c$). This angle forms in the plane defined by the rake face of the cutting edge and is measured from a plane normal to the cutting edge and affected by the inclination angle. In orthogonal cutting chip flow angle is zero ($\eta_c = 0$) but in oblique machining, the chip changes its direction due to the inclination angle ($i$). Therefore, the tool geometry and orientation direct the chip flow. Stabler [1951] developed a geometrical analysis in which the chip flow angle ($\eta_c$) was found to be equal to the inclination angle ($i$).
The normal rake angle ($\alpha_n$) is measured in a plane normal to the inclined cutting edge as in orthogonal machining. This reference prompts similar expressions as in orthogonal cutting when the shear plane is measured in the same plane as normal rake angle ($\alpha_n$). The normal shear angle ($\phi_n$) is the shear angle in this plane. The velocity and strain triangle gives the relation:

\[
\begin{align*}
\frac{V_c \cos i}{\cos(\phi_n - \alpha_n)} &= \frac{V_n}{\sin \phi_n} = \frac{V_c \cos \eta_s}{\cos \alpha_n} \\
\eta_s &= \tan^{-1} \left( \frac{\tan i \cos(\phi_n - \alpha_n) - \tan \eta_s \cos \phi_n}{\cos \alpha_n} \right)
\end{align*}
\]

(3.19)  

(3.20)

is referred to as the shear flow direction. Shear strain and strain rate can be written:

\[
\dot{\gamma} = \frac{\cos \alpha_n}{\sin \phi_n \cos(\phi_n - \alpha_n) \cos \eta_s}
\]

(3.21)

\[
\dot{\gamma} = \frac{V_c \cos \alpha_n}{\Delta \cos(\phi_n - \alpha_n) \cos \eta_s}
\]

(3.22)

When $i=0$, then $\eta_s=\eta_s=0$, the above equations reduce to those for orthogonal machining. The cutting force vector for oblique machining $\vec{F}_c = [F_c, F_t, F_r]^T$ where cutting force ($F_c$), thrust force ($F_t$), and radial force ($F_r$) can be estimated. A transformation [DeVries; 1992], where the $y$-$z$ plane is rotated by $i$ about the $x$ axis, forms a primary set of coordinates ($x'$-$y'$-$z'$) as:

\[
\vec{F}_c' = T_{xy}(i) \vec{F}_c = \begin{bmatrix} F_{c'} \\ F_{t'} \\ F_{r'} \end{bmatrix} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos i & \sin i \\ 0 & -\sin i & \cos i \end{bmatrix} \vec{F}_c
\]

(3.23)

An oblique cutting force analysis is made in this $x'$-$y'$-$z'$ coordinate system and with the inverse transform force vector is transformed to $x$-$y$-$z$ coordinate system, Figure 3.5.
Figure 3.5: Cut geometry and cutting forces in oblique machining [Oxley; 1989]
On the shear plane, the shear force vector $\vec{F}_s = [F_{sx} \ F_{sy} \ F_{sz}]^T$ has three components, $F_{sx}$, shear component normal to edge, $F_{sy}$, compressive component, and $F_{sz}$, shear component parallel to the edge. Shear force can be expressed as a function of stress ($\tau_r$) on the shear plane and normal shear angle ($\phi_n$):

$$
\vec{F}_s = \begin{bmatrix}
F_{sx} \\
F_{sy} \\
F_{sz}
\end{bmatrix} = \begin{bmatrix}
\frac{\tau_r w_n \cos \eta_n}{\sin \phi_n \cos \theta} \\
\frac{\tau_r w_n \cos \eta_n}{\sin \phi_n \cos \theta} \\
\frac{\tau_r w_n \sin \eta_n}{\sin \phi_n \cos \theta}
\end{bmatrix} = \begin{bmatrix}
\cos \phi_n & -\sin \phi_n & 0 \\
\sin \phi_n & \cos \phi_n & 0 \\
0 & 0 & 1
\end{bmatrix} \vec{F}_R 
$$

(3.24)

Using the similar transformation defined in Equation 3.23, shear force can be obtained if normal shear and inclination angles are known from resultant cutting force vector as:

$$
\vec{F}_s = T_{\tau_\gamma} (\phi_n) T_{\tau_\gamma} (\theta) \vec{F}_R 
$$

(3.25)

The friction force is defined as the reaction force on the rake face in the opposite direction of chip velocity, $V_{\text{chip}}$. Thus, the rake face force vector can be represented with three force components; normal rake $F_n$, friction normal to cutting edge $F_{f1}$, and friction parallel to cutting edge $F_{f2}$, and is indicated by

$$
\vec{F}_F = \begin{bmatrix}
F_n \\
F_{f1} \\
F_{f2}
\end{bmatrix} = \begin{bmatrix}
\frac{F_n}{\mu F_n \cos (\eta_n)} \\
\frac{F_n}{\mu F_n \cos (\eta_n)} \\
\frac{\mu F_n \sin (\eta_n)}{\mu F_n \sin (\eta_n)}
\end{bmatrix} = \begin{bmatrix}
\cos \alpha_n & -\sin \alpha_n & 0 \\
\sin \alpha_n & \cos \alpha_n & 0 \\
0 & 0 & 1
\end{bmatrix} \vec{F}_R 
$$

(3.26)

The following expression can be obtain with a transformation of cutting force vector in $x'-y'-z'$ coordinates:

$$
\vec{F}_F = T_{\tau_\gamma} (-\alpha_n) \vec{F}_R 
$$

(3.27)

The friction coefficient ($\mu$) is related to normal friction angle ($\beta_n$), and the chip flow angle ($\eta_0$) with;
\[ \mu = \frac{\sqrt{F_{f1}^2 + F_{f2}^2}}{F_n} = \frac{\tan \beta_n}{\cos \eta_c} = \tan \beta \]  

(3.28)

This indicates the friction angle \( (\beta) \) in Equation 3.13; therefore the friction coefficient in orthogonal machining can be used for oblique machining. Thus, the static equilibrium equations in the \( x'-y'-z' \) coordinates are:

\[ \vec{F}_r = T_{x'y'}(\phi_n) \vec{F}_s = T_{x'y'}(\alpha_n) \vec{F}_r \]  

(3.29)

\[ \vec{F}_s = T_{x'y'}(\alpha_n - \phi_n) \vec{F}_s \]  

(3.30)

\[ \vec{F}_r = T_{x'y'}(-i) \vec{F}_r = T_{x'y'}(-i)T_{x'y'}(\phi_n) \vec{F}_s \]  

(3.31)

Finally, the cutting force vector can be written as:

\[
\vec{F}_r = \begin{bmatrix}
\cos(\beta_n - \alpha_n) \cos \eta_s \cos i + \cos(\phi_n + \beta_n - \alpha_n) \sin \eta_s \sin i \\
\cos(\phi_n + \beta_n - \alpha_n) \\
\sin(\beta_n - \alpha_n) \cos \eta_s \\
\cos(\phi_n + \beta_n - \alpha_n) \\
\cos(\beta_n - \alpha_n) \cos \eta_s \sin i - \cos(\phi_n + \beta_n - \alpha_n) \sin \eta_s \cos i \\
\cos(\phi_n + \beta_n - \alpha_n) \\
\end{bmatrix} \frac{\tau_{w f_w}}{\sin \phi_n \cos i} 
\]  

(3.32)

It should be noted that if the inclination angle is zero, Equation 3.32 simplifies to Equation 3.12 in the orthogonal machining case.

### 3.3 Tool Wear and Premature Tool Failure

Wear is basically removal of material from the body of the tool and is a result of process parameters existing at the tool-workpiece interface due to relative motion and cutting conditions. There are various mechanisms and modes of tool wear that have been observed in metal cutting. The investigation of machining wear due to cutting speed and temperature shows that tool wear is due to a complex combination of wear modes and is highly sensitive to cutting conditions (see Figure 3.6).
Figure 3.6: Schematic diagram of the tool wear mechanisms appearing at different cutting temperatures corresponding to cutting speed and feed [König; 1984]

Figure 3.7: Typical locations of location dependent wear on the tool [Holmberg; 1994]
Flank wear at the front edge of the tool flank and crater wear at the tool rake face (see Figure 3.7a) are the most typical modes of tool wear in metal cutting. Flank wear is mainly a result of abrasion of the tool by hard particles, but there may be adhesive effects also. It is the dominating wear mode at low cutting speeds. Crater wear is the formation of a groove or a crater on the tool rake face where the chip rubs the tool surface (see Figure 3.7b).

Different wear mechanisms, predominantly present in metal cutting, can be classified as sliding wear mechanisms and non-sliding wear mechanisms.

3.3.1 Sliding Tool Wear Mechanisms

Sliding wear mechanisms include abrasive wear, adhesive wear and delamination wear. Abrasive wear is caused by the contact of a tool body with a particle, where the particle slides over the body and the movement causes material loss. Adhesive wear occurs between two surfaces rubbing against each other as a result of formation and breakage of the interfacial bonds. Delamination wear is another mechanism of wear when chips are sliding against tool surfaces at low speeds which do not generate very high contact temperatures. In this process large wear particles are removed in the form of layers by the process of plastic deformation of the surface layer [Suh; 1973].

3.3.2 Non-Sliding Tool Wear Mechanisms

Non-sliding wear mechanisms include solution wear, diffusion wear, electrochemical wear and oxidation wear and become dominant with the chemical instability where cutting temperatures are very high, up to 1600°C as in high speed machining [Schulz; 1995]. Solution and diffusion wear is a result of a tendency in workpiece and tool material to dissolve in each other to form a solution. The rate of the solution is dependent on cutting temperature at the interface and increases with the increasing temperature [Suh; 1986].
The wear due to diffusion is more dependent on the chemical properties of the material rather than its mechanical strength or hardness. *Electrochemical wear* is caused by a thermoelectric electromotive force generated at the work-tool junction during the cutting process. The e.m.f. causes electric currents to circulate and results in the passage of ions from the tool to the workpiece [Moore; 1975]. *Oxidation wear* is a result of the chemical bonding of atoms of tool material with oxygen. Oxidation in metal cutting occurs in the area where the chips are generated at elevated temperatures, usually above 700 °C [König; 1984].

3.3.3 Premature Tool Failure

The sudden and premature tool failure caused by chipping of the tool cutting edge is one of the most important aspects of metal cutting that needs further investigation. In production with transfer lines, cutting tools should have a life longer than the predetermined period of time to facilitate tool changes at the end of the shifts. Premature tool failure not only damages the workpiece, but also causes production downtime. In the past, chipping of the cutting edge was attributed to the impact loading resulting from the sudden engagement of the tool into the workpiece material.

In flat end milling, the entrance and exit angles, as defined in Figure 2.3, have an influence on tool chipping and catastrophic failure. In an early study, Kronenberg provided a detailed analysis of the insert’s initial contact in face milling [Kronenberg; 1946]. In this analysis, the entrance angle and tool geometry (axial rake angle, radial rake angle, etc.) determine the type of initial contact and penetration time (time required for the cutting edge to travel from initial contact to a fully developed chip). Other researchers have used Kronenberg’s analysis to investigate the influence of entrance angle on tool chipping [Hoshi; 1965] [Opitz; 1970] [Lehtihet; 1986].
However, more recent investigations [Pekelharing; 1978] revealed the fact that this phenomenon is mostly influenced by the exit conditions of the tool from the workpiece where the shear plane changes direction (negative shear angle), which causes foot formation in the workpiece [Pekelharing; 1978], [Pekelharing; 1984], [Shaw; 1979], [Ramaraj; 1988]. Most of the cutting tools are made of brittle (low toughness) materials. Thus, the induced tensile stresses on the tool cutting edge may cause tool failure by chipping.

3.4 Mechanistic Modeling of Metal Cutting Processes

Mechanics of metal cutting have been extensively researched over the last four decades with several different analytical, experimental, and numerical approaches. The main goal was to develop broad mathematical models for the prediction of various performance measures in the cutting processes. However, fundamental studies and mathematical analysis of the cutting process were restricted to the orthogonal and, to a lesser extent, oblique cutting. However, the estimation of machining performance of the complex practical machining operations such as turning and milling was based, on empirical testing (see Figure 3.8).

Most of the researchers worked on development of predictive models in order to estimate cutting forces in orthogonal and oblique cutting operations. These analyses were based on modified thin shear zone or plane deformation models, some ignoring the edge forces due to rubbing and plowing [Merchant; 1945] [Usui; 1982] and some incorporating them [Armarego; 1983] [Oxley; 1989].

Many researchers have focused on the effects of the mechanical properties of the work material at the high strain, strain-rates and temperatures of the deformation process in the shear zone and the friction process at the tool-chip interface [Boothroyd; 1966] [Thomsen; 1967] [Hastings; 1976] [Usui; 1982] [Maekawa; 1983] [Kitigawa; 1988] [Oxley; 1989].
Figure 3.8: Mechanistic modeling of machining based on analytical and empirical relations
Attempts to extend the orthogonal cutting (plane strain) model to oblique cutting have also been made and the approximation refined in the 1970s. More recently these approximate oblique cutting models have been applied to turning operations with sharp cornered turning tools to allow for the secondary cutting edge effects as well as for zero rake angle turning tools with a nose radius [Endres; 1994].

Following the same approach, some models for predicting forces in end milling process, based on an estimation of chip flow angle with oblique cutting theory [Stabler; 1951] [Shaw; 1989], have been developed [Kline; 1982] [Yang; 1989] [Budak; 1993] [Shin; 1997]. However, the orthogonal cutting experimental data is needed for each new material used for workpiece and tool. Furthermore, the validity of the application of oblique cutting theory to end milling models at high helical angles is questionable [Feng; 1994].

3.5 Finite Element Modeling of Metal Cutting Process

Many analytical and geometrical modeling attempts have been made in order to explain the mechanics of cutting process. Despite the fact that researchers gave better insight and interpretation to the cutting processes, the practical use of these theories in predicting cutting parameters has been limited. This is partly due to the simplifications and partly due to the fact that the models require empirical coefficients that should be obtained from costly and extensive cutting tests for each workpiece-tool pair.

In metal cutting, chip formation is a result of extremely severe and localized deformation that includes:
- temperatures of up to 1600°C [Schulz; 1992] in the case of high speed machining,
- pressures of more than 800 MPa (116 ksi) on the rake face [Oxley; 1989],
- strain rates that are as high as $10^6$ sec$^{-1}$ [Mathew; 1982].
The material properties under the above given processing conditions are different than that obtained in tensile or compression tests and need to be determined. Another aspect is the friction conditions on the chip-tool interface. In orthogonal and oblique machining models, estimation of friction properties is required to predict cutting forces, stresses and temperatures.

3.5.1 Research Efforts in Finite Element Modeling of Cutting Processes

A review of the technical literature reveals that currently finite element analysis of machining is not fully capable of simulating practical machining operations. In other words, the complexity and the diversity of cutting processes are such that a single process model or simulation cannot be applied to all materials and cutting conditions. However it should be noted that each of the reviewed analyses is valid for its target range of material properties and cutting conditions, as long as verification through accurate experimentation is carried out. A summary of the most significant efforts in finite element modeling of cutting process is shown in Appendix A.

- Childs and Maekawa

Childs and Maekawa developed a Lagrangian formulation for elasto-plastic deformation in orthogonal machining. In their model, an initial chip shape assumption was required to iterate deformation and strain rate; a strain and temperature-sensitive flow stress model was used [Childs; 1990]. Their focus was on prediction of tool wear by using a wear model based on temperature and normal stress on the tool surface. The comparison of predicted cutting forces and tool wear with experimental forces in machining steel seemed to be reasonable. Later, Maekawa [1996] extended their model to 3-D orthogonal cutting with two cutting edges and studied turning of Cr-Mo and Mn-B steels.

Cutting temperatures and predicted tool wear were compared with the experiment [Maekawa; 1993]. In a recent study, orthogonal machining of low-carbon free cutting steel was simulated [Childs; 1997]. Frictional and normal
stresses on tool rake faces were predicted by using a friction law obtained from experiments at zero rake angle.

- **Eldridge**

Eldridge [1991] developed a thermo-viscoplastic FEM model for orthogonal cutting simulations. This model was able to predict temperature, stress, strain and strain-rate distribution in workpiece and tool to a certain extent.

- **Iwata**

In this model a rigid-plastic workpiece was assumed and an iterative solution for chip geometry was performed [Iwata; 1984]. Later many researchers followed this approach to predict steady-state chip form.

- **Klamecki**

One of the first finite element models was only for the transient stage of cutting [Klamecki; 1973]. In Klamecki’s model, strain-rate effect and friction were not included. Later, this model was improved by applying thermo-elastic-plastic deformation [Klamecki; 1988]. However, temperature softening of work material assumed overcoming the strain-rate effects and the latter was neglected.

- **Komvopoulos and Erpenbeck**

A commercial software ABAQUS-2D™ and an updated Lagrangian FEM model was used to analyze orthogonal metal cutting by [Komvopolus; 1991]. They studied the effect of factors such as plastic flow of the workpiece material and friction at the chip-tool interface. Elasto-plastic workpiece models with isotropic strain hardening and strain rate sensitivity were simulated. Chip separation was simulated using superimposed nodes at the parting lines and was based on effective strain criteria. They modeled friction on the tool face based on Coulomb’s law. Different values of the friction coefficient were fabricated and the
results were compared. The results demonstrated that the thickness of the primary deformation zone was decreased for higher friction coefficients, while cutting forces, stresses, chip thickness and contact length were increased.

- **Lin**

Lin and Lin also developed a thermo-elastic-plastic FEM model based on the updated Lagrangian method and the Prandtl-Ruess flow rule [Lin; 1992]. In this study, the strain energy density was used as a chip separation criterion. The strain energy was determined from uniaxial tensile tests. Later, this deformation model was applied to study orthogonal cutting with worn tools and sticking behavior on the chip-tool interface [Lin; 1992] [Lin; 1995] [Lin; 1994].

- **Marusich and Ortiz**

Marusich developed a dynamic explicit code to solve constitutive equation of the cutting process with adaptive remeshing and simulated continuous and segmented chip formation [Marusich; 1995].

- **Rakotomalala**

Rakotomalala developed an Arbitrary Lagrangian Eulerian (ALE) software to simulate orthogonal cutting to avoid frequent remeshing for chip separation [Rakotomalala; 1993]. Some simulation results of predicted chip shape, stresses and temperatures were presented for machining mild steel without experimental verification.

- **Pantale**

Pantale et al. implemented an arbitrary Lagrangian Eulerian formulation into 3-D finite element modeling of the cutting process [Pantale; 1996]. Surface locations on the workpiece were continuously updated for chip separation and Coulomb's friction law was applied for both sticking and sliding regions at the chip-tool
interface. 3-D orthogonal (zero inclination angle) and oblique cutting with single edge were simulated when machining mild steel. Chip geometry such as chip contact length and shear angle, and cutting temperatures were compared with experimental results.

- **Sekhon and Chenot**

Sekhon and Chenot [1992] used FORGE-2™, another FEM software for metal forming processes, to simulate the cutting process in orthogonal machining. An updated Lagrangian implicit formulation with automatic remeshing for chip separation was used. Qualitative results have been presented for chip formation without experimental verification. Later, Fourment [1997] also used FORGE-2™ to predict chip flow and tool wear in orthogonal machining of mild steel.

- **Shih**

Shih [1990] developed an updated Lagrangian 2-D model including the effects of elasto-viscoplasticity, material flow stress, strain hardening and friction. The model for friction consisted of the sticking region for which the friction force is constant, and the sliding region for which the friction force varies linearly keep according to Coulomb's law. The location of the separation of these two regions was estimated from examining the marks left on worn tool rake face. The coefficient of friction was hard to predict in the sliding region and was thus chosen arbitrarily. The separation criteria for the elements in front of the tool tip created a numerical stability problem. That was the drawback of this FEM model. The simulations were conducted for low-carbon steel at about 35 m/min cutting speed, and cutting forces compared with experiments showed good agreement [Shih; 1995] [Shih; 1996].

- **Strenkowsk**

The first FEM model of cutting using an updated Lagrangian formulation to simulate chip formation from the transient stage to steady state was proposed
NIKE-2D software was used with some modifications. In this model, material was considered as elasto-plastic and strain-rate effects were neglected. A constant coefficient of friction was assumed on the chip-tool interface. A separation criterion based on effective strain of the elements in front of the tool tip was chosen. It was found that the chip geometry and tool forces were not affected by the choice of the criteria, but residual stresses were widely affected.

A viscoplastic FEM model with Eulerian method, in which the FEM mesh was considered as a control volume through which material flowed, was used. The advantages of using the Eulerian method are that there is no need for an explicit failure criterion, and that elements will not get distorted, thus, no element remeshing is required. However, chip geometry should be pre-defined; it could be revised after an iteration based on the criterion that the velocity vectors should not have components perpendicular to the tool face, in a trial-error fashion. Strain-rate and temperature effects on work material properties were avoided by simulating very low speed cutting such as 0.072 m/min [Strenkowski; 1986].

Later the FEM Eulerian model was improved by predicting and updating the chip geometry automatically. At each iteration, velocity and temperature distributions were calculated, and then the chip geometry was revised. Strain-rate insensitive aluminum alloy was used as workpiece and simulations were compared with experimental results. Later some improvements were accomplished with a fracture criterion to predict cutting in ductile regimes. By using a diffusion wear model, tool wear was estimated in orthogonal cutting [Strenkowski; 1990].

This analysis included a chip prediction capability and accounted for heat conduction between the chip and tool. The model predicted the temperature distribution in the chip, tool and work material. Comparison of the computational values with experimentally obtained values of the temperature and tool force shows good agreement. The model also predicted a built-up edge in orthogonal machining.
• Tay

Tay used FEM to predict temperatures in steady state orthogonal machining. Plastic deformation and friction were taken as heat sources in the analysis and cutting forces measured from experiments used as input to the numerical model [Tay; 1974].

• Ueda

Ueda [1992] also used a numerical approach and simulated the chip formation in the cutting process. The approach uses a Merchant's shear plane solution with a view to including the effect of varying cutting speed and depth of cut in a fairly wide range. Transient chip formation in particular is studied in detail. The influence of cutting speed and depth of cut on cutting temperature which, in turn, affects the seizure of the chip material with the tool rake face is studied. The results exhibit good agreement with experimental findings [Ueda; 1992]. The next approach was to investigate the transient chip formation in single edge oblique cutting where the cutting edge inclination angle is varied in a criterion for material separation at the cutting edge. The simulation results exhibit qualitative agreement with observations obtained using a scanning electron microscope while machining carbon steels. Likewise, there is qualitative agreement with measured cutting forces. However, predicted force magnitudes deviated from the corresponding experimental values by a large factor [Ueda; 1993].

• Usui and Shirakashi

Usui and Shirakashi [1982] made a significant contribution by modeling steady state cutting process. They established a theory to predict process parameters and performed rigid-plastic FEM simulations of cutting process in orthogonal machining. Their model was able to solve the simultaneous equations of stress equilibrium, stress-strain increment relationship, heat transfer, material flow stress and frictional stress characteristic in the chip-tool interface. The strength
and the weakness of this work was the determination of flow stress for work material at high strain rates and temperatures from impact compression tests requiring a special apparatus [Usui; 1984]. They also developed a friction model including both sticking and sliding actions. Later they developed a tool wear model [Kitagawa; 1988] [Maekawa; 1993] and estimated tool wear using FEM, and further improved their FEM approach to simulate 3-D oblique cutting [Sasahara; 1994a], chip breaking [Shinozuka; 1996], discontinuous chip formation, and machined layer residual stresses [Sasahara; 1994b]. In a recent study, tool wear for carbide tools was investigated and an equation for flank wear was developed based on the process parameters obtained from FEM modeling [Obikawa; 1996]. Using two wear constants this equation gives a relationship between flank wear, temperature, relief angle, normal stresses and cutting velocity. Despite its simplicity, this equation would predict tool wear with fair agreement with experiments.

• Ceretti and Altan

Ceretti [1996] applied DEFORM-2D™, a Lagrangian implicit large plastic deformation FEM software, to steady state of orthogonal cutting process. The strength of the FEM software is its ability to automatically re-mesh and generate a very dense grid of nodes near the tool tip so that large gradients of strain, strain-rate and temperature can be handled. However, the flow stress characteristics of work material at high strain rate and temperature has to be input to the model. Although the assumed input data for material properties and friction were quite approximate, this approach was able to simulate orthogonal machining with relatively little effort [Ceretti; 1996]. This preliminary investigation illustrated that with reliable input data on properties it is possible to estimate chip flow and cutting forces. Continuous chip formation and the effects of rake angle, cutting speed, and uncut chip thickness were studied. Furthermore, they developed a module that is being incorporated into this code to predict
segmented chip formation by deleting the elements that reach a "critical damage value" based on damage that cause fracture.

Later, a flow stress determination for mild steel (AISI 1045) was attempted from orthogonal cutting test data and the effect of cutting tool rake face geometry on chip flow was studied by using continuous chip formation simulations in orthogonal machining [Kumar; 1997].

In addition, FEM simulations were performed to estimate flank wear using wear models given in the literature. Recently, a programming tool was developed and a prediction of tool wear during cutting simulations was attempted [Shintre; 1997].

The recent progress in the numerical simulation of the cutting processes shows that the simulation can provide relatively reliable information about the orthogonal cutting process. Several approaches have been used for chip separation criteria such as those based on the strain energy [Lin; 1992], based on effective plastic strain [Strenkowski; 1985], and relative to finite element nodes [Lin; 1994]. Some other approaches to chip separation are to allow natural flow around the tool tip where material splits into two parts one flowing upward to the chip, the other flowing under the tool flank [Shaw; 1989] [Childs; 1990] [Rakotomalala; 1993] [Pantale; 1996] [Ceretti; 1996]. However, this approach requires either a Lagrangian formulation with frequent automatic remeshing [Sekhon; 1992] [Ceretti; 1996] [Marusich; 1995] or the use of an Arbitrary Lagrangian Eulerian formulation [Rakotomalala; 1993] [Pantale; 1996].

In many of these studies, the numerical codes developed were not practical and not available commercially for end users. However, they all contributed to investigate various aspects of fundamental modeling of cutting processes.

Among those research efforts, use of commercially available FEM codes was carried out successfully in cutting simulations such as NIKE-2D™ [Strenkowski;
Some of the major deficiencies of the FEM applications in machining found in literature include:

- Use of non-commercial FEM codes that make it difficult to apply by end users.
- Lack of reliable material and friction data under the processing conditions (strain, strain rate, and temperature) that must be used as input to any material flow simulation program.
- Difficulty in applying the simulations to 3-D machining.
- Need for extensive computer time and engineering effort, making the technique uneconomical to use.

3.5.2 Flow Stress Models for Workpiece Materials

The yield stress of a metal under uniaxial conditions, as a function strain, strain-rate, and temperature, is defined as the flow stress (or the effective stress). The metal starts deforming plastically when the applied stress reaches the values of the "yield stress" or "flow stress" [Altan; 1983]. The flow stress of a metal is influenced by:

- Factors unrelated to the deformation process (S) such as microstructure, chemical composition, phases, grain size, strain-history, etc.
- Factors explicitly related to the deformation process, such as temperature (T) degree of deformation, effective strain (\(\bar{\varepsilon}\)) and rate of deformation, effective strain-rate (\(\dot{\varepsilon}\)).

Thus, the flow stress (\(\bar{\sigma}\)) can be given as:

\[
\bar{\sigma} = f(T, \bar{\varepsilon}, \dot{\varepsilon}, S)
\] (3.33)
Figure 3.9: Flow stress and strain-hardening index obtained for 0.2% (solid line) and 0.38% (dashed line) carbon steels from high speed compression tests (at strain-rate=450 sec⁻¹) [Oxley; 1989]

Figure 3.10 Flow stress at strain-rate=1000 sec⁻¹ obtained for low-carbon free cutting steel from high speed compression [Maekawa; 1983]
For practical cutting speeds in machining, the average strain rate in the shear zone lies in the range of $10^3$ to $10^5$ sec$^{-1}$ or even higher [Oxley; 1989]. These values are much higher than the strain rates of $10^{-3}$ to $10^{-1}$ sec$^{-1}$ that are normally encountered in compression and tension tests. Strain rate has a significant effect on stress-strain properties and the flow stress of the workpiece material increases with an increase in strain rate.

Many researchers developed several techniques to determine the flow stress of metals at high strain-rates and temperatures [Kobayashi; 1962] [Lira; 1967] [Fenton; 1970]. The same approach was used to determine the work material flow stress parameters from a small number of machining test results using machining theory in reverse and then applying these in making a prediction for a much wider range of cutting conditions.

Some assumed that for a particular strain-rate and temperature combination the relationship between the effective flow stress ($\bar{\sigma}$) and effective strain ($\varepsilon$) for the work material considered is given by

$$\bar{\sigma} = \sigma_1 \varepsilon^n$$ \hspace{1cm} (3.34)

where the constants ($\sigma_1$) and ($n$) vary with strain rate ($\dot{\varepsilon}$) and temperature ($T$) [Usui; 1982]. Velocity-modified temperature was defined as ($T_{mod}$) which consists of strain rate and temperature as:

$$T_{mod} = T \left(1 - \nu \log \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right)$$ \hspace{1cm} (3.35)

the constants ($\nu$) and ($\dot{\varepsilon}_0$) are taken as 0.09 and 1 sec$^{-1}$, respectively. Figure 3.9 shows the ($\sigma_1$) and ($n$) for different values of velocity-modified temperature for 0.16%C mild steel presented [Oxley; 1989].

An impact compression material testing machine is used in order to obtain a flow stress model with variable temperature ($T$), and strain-rate ($\dot{\varepsilon}$) for the plastic deformation process that takes place in the shear zone in metal cutting [Usui; 1982]. This set up was used for 0.18%C mild steel. The flow stress at
temperatures 293-1000 °K and strain-rates 200-2000 sec⁻¹ was measured. An empirical expression for the flow stress model including the history effects of the temperature and strain-rate without anneal softening and age hardening effects was determined [Maekawa; 1983] and given as:

\[
\sigma = A(T) \left( \frac{\dot{\varepsilon}}{1000} \right)^{0.0195} \varepsilon^{0.21}
\]  

(3.36)

where,

\[
A(T) = 1394 \exp(-0.001187) + 339 \exp \left[ -0.0000184 \left( T - \left( 943 + 23.5 \ln \frac{\dot{\varepsilon}}{1000} \right) \right)^2 \right]
\]

In Figure 3.10, the flow stress curve at strain-rate 1000 sec⁻¹ is given by using a similar model (see Equation 3.38) for low carbon free cutting steel. The effect of strain on flow stress at high temperature weakens as can be seen from the curve.

Later, this group determined flow stress data for various materials and used them in their FEM simulations of machining. Expressions of flow stress for carbon (0.15%C) steel, low-carbon free cutting steel, titanium alloy Ti-6Al-4V, Cr-Mo steel were presented:

- For low carbon (0.15%C) steel [Usui; 1982]:

\[
\sigma = \left[ 103.0 \exp(-1.42 \times 10^{-3} T) + 34.6 \exp \left\{ -1.84 \times 10^{-3} \left( T - 7670.0 \right) \right\} \right]^{0.0228} \left( \frac{\dot{\varepsilon}}{1000} \right)^{0.021} \left\{ 0.0079 + \int \exp(0.001147 \left( \frac{\dot{\varepsilon}}{1000} \right)^{-0.0157} d\varepsilon \right\}^{0.21}
\]

(3.37)

- For low carbon free cutting steel [Childs; 1990]:

\[
\sigma = A \left( \frac{\dot{\varepsilon}}{1000} \right)^M \varepsilon^N
\]

(3.38)
where:
\[ A(T) = 910 \exp(-0.0011 T) + 120 \exp(-0.00004(T-280)^2) + 50 \exp(-0.0001(T-600)^2) \]
\[ M(T) = 0.018 + 0.000038 T, \quad N(T) = 0.16 \exp(-0.0017 T) + 0.09 \exp(-0.00003(T-370)^2) \]

- For titanium-aluminum alloy (Ti-6Al-4V) [Obikawa; 1996]:
\[ \bar{\sigma} = 2.28 \left( \frac{\dot{\varepsilon}}{1000} \right)^{0.028} \exp(-0.00155T) \]
\[ \times \left[ \left( \frac{\dot{\varepsilon}}{1000} \right)^{0.029} \exp(0.00175T) \times \int \left( \frac{\dot{\varepsilon}}{1000} \right)^{0.029} \exp(-0.00175T)d\bar{\varepsilon} + 0.12 \right]^{0.5} + 0.239 \] (3.39)

- For cold work mold steel (Cr-Mo steel) [Maekawa; 1996]:
\[ \bar{\sigma} = A \left( 10^{-3} \dot{\varepsilon} \right)^M e^{kT} \left( 10^{-3} \dot{\varepsilon} \right)^N \int_{T, \dot{\varepsilon} = \dot{\varepsilon}} e^{-kT/N} \left( 10^{-3} \dot{\varepsilon} \right)^{-m/N} d\bar{\varepsilon} \] (3.40)

where:
\[ A(T) = 1.46 \exp(-0.00137T) + 0.196 \exp(-0.000015(T-400)^2) - 0.0392 \exp(-0.01(T-100)^2) \]
\[ N(T) = 0.162 \exp(-0.0017T) + 0.092 \exp(-0.0003(T-380)^2) \]
\[ M(T) = 0.047, \quad k = 0.000065, \quad m = 0.0039 \]

Flow stress characteristics under machining conditions were also investigated using FEM simulations by Kumar [1997]. FEM simulations for machining AISI 1045 mild steel with high speed steel (HSS) tool with the cutting conditions such as 0.125 mm of uncut chip thickness, cutting speeds from 10 to 140 m/min and a constant friction factor as \( m = 0.5 \) was used in orthogonal machining. The simulation results demonstrated that for low cutting speeds up to 14 m/min, where the flow stress data is valid, the simulations gave satisfactory results. The results for the speeds higher than 16 m/min indicated that the material properties are invalid for the prediction of the cutting process at higher cutting speeds. Thus, the need for material property validation for higher strain rates and temperatures was confirmed.

The average values of strain rates and temperatures were obtained using the simulation database to collect the values of strain rates, element connectivity,
nodal coordinates and temperatures; these were then used as input in a program to get the output for average strain rate and temperature. Kumar [1997] assumed that at high cutting speeds the flow stress was insensitive to strain and could be considered as a function of strain rate and temperature only:

$$\bar{\sigma} = f(\dot{\varepsilon}, T) \quad (3.41)$$

A methodology was developed to determine flow stress, and the following flow stress equation was obtained for AISI 1045 mild steel:

$$\bar{\sigma} = 872 + 0.0190 \dot{\varepsilon} - 0.643 T \quad (3.42)$$

The interesting aspect of this work was the observation of the opposing effect of strain rate and temperature on flow stress. Similarly, it was found that temperature has a more profound effect on flow stress than strain rate. It was also suggested that in order to obtain better results from simulations, material properties should be determined at a wide range of speeds and uncut chip thickness.

3.5.3 Friction Models for Contact at Chip-Tool Interface

Friction conditions at the chip-tool interface in early FEM models of metal cutting were ignored [Klamecki; 1973] [Tay; 1974] or assumed to be constant with a coefficient of friction based on Coulomb’s law at the chip-tool interface [Carroll; 1988]. Some of later models consider the following conditions:

Usui [1982] modeled the tool-chip interaction with a frictional force as a function of normal force as boundary conditions by:

$$\tau_f = k_{\text{primary}, e} \left[ 1 - \exp \left( \mu \frac{\sigma_n}{k_{\text{primary}}} \right) \right] \quad (3.43)$$

where $\tau_f$ and $\sigma_n$ are the frictional and normal stresses, $k_{\text{primary}}$ was the shear flow stress of the chip at the primary zone and $\mu$ was the coefficient of friction obtained from experiments for different workpiece-tool material combination [Usui; 1982].
Iwata [1984] incorporated the frictional stress as a function of normal stress at the boundary conditions. An empirical relationship was introduced as:

\[ \tau_j = \left( \frac{H_v}{0.07} \right) \tanh \left( 0.07 \mu \sigma_n / H_v \right) \]  

(3.44)

where \( H_v \) is the Vickers hardness of workpiece and \( \mu \) was the coefficient of friction obtained from friction tests.

Shih [1990] used a constant coefficient of friction in the sticking region at the chip-tool interface and a coefficient linearly decaying to zero in the sliding region. The size of sticking and sliding regions were specified with the length of the regions. The values for those parameters and coefficients of friction were taken from past experimental results for AISI 1020 mild steel.

All of the above works for friction models show that currently there is no analytical model for friction conditions at the chip-tool interface, which is the primary heat source for the tool temperatures. Heat generated in the primary deformation zone is usually absorbed by the chip. Although heated chip conducts some of the heat into the tool with the thermal conductivity of the tool material. The major amount of heat still remained at the chip-tool interface. Thus, the mechanical interaction between chip and tool rake, and machined surface with tool flank creates heat sources that directly affect tool temperatures and therefore tool wear. High temperature gradients on the tool surface can be avoided with good heat conductivity of tool material. Therefore, an ideal cutting tool should have less surface friction and higher thermal conductivity without sacrificing toughness and hardness. There are constantly new coatings and cutting tool materials such as CBN, PVD and Cermets developed to achieve this goal.
CHAPTER 4
MODELING OF HIGH SPEED ORTHOGONAL CUTTING PROCESS

In modeling of orthogonal cutting process, the goal is to develop a reliable FEM simulation technique to predict process variables (temperatures, stresses, forces and chip geometry) with an acceptable accuracy. For this purpose, the software DEFORM-2D™ is used throughout this dissertation.

The flow stress data of workpiece material under machining conditions and friction characteristics at the chip-tool interface should be determined in order to simulate the cutting process.

4.1 Development of Process Model for Orthogonal Cutting

FEM software DEFORM-2D™, which is a Lagrangian implicit code designed for metal forming processes, was used to simulate the cutting process. The workpiece was modeled as rigid-plastic due to large plastic deformations. Furthermore, high mesh density was defined around the cutting edge with moving windows and excessively deformed workpiece mesh was automatically remeshed as needed during the simulation. Thus, formation of the chip is simulated step by step per tool advance. In addition, this software has the capability to model the tool as rigid or elastic so that stresses in the tool body can be predicted. The flow diagram of the process simulations is shown in Figure 4.1.

In order to produce a reliable process model for machining of P20 mold steel with an uncoated carbide (WC) cutting tool, a simulation of orthogonal cutting process was performed and methodologies for obtaining material properties and friction characteristics under machining conditions were developed.
Figure 4.1: Modeling of orthogonal cutting process using FEM based process simulation
4.1.1 Application of Friction Models at Chip-Tool Interface for Process Simulations

At first, influence of friction on the process variables and accuracy of the predictions were investigated using FEM simulations. For this purpose, the predicted values were compared with experimental data taken from a recent work [Childs; 1997]. In the simulations, the workpiece was modeled as rigid-plastic, and the tool was modeled as elastic. The cutting conditions, tool geometry, material properties of the workpiece (low-carbon free cutting steel) and the tool (uncoated tungsten carbide) used in the simulations are given in Appendix B.

Friction at the chip-tool contact was modeled using shear friction \((m)\) and friction coefficient \((\mu)\) relations for machining LCFC steel with uncoated tungsten carbide tooling. Initially, experimental data under the cutting conditions of cutting speed, \(V_c=150\ \text{m/min}\), width of cut, \(w=1\ \text{mm}\), and uncut chip thickness, \(t_u=0.1\ \text{mm}\), from orthogonal end turning experiments were used to estimate the shear friction factor. For those conditions, measured cutting force \(F_c=174\ \text{N}\), thrust force, \(F_t=83\ \text{N}\) and shear angle, \(\phi=18.8^0\) were given.

Maximum shear yield stress on the shear plane was calculated as \(\tau_r=440\ \text{MPa}\).

\[
\tau_r = \frac{F_c \cos \phi \sin \phi - F_t \sin^2 \phi}{t_u w} \quad (4.1)
\]

Childs [1997] assumed that maximum shear yield stress of workpiece material was the shear flow stress at the primary shear zone. Thus, \(k_{primary} = 440\ \text{MPa}\). On the chip-tool contact, frictional shear stress cannot be greater than shear flow stress \((\tau \leq \frac{\tau_r}{\sqrt{3}})\). By definition, the shear friction factor, \((m)\), is given as:

\[
m = \frac{\tau \sqrt{3}}{\tau_r} \quad (4.2)
\]

\[
m = \tau / k_{primary} \quad (4.3)
\]
Figure 4.2: Theoretical model for normal and shear stress distribution on cutting tool rake face [Zorev; 1963]

Figure 4.3: Measured normal and shear stress distribution on tool rake face in orthogonal cutting of LCFCS with WC tool [Child; 1997]
In addition, for the given cutting conditions, maximum frictional shear stress on the rake face was measured as $\tau_{f_{\text{max}}} = 360 \text{ MPa}$ as shown in Figure 4.3 [Childs; 1997]. Thus, from Equation 4.3 the average shear friction factor was estimated as $m = \frac{360}{440} = 0.82$.

Using the stress distributions [Childs; 1997] given in Figure 4.3, the following friction models for chip-tool contact were established in a preliminary investigation as part of this research.

- **Model I. Based on constant shear friction ($m$) in entire chip-tool interface:**

  A constant shear friction model was applied by considering $m = 0.82$ over the entire chip-tool contact area.

- **Model II. Based on constant shear factor and constant friction coefficient:**

  Two distinct friction windows were defined in the process simulation. Constant shear friction, $m = 1$ was set over the sticking region ($0 < x < l_p = 0.1 \text{ mm}$) and a constant friction coefficient, $\mu = 1$ was set over the sliding region ($l_p = 0.1 < x < l_c = 0.6 \text{ mm}$).

- **Model III. Based on variable shear friction factor only:**

  A variable friction relation [Childs; 1997] was applied by considering the shear friction factor ($m$) as a function of normal stress along the entire chip-tool contact region as follows:

  $$m = \left[ 1 - \exp \left\{ - \frac{\sigma_n}{\tau_p} \right\} \right]^{1/1.7}$$ (4.4)

  where $\tau_p$ is the maximum frictional shear stress and is equal to 360 MPa.
• **Model IV: based on variable friction coefficient only:**

The Coulomb friction law was applied by considering the friction coefficient ($\mu$) as a function of normal stress along the entire tool-chip contact area (see Figure 4.3). Friction coefficient was directly calculated from $\mu = \tau / \sigma_n$ using the stress distributions of $\tau$ and $\sigma_n$ given in Figure 4.2.

FEM simulations of orthogonal cutting using a flat face tool and zero rake angle were conducted under the given cutting conditions: cutting speed of 150 m/min and uncut chip thickness of 0.1 mm. The predicted process variables using different friction models (Models I-IV) were compared with experimental results and are summarized in Table 4.1.

Comparison with experimental results shows that forces ($F_c$) and ($F_r$) and chip-tool contact length ($l_c$) are not in very good agreement for most of the friction models while predicted temperature values yield good agreement with experimental results. It should be noted that predicted values are closest to the experimental ones when using variable friction models (III, IV). Thus, it is concluded that variable friction models should be used in order to obtain more accurate results in process simulations.

In order to predict cutting forces and tool stresses with developed process simulation, variable friction models for cutting conditions of 50, 150 and 250 m/min were generated. Measured normal and frictional stress distributions indicated in Figure 4.3 were used and the shear friction factor ($m$) and friction coefficient ($\mu$) as a function normal stress on the tool rake face were determined. With process simulations, cutting forces were predicted and compared with measured forces as shown in Figure 4.5. Other predicted process measures were presented in Table 4.3.
Figure 4.4: Variable shear friction factor and friction coefficient for orthogonal cutting of LCFCS with WC tool ($V_c=150$ m/min, $t_w=0.1$ mm)

<table>
<thead>
<tr>
<th>Experimental Results [Childs; 1997]</th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>$F_r$ (N/mm)</td>
<td>$F_i$ (N/mm)</td>
<td>Contact length, $l_c$ (mm)</td>
<td>Shear angle, $\phi$ (°)</td>
<td>$T_{max}$ (°C)</td>
<td></td>
</tr>
<tr>
<td>174</td>
<td>83</td>
<td>0.6</td>
<td>18.8</td>
<td>590</td>
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</table>

<table>
<thead>
<tr>
<th>Predicted Values from FE Simulations Using DEFORM-2D</th>
<th></th>
<th></th>
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<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Friction Model</td>
<td>$F_r$ (N/mm)</td>
<td>error %</td>
<td>$F_i$ (N/mm)</td>
<td>error %</td>
<td>Contact length, $l_c$ (mm)</td>
</tr>
<tr>
<td>I</td>
<td>270</td>
<td>55</td>
<td>108</td>
<td>30</td>
<td>0.38</td>
</tr>
<tr>
<td>II</td>
<td>283</td>
<td>62</td>
<td>126</td>
<td>52</td>
<td>0.38</td>
</tr>
<tr>
<td>III</td>
<td>265</td>
<td>52</td>
<td>101</td>
<td>21</td>
<td>0.34</td>
</tr>
<tr>
<td>IV</td>
<td>272</td>
<td>56</td>
<td>115</td>
<td>38</td>
<td>0.47</td>
</tr>
</tbody>
</table>

Table 4.1: Comparison of predicted performance measures with experimental results in orthogonal cutting of LCFCS with WC tool
Figure 4.5: Predicted and measured cutting forces at undeformed chip thickness of 0.1 mm in orthogonal cutting of LCFCS with WC tool

<table>
<thead>
<tr>
<th>Cutting speed, Vc (m/min)</th>
<th>Predicted fch (C)</th>
<th>Measured fch (C)</th>
<th>Predicted contact length, l (mm)</th>
<th>Measured contact length, l (mm)</th>
<th>Predicted Shear angle, o (°)</th>
<th>Measured Shear angle, o (°)</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>399</td>
<td>420</td>
<td>0.38</td>
<td>0.6</td>
<td>14.4</td>
<td>12.6</td>
</tr>
<tr>
<td>150</td>
<td>600</td>
<td>590</td>
<td>0.34</td>
<td>0.6</td>
<td>21.1</td>
<td>18.8</td>
</tr>
<tr>
<td>250</td>
<td>780</td>
<td>775</td>
<td>0.36</td>
<td>0.6</td>
<td>17.2</td>
<td>17.4</td>
</tr>
</tbody>
</table>

Table 4.2: Comparison of predicted process variables with experiments in orthogonal cutting of LCFCS with WC tool

This preliminary study yields that the friction model affects the predicted friction force ($F_f$), thrust force ($F_t$), and thus, cutting force ($F_c$) in process simulations using DEFORM-2D™. Furthermore, the flow stress model used may not be accurate to predict cutting forces for the high strain-rate and temperature regimes. Therefore, friction at the chip-tool interface and flow stress of the workpiece material should be determined simultaneously in order to predict process variables at high cutting speeds (>200 m/min).
4.2 A Methodology to Determine Flow Stress and Friction at High Speed Cutting Conditions

A methodology to determine flow stress of the workpiece material and to estimate friction at the chip-tool interface was developed. Briefly, the methodology indicates the following steps:

- **Step I. High speed orthogonal cutting tests:**
  Experimental data for cutting forces ($F_c$ and $F_t$), chip-tool contact length ($l_c$) and chip thickness ($t_c$) at various cutting speeds ($V_c$) and feeds ($f_w$) are obtained in high speed orthogonal cutting tests in a CNC turning center.

- **Step II. Initial flow stress data available for the workpiece material:**
  Initially, the flow stress data available for the workpiece material are used. It should be noted that those flow stress data are usually obtained at a strain rate of 0.01-1000 sec$^{-1}$. However, in high speed cutting strain rates are much higher.

- **Step III. Process simulations for orthogonal cutting:**
  FEM simulations of 2-D chip flow at related cutting conditions were initially performed with available flow stress data for the workpiece material using the mean friction coefficient calculated with the orthogonal cutting theory. Cutting force is used to determine flow stress of the workpiece material.

- **Step IV. Determine flow stress at average strain rate and temperatures:**
  The average values of strain rate and temperature in the deformation zone of the workpiece are computed and the predicted cutting force is compared with the measured force. The flow stress is modified until the prediction error is minimized.

- **Step V. Estimate friction at chip-tool interface:**
  From the flow stress determined, the average shear flow stress $k_{chip}$ is computed at the chip-tool interface. Friction coefficient is iterated until prediction error for friction force is minimized. The procedure is repeated for other cutting conditions.
4.3 High Speed Orthogonal Cutting Experiments

High speed orthogonal cutting experiments using the primary cutting edge of the flat end milling insert were prepared. The purpose of these experiments was to obtain cutting force and chip-tool contact length data for a process model of orthogonal cutting. Using these experiments, a methodology was aimed at estimating the flow stress of P20 mold steel workpiece at high cutting speeds and also the contact friction between chip and tool.

A limited number of experiments were conducted in the machining laboratories at the Institute of Advanced Manufacturing Sciences, Cincinnati, Ohio.

4.3.1 Experimental Set-Up

The experimental setup, workpiece and cutting tool geometry are shown in Figure 4.6 and Figure 4.7. In the experiments, a high speed CNC turning center was used. The turning center has good accuracy and rigidity to conduct stable orthogonal cutting experiments at high rotational speeds using P20 mold steel disks at hardnesses of 20, 30 and 40 HRC. The steel disks were clamped from both sides in two round rods. Thus, the eccentricity of the steel disk rotation was eliminated. End turning was performed for 3 mm thick disks as an orthogonal cutting process where chip flow on a plane. A limited number of experiments are conducted under stable machining conditions, i.e. minimized vibration of cutting process by designing a stiff fixture for tool holder and workpiece holder. The following are the fixed conditions in these experiments:

- Machine tool: Cincinnati Milacron CNC Turning Center
- Operation: orthogonal turning
- Workpiece material: P20 tool steel at 30 HRC
- Cutting tool material: uncoated tungsten carbide (WC)
- Rake angle: -7°
- Width of cut: 3.0 mm
Figure 4.6: A schematic for tool and workpiece geometry used in high speed orthogonal turning experiments
Figure 4.7: Photos and specifications of the equipment used in high speed orthogonal cutting experiments

<table>
<thead>
<tr>
<th>Specifications of the CNC Turning Center</th>
</tr>
</thead>
<tbody>
<tr>
<td>Brand : Cincinnati Milacron</td>
</tr>
<tr>
<td>Spindle Drive Motor : 60 KW</td>
</tr>
<tr>
<td>Maximum Spindle Speed : 2000 rpm</td>
</tr>
<tr>
<td>Maximum Workpiece Dia. : 250 mm</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Measurement Equipment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dynamometer (piezoelectric) : Kistler Type 9257 A</td>
</tr>
<tr>
<td>Amplifier : Kistler Type 5001-1KHz Filter</td>
</tr>
<tr>
<td>Data Acquisition System : NI LabView 4.1</td>
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</tbody>
</table>
4.3.2 Selection of Experimental Conditions

The experimental conditions, cutting speed ($V_c$) and feed rate ($V_f$) or related undeformed chip thickness ($t_u$), were selected by considering cutting tool manufacturer's recommended values. The recommended cutting speed range for this specific flat end mill insert (DAPRA FF1) is from 50 to 170 m/min when machining P20 mold steel. However, the cutting speeds of 200, 300 and 550 m/min were selected in order to obtain a wide spectrum of data from the experiments. At these cutting speeds, the undeformed chip thicknesses (feed) of 0.025, 0.051, 0.075 and 0.100 mm/rev were selected, Table 4.3.

Experiments were conducted with two replications at each cutting condition to eliminate experimental errors. Due to the sudden tool failure, experiments at cutting speed of 550 m/min with feed rates of 0.075 and 0.100 mm/rev were cancelled. Due to the size effect, measured cutting forces at the smallest undeformed chip thickness were ignored in flow stress and friction estimation.

<table>
<thead>
<tr>
<th>Experiment Number</th>
<th>Cutting Speed, $V_c$ (m/min)</th>
<th>Feed Rate, $V_f$ (mm/rev)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A1</td>
<td>200</td>
<td>0.025</td>
</tr>
<tr>
<td>A2</td>
<td>200</td>
<td>0.051</td>
</tr>
<tr>
<td>A3</td>
<td>200</td>
<td>0.075</td>
</tr>
<tr>
<td>A4</td>
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</tr>
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<tr>
<td>B4</td>
<td>300</td>
<td>0.100</td>
</tr>
<tr>
<td>C1</td>
<td>550</td>
<td>0.025</td>
</tr>
<tr>
<td>C2</td>
<td>550</td>
<td>0.051</td>
</tr>
</tbody>
</table>

Table 4.3: Cutting conditions used for orthogonal turning of P20 mold steel at 30 HRC using the side (primary) cutting edge of uncoated tungsten carbide (WC) flat end milling inserts
4.3.3 Chip Formation and Chip-Tool Contact

The flat end mill inserts were covered with gold spotter which does not change surface conditions significantly and can easily be removed. Nevertheless, the chip marks rubbing surface of the insert became visible. These marks were investigated and chip-tool contact length was measured using a measurement microscope (Nikon MM-11). On the other hand, the chips were collected and their shape and geometry were investigated using the same equipment.

The chip formation indicated continuous type of chips at the lower feed rates (< 0.100 mm/rev) and at the lower cutting speeds (<550 m/min). It was observed that the cutting edge was chipped off when cutting at cutting speed of 550 m/min and when undeformed chip thickness was greater than 0.075 mm/rev. A summary of chip shapes observed during the experiments is given in Table 4.3.

<table>
<thead>
<tr>
<th>Speed (m/min)</th>
<th>0.025 mm/rev</th>
<th>0.051 mm/rev</th>
<th>0.075 mm/rev</th>
<th>0.100 mm/rev</th>
</tr>
</thead>
<tbody>
<tr>
<td>200 m/min</td>
<td>Continuous</td>
<td>Continuous</td>
<td>Continuous</td>
<td>Continuous</td>
</tr>
<tr>
<td>300 m/min</td>
<td>Continuous</td>
<td>Continuous</td>
<td>Continuous</td>
<td>Saw-tooth &amp; Cont</td>
</tr>
<tr>
<td>550 m/min</td>
<td>Continuous</td>
<td>Continuous</td>
<td>Insert chipped off</td>
<td>Insert chipped off</td>
</tr>
</tbody>
</table>

Table 4.3: Summary of chip shapes observed in machining P20 mold steel with uncoated tungsten carbide (WC) flat end milling insert

The chip pictures, measured chip thickness and chip-tool contact length values are presented from Figure 4.8 through Figure 4.12. In this analysis, the chip shapes found were mostly continuous (Type II) chips except the appearance of the saw-tooth shaped chips in case B3. This observation supports the shear localization behavior of hard steels in the secondary deformation zone due to thermo-mechanical fracture at high feed and cutting speeds according to most recently developed models [Elbestawi; 1996, Zhen-Bin; 1995]. Since this type of chip formation is not observed in intermitted cutting such as flat end milling using uncoated tungsten carbide (WC) inserts, saw tooth type of chips does not occur. For this reason, shear localized chip formation is not considered in the scope of this work. However, it is discussed as future work in Chapter 8.
Workpiece: P20 mold steel
Hardness: 30 HRC
Cutting Tool: Uncoated WC
Rake angle: -7 deg.
Edge Preparation: 0.012 mm hone

Cutting Conditions:
\[ V_c = 200 \text{ m/min} \]
\[ V_f = 0.025 \text{ mm/rev} \]
Chip-tool contact length:
\[ l_c = 0.367 \text{ mm} \]
Chip Thickness:
\[ t_c = 0.119 \text{ mm} \]

Cutting Conditions:
\[ V_c = 200 \text{ m/min} \]
\[ V_f = 0.051 \text{ mm/rev} \]
Chip-tool contact length:
\[ l_c = 0.407 \text{ mm} \]
Chip Thickness:
\[ t_c = 0.142 \text{ mm} \]

Figure 4.8: Chip shapes, measured chip-tool contact length and chip thickness from experiments A1 and A2
Workpiece: P20 mold steel
Hardness: 30 HRC
Cutting Tool: Uncoated WC
Rake angle: -7 deg.
Edge Preparation: 0.012 mm hone

Cutting Conditions:
Vg = 200 m/min
Vf = 0.075 mm/rev
Chip-tool contact length: lc = 0.452 mm
Chip Thickness: tc = 0.165 mm

Cutting Conditions:
Vg = 200 m/min
Vf = 0.100 mm/rev
Chip-tool contact length: lc = 0.477 mm
Chip Thickness: tc = 0.184 mm

Figure 4.9: Chip shapes, measured chip-tool contact length and chip thickness from experiments A3 and A4
Figure 4.10: Chip shapes, measured chip-tool contact length and chip thickness from experiments B1 and B2
<table>
<thead>
<tr>
<th>Workpiece</th>
<th>P20 mold steel</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hardness</td>
<td>30 HRC</td>
</tr>
<tr>
<td>Cutting Tool</td>
<td>Uncoated WC</td>
</tr>
<tr>
<td>Rake angle</td>
<td>-7 deg.</td>
</tr>
<tr>
<td>Edge Preparation</td>
<td>0.012 mm hone</td>
</tr>
</tbody>
</table>

Cutting Conditions:

**B3**
- $V_c = 300$ m/min
- $V_f = 0.075$ mm/rev
- Chip-tool contact length: $l_c = 0.554$ mm
- Chip Thickness: $t_c = 0.137$ mm

**B4**
- $V_c = 300$ m/min
- $V_f = 0.100$ mm/rev
- Chip-tool contact length: $l_c = 0.649$ mm
- Chip Thickness: $t_c = 0.167$ mm

Figure 4.11: Chip shapes, measured chip-tool contact length and chip thickness from experiments B3 and B4
Workpiece: P20 mold steel
Hardness: 30 HRC
Cutting Tool: Uncoated WC
Rake angle: -7 deg.
Edge Preparation: 0.012 mm hone

Workpiece Information:

Cutting Conditions:
$V_c = 550 \text{ m/min}$
$V_f = 0.025 \text{ mm/rev}$
Chip-tool contact length: $l_c = 0.293 \text{ mm}$
Chip Thickness: $t_c = 0.073 \text{ mm}$

Cutting Conditions:
$V_c = 550 \text{ m/min}$
$V_f = 0.051 \text{ mm/rev}$
Chip-tool contact length: $l_c = 0.593 \text{ mm}$
Chip Thickness: $t_c = 0.122 \text{ mm}$

Figure 4.12: Chip shapes, measured chip-tool contact length and chip thickness for experiments C1 and C2
4.3.4 Cutting Force Measurements

The cutting force ($F_c$) in the cutting direction (along z axis) and thrust force ($F_t$) in the feed direction (along y axis) were measured during orthogonal turning of P20 mold steel disks. In each experiment, a fresh part of the cutting tool was used and experiments were replicated twice at each cutting condition in order to keep experimental error at a minimum.

P20 mold steel disks were prepared at three different hardnesses of 20, 30 and 40 HRC. The purpose of having experimental data for various hardness of workpiece material was to obtain cutting force data in order to analyze the influence of hardness upon cutting forces. The general trends for higher cutting forces at higher cutting speeds and lower hardness of workpiece were observed.

Furthermore, cutting forces versus undeformed chip thickness data was extrapolated at zero undeformed chip thickness and edge (plowing) forces were determined. Specific cutting forces ($K_c$, $K_t$) per unit area on the rake face were also calculated using measured forces.

In Figure 4.13, the cutting forces ($F_c$) and thrust forces ($F_t$) per unit width of cut are plotted. Although a significant increment in cutting forces with the increase of undeformed chip thickness found this behavior to be rather weak for thrust forces. This is due to high and consistent friction contact between chip and tool at the tool rake face. However, it is noted that more replication of the experiments may change the average values of cutting forces as opposed to the average of two replications.
Figure 4.13: Measured cutting ($F_c$) and thrust ($F_t$) forces per unit width and the influence of cutting speed
Figure 4.14: Measured cutting forces ($F_c$ and $F_t$) per unit width and extrapolated edge forces at zero undeformed chip thickness when machining P20 disks at hardness of 20 HRC.
Figure 4.15: Measured cutting forces ($F_c$ and $F_t$) per unit width and extrapolated edge forces at zero undeformed chip thickness when machining P20 disks at hardness of 30 HRC
Figure 4.16: Measured cutting forces ($F_c$ and $F_t$) per unit width and extrapolated edge forces at zero undeformed chip thickness when machining P20 disks at hardness of 40 HRC
4.4 Determination of Flow Stress for P20 Mold Steel and Estimation of Friction at Chip-Tool Interface

The process model for orthogonal cutting and flat end milling is highly dependent on the friction conditions at the chip-tool interface and the data for the flow stress of the workpiece material at high strain rates and temperatures. Therefore, the objective was to determine the flow stress of P20 mold steel (hardness of 30 HRC) and to estimate the friction at the chip-tool interface for high speed machining conditions (cutting speed up to 550 m/min) using the methodology given in section 4.3.

4.4.1 Initial Flow Stress Used For P-20 Mold Steel

The flow stress data for a Cr-Mo steel workpiece and uncoated tungsten carbide (WC) tool pair, given in the literature, were used. The flow stress (σ) data are generated by using impact compression tests in a Hopkinson bar apparatus for temperatures (T) up to 1200°C, strain rates (\(\dot{\varepsilon}\)) up to \(10^4\) sec\(^{-1}\) and strains (\(\varepsilon\)) up to 20 mm/mm represented with Equation (4.1) [Maekawa; 1996].

\[
\sigma = A (10^{-3} \dot{\varepsilon})^M e^{kT} (10^{-1} \dot{\varepsilon})^{KT} \left[ \int_{T, \dot{\varepsilon} = \dot{\varepsilon}} e^{-kT/N} (10^{-3} \dot{\varepsilon})^{-m/N} d\varepsilon \right]^N
\]

(4.5)

where;

\(A = 1.46 \exp(-0.0013T) + 0.196 \exp(-0.00015(T-400)^2) - 0.0392 \exp(-0.01(T-100)^2)\)

\(N = 0.162 \exp(-0.001T) + 0.092 \exp(-0.0003(T-380)^2)\)

\(M = 0.047, k = 0.000065, m = 0.0039\)

The chemical composition of P20 mold steel is similar to the Cr-Mo steel used in Maekawa's study as shown in Table 4.4. Therefore, the flow stress equation obtained for Cr-Mo steel was assumed to be also valid for P20 mold steel. The flow stress curves (see Figure 4.17) were computed using Equation 4.5 and integrated into DEFORM-2D™.
Table 4.4: Comparison of chemical comparison of Cr-Mo steel and P20 mold steel (AISI 4130)

<table>
<thead>
<tr>
<th></th>
<th>C</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Si</th>
<th>Cr</th>
<th>V</th>
<th>Mo</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cr-Mo Steel</td>
<td>0.38</td>
<td>0.85</td>
<td>0.012</td>
<td>0.025</td>
<td>0.21</td>
<td>0.96</td>
<td>0.00</td>
<td>0.16</td>
</tr>
<tr>
<td>P20 mold steel</td>
<td>0.30</td>
<td>0.85</td>
<td>0.015</td>
<td>0.003</td>
<td>0.30</td>
<td>1.10</td>
<td>0.08</td>
<td>0.55</td>
</tr>
</tbody>
</table>

Figure 4.17: Flow stress obtained for Cr-Mo steel from compression tests using Hopkinson's bar apparatus [Maekawa; 1996]
4.4.2 Initial Estimation of Friction Conditions at Chip-Tool Interface

Friction conditions at the chip-tool contact interface can be interpreted in terms of two frictional modes, which are represented by shear friction and friction coefficient relations (see Figure 4.18). Constant shear stress law \( \tau = \frac{\sigma}{\sqrt{3}} = k_{chip} \) with Von Mises plastic flow criterion and friction coefficient, \( \mu = \frac{\tau}{\sigma_p} \), need to be properly defined on the sticking region \( (0 \leq x < \lambda_p) \) and the sliding region \( (\lambda_p \leq x < \lambda_c) \), respectively.

An appropriate initial guess for the friction coefficient \( \mu_p \) was selected as the ratio of the frictional force \( (F_f) \) and normal force \( (F_n) \) acting on the tool rake face. Both forces were calculated from the measured force components at a given rake angle (Figure 4.18).

\[
F_{n, estimated} = (F_{c, measured}) \cos \alpha - (F_{t, measured}) \sin \alpha \tag{4.7}
\]

\[
F_{f, estimated} = (F_{c, measured}) \sin \alpha + (F_{t, measured}) \cos \alpha \tag{4.8}
\]

Therefore, the mean friction coefficient \( \mu_{mean} \) was calculated using measured cutting forces as follows:

\[
\mu_{mean} = \frac{F_{f, estimated}}{F_{n, estimated}} \tag{4.9}
\]

The mean friction coefficient was between 0.5 to 0.7 except for the undeformed chip thickness of 0.025 mm which was about 1.0 (see Figure 4.19). The increase in the mean friction coefficient is due to the size effect of the cutting edge. At the undeformed chip thickness of 0.025 mm, the ratio of the cutting edge radius \( (\rho=0.012 \text{ mm}) \) to the undeformed chip thickness becomes 0.5. It is well known that the higher this ratio is, the higher the specific cutting pressure, i.e. friction force.
Figure 4.18: Shear friction, friction coefficient, normal and frictional stress distributions and forces on the tool rake face.

Figure 4.19: Mean coefficient of friction determined from high speed orthogonal cutting tests.
As demonstrated in section 4.1.1, DEFORM-2D™ is responding to the friction models differently. The most appropriate way of implementing chip-tool contact friction was found to be the variable friction coefficient as a function of normal pressure at the tool rake surface \( \mu_i = f(\sigma_n) \).

Since the uniformly distributed shear frictional stress in the sticking region is known to be equal to the local shear flow stress \( \tau_f = k_{chip} \), the friction coefficient in the sticking region was defined as:

\[
\mu_i = \mu_0 = \frac{k_{chip}}{\sigma_{max}} \quad \text{at } x=0 \tag{4.10}
\]

\[
\mu_i = \frac{k_{chip}}{\sigma_n} \quad 0 < x \leq l_p \tag{4.11}
\]

In the sliding region, a constant friction coefficient \( \mu_p \) was defined as:

\[
\mu_i = \mu_p \quad l_p < x \leq l_c \tag{4.12}
\]

Thus, this friction model includes two major unknowns, the local shear flow stress of the chip \( k_{chip} \) in the sticking region, and a constant friction coefficient \( \mu_p \) in the sliding region used in the process simulations (see Figure 4.21).

From the same study [Maekawa; 1996] used for flow stress model, the measured normal and frictional stresses on the rake face of a tungsten carbide (WC) tool were given for cutting Cr-Mo steel. The friction model presented in Equations 4.10, 4.11 and 4.12 was applied and the initial friction parameters were found as shown in Figure 4.20. Thus, the initial values \( k_{chip} = 400 \text{ MPa}, \mu_p = 1.5, \) and \( \mu_0 = 0.15 \) were used in the process simulations.
Figure 4.20: Friction model and representation of variable friction coefficient

Figure 4.21: Estimation of friction from the normal and frictional stress distributions
For the FEM simulation of high speed cutting of P20 mold steel (30 HRC) with uncoated carbide (WC) tooling, the initial flow stress data and friction model, described in section 4.4.1 and 4.4.2 respectively, were used. At the each cutting conditions, the chip flow was simulated, and cutting force \( F_c \), thrust force \( F_t \), deformed chip thickness \( t_r \) and chip-tool contact length \( l_c \) were predicted. A detailed flow chart explaining this procedure is given in Figure 4.22.

4.4.3 Computation of Average Strain Rate and Temperatures in Deformation Zones

FEM simulation predicts the state variables such as strain, strain rate, temperature, Von Mises stresses and saves the values for each node and mesh element in a database file. By using the following database files, element connectivity (ELCOM), coordinates of the nodes (RZ), defined boundary conditions (BCCDEF), strain rates (STRATE) and node temperatures (NDTEMP) the deformation zones were identified using a computer program developed (see Appendix C). In the identified primary and secondary deformation zones, average strain rates \( \dot{\varepsilon}_{\text{ave}} \) and temperatures \( T_{\text{ave}} \) were calculated.

<table>
<thead>
<tr>
<th>( V ) (m/min)</th>
<th>( t_c = 0.051 \text{ mm/rev} )</th>
<th>( t_c = 0.075 \text{ mm/rev} )</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Primary zone</td>
<td>Secondary zone</td>
</tr>
<tr>
<td></td>
<td>( T_{\text{ave}} ) (°C)</td>
<td>( \dot{\varepsilon}_{\text{ave}} ) (sec(^{-1}))</td>
</tr>
<tr>
<td>200</td>
<td>535</td>
<td>60,746</td>
</tr>
<tr>
<td>300</td>
<td>544</td>
<td>120,448</td>
</tr>
<tr>
<td>550</td>
<td>560</td>
<td>157,625</td>
</tr>
</tbody>
</table>

Table 4.5: Calculated average temperatures and strain rates in the primary and the secondary deformation zones

<table>
<thead>
<tr>
<th>( V ) (m/min)</th>
<th>( t_c = 0.051 \text{ mm/rev} )</th>
<th>( t_c = 0.075 \text{ mm/rev} )</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( k_{\text{chip}} )</td>
<td>( \mu_p )</td>
</tr>
<tr>
<td>200</td>
<td>907</td>
<td>0.7</td>
</tr>
<tr>
<td>300</td>
<td>911</td>
<td>0.6</td>
</tr>
<tr>
<td>550</td>
<td>923</td>
<td>0.5</td>
</tr>
</tbody>
</table>

Table 4.6: Determined shear flow stress and estimated friction coefficients for friction at chip-tool contact interface
Obtain experimental data for orthogonal cutting, including cutting force ($F_c$), thrust force ($F_t$), tool-chip contact length ($l_c$), and chip thickness ($t_c$) at different speeds and uncut chip thickness.

Choose a set of cutting conditions used in the experiment.

**Flow stress model:**
- Flow stress data from literature at low strain and strain rates
- Flow stress is only temperature and strain rate dependent at high strain rates
- Flow stress is extrapolated for strain rates higher than those obtained from literature

**Friction model:**
- Define variable friction for the sticking and constant friction in sliding region, respectively
- Estimate initial value $k_{sw}$
- Set $\mu_s = \frac{F_t}{F_c}$ for the sliding region
- Set $\mu = \frac{k_{sw}}{\sigma_f}$ for the sticking region until $\mu = \mu_s(\sigma_f)$

Perform simulation in DEFORM-2D with the same cutting condition and with the above assumptions.

Determine the average values of strain rate and temperature in the primary deformation zone from the simulation results.

Modify flow stress for the determined strain rate and temperature regimes.

Does the simulated result of cutting force $F_c$ match the measured cutting force?

NO → YES

Do the predicted forces $F_c$ and $F_t$ match with the measured forces for the given speed and undeformed chip thickness?

NO → YES

1) Output a list of $\mu_s$ and $k_{sw}$ used in each simulated case
2) Output flow stress data, average strain rates, average temperatures for all cases
3) Identify coefficients of flow stress equation with least square method

ALL CASES DONE

Figure 4.22: Flow diagram representing the procedure for determination of workpiece flow stress behavior and friction conditions at chip-tool interface.
Figure 4.23: Predicted strain rate distribution and identified primary and secondary regions in orthogonal cutting of P20 mold steel ($V_c=550$ m/min, $t_w=0.051$ mm)

Figure 4.24: Predicted temperature distribution and the average temperatures in orthogonal cutting of P20 mold steel ($V_c=550$ m/min, $t_w=0.051$ mm)
4.4.4 Identification of Coefficients in Flow Stress Expression

An empirical expression of flow stress, developed by Maekawa [1983], was used in order to represent the flow stress data determined for high speed machining conditions (cutting speed up to 550 m/min).

$$\sigma = K\left(e^{aT} + Ae^{b(T-T_0)}\right)\left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_R}\right)^c (\bar{\varepsilon})^d$$  \hspace{1cm} (4.9)

In Equation 4.9, $\sigma$ represents flow stress, and $\dot{\varepsilon}$, $\bar{\varepsilon}$, and $T$ represent strain rate, strain, and temperature, respectively. The specified parameter ($\dot{\varepsilon}_R$) is introduced to neutralize the strain rate unit. The unknown flow stress coefficients $a$, $b$, $c$, $A$, $K$, and $T_0$ were identified by using determined flow stress data at temperatures of 20, 100, 500, 1000 °C, strains 0.01, 0.1, 1, 10 mm/mm and strain rates 0.1, 100, 1000, 10000 and 50000 sec\(^{-1}\) by applying the least squares method to minimize the sum of the square of the error (see Appendix D).

Therefore, the following values were found for the coefficients of flow stress for P20 mold steel (at 30 HRC): $K = 1339.4$, $T_0 = 400$, $A = 0.18268$, $a = -0.0013$, $b = -0.00001$, $c = 0.02964$, and $d = 0.0363$. The determined flow stress graphs for P20 mold steel were given in Figure 4.23.

By using the determined flow stress and friction data process simulations were run again and the predicted cutting forces were compared in Figure 4.24 and Figure 4.25. The prediction error in each final simulation was found to be less than 10%.
Figure 4.23: Flow stress determined for P20 mold steel (at hardness of 30 HRC) for high speed cutting conditions (cutting speed up to 550 m/min)
Figure 4.24: Comparison of predicted and measured cutting forces per unit width of cut at cutting speed of 200 m/min

Figure 4.25: Comparison of predicted and measured cutting forces per unit width of cut at cutting speed of 300 m/min
4.5 Summary

In this chapter, a methodology was developed to determine simultaneously the flow stress of the workpiece material at high deformation rates, temperatures encountered in the cutting zone, and an estimation of friction at the chip-tool contact. This methodology, also explained with a flow chart in Figure 4.22, uses the cutting and thrust force \( (F_c, m, measured, F_t, measured) \) data measured from high speed orthogonal cutting experiments as reference in order to calibrate a simulated process model. In the orthogonal cutting process simulations, by using criteria from the cutting conditions \( (V_c, t_u) \), boundaries of the primary deformation zone (Figure 4.23) were identified and the average strain rates \( (\dot{\varepsilon}_{ave}) \) and temperatures \( (T_{ave}) \) were computed. Flow stress of the workpiece material at computed average strain rates and temperatures \( [\bar{\sigma}(\dot{\varepsilon}_{ave}, T_{ave})] \) was determined in an iterative scheme until the prediction error for cutting force becomes less than 10\% \( (|\bar{F}_c| \leq 0.10) \).

In addition, the average strain rate and temperature were computed at the chip-tool contact site and the average shear friction \( (k_{chip}) \) was estimated. The unknown friction coefficient \( (\mu_p, \mu_o) \) was determined in another iterative scheme until the prediction error for thrust force becomes less than 10\% \( (|\bar{F}_t| \leq 0.10) \).

Therefore, the unknown parameters in the friction model were found by using determined flow stress data simultaneously.

This methodology was applied to obtain flow stress for P20 mold steel (at hardness of 30 HRC) and friction data in machining with uncoated tungsten carbide (WC) cutting tool. The experimental cutting conditions of 200-550 m/min cutting speed \( (V_c) \), and 0.025-0.100 mm/rev feed rate \( (V_f) \) were used. Cutting force predictions from process simulation using the determined flow stress and friction models were compared with experiments and good agreements was found (Figures 4.24 and 4.25).
CHAPTER 5

MODELING OF HIGH SPEED FLAT END MILLING PROCESS:

2-D CHIP FLOW

In die and mold machining, cutting tools for end milling tools and cutting process parameters for optimum cutting conditions are usually selected based on experimental results. Therefore, design of cutting tools and generation of machinability data for cutter performance involve extensive experimentation. Simulation of chip flow in milling processes can reduce the number of experiments and provide more detailed information not only for cutting forces but also for tool stresses and temperatures using FEM-based techniques. Thus, the objective of this work was to develop a process model for 2-D flat end milling and then methodologies for simulating 3-D practical machining operations. Currently, there is no investigation for FEM simulation of the milling process presented in the literature.

In high speed machining, indexable end milling cutters with a single insert are widely used as mentioned in Chapter 2. Various carbide grades are used for the insert material with or without coatings. An indexable flat end mill with straight cutting edge inserts from DAPRA cutting tool company were used in this dissertation research (see Figure 5.1). The insert material was uncoated tungsten carbide (WC) and the diameter ($D$) was 15.88 mm (5/8 in.). The rake ($\alpha$) and clearance ($\gamma$) angles of the insert were -11.4° and 17.9°, respectively.

For a flat end mill, the average chip thickness ($h$) is a function of tool diameter ($D$), radial depth of cut ($a_r$) and feed per tooth ($f_z$) (Figure 5.2).
Figure 5.1  Flat end mill tool and the insert geometry
5.1 Development of Process Model for Flat End Milling with Straight Cutting Edge Inserts

The chip flow in slot milling using a flat end mill with straight cutting edge inserts was modeled with a process model based on FEM simulation of chip flow. A special flat end milling operation using a single insert indexable cutting tool with a straight cutting edge (zero helix angle, $\lambda=0$) was selected to investigate 2-D chip flow in high speed flat end milling. In this operation the bottom of the insert was not engaged with the workpiece. Therefore, chip flow with plane strain deformation in $x$-$y$ plane was obtained (see Figure 5.2). Due to the slotting geometry, the axial depth of cut ($a_n$) was equal to the thickness of the workpiece (1 mm), and the radial depth of cut ($a_r$) was equal to the diameter of the insert ($D$).

In this operation, the bottom of the insert was not engaged when slotting thin plates. Only the side cutting edge of the tool was engaged with thin workpiece plates. The bottom of the insert stays in the air and does not engage with the workpiece. This eliminates the chip formation in the direction of the tool axis ($z$-axis). Therefore, 2-D FEM simulation of plane strain deformation could be performed in order to predict the chip formation and resultant process variables.

The selection of cutting speed is based on the manufacturer recommended values and the maximum spindle speed of the available machining center. The vendor-recommended values for uncoated WC (DAPRA-FF1) are 91.4 - 170 m/min (300 - 560 ft/min). At a cutting speed of 200 m/min and a maximum spindle speed of 14,000 rpm, the minimum cutting tool diameter is 4.5 mm. Therefore, spindle speed was not a limitation with the available machining center (Makino A55 Delta with 14,000 rpm maximum spindle speed).
Figure 5.2  Slot milling of a thin plate using a flat end mill with a straight cutting edge insert
In the process simulations, two different feed values (0.1 and 0.155 mm/tooth) and three different cutting speeds (100, 200 and 300 m/min) were used and the geometry of the tool and workpiece were kept the same. Two cutting speeds were chosen as the lower speed falls within the range of recommended values and the higher speed falls above. Therefore, 200 and 300 m/min was considered as high speed condition for machining P20 mold steel (at a hardness of 30 HRC) with uncoated tungsten carbide (WC) tools according to the manufacturer's recommended values. The cutting conditions used in the process simulations were listed in Table 5.1.

Thus, this chapter deals with the development and evaluation of the FEM simulation models which predict chip formation and cutting forces in a flat end milling operation where chip formation occurs in a plane (2-D chip flow).

5.2 Process Model for FEM Simulation

The FEM model for process simulations is shown in Figure 5.3. Only the tip of the tool and an arch (with a thickness of 5 mm) from the workpiece were meshed in order to have a practical number of elements for calculations. The diameter of the tool was 15.88 mm. The maximum chip thickness was 0.1 to 0.155 mm. In the actual milling operation, the tip of the tooth travels on a trochoidal path due to the feed rate and spindle rotation. However, this path can be considered circular for small values of maximum chip thickness. Thus, only rotational motion of the tool with spindle speed was considered in the process model.

Under the given cutting conditions, i.e. cases A through E, (see Table 5.1) the continuous mode of this chip formation process was simulated using the process model developed and the workpiece and friction data determined for P20 mold steel at a hardness of 30 HRC and uncoated tungsten carbide (WC) pair in Chapter 4. The material properties for P20 mold steel workpiece and WC tool used in the simulations are given in Table B.2 of Appendix B.
Figure 5.3: Geometry of the cutting tool and the workpiece: only part of the tool and the workpiece was meshed and used in the process model.

Table 5.1: Influence of high cutting speed on predicted deformed chip thickness

<table>
<thead>
<tr>
<th>Process Simulations</th>
<th>Undeformed maximum chip thickness, ( f_x ) (mm/tooth)</th>
<th>Cutting speed ( V ) (m/min)</th>
<th>Predicted deformed maximum chip thickness ( h_{max} ) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>0.100</td>
<td>100</td>
<td>0.34</td>
</tr>
<tr>
<td>B</td>
<td>0.155</td>
<td>100</td>
<td>0.47</td>
</tr>
<tr>
<td>C</td>
<td>0.100</td>
<td>200</td>
<td>0.28</td>
</tr>
<tr>
<td>D</td>
<td>0.155</td>
<td>200</td>
<td>0.37</td>
</tr>
<tr>
<td>E</td>
<td>0.100</td>
<td>300</td>
<td>0.22</td>
</tr>
</tbody>
</table>
5.3 Prediction of 2-D Chip Flow in Flat End Milling

The chip shapes were predicted as shown in Figure 5.4 through 5.7 at the rotation angles of 30°, 60°, and 72° for Case A through D and in Figure 5.8 at the rotation angles of 30°, 50°, and 61°.

The influence of the cutting conditions on the chip formation was also investigated. At higher cutting speed with constant feed, the chip curls at an earlier position of tool rotation. For the Cases C and D i.e. cutting speed of 200 m/min, almost same chip formation was observed. For the Case E, cutting speed of 300 m/min, the chip developed and curled much earlier than other cases. However, at higher feed per tooth values (maximum undeformed chip thickness), the chip curls at a later position of tool rotation.

The predicted deformed chip thickness in each cutting condition (Case A-E) with increasing cutting speed and feed was given in Table 5.1. The results of process simulation show that at higher cutting speeds, the predicted deformed chip thickness is lower. Thus, the actual chip load is decreasing with the increase in the cutting speed for a constant feed rate. This is usually referred as chip thinning and one of the major advantages of high speed milling.

It has been shown [Jawahir; 1995] that the broken chip produced in 2-D up-curl machining can be described by a typical spiral shape as predicted in Figure 5.6c, 5.7c and 5.8c. Therefore, once the chips form a spiral shape, the breaking process starts [Jawahir; 1995]. The free end of the chips contacted with the workpiece and due to the sliding friction at this interface the dynamic weight of the chip results a bending moment at the chip root. Eventually, when the ultimate strain \( \varepsilon_b \) of the chip material was reached the chip broke. Experiments revealed that for medium carbon steels \( \varepsilon_b =0.05 \) [Nakayama; 1974]. At this position, the elements representing the chips were removed from the workpiece and cutting process was continued.
Figure 5.4: Predicted chip formation in Case A ($f=0.100$ mm/tooth, $V_c=100$ m/min) when slot milling of P20 mold steel with WC tool.
Figure 5.5: Predicted chip formation in Case B ($f_t=0.155$ mm/tooth, $V_c=100$ m/min) when slot milling of P20 mold steel with WC tool
Figure 5.6: Predicted chip formation in Case C ($f=0.100$ mm/tooth, $V_c=200$ m/min) when slot milling of P20 mold steel with WC tool
Figure 5.7: Predicted chip formation in Case D ($f_z=0.155$ mm/tooth, $V_c=200$ m/min) when slot milling of P20 mold steel with WC tool.
Figure 5.8: Predicted chip formation in Case E ($f_r=0.100$ mm/tooth, $V_c=300$ m/min) when slot milling of P20 mold steel with WC tool.
5.4 Prediction of Cutting Forces in 2-D Chip Flow

5.4.1 Formulation of Cutting Forces

The mechanics of the flat end milling operation have been extensively studied both analytically and empirically in the past. Martellotti [1941] carried out a rigorous mechanical analysis of the milling operation and showed that the tip of the tooth follows a trochoidal path. However, when the feed rate \( V_f \) is very small compared to the peripheral speed of the cutter \( V_c \), the path is essentially circular (see Figure 5.9). Therefore, a circular arc is a reasonable approximation in most practical circumstances. Since then, most of the researchers have used the equation given below (5.1), which gives tangential force as proportional to the instantaneous uncut chip thickness.

\[
F_{\text{tan}} = K_{\text{mean}} a_n h(\phi)
\]  (5.1)

In Equation 5.1, the mean specific cutting force \( K_{\text{mean}} \) depends primarily on the workpiece material. The undeformed chip thickness \( h \) is a function of the rotation angle \( \phi \) and \( a_n \) is the normal depth of cut. For a cutter with a straight cutting edge, i.e. zero helix angles \( \lambda=0 \) and zero axial rake \( \alpha_a=0 \), there is no axial force component. However, a force normal to the cutting edge exists. The normal force is found proportional to tangential force with a constant \( \eta_{\text{mean}} \). In addition, for an insert with a straight edge, this normal force is radial \( F_{\text{rad}} \), (see Figure 5.99).

\[
F_{\text{rad}} = F_{\text{tan}} \eta_{\text{mean}}
\]  (5.2)

The tangential force at any instant will be proportional to the instantaneous chip thickness. The chip thickness, with the improved circular motion assumption [Speiwak; 1995], is given by:

\[
h(\phi) = f_c \sin \phi + R - \sqrt{R^2 - f_c^2 \cos^2 \phi}
\]  (5.3)

where \( f_c \) represents the feed per revolution for a single tooth cutter.
Figure 5.9: Cutting forces developed during slot milling using a flat end mill with straight cutting edge insert
Substituting Equation 5.3 in Equation 5.1 gives tangential force:

\[ F_{\text{tan}} = K_{\text{mean}} a_n \left[ f_z \sin \phi + R - \sqrt{R^2 - f_z^2 \cos^2 \phi} \right] \] (5.4)

The rubbing action due to finite cutting edge radius (\( \rho \)) of the insert and workpiece-tool interface contact generates a force that is called the “plowing” force. Thus, the tangential (\( F_{\text{tan}} \)) and radial forces (\( F_{\text{rad}} \)) can be written in a final form including plowing effects:

\[ F_{\text{tan}} = K_{\text{tan}} a_n \left[ f_z \sin \phi + R - \sqrt{R^2 - f_z^2 \cos^2 \phi} \right] \] (5.5)

\[ F_{\text{rad}} = K_{\text{rad}} a_n \left[ f_z \sin \phi + R - \sqrt{R^2 - f_z^2 \cos^2 \phi} \right] \] (5.6)

where \( K_{\text{tan}} \) and \( K_{\text{rad}} \) can be evaluated once the values of \( K_{\text{mean}} \) and \( \eta_{\text{mean}} \) are known.

Accordingly, the cutting forces in the x and y directions (see Figure 5.9) can be written as:

\[ F_x = F_{\text{tan}} \cos \phi + F_{\text{rad}} \sin \phi \] (5.7)

\[ F_y = F_{\text{tan}} \sin \phi - F_{\text{rad}} \cos \phi \] (5.8)

By substituting Equations 5.5 and 5.6 into 5.7 and 5.8, cutting forces \( F_x \) and \( F_y \) can also be written as:

\[ F_x = a_n \left[ f_z \sin \phi + R - \sqrt{R^2 - f_z^2 \cos^2 \phi} \right] \left[ K_{\text{rad}} \sin \phi + K_{\text{tan}} \cos \phi \right] \] (5.9)

\[ F_y = a_n \left[ f_z \sin \phi + R - \sqrt{R^2 - f_z^2 \cos^2 \phi} \right] \left[ K_{\text{tan}} \sin \phi - K_{\text{rad}} \cos \phi \right] \] (5.10)

The unknown cutting force coefficients (\( K_{\text{rad}} \) and \( K_{\text{tan}} \)) can be identified from experiments. Therefore, the cutting forces (\( F_x \) and \( F_y \)) in the x and y directions can be predicted for different cutting conditions using Equations 5.9 and 5.10.

The force coefficients are dependent on workpiece material hardness and processing conditions, i.e. dry cut, use of coolant, or laser assistance. Therefore, a correction factor may be applied to simulated forces for the workpiece which are harder than the samples used to identify unknown force coefficients.
Based on a recent work, the following relationships were recommended for steels in order to estimate the influence of workpiece hardness on the cutting forces [Stephenson; 1997b]. The cutting forces \((F_x(H)\) and \(F_y(H)\)) at a given hardness \((H)\), (Brinell hardness in kg/mm\(^2\)) can be estimated from cutting force coefficients \((F_x(H_{ref})\) and \(F_y(H_{ref})\)) at a reference hardness, \((H_{ref})\).

\[
F_x(H) = \left[ \frac{H}{H_{ref}} \right]^{0.5} F(H_{ref}) \quad H > H_{ref} \tag{5.11}
\]

\[
F_y(H) = \left[ \frac{H}{H_{ref}} \right]^{0.25} F(H_{ref}) \quad H \leq H_{ref} \tag{5.12}
\]

By substituting Equations 5.9 and 5.10 into 5.11 and 5.12, the following cutting force equations are developed to predict the cutting forces in flat end milling for different hardnesses of workpiece using empirically identified cutting force coefficients.

\[
F_x = \left[ \frac{H}{H_{ref}} \right]^p a_n [ f_z \sin\phi + R - \sqrt{R^2 - f_z^2 \cos^2\phi} ] [ K_{rad} \sin\phi + K_{tan} \cos\phi ] \tag{5.9}
\]

\[
F_y = \left[ \frac{H}{H_{ref}} \right]^p a_n [ f_z \sin\phi + R - \sqrt{R^2 - f_z^2 \cos^2\phi} ] [ K_{tan} \sin\phi - K_{rad} \cos\phi ] \tag{5.10}
\]

where \(p=0.5\) for \(H > H_{ref}\) and \(p=0.25\) for \(H \leq H_{ref}\).

The unknown cutting force coefficients \((K_{rad}\) and \(K_{tan}\)) for P20 mold steel at a hardness of 30 HRC were identified from slot milling experiments using flat end mills with straight cutting edge inserts. The least square method on the sum of squared differences between the measured and predicted cutting forces was applied (see Appendix D). The cutting force coefficients were identified as: \(K_{rad} = 2805\) N/mm\(^2\) and \(K_{tan} = 3795\) N/mm\(^2\) for a cutting speed of \(V_c = 300\) m/min and radial rake angle of \(-11.4^\circ\) and can also be identified for different cutting conditions. The empirically predicted cutting forces and measured cutting forces were given for \(f_z = 0.100\) and 0.155 mm/tooth with Figures 5.15 through 5.18.
5.4.2 Prediction of Cutting Forces from Process Simulations

In the past, cutting forces in flat end milling using mechanistic models were predicted including cutter run-out effects, surface generation and tool deflections [DeVor; 1980] [Kline; 1982] [Wang; 1988].

One of the advantages of the process simulations in 2-D chip flow of flat end milling is to directly predict the cutting forces ($F_x$ and $F_y$) per unit depth of cut.

After developing a reliable process model for the end milling operation, predicted cutting forces as well as measured cutting forces can be used to identify cutting force coefficients ($K_{rad}$ and $K_{sun}$) by applying the least squares method.

For the cutting conditions in Cases A through D, cutting forces ($F_x$ and $F_y$) in the $x$ and $y$ directions were predicted with the process simulations independent from experiments.

The predicted cutting forces ($F_x$ and $F_y$) from the simulations for $f_c = 0.1$ mm/tooth are given in Figures 5.10 and 5.11 and for $f_c = 0.155$ mm/tooth are given in Figures 5.12 and 5.13.

It is clear that the cutting forces ($F_x$ and $F_y$) are influenced by cutting speed. The increase in cutting speed result decrease in cutting forces.

The influence of the cutting speed is more significant on the cutting force component $F_x$ in the feed direction than the cutting force component $F_y$ in $y$ direction.
Figure 5.10: Predicted cutting force, $F_x$ ($f_z=0.100$ mm/tooth) in slot milling of P20 mold steel with WC tool.

Figure 5.11: Predicted cutting force, $F_y$ ($f_z=0.100$ mm/tooth) in slot milling of P20 mold steel with WC tool.
Figure 5.12: Predicted cutting force, $F_x$ ($f_z=0.155 \text{ mm/tooth}$) in slot milling of P20 mold steel with WC tool.

Figure 5.13: Predicted cutting force, $F_y$ ($f_z=0.155 \text{ mm/tooth}$) in slot milling of P20 mold steel with WC tool.
5.5 Measured Cutting Forces from Flat End Milling (Slotting) Experiments

An experimental set up was prepared as illustrated in Figure 5.14. A workpiece plate with a thickness of 1 mm was mounted on a three-component piezoelectric force platform (Kistler type 9257). The charge amplifier (Kistler type 5001) of the force platform was connected to a PC-pentium data acquisition system. The experiments were conducted on a 4-axis high speed horizontal milling center (Makino A-55 Delta).

The workpiece material used was P-20 mold steel (pre-hardened at 30 HRC). The cutting tool material was uncoated tungsten carbide (WC). In order to avoid run-out effects, only one cutting edge of the insert was used during machining. Therefore, the other cutting edge was ground in each insert. Fresh inserts were used in each experiment and experiments were replicated at each cutting condition to reduce experimentation error. The cutting forces were measured in the directions according to Figure 5.14.

The cutting conditions (feed per tooth and cutting speed) and related milling parameters (spindle speed and feed rate) used in the experiments are given in Table 5.2. At referred spindle speeds, the cutting vibrations were insignificant owing to the fact that a tooth passing frequency (420-630 Hz) was relatively lower than those of causing regenerative vibrations [Tlusty; 1986].

\begin{table}[h]
\centering
\begin{tabular}{|c|c|c|c|}
\hline
Maximum undeformed chip thickness $f_r$ (mm/tooth) & Cutting Speed $V$ (m/min) & Spindle speed $n$ (rpm) & Feed Rate $f_r$ (mm/min) \\
\hline
0.100 & 200 & 4010 & 401.0 \\
0.155 & 200 & 4010 & 621.6 \\
0.100 & 300 & 6014 & 601.4 \\
0.155 & 300 & 6014 & 932.2 \\
\hline
\end{tabular}
\caption{Cutting conditions and milling parameters used in slot milling experiments (using flat end mills with straight cutting edge)}
\end{table}
Figure 5.14: Experimental set up for slot milling of thin workpiece plate using flat end mill with straight cutting edge inserts
Figure 5.15: Measured cutting force $F_x$, ($f_z=0.100$ mm/tooth, $V_c=300$ m/min) in slot milling of P20 mold steel with WC tool.

Figure 5.16: Measured cutting force $F_y$, ($f_z=0.100$ mm/tooth, $V_c=300$ m/min) in slot milling of P20 mold steel with WC tool.
Figure 5.17: Measured cutting force $F_x$, ($f_z=0.155$ mm/tooth, $V_c=300$ m/min) in slot milling of P20 mold steel with WC tool.

Figure 5.18: Measured cutting force $F_y$, ($f_z=0.155$ mm/tooth, $V_c=300$ m/min) in slot milling of P20 mold steel with WC tool.
5.6 Comparison of Predicted Forces with Experiments

Predicted cutting forces $F_x$ (feed direction) and $F_y$ (perpendicular to feed direction) were compared with measured forces as shown in Figures 5.19 through 5.22.

It is seen that the cutting force increases with increasing undeformed chip thickness. The force values, predicted by FEM, showed fluctuations. This is common in FEM calculations with remeshing capability because of the discretization involved. The force is computed by integrating stresses at elements in contact with the tool at each step of tool advance. However the number of nodes at the tool-chip contact is different in each remeshing resulting variations in calculated force. Therefore, the force values are not in a smooth fashion.

The predicted cutting forces, given in Figures 5.19 through 5.22 were smoothed out in order to make the graphs easier to read (i.e. each data point corresponds to the average cutting force from 100 simulation steps). In these figures, the solid triangles represent average measured cutting forces of 20 cutting cycles (revolutions). Similarly, the dashed lines indicate the range for these average forces. For the cutting conditions used in this study, it is generally observed that the predicted forces are in reasonable agreement with the measured forces.

5.7 Comparison of Predicted Chip Shapes with Experiments

For each experimental cutting condition, the chips coming out of the operation were collected. Those chips were embedded in a bakelite material and their pictures were taken under a microscope.

The predicted chip shapes were compared with the ones obtained from experiments. As shown in Figures 5.23 and 5.24, predicted chip shapes are very similar to the experimental ones. However, chips collected from experiments are slightly longer. Thus, it is concluded that the breaking of chips occurs after this position of the cutting tool.
Figure 5.19: Comparison of cutting force $F_x$, ($f_z=0.1$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool.

Figure 5.20: Comparison of cutting force $F_y$, ($f_z=0.1$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool.
Figure 5.21: Comparison of cutting force $F_x$, ($f_z=0.155$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool

Figure 5.22: Comparison of cutting force $F_y$, ($f_z=0.155$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool
Figure 5.23: Comparison of chip shapes (a) experimental, (b) predicted ($f_i=0.1$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool.

Figure 5.24: Comparison of chip shapes (a) experimental, (b) predicted ($f_i=0.155$ mm/tooth, $V_c=200$ m/min) in slot milling of P20 mold steel with WC tool.
CHAPTER 6
MODELING OF CUTTING PROCESS IN FLAT END MILLING:
3-D CHIP FLOW

6.1 Introduction

Practical machining operations, such as turning, face and end milling, often involve cutting tools with multiple cutting edges and an included non-zero radius of the tool nose. As an example, a schematic representing the turning process using tools with a nose radius is shown in Figure 6.1.

In order to investigate the effects of tool nose radius on the cutting process, modeling of 3-D metal cutting processes with the finite element technique is possible but requires extensive computation time and capacity with existing workstations. Therefore, an alternative of process simulation using 2-D deformation models for predicting 3-D metal flow in cutting processes is considered. This chapter presents the modeling of the cutting process in flat end milling when using inserts with a nose radius, i.e. engagement with both, side and bottom cutting edges. This methodology was derived from modeling the workpiece deformation around nose radius turning tools.

The cutting forces \((F_x, F_y, F_z)\) for 3-D chip flow resulting from nose radius cutting tools were estimated through analytical models with simplified assumptions [Endres; 1994] [Strenkowski; 1996] [Stephenson; 1997b]. However, the cutting temperature and stress distributions may not be predicted without using numerical methods. Therefore, FEM-based simulation techniques are most appropriate for estimating tool temperature and stress distributions, which is the main objective of the presented research.
Figure 6.1: Schematic for turning process using tools with nose radius

Undeformed Chip Area Around Nose Radius with Axisymmetric Deformation $A_2 = 0.0816 \text{ mm}^2$

Undeformed Chip Area At Primary Cutting Edge with Plane Strain Deformation $A_1 = 0.0408 \text{ mm}^2$

Figure 6.2: Undefomed chip geometry for turning process using tools with nose radius

$C_s = 0^\circ$

Nose Radius $r = 1.6 \text{ mm}$

Depth of Cut $d = 2.4 \text{ mm}$
6.1.1 Modeling of Cutting Process in Turning with Nose Radius Tools

As an example, a simple turning insert geometry with a nose radius of 1.6 mm, zero normal rake ($\alpha_n$) and zero side cutting edge angle ($\gamma_i$) was used. A depth of cut ($d=2.4$ mm) was selected to emphasize the size effect due to an increase in specific cutting forces. The workpiece material was P20 mold steel (at 30 HRC) and the cutting tool was uncoated tungsten carbide (WC). The flow stress and friction data discussed in Chapter 4 were used. As shown in Figure 6.2, the deformation of workpiece and chip flow around the tool nose can be analyzed with two separate regions based on the cutting edge geometry. An equivalent average chip thickness of 0.0321 mm was computed using the undeformed chip geometry (Figure 6.3). An axisymmetric chip load model was then applied to the chip elements along the nose radius, whereas a plane strain model of chip load was used for the elements with straight edge cutting. Finally, the orthogonal data of simulation for each chip element was coupled and the overall process variables were predicted accordingly.

In this analysis, the undeformed chip geometry around the nose radius was represented using only one equivalent chip element for axisymmetric deformation simulations. In other words, the entire non-uniform chip thickness was approximated with one equivalent chip of uniform average thickness. The number of equivalent chip elements around the nose radius can be increased to obtain more accurate results in predictions by considering several average chip thicknesses along the chip length.

Therefore, the equivalent chip around nose radius can be simulated by assuming a round tool and cylindrical workpiece as illustrated in Figure 6.3. This axisymmetric deformation process was simulated using DEFORM-2D™. The cutting forces predicted from axisymmetric simulation ($F_{c\text{ax symmetric}}, F_{t\text{ax symmetric}}$) were used to determine the cutting forces around the nose radius ($F_{c2}, F_{t2}$) as shown in Figure 6.4
Figure 6.3: Illustration of FEM simulation of axisymmetric chip deformation using a round tool and an equivalent average chip thickness
At Section AA':
Cutting forces per 0.8mm width of cut:

\[ F_{c1} = 141.6 \text{ N} \]
\[ F_{t1x} = 40.8 \text{ N} \]
\[ F_{t1y} = 0 \text{ N/mm} \]

At Section BB':
Cutting forces for 360° piece:

\[ F_{c, \text{axisymmetric}} = 1264 \text{ N} \]
\[ F_{t, \text{axisymmetric}} = 550 \text{ N} \]

Cutting forces for 91° piece

\[ F_{c2} = 319.5 \text{ N} \]
\[ F_{t2} = 139 \text{ N} \]
\[ F_{t2x} = 97.5 \text{ N} \]
\[ F_{t2y} = 99.1 \text{ N} \]

Resultant Cutting Forces from Turning Process:

Feed force, \( F_x = 138.3 \text{ N} \)

Thrust or radial force, \( F_y = 99.1 \text{ N} \)

Main cutting force, \( F_z = 139 \text{ N} \)

Figure 6.4: Equivalent chip geometry for turning process using tools with nose radius and predicted cutting forces
The predicted tool temperature distributions are calculated for the cross sections of AA', Figure 6.4, where a plane strain deformation model was used and for BB' where an axisymmetric deformation model was used. Predicted chip flow and temperature distribution in the tool and the workpiece from FEM simulations are shown in Figure 6.5. Similarly, the predicted distributions of the maximum principle stresses on the cross sections AA' and BB' are also given with Figure 6.6.

As shown in Figure 6.5, the predicted maximum cutting temperatures were found around 800 °C from FEM simulations with 2-D plane strain and 2-D axisymmetric chip flow. Therefore, the same cutting temperature along the tool cutting edge in plane strain and axisymmetric regions was obtained. The cutting temperatures in primary cutting edge (2-D plane strain chip flow and 2-D axisymmetric chip flow) were about 700-800 °C.

Consequently, a very similar distribution for maximum principle tool stresses in 2-D plane strain simulation of chip flow and 2-D axisymmetric simulation of chip flow was determined. A maximum of 3507 MPa compressive stress at section AA' and 3387 MPa compressive stress at section BB' were found. These predicted maximum tool stresses indicate that mechanical fracture of the cutting edge is likely to occur due to the fracture toughness of uncoated tungsten carbide cutting tool that is usually less than 2000 MPa (see Table 2.4).

Although the predictions were not verified with experimental data, this procedure illustrates the applicability of using modular elements to represent undeformed chip geometry in order to predict tool forces, stresses and temperatures in turning process using tools with nose radius.
Figure 6.5: Predicted chip flow and temperatures in turning process using tools with nose radius insert

Figure 6.6: Predicted distribution of tool principle stresses in turning process with nose radius insert
6.2 Development of Process Model for 3-D Chip Flow in Flat End Milling

All practical milling operations, especially milling of sculptured surfaces, using flat, ball and bull end mills involve 3-D chip flow. A realistic model to predict cutting forces, temperatures and tool stresses is needed to improve the insert design and to optimize the cutting conditions. Therefore, the cutting mechanism in high speed, flat end milling with engagement of side (primary) cutting and bottom (secondary) cutting tool edges is investigated. This process is illustrated in Figure 6.7.

Similar to turning with a nose radius tool, the flat end milling operation with indexable inserts also involves 3-D metal flow around the insert tip, which has a non-zero radius. Some mechanistic models can predict the cutting forces generated \( F_x, F_y, F_z \) with the various depth of cuts \( (a_n \text{ and } a_r) \), feeds per tooth \( (f_z) \) and cutting speeds \( (V_c) \) after conducting a number of calibration experiments even for more complicated cutter geometries [Budak; 1993] [Fu; 1984] [Shin; 1997]. However, other process variables such as tool stresses and temperatures can not be predicted by using only mechanistic modeling.

In this study, the flat end milling operation using a single insert indexable tool with a straight cutting edge was selected to investigate the cutting process in milling as shown in Figure 6.8. Chip flow in dry milling of P20 mold steel using WC tool was simulated for selected cutting conditions (cutter diameter: \( D = 15.88 \text{ mm} \), axial depth of cut: \( a_n = 1 \text{ mm} \), and radial depth of cut: \( a_r = 15.88 \text{ mm} \)).

<table>
<thead>
<tr>
<th>Process Simulations</th>
<th>Undeformed maximum chip thickness, ( \delta ) (mm/tooth)</th>
<th>Cutting speed ( V_c ) (m/min)</th>
</tr>
</thead>
<tbody>
<tr>
<td>P</td>
<td>0.100</td>
<td>200</td>
</tr>
<tr>
<td>Q</td>
<td>0.155</td>
<td>200</td>
</tr>
</tbody>
</table>

Table 6.1: Process simulation conditions for 3-D flat end milling
Figure 6.7: Slot milling operation using a flat end mill with indexable insert: 3-D chip flow
Figure 6.8: Slot milling operation using a flat end mill with indexable insert

Figure 6.9: Slot milling operation using a flat end mill with indexable insert
6.2.1 Modular Representation of Flat End Mill Insert with Nose Radius

The cutting edges of the insert of flat end mills are exposed to contact with the workpiece unevenly. Thus, most of the chip load, depending on axial depth of cut, is taken by the side cutting edge of the insert as illustrated in Figure 6.9.

On the other hand, the cutting action on the bottom edge creates 3-D chip flow and the nose radius of the tool tip defines the surface finish on the machined part. It is important to simulate the cutting process around the nose radius tip in order to predict cutting process variables in 3-D milling. For this purpose, a modular representation was used to simulate 3-D cutting that takes place on the nose radius (secondary) cutting edge of the flat end mill insert as shown in Figure 6.10.

Figure 6.10: Modular representation of undeformed chip geometry of flat end milling insert with nose radius (r)
6.2.2 Determination of Equivalent Chip Geometry for Chip Elements

The chip geometry around the nose radius is dependent upon feed per tooth \(f_z\) and nose radius \(r\) when normal depth of cut is greater than nose radius \(a_n \geq r\). The nose of the insert engages with the chip along the nose-chip angle \(\kappa_{\text{nose,chip}}\) as shown in Figure 6.11. An equation determining this angle can be derived in Equation 6.1 by using geometrical relationships.

\[
\kappa_{\text{nose,chip}} = \pi - \cos^{-1}\left(\frac{f_z}{2r}\right) \quad (6.1)
\]

The area of the chip around the nose radius \(A_{\text{nose,chip}}\) is the difference between the area of half circle with nose radius and the area of the segment of the circle of nose radius [Spiegel; 1968].

\[
A_{\text{nose,chip}} = \frac{1}{2} \pi r^2 - A_{\text{nose,seg}} \quad (6.2)
\]

The area of the segment of the nose radius can be written as a function of feed per tooth \(f_z\) and the radius of insert nose \(r\) as:

\[
A_{\text{nose,seg}} = \frac{1}{2} r^2 \left[ 2 \cos^{-1}\left(\frac{f_z}{2r}\right) - \sin\left(2\cos^{-1}\left(\frac{f_z}{2r}\right)\right) \right] \quad (6.3)
\]

Consequently, the area of the cross section of the chip around the nose radius can be found by substituting Equation 6.3 in to Equation 6.2 and written as:

\[
A_{\text{nose,chip}} = \frac{1}{2} r^2 \left[ \pi - 2 \cos^{-1}\left(\frac{f_z}{2r}\right) + \sin\left(2\cos^{-1}\left(\frac{f_z}{2r}\right)\right) \right] \quad (6.4)
\]

An equivalent axisymmetric chip element of average uniform thickness as shown in Figure 6.12 can be represented by using this value of chip area. The equivalent chip thickness for the modular element can be found as:

\[
h_{\text{equivalent}} = r - \sqrt{r^2 - 2 \frac{A_{\text{nose,chip}}}{\kappa_{\text{nose,chip}}}} \quad (6.5)
\]

The undeformed chip geometry around nose radius can be divided smaller axisymmetric elements of constant equivalent thickness to improve the accuracy of the approximate method used here.
Figure 6.11: Geometry of the chip around nose radius

Figure 6.12: Equivalent chip model represented with axisymmetric deformation
6.3 Process Model for Axisymmetric Simulations

Using the proposed approaches discussed in the previous section, undeformed equivalent chip geometries around the insert nose for both cutting conditions were calculated and process simulations were performed in order to predict process variables such as cutting forces, temperatures, and tool stresses in 3-D flat end milling. The calculated chip geometry for a single equivalent chip element representing the axisymmetric element was given in Table 6.2. The variable chip thickness was also calculated using Equation 5.3.

<table>
<thead>
<tr>
<th>Undeformed maximum chip thickness, ( f_s ) (mm/tooth)</th>
<th>Average cutting speed at nose, ( V_{ave} ) (m/min)</th>
<th>Chip area at nose, ( A_{ave, chip} ) (mm²)</th>
<th>Angle of chip at nose, ( \phi_{nose, chip} ) (deg)</th>
<th>Undeformed equivalent chip thickness at nose, ( l_{i,undeformed} ) (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.100</td>
<td>190.3</td>
<td>0.0793</td>
<td>93.6</td>
<td>0.0637</td>
</tr>
<tr>
<td>0.155</td>
<td>190.1</td>
<td>0.1229</td>
<td>95.6</td>
<td>0.0989</td>
</tr>
</tbody>
</table>

Table 6.2: Calculated geometry of the equivalent chip around nose radius

The process model for axisymmetric model was given in Figure 6.13. The predicted chip flow in a cross section around the nose radius was given for two cutting conditions, listed in Table 6.1 in Figures 6.14 and 6.15.

The cutting forces were predicted from the axisymmetric deformation model \((F_{rad,asy}, F_{tan,asy})\) using FEM simulations. The resultant cutting forces in the \(x\)-y-\(z\) directions with rotation of the insert were calculated as:

\[
F_{x, predicted} = F_{tan,asy} \cos \phi + F_{rad,asy} \sin \phi \cos \left( \frac{\phi_{nose, chip}}{2} \right) \tag{6.6}
\]

\[
F_{y, predicted} = F_{tan,asy} \sin \phi - F_{rad,asy} \cos \phi \cos \left( \frac{\phi_{nose, chip}}{2} \right) \tag{6.7}
\]

\[
F_{z, predicted} = F_{rad,asy} \sin \left( \frac{\phi_{nose, chip}}{2} \right) \tag{6.8}
\]
Figure 6.13: Illustration of FEM simulation of axisymmetric chip deformation in flat end milling with a nose radius tool
Figure 6.14: Process simulation model with variable chip thickness from rotation angle 0° to 90° for axisymmetric deformation around nose radius ($V_{c,nose}=191$ m/min, $f_z=0.100$ mm/tooth)
Figure 6.15: Process simulation model with variable chip thickness from rotation angle 0° to 90° for axisymmetric deformation around nose radius (\(V_{c,nose}=190\, \text{m/min}, f_z=0.155\, \text{mm/tooth}\))
6.4 Flat End Milling Experiments for 3-D Chip Flow

A limited number of experiments were conducted, using the setup illustrated in Figure 5.13 of Chapter 5 to measure cutting forces under practical machining conditions. Slot milling of P20 mold steel using a flat end mill insert with a straight cutting edge was used in experiments. One side of the insert was completely ground to avoid cutter run-out. The side (primary) and bottom (secondary) cutting edges of the insert were in contact with the workpiece during the slot milling operation. Therefore, the deformation of the workpiece resulted in 3-D chip flow. Cutting forces in the $x$-$y$-$z$ direction ($F_x$, $F_y$, $F_z$) were measured and the average of two replications with minimum and maximum values were given in Figures 6.16 through 6.21.

The experimental setup and cutting tool geometry are shown in Figure 5.13. The nomenclature for this type of milling operation and chip geometry is shown in Figures 6.7, 6.8 and 6.9. The following are the fixed conditions used in these experiments:

- **Machine tool:** Makino A55 Delta
- **Operation:** slot milling with flat end mill
- **Workpiece material:** P-20 mold steel at 30 HRC
- **Cutting tool material:** uncoated tungsten carbide (WC)
- **Cutting tool diameter ($D$):** 15.88
- **Number of cutting edges ($z$):** 1 tooth
- **Normal depth of cut ($a_n$):** 1.0 and 2.0 mm
- **Cutting Speed ($V_c$):** 200 m/min
- **Spindle Speed ($n$):** 4010 rpm
- **Feed per tooth ($f_z$):** 0.100 and 0.155 mm/tooth
Figure 6.16: Measured cutting force $F_x$, ($V_c=200 \text{ m/min}, f_z=0.100 \text{ mm/tooth}, a_n=1$ mm and 2 mm) in 3-D slot milling of P20 mold steel with WC tool.

Figure 6.17: Measured cutting force $F_x$, ($V_c=200 \text{ m/min}, f_z=0.155 \text{ mm/tooth}, a_n=1$ mm and 2 mm) in 3-D slot milling of P20 mold steel with WC tool.
Figure 6.18: Measured cutting force $F_y$, ($V_c=200 \text{ m/min}, f_z=0.100 \text{ mm/tooth}, a_n=1 \text{ mm and 2 mm}$) in 3-D slot milling of P20 mold steel with WC tool.

Figure 6.19: Measured cutting force $F_y$, ($V_c=200 \text{ m/min}, f_z=0.155 \text{ mm/tooth}, a_n=1 \text{ mm and 2 mm}$) in 3-D slot milling of P20 mold steel with WC tool.
Figure 6.20: Measured cutting force $F_z$, ($V_c=200 \text{ m/min}, f_z=0.100 \text{ mm/tooth, } a_n=1 \text{ mm and 2 mm}$) in 3-D slot milling of P20 mold steel with WC tool

Figure 6.21: Measured cutting force $F_z$, ($V_c=200 \text{ m/min}, f_z=0.155 \text{ mm/tooth, } a_n=1 \text{ mm and 2 mm}$) in 3-D slot milling of P20 mold steel with WC tool
6.5 Comparison of Predicted Cutting Forces with Experiments

Predicted cutting forces from FEM simulations both in axisymmetric and plane strain deformation models were combined and compared with the experimental forces as shown in Figures 6.22, 6.23, and 6.24. The simulations for the axisymmetric model were run until the undeformed chip length corresponded to 90° of rotation angle. Therefore, the predicted forces were compared with experimental force for the first quarter of the cutter rotation.

The predicted cutting forces from FEM simulations showed fluctuations due to discretization of the chip-tool contact and the frequent remeshing of the mesh representing chip geometry.

The comparison of the forces with experiments shows that it is possible to simulate 3-D chip flow and predict cutting forces with reasonable accuracy from the presented approximated 2-D modular representation of the undeformed chip around nose radius for flat end mill inserts by using axisymmetric and plane strain models.

6.6 Summary

An approximate method was suggested to simulate the cutting process by integrating two deformation models. Basically, this method includes a plane strain process model for the side cutting edge as discussed in Chapter 5, and an axisymmetric deformation model representing 3-D chip flow around nose radius using a single equivalent chip geometry. Therefore, the 3-D cutting edge geometry was approximated using 2-D analysis. This approach is similar to that used for turning with nose radius tools. It represents, however, an improvement because the chip flow at the tip of the flat end mill insert is approximated by axisymmetric metal flow which is closer to the physical reality.
Figure 6.22: Comparison of cutting force $F_x$, ($V_c=200$ m/min, $f_z=0.100$ mm/tooth, $a_n=1$ mm) in 3-D slot milling of P20 mold steel with WC tool

Figure 6.23: Comparison of cutting force $F_y$, ($V_c=200$ m/min, $f_z=0.100$ mm/tooth, $a_n=1$ mm) in 3-D slot milling of P20 mold steel with WC tool
Figure 6.24: Comparison of cutting force $F_z$, ($V_c=200$ m/min, $f_z=0.100$ mm/tooth, $a_n=1$ mm) in 3-D slot milling of P20 mold steel with WC tool
CHAPTER 7
PREDICTION OF TOOL STRESSES AND TEMPERATURES
IN 2-D FLAT END MILLING

7.1 Introduction

The main objective of using FEM based modeling of flat end milling process is to predict tool stresses and the temperatures during in the cutting zone. The detail knowledge of stresses and temperatures can be used to develop predictive fracture or wear models to estimate tool life and premature tool failure, both in continuous and intermitted cutting operations.

In the present study, slot milling and peripheral milling operations were considered using flat end mills. Only one of the two cutting edges, or flutes, was engaged with the workpiece while the other cutting edge was exposed to the environment. The cutting process is then repeated in an intermitted scheme by two cutting edges one after another.

In peripheral milling (see Figure 2.2), the stresses developed on the surface of the cutting edge during (a) entrance (Figure 7.2 and 7.4), (b) fully developed chip load (Figure 7.3 and 7.5), and (c) exit create significantly different stress states.

At the highest chip load, as predicted in Chapter 6, the highest values of compressive tool stresses were observed. The geometry of the insert is given in Figure 7.1.
**Insert Material**

- Uncoated Tungsten Carbide (WC)

**Insert Geometry**

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diameter</td>
<td>D = 15.88 mm</td>
</tr>
<tr>
<td>Helix Angle</td>
<td>$\lambda = 0.0$ deg</td>
</tr>
<tr>
<td>Rake Angle</td>
<td>$\alpha = -11.4$ deg</td>
</tr>
<tr>
<td>Clearance Angle</td>
<td>$\gamma = 17.9$ deg</td>
</tr>
</tbody>
</table>

Edge Preparation:
- 0.012 mm hone

![Diagram of flat end mill insert](image)

Figure 7.1: The geometry of the flat end mill insert used in FEM simulations and experiments
The state of stress changes in peripheral milling depending on the milling strategy that has been used. There are two cases:

- Peripheral conventional (up) milling: This is the same as slot milling for tool entrance to workpiece, i.e. chip thickness starts from zero. In this case, chip thickness is maximum when the cutting edge exiting the workpiece. The chip flow was simulated and presented in Chapter 5 for P20 mold steel workpiece and uncoated tungsten carbide (WC) cutting tool.

- Peripheral climb (down) milling: In this case, chip thickness is maximum when the cutting edge engages the workpiece. The entrance and the fully developed chip flow were simulated for P20 workpiece and uncoated tungsten carbide (WC) cutting tool.

The effective rake angle for a large axial depth of cut \( (a_e=D/2) \) is negative. However, the effective rake angle becomes positive for a small axial depth of cut \( (a_e=D/4) \). The change in the effective rake angle affects the tool stresses. The principle tool stresses at entrance, for two different axial depths of cut \( (a_e=D/2 \text{ and } D/4 \text{ mm}) \) were predicted. The results, for the same insert geometry, are seen in Figures 7.6 and 7.7.

The most critical stress condition occurs when the cutting edge of the insert exits the workpiece at the end of the each intermittent cut [Pekelharing; 1978] [Ramaraj; 1988].

In slot milling operation, see Figure 5.3, the chip thickness or load at the entering and exiting locations is zero. Therefore, the severe stress loads at the cutting edge occurs only at the fully developed chip location. In other words, the cutting edge is not severely loaded during entrance and exit conditions.
Figure 7.2: Flat end mill insert entering workpiece when climb (down) milling ($f_c = 0.155$ mm/tooth, $V_c = 200$ m/min, $a_c = \frac{D}{2} = 7.94$ mm)

Figure 7.3: Predicted fully developed chip flow in climb (down) milling ($f_c = 0.155$ mm/tooth, $V_c = 200$ m/min, $a_c = \frac{D}{2} = 7.94$ mm)
Figure 7.4: Flat end mill insert entering workpiece when climb (down) milling
($f_z = 0.155$ mm/tooth, $V_c = 200$ m/min, $a_c = D/4 = 3.97$ mm)

Figure 7.5: Predicted fully developed chip flow in climb (down) milling
($f_z = 0.155$ mm/tooth, $V_c = 200$ m/min, $a_c = D/4 = 3.97$ mm)
Figure 7.6: Maximum principle tool stress distribution in entering with a negative effective rake angle ($a_e = D/2 = 7.94$ mm)

Figure 7.7: Maximum principle tool stress distribution in entering with a positive effective rake angle ($a_e = D/4 = 3.97$ mm)

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7.2 Prediction of Tool Stresses in 2-D Plane Strain Chip Flow

The chip flow developed at the side cutting edge of the flat end milling insert was simulated by using 2-D plane strain model as discussed in Chapter 5 and 6. The workpiece material was P20 mold steel at hardness of 30 HRC. The cutting tool material was uncoated tungsten carbide. The material properties used are given in Table B.2. The flow stress data for the workpiece and the variable friction coefficient used were also given in Figure 4.17 and Figure 4.21 respectively.

Predicted effective stress distributions in the cutting tool and the workpiece for the different cutting conditions, at 72 degrees of tool rotation, are shown in Figure 7.8 through 7.11. For this purpose, the stresses in the tool were calculated by using the predicted stresses at the interface as boundary condition for the elastically deforming tool. All stress components could be predicted. The maximum effective stress in the tool and the workpiece for the different cutting conditions, at 72 degrees of rotation are shown in Table 7.1. It was found that the effective stress in the cutting tool increases with increasing cutting speed. The maximum effective stress in the tool was located on the rake face close to the tool tip.

<table>
<thead>
<tr>
<th>( v_r ) (m/min)</th>
<th>( f ) (mm/tooth)</th>
<th>( v_r ) (m/min)</th>
<th>( f ) (mm/tooth)</th>
</tr>
</thead>
<tbody>
<tr>
<td>100</td>
<td>0.100</td>
<td>100</td>
<td>0.155</td>
</tr>
<tr>
<td>Case A</td>
<td>Case B</td>
<td>Case C</td>
<td>Case D</td>
</tr>
<tr>
<td>2430 MPa</td>
<td>3082 MPa</td>
<td>2541</td>
<td>3060 MPa</td>
</tr>
</tbody>
</table>

Table 7.1: Predicted maximum effective tool stresses (at 72° of rotation angle)

7.3 Prediction of Tool Stresses in 2-D Axisymmetric Chip Flow

The chip flow around nose radius of the insert was simulated by using 2-D axisymmetric deformation model as discussed in Chapter 6. Predicted tool stresses from axisymmetric 2-D chip flow at different cutting conditions, are shown in Figures 7.12 through 7.15. The maximum principle stresses were compressive at the tool cutting edge in all cases. Predicted maximum principle stress distributions in the tool are given in Figure 7.13 and 7.15.
Figure 7.8: Effective stress distribution in Case A
($f_c = 0.100 \text{ mm/tooth}, V_c = 100/\text{min}, \text{at } 72^\circ \text{ of rotation}$)

Figure 7.9: Effective stress distribution in Case B
($f_c = 0.100 \text{ mm/tooth}, V_c = 200/\text{min}, \text{at } 72^\circ \text{ of rotation}$)
Figure 7.10: Effective stress distribution in Case C
($f_c = 0.155$ mm/tooth, $V_c = 100$ min, at $72^\circ$ of rotation)

Figure 7.11: Effective stress distribution in Case D
($f_c = 0.155$ mm/tooth, $V_c = 200$ min, at $72^\circ$ of rotation)
Figure 7.12: Effective stress distribution in Case B from 2-D axisymmetric simulation model ($f_c = 0.100$ mm/tooth, $V_c = 200$/min)

Figure 7.13: Maximum principle stress distribution in Case B from 2-D axisymmetric simulation model ($f_c = 0.100$ mm/tooth, $V_c = 200$/min)
Effective Tool Stresses [MPa]

<p>| | |</p>
<table>
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<tbody>
<tr>
<td>A</td>
<td>0</td>
</tr>
<tr>
<td>B</td>
<td>556</td>
</tr>
<tr>
<td>C</td>
<td>1111</td>
</tr>
<tr>
<td>D</td>
<td>1667</td>
</tr>
<tr>
<td>E</td>
<td>2222</td>
</tr>
<tr>
<td>F</td>
<td>2778</td>
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<tr>
<td>G</td>
<td>3333</td>
</tr>
<tr>
<td>H</td>
<td>3889</td>
</tr>
<tr>
<td>I</td>
<td>4444</td>
</tr>
<tr>
<td>J</td>
<td>5000</td>
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</table>

Max = 5012

Figure 7.14: Effective stress distribution in Case D from 2-D axisymmetric simulation model ($f_z = 0.155$ mm/tooth, $V_c = 200$/min)

Maximum Principle Tool Stresses

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
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</thead>
<tbody>
<tr>
<td>A</td>
<td>-1500</td>
</tr>
<tr>
<td>B</td>
<td>-1311</td>
</tr>
<tr>
<td>C</td>
<td>-1122</td>
</tr>
<tr>
<td>D</td>
<td>-933</td>
</tr>
<tr>
<td>E</td>
<td>-744</td>
</tr>
<tr>
<td>F</td>
<td>-556</td>
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<td>G</td>
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<td>H</td>
<td>-178</td>
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<tr>
<td>I</td>
<td>-11</td>
</tr>
<tr>
<td>J</td>
<td>200</td>
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</table>

Min = -3238
Max = 518

Figure 7.15: Maximum principle stress distribution in Case D from 2-D axisymmetric simulation model ($f_z = 0.155$ mm/tooth, $V_c = 200$/min)
7.4 Prediction of Temperatures in 2-D Plane Strain Chip Flow

In Figures 7.16 through 7.22, the predicted temperature distributions in the tool and the workpiece for the different cutting conditions are shown at 72 degree of tool rotation. Chip shapes were found to be very similar to the experimental ones at this position. Influences of cutting speed and feed per tooth on maximum predicted temperatures of the tool and the workpiece are shown in Table 7.2. It is seen that maximum temperatures in the workpiece and the cutting tool increase with increasing cutting speed. However, the predicted temperatures in the workpiece and the cutting tool are not affected much with the increase in the feed. The maximum temperature in the tool was found on the rake face in a location close to the tool tip.

<table>
<thead>
<tr>
<th>Maximum Temperature $T_{max}$ °C</th>
<th>$v_c = 100$ m/min</th>
<th>$v_c = 100$ m/min</th>
<th>$v_c = 200$ m/min</th>
<th>$v_c = 200$ m/min</th>
<th>$v_c = 300$ m/min</th>
</tr>
</thead>
<tbody>
<tr>
<td>Workpiece</td>
<td>896</td>
<td>941</td>
<td>1054</td>
<td>1099</td>
<td>1196</td>
</tr>
<tr>
<td>Tool</td>
<td>852</td>
<td>904</td>
<td>1029</td>
<td>1057</td>
<td>1180</td>
</tr>
</tbody>
</table>

Table 7.2: Predicted maximum cutting temperature at chip-tool interface of side cutting edge using 2-D plane strain simulations

7.5 Prediction of Temperatures in 2-D Axisymmetric Chip Flow

Temperatures developed during cutting process in flat end milling around the nose radius of the insert were also predicted (see Figure 7.19 and 7.20) using FEM simulations of axisymmetric deformation for equivalent chip geometry.

<table>
<thead>
<tr>
<th>Maximum Temperature $T_{max}$ °C</th>
<th>$v_c = 200$ m/min</th>
<th>$v_c = 200$ m/min</th>
</tr>
</thead>
<tbody>
<tr>
<td>Workpiece</td>
<td>858</td>
<td>820</td>
</tr>
<tr>
<td>Tool</td>
<td>820</td>
<td>771</td>
</tr>
</tbody>
</table>

Table 7.3: Predicted maximum cutting temperature at chip-tool interface of nose radius using 2-D axisymmetric simulations
Figure 7.16: Temperature distribution of Case A in 2-D plane strain model 
($f_z = 0.100$ mm/tooth, $V_c = 100$ m/min, at $72^\circ$ of rotation)

Figure 7.17: Temperature distribution of Case B in 2-D plane strain model 
($f_z = 0.155$ mm/tooth, $V_c = 100$ m/min, at $72^\circ$ of rotation)
Figure 7.18: Temperature distribution of Case C in 2-D plane strain model ($f_z = 0.100$ mm/tooth, $V_c = 200$ m/min, at 72° of rotation)

Figure 7.19: Temperature distribution of Case D in 2-D plane strain model ($f_z = 0.155$ mm/tooth, $V_c = 200$/min, at 72° of rotation)
Figure 7.20: Temperature distribution of Case E in 2-D plane strain model

dent \( f_s = 0.100 \text{ mm/tooth}, V_c = 300 \text{ m/min}, 72^\circ \) of rotation

Figure 7.21: Temperature distribution of Case C in 2-D axisymmetric model

dent \( f_s = 0.100 \text{ mm/tooth}, V_c = 200 \text{ m/min}, 72^\circ \) of rotation
Figure 7.22: Temperature distribution of Case D in 2-D axisymmetric model (\( f_z = 0.155 \, \text{mm/tooth}, \, V_c = 200 \, \text{m/min}, \, \text{at 42° of rotation} \))
7.6 Summary

In this chapter, tool stresses and temperatures were predicted during cutting process in high speed flat end milling of P20 mold steel at a hardness of 30 HRC using uncoated carbide (WC) inserts.

3-D chip flow in flat end milling process was approximated using two modular pieces to represent deformations in the workpiece that result in spiral shaped chips.

In addition, the stress states at the tool entrance to workpiece in climb milling were predicted through 2-D plane strain FEM simulations.

Highest tool temperatures (>1000°C) were predicted at the primary (side) cutting edge of the flat end mill insert regardless of cutting conditions. Those temperatures increase wear development at the primary cutting edge. However, highest tool stresses were predicted at the secondary (around nose radius) cutting edge. Premature failure of the cutting edge is more likely at nose radius due to high stresses. This information can be further elaborated to predict tool failure and/or improve cutting tool insert design for high speed milling.

A wide range of problems associated with cutting tool insert design can also be addressed using predicted process variables such as:

- Edge preparation development
- Chip breaker design on the rake face of the inserts
- Estimation of tool wear and tool life
- Estimation of tool chipping and catastrophic failure of the cutting edges
CHAPTER 8
CONCLUSIONS AND FUTURE WORK

8.1 Summary

The rationale of the presented research can be summarized as follows:

- High speed milling technology is rather new and additional process knowledge and selection of appropriate cutting tool geometry would increase the broad applications of this technology.

- The use of high speed machining in the die and mold industry can reduce the machining time, produce improved workpiece surface quality and also provide longer tool life.

- FEM simulation of cutting process provides an analysis technique to optimize the cutting conditions and improve cutting tool geometry.

- Process simulation requires detailed understanding of the flow stress behavior of the workpiece material and friction conditions in the chip-tool contact interface.

- Process models for orthogonal cutting and basic flat end milling can be developed as a first step for predicting process variables in machining operations such as 3-D turning, flat end milling, ball end milling and drilling.
8.2 Research Objective

The main objective of this research was to analyze, through FEM simulations and experiments, the high speed flat end milling process. The specific objectives were to:

- develop a methodology to simulate flat end milling process by determining flow stress of the workpiece material and friction between chip and the cutting tool at high speed machining conditions,
- predict cutting forces developed, tool stresses and temperatures resulted from 2-D and 3-D chip flow,
- compare the predictions with experiments in order to validate the methodology and the process models for 2-D and 3-D chip flow.

8.3 Contributions

In this research, the simulation of high speed flat end milling process was studied using an FEM software, DEFORM-2D™, widely employed to simulate forging processes. First, orthogonal high speed cutting process was investigated and flow stress of the workpiece material and friction at chip-tool interface was determined using a methodology developed. Simulation of 2-D flat end milling and 3-D flat end milling by using a modular representation of the chip geometry around the cutting edge were presented.

The fundamental understanding of chip flow process in the high speed flat end milling, as indicated by the objectives of the proposed research, is expected to yield the following results of industrial relevance:

- A process model for high speed cutting, based on the commercially available FEM software DEFORM-2D™. This model can be used to; a) study the mechanics of deformation in cutting process, b) predict tool wear, c) predict the resultant cutting forces and stresses, and d) estimate the cutting
temperatures without conducting a very large number of costly and time consuming experiments.

- A methodology to determine workpiece flow stress properties and chip-tool contact friction data at high strain rates, and temperatures using high speed cutting tests and process simulations.

- A process model and a methodology to investigate flat end milling process in order to predict cutting forces, tool stresses, and cutting temperatures. This methodology can be used for designing tool and insert geometries to avoid premature failure or chipping of cutting edge, to control chip flow, chip breakage, and to improve cutting conditions.

Thus, the contributions derived from this work, can be summarized as:

- **Methodology to determine flow stress data and estimate friction at chip-tool interface.** A methodology to determine flow stress under high speed cutting conditions and accordingly estimate friction at chip-tool contact was developed. This methodology was demonstrated for high speed machining of P20 mold steel using uncoated tungsten carbide cutting tool. Currently, there is no comprehensive cutting process model presented in the literature for predictions of process variables in high speed machining.

- **Prediction of 2-D Chip Formation in Flat End Milling.** A special slot milling operation using flat end mill inserts with straight cutting edges was studied. 2-D chip flow on the side cutting edge of the insert was simulated and cutting forces were predicted. The presented process model was verified with experiments.

- **Modeling of 3-D Flat End Milling with Nose Radius Inserts.** An approximation of deformation models for the undeformed chip area around the nose radius of the cutting tools was developed using plane strain and axisymmetric workpiece deformation model in process simulations. A
procedure for segmenting undeformed chip area into several elements to compute cutting conditions was also outlined.

8.4 Suggested Future Work

Data for flow stress of the workpiece material and friction at the chip-tool interface, necessary as input into the FEM codes, are usually not available for the deformation rate and temperature regimes that exist in high speed cutting conditions. Thus, it is necessary to apply the methodology, developed here, to other cutting conditions and workpiece/tool materials than those investigated in this study.

The methodology developed in this work is also applicable to predict the tool temperature and stress distributions in other cutting processes such as turning, ball end milling and drilling with more complicated cutting tool geometries.

Using predicted process variables and structural cutting tool models, tool wear and tool failure can also be modeled. Premature failure of the cutting edge, chipping, due to crater wear or tool entry and exit conditions can be estimated for the cutting conditions and cutting tool geometries using the predicted tool stresses along with fracture mechanics.

A majority of FEM codes are limited and cannot simulate 3-D chip flow in cutting. For example, they do not allow for automatic remeshing that is essential for making accurate predictions or do not permit localized mesh density to reduce the computational effort, or can not handle large plastic deformations efficiently. As a result, prohibitively large computational times and capabilities are required. Further developments in 3-D FEM codes are necessary to simulate cutting process and 3-D chip flow accurately.
LIST OF REFERENCES


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# APPENDIX A
FINITE ELEMENT MODELS FOR CUTTING PROCESSES

<table>
<thead>
<tr>
<th>Authors</th>
<th>Formula</th>
<th>Cut Geometry</th>
<th>Material Model</th>
<th>Chip-Work Separation</th>
<th>Chip-Tool Interface</th>
<th>Workpiece</th>
<th>Prediction</th>
<th>Experimental Verification</th>
</tr>
</thead>
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<tr>
<td>Klamecki [1973]</td>
<td>Lagrangian</td>
<td>3D-orthogonal</td>
<td>elasto- plastic</td>
<td>no separation</td>
<td>no friction</td>
<td>aluminum</td>
<td>chip geometry</td>
<td>chip form</td>
</tr>
<tr>
<td>Usui and Shirakashi [1974, 1982]</td>
<td>Lagrangian</td>
<td>2D-orthogonal</td>
<td>thermo- elasto- plastic</td>
<td>assumed chip shape and shear angle</td>
<td>sliding friction</td>
<td>15%C steel</td>
<td>strain, stress, temperature distributions</td>
<td>chip flow lines, tool rake face stress and temperatures</td>
</tr>
<tr>
<td>Iwata, Osakada, Terasaka [1984]</td>
<td>Lagrangian -implicit</td>
<td>2D-orthogonal</td>
<td>rigid- plastic</td>
<td>assumed chip shape</td>
<td>no friction</td>
<td>steel</td>
<td>stresses, strains, shear angle</td>
<td>cutting forces chip length</td>
</tr>
<tr>
<td>Strenkowski and Carroll [1985]</td>
<td>Lagrangian -implicit</td>
<td>2D-orthogonal</td>
<td>thermo- elasto- plastic</td>
<td>Predefined line of separation</td>
<td>Coulomb friction law</td>
<td>aluminum 2024-T4</td>
<td>chip geometry, forces</td>
<td>none</td>
</tr>
<tr>
<td>Strenkowski and Carroll [1986, 1988]</td>
<td>Eulerian</td>
<td>2D-orthogonal</td>
<td>visco- plastic</td>
<td>based on effective strain level</td>
<td>constant coefficient of friction</td>
<td>aluminum 2024-T361</td>
<td>chip formation, chip separation</td>
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<tr>
<td>Maekawa and Childs [1989, 1990]</td>
<td>Lagrangian</td>
<td>2D-orthogonal</td>
<td>elasto- plastic</td>
<td>assumed chip shape and shear angle</td>
<td>non linear frictional boundary</td>
<td>low alloy steel 708M40</td>
<td>contact stresses tool forces temperatures tool wear</td>
<td>tool forces tool wear</td>
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<td>Strenkowski and Moon [1990]</td>
<td>Eulerian</td>
<td>2D-orthogonal</td>
<td>elasto- visco- plastic</td>
<td>based on effective strain level</td>
<td>no friction</td>
<td>aluminum 6061-T6</td>
<td>chip geometry, temperatures cutting forces</td>
<td>cutting forces, tool temperatures</td>
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<tr>
<td>Komvopoulos, Erpenbeck [1991]</td>
<td>Lagrangian -implicit</td>
<td>2D-orthogonal</td>
<td>elasto- plastic</td>
<td>Superimposed nodes at parting line based on effective strain</td>
<td>constant coefficient of friction</td>
<td>steel AISI 4340</td>
<td>chip geometry cutting forces contact stresses built-up-edge</td>
<td>chip geometry cutting forces</td>
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<tr>
<td>Elderidge, Oscar, Lu [1991]</td>
<td>not mentioned</td>
<td>2D-orthogonal</td>
<td>visco- plastic</td>
<td>assumed chip shape and shear angle</td>
<td>Experimental frictional stress</td>
<td>mild steel</td>
<td>cutting forces shear angle contact stresses tool temperatures</td>
<td>none</td>
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<tr>
<td>Shih, Channasekar, Yang, [1990]</td>
<td>not mentioned</td>
<td>2D-orthogonal</td>
<td>elasto- visco- plastic</td>
<td>Separation of nodes ahead of tool</td>
<td>Coulomb friction in both sticking-sliding region</td>
<td>low carbon steel AISI 1020</td>
<td>cutting forces effective stresses, temperatures residual stresses</td>
<td>cutting forces</td>
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<tr>
<td>Sekhon and Chenot [1992]</td>
<td>Lagrangian -implicit</td>
<td>2D-orthogonal</td>
<td>elasto- visco- plastic</td>
<td>Automatic remeshing for separation</td>
<td>constant friction factor</td>
<td>not mentioned</td>
<td>chip geometry cutting forces stress, temperature</td>
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Table A.1: Research Efforts in Finite Element Modeling of Cutting Process (to be continued)
<table>
<thead>
<tr>
<th>Authors</th>
<th>Formula</th>
<th>Cut Geometry</th>
<th>Material Model</th>
<th>Chip-Work Interface</th>
<th>Chip-Tool Interface</th>
<th>Workpiece</th>
<th>Prediction</th>
<th>Experimental Verification</th>
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<tbody>
<tr>
<td>Zhang and Bagchi [1992]</td>
<td>Lagrangian-implicit ABACUS-2D®</td>
<td>2D-orthogonal</td>
<td>elasto-plastic</td>
<td>Predefined link elements</td>
<td>Friction in sticking-sliding region</td>
<td>brass 70%Cu-30%Zn</td>
<td>copper</td>
<td>chip geometry strain and stress distribution</td>
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<tr>
<td>Lin and Lin [1992] Lin and Pan [1994]</td>
<td>Updated Lagrangian Implicit</td>
<td>2D-orthogonal</td>
<td>thermo-elasto-plastic</td>
<td>strain energy density</td>
<td>constant coefficient of friction</td>
<td>mild steel</td>
<td></td>
<td>cutting forces shear angle stresses and strains</td>
</tr>
<tr>
<td>Ueda and Manabe [1992, 1993]</td>
<td>Lagrangian</td>
<td>3D-orthogonal single-edge</td>
<td>rigid-plastic</td>
<td>Separation of nodes ahead of tool</td>
<td>non-linear frictional boundary</td>
<td>mild steel 45%Cu</td>
<td></td>
<td>chip geometry cutting forces</td>
</tr>
<tr>
<td>Rakotomalala, Joyol, Touratier [1993]</td>
<td>Arbitrary Lagrangian Eulerian-explicit RADIOSS™</td>
<td>2D-orthogonal</td>
<td>thermo-visco-plastic</td>
<td>Continuous update of free surface location</td>
<td>Coulomb friction law sticking-sliding</td>
<td>42CD4 steel</td>
<td></td>
<td>chip geometry cutting forces</td>
</tr>
<tr>
<td>Sasahara, Obikawa, Shirakashi [1994a, 1997]</td>
<td>Updated Lagrangian</td>
<td>2D-orthogonal</td>
<td>thermo-elasto-plastic</td>
<td>Separation of nodes based on proximity to cutting edge</td>
<td>non-linear frictional boundary</td>
<td>brass 70%Cu-30%Zn</td>
<td></td>
<td>chip geometry cutting forces frictional, normal and residual stresses temperatures</td>
</tr>
<tr>
<td>Sasahara, Obikawa, Shirakashi [1994b]</td>
<td>Updated Lagrangian</td>
<td>3D-orthogonal two-edge (incipient stage)</td>
<td>elasto-plastic</td>
<td>Separation of nodes based on proximity to cutting edge</td>
<td>non-linear frictional boundary</td>
<td>plasticine</td>
<td></td>
<td>chip geometry cutting forces normal stresses equivalent strain</td>
</tr>
<tr>
<td>Marusich and Ortiz [1995]</td>
<td>Lagrangian-implicit MACH-2D™(1996)</td>
<td>2D-orthogonal</td>
<td>thermo-mechanic</td>
<td>Adaptive meshing for separation</td>
<td>constant coefficient of friction</td>
<td>AISI 4340 titanium aluminum</td>
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<tr>
<td>Watanabe and Umezu [1995]</td>
<td>Explicit LS-DYNA-3D™</td>
<td>2D-orthogonal 3D-orthogonal</td>
<td>elasto-plastic</td>
<td>Predefined slide interface (tie-break)</td>
<td>Coulomb friction</td>
<td>type 3</td>
<td></td>
<td>chip geometry cutting forces</td>
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<tr>
<td>Kim and Sin [1996]</td>
<td>Eulerian</td>
<td>2D-orthogonal</td>
<td>thermo-visco-plastic</td>
<td>Assumed chip shape and modification based on free surface contact</td>
<td>non-linear frictional boundary</td>
<td>low carbon steel SM20C</td>
<td></td>
<td>cutting forces workpiece stress, strain distributions tool temperatures</td>
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<tr>
<td>Maskawa, Ohhata, Kitagawa, Childs [1996]</td>
<td>Eulerian (iterative convergence method)</td>
<td>3D-orthogonal two-edge</td>
<td>rigid-plastic</td>
<td>Assumed chip shape</td>
<td>non-linear frictional boundary</td>
<td>Cr-Mo steel Mn-B steel</td>
<td></td>
<td>chip geometry normal stress temperature wear rate</td>
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</tbody>
</table>

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Table A.1 (continued)

<table>
<thead>
<tr>
<th>Authors</th>
<th>Formula</th>
<th>Cut Geometry</th>
<th>Material Model</th>
<th>Chip-Work Separation</th>
<th>Chip-Tool Interface</th>
<th>Workpiece</th>
<th>Prediction</th>
<th>Experimental Verification</th>
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<tr>
<td>Obikawa and Usui [1996]</td>
<td>Updated Lagrangian</td>
<td>2D-orthogonal</td>
<td>thermo-elastic-plastic fracture for crack propagation</td>
<td>Separation of nodes based on proximity to cutting edge</td>
<td>non-linear frictional boundary</td>
<td>Titanium alloy Ti-6Al-4V</td>
<td>serrated chip formation cutting forces equivalent stress</td>
<td>none</td>
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<tr>
<td>Shinozuka, Obikawa, Shirakashi [1996]</td>
<td>Updated Lagrangian</td>
<td>2D-orthogonal</td>
<td>thermo-elasto-plastic</td>
<td>with a fracture criterion for crack propagation</td>
<td>non-linear frictional boundary</td>
<td>Low carbon steel 15%C</td>
<td>chip geometry cutting forces stresses chip breaking</td>
<td>chip shapes for breakability</td>
</tr>
<tr>
<td>Cerotti, Fallbohmer, Wu, Altan [1996]</td>
<td>Lagrangian-implicit DEFORM-2D™</td>
<td>2D-orthogonal</td>
<td>rigid-plastic with fracture criterion</td>
<td>Automatic remeshing for separation Element deletion for fracture</td>
<td>Shear friction factor</td>
<td>Mild steel AISI 1045</td>
<td>Chip morphology cutting forces stresses temperatures tool wear</td>
<td>Cutting forces</td>
</tr>
<tr>
<td>Pantale, Rakotomalala, Touratier, Hakem [1996]</td>
<td>Arbitrary Lagrangian Eulerian explicit</td>
<td>3D-orthogonal single edge</td>
<td>thermo-visco-plastic</td>
<td>Continuous update of free surface location</td>
<td>Coulomb law with stick-slip regions</td>
<td>42CD4 steel</td>
<td>Chip geometry Von-Mises stresses temperatures cutting forces</td>
<td>Chip contact length comparison between predicted temperatures and measured tool wear</td>
</tr>
<tr>
<td>Fourment, Oudin, Massoni, Bittes, Le Calvez [1997]</td>
<td>Lagrangian implicit FORGE2®</td>
<td>2D-orthogonal</td>
<td>elasto-visco-plastic</td>
<td>Automatic remeshing for separation</td>
<td>Norton-Hoff law or Coulomb friction law</td>
<td>42CD4 steel</td>
<td>Chip geometry temperatures tool wear</td>
<td>None</td>
</tr>
<tr>
<td>Leopoldi, Semmler [1997]</td>
<td>Lagrangian Eulerian coupled FEDAP™</td>
<td>2D-orthogonal 3D-orthogonal 3D-oblique single-edge</td>
<td>thermo-visco-plastic</td>
<td>not mentioned</td>
<td>sticking and sliding</td>
<td>Mild steel C45</td>
<td>Chip geometry</td>
<td>None</td>
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### Table B.1: List of cutting conditions, material properties for LCFCS workpiece and uncoated P20 grade carbide tool used in the process simulations

<table>
<thead>
<tr>
<th>Cutting Speed, ( V_c ), (m/min)</th>
<th>50, 150, 250</th>
</tr>
</thead>
<tbody>
<tr>
<td>Feed, Uncut/Undefomed Chip Thickness, ( t_r ), (mm)</td>
<td>0.100</td>
</tr>
<tr>
<td>Width of Cut, (mm)</td>
<td>1</td>
</tr>
<tr>
<td><strong>Emissivity (-)</strong></td>
<td>0.295 (100 °C)</td>
</tr>
<tr>
<td></td>
<td>0.335 (500 °C)</td>
</tr>
<tr>
<td></td>
<td>0.635 (1000 °C)</td>
</tr>
<tr>
<td><strong>Coefficient of Thermal Expansion (10^6/°C)</strong></td>
<td>13.05 (20 °C)</td>
</tr>
<tr>
<td></td>
<td>13.40 (300 °C)</td>
</tr>
<tr>
<td><strong>Heat Capacity (N/mm^2/°C)</strong></td>
<td>3.5325 + 0.002983*T °C</td>
</tr>
<tr>
<td><strong>Specific Heat (J/kg/°C)</strong></td>
<td>450 + 0.38*T °C</td>
</tr>
<tr>
<td><strong>Thermal Conductivity (W/m°C)</strong></td>
<td>62 - 0.044*T °C</td>
</tr>
<tr>
<td><strong>Poisson’s ratio (-)</strong></td>
<td>0.3</td>
</tr>
<tr>
<td><strong>Young’s modulus (GPa)</strong></td>
<td>200</td>
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</table>

<table>
<thead>
<tr>
<th><strong>Geometry and Properties of Cutting Tool (Tungsten Carbide)</strong></th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Tool Tip Radius, ( r_p ), (mm)</strong></td>
</tr>
<tr>
<td><strong>Rake Angle, ( \alpha ), (deg)</strong></td>
</tr>
<tr>
<td><strong>Clearance Angle, ( \gamma ), (deg)</strong></td>
</tr>
<tr>
<td><strong>Density (g/cm^3)</strong></td>
</tr>
<tr>
<td><strong>Emissivity</strong></td>
</tr>
<tr>
<td><strong>Coefficient of Thermal Expansion (10^6/°C)</strong></td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td><strong>Heat Capacity (N/mm^2/°C)</strong></td>
</tr>
<tr>
<td><strong>Specific Heat (J/kg/°C)</strong></td>
</tr>
<tr>
<td><strong>Thermal Conductivity (W/m°C)</strong></td>
</tr>
<tr>
<td><strong>Poisson’s Ratio (-)</strong></td>
</tr>
<tr>
<td><strong>Young’s Modulus (GPa)</strong></td>
</tr>
</tbody>
</table>
### Orthogonal Cutting Conditions

<table>
<thead>
<tr>
<th>Cutting Speed, $V_c$ (m/min)</th>
<th>200, 300, 550</th>
</tr>
</thead>
<tbody>
<tr>
<td>Feed, Uncut/Undeformed Chip Thickness, $t_u$ (mm)</td>
<td>0.025, 0.051, 0.075, 0.100</td>
</tr>
<tr>
<td>Width of Cut (mm)</td>
<td>1</td>
</tr>
</tbody>
</table>

#### Properties of Workpiece (P20 Mold Steel)

<table>
<thead>
<tr>
<th>Emissivity (-)</th>
<th>0.60 (100 °C)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0.65 (500 °C)</td>
</tr>
<tr>
<td></td>
<td>0.75 (1000 °C)</td>
</tr>
<tr>
<td>Coefficient of Thermal Expansion ($10^{-6}/°C$)</td>
<td>1.3 (425 °C)</td>
</tr>
<tr>
<td></td>
<td>1.4 (650 °C)</td>
</tr>
<tr>
<td>Heat Capacity (N/mm²/°C)</td>
<td>3.68</td>
</tr>
<tr>
<td>Specific Heat (J/kg/°C)</td>
<td>(470)</td>
</tr>
<tr>
<td>Thermal Conductivity (W/m°C)</td>
<td>51.5</td>
</tr>
<tr>
<td>Poisson’s ratio (-)</td>
<td>0.3</td>
</tr>
<tr>
<td>Young’s modulus (GPa)</td>
<td>260</td>
</tr>
</tbody>
</table>

#### Geometry and Properties of Cutting Tool (Tungsten Carbide F Grade)

| Tool Tip Radius, $p_t$ (mm) | 0.012 |
| Rake Angle, $\alpha$ (deg) | -11.4 |
| Clearance Angle, $\gamma$ (deg) | 19.7 |
| Emissivity (-) | 0.5 |
| Coefficient of Thermal Expansion ($10^{-6}/°C$) | 5.2 |
| Heat Capacity (N/mm²/°C) | 4.6 |
| Specific Heat (J/kg/°C) | 343.28 |
| Thermal Conductivity (W/m°C) | 120 |
| Poisson’s Ratio (-) | 0.22 |
| Young’s Modulus (GPa) | 552 (20 °C) 620 (100 °C) |

Table B.2: List of cutting conditions, material properties for P20 mold steel workpiece and uncoated M20 (F) grade carbide tool used in the process simulations
APPENDIX C
COMPUTATION OF AVERAGE STRAIN, STRAIN RATE AND TEMPERATURE IN PRIMARY AND SECONDARY DEFORMATION ZONE

• C - PROGRAM:

#include <stdio.h>
#include <math.h>
declare ELMARR 1200 /* Array Size of Element Matrices*/
declare NODEARR 2000 /* Array Size of Node Matrices*/
declare ELMTSIZ 50 /* Array Size of Contacting-Element Matrices*/
declare NODESDZ 100 /* Array Size of Contacting-Node Matrices*/
declare TIP 2 /* NO. OF ELEMENTS FOR AROUND TOOL TIP*/
declare SEP 1 /* NO. OF ELEMENTS FOR AT SEPERATION POINT */
struct pos
{
    int no;
    float xp;
    float yp;
    float t;
};
main ()
{
    int ELM, NODE;
    int i, j, a, r, l, t;
    int PZ_ne, SDZ_ne, CT, Elmt;
    float SPEED, DOC, STRATIO, STRATE_BOUND;
    float W[NODEARR][5];
    float ELM_area, ELMP_stn, ELMP_tmp, TOTA_pr, TOTA_sd, TOTP_stn,
    TOTP_tmp;
    float AVESTR_pr, AVETMP_pr, AVESTR_sd, AVETMP_sd;
    float X[ELMARR][2], Max[ELMARR][2], SDZ_stn[ELMTSIZ];
    float Strn_max, Elm_max;
    int ContactList(int mn, int *SDZ_E, int Elmt);
    FILE *fin1;
    FILE *fin2;
    FILE *fin3;
    FILE *fin4;
    FILE *fin5;
    FILE *fout;
    struct pos N[ELMARR][4], NS[ELMTSIZ][4];

    /* PART 0: DATA INPUT PREPARATION FROM THE USER */
    printf("INPUT THE NUMBER OF ELEMENT IN THE WORKPIECE: ");
    scanf("%i", &ELM);
    printf("INPUT THE NUMBER OF NODES IN THE WORKPIECE: ");
    scanf("%i", &NODE);
printf("INPUT THE CUTTING SPEED OF TOOL (mm/sec): ");
scanf("%f", &SPEED);
printf("INPUT THE UNCUT CHIP THICKNESS OF TOOL (DOC) (mm): ");
scanf("%f", &DOC);
printf("INPUT THE RATIO OF THE MINIMUM STRAIN RATE OF THE PRIMARY ZONE TO 'AVESTR' : ");
scanf("%f", &STRATIO);

STRATE_BOUND = STRATIO * (SPEED/DOC);

PART 1: CALCULATION OF AVE STRAIN RATE & TEMP FOR THE PRIMARY ZONE /

/* INPUT STRATE FILE */
fin = fopen("STRATE.DAT", "r");
fout = fopen("OUTPUT.DAT", "w");
for (i=0; i<ELM; i++)
    fscanf(fin, "%f %f", &X[i][0], &X[i][1]);
fclose(fin);

/* DETERMINING THE ELEMENT HAVING THE HIGHEST STRAIN RATE */
Elm_max =X[0][0];
Strn_max=X[0][1];
for (i=0; i<ELM; i++)
    if (X[i][1] > Strn_max)
    {
        Strn_max=X[i][1];
        Elm_max =X[i][0];
    }

printf("\n\nThe value of Max Strain Rate is %f (Element No=%f)\n",
        Strn_max, Elm_max);
fprintf(fout, "The value of Max Strain Rate is %f (Element No=%f)\n",
        Strn_max, Elm_max);

/* COMPARING THE VALUES OF THE STRAIN RATE WITH OTHER ELEMENTS AND LISTING ELEMENTS HAVING STRAIN RATE WITHIN THE SPECIFIED RANGE OF THE HIGHEST STRAIN RATE ELEMENT */
t=0;
for (i=0; i<ELM; i++)
    if (X[i][1] > STRATE_BOUND)
    {
        Max[t][1]=X[i][1];
        Max[t][0]=X[i][0];
        fprintf(fout, "The STRATE value for Element %f is %f\n",
            Max[t][0], Max[t][1]);
        t=t+1;
    }
PZ_ne=t;

/* DETERMINING THE NODES OF THE ELEMENTS WHICH ARE IN THE DEFORMATION ZONE */
for (t=0; t<PZ_ne; t++)
for (a=0; a<ELM; a++)
    if (Max[t][0] == E[a][0])
    {
        N[t][0].no=E[a][1];
        N[t][1].no=E[a][2];
        N[t][2].no=E[a][3];
        N[t][3].no=E[a][4];
    }

/* INPUT NODAL COORDINATES AND TEMPERATURE DATA */
fin3 = fopen("RZ.DAT", "r");
for (r=0; r<NODE; r++)
    fscanf(fin3, "%f %f %f", &W[r][0], &W[r][1], &W[r][2]);
fclose(fin3);
fin4 = fopen("NDTMP.DAT", "r");
for (r=0; r<NODE; r++)
    fscanf(fin4, "%f %f", &W[r][3], &W[r][4]);
fclose(fin4);

/* DETERMINE THE COORDINATES OF FOUR NODES ASSOCIATED WITH EACH
ELEMENT IN PRIMARY ZONE */
for (i=0; i<PZ_ne; i++)
    for (j=0; j<4; j++)
        for (t=0; t<NODE; t++)
            if (N[i][j].no==W[t][0])
                N[i][j].xp=W[t][1];
                N[i][j].yp=W[t][2];
                N[i][j].t =W[t][4];

/* DETERMINE THE AREA OF EACH ELEMENT AND CALCULATE TOTAL AREA OF
SECONDARY ZONE*/
/* DETERMINE THE PRODUCT OF STRAIN RATE x AREA FOR EACH ELEMENT AND
TOTAL PRODUCT*/
/* DETERMINE THE PRODUCT OF TEMPERATURE x AREA FOR EACH ELEMENT AND
TOTAL PRODUCT*/
TOTA_pr=0;
TOTP_stn=0;
TOTP_tmp=G;
for (t=0; t<PZ_ne; t++)
{
    ELM_area = fabs(N[t][0].xp*(N[t][1].yp-N[t][2].yp)-
        N[t][0].yp*N[t][1].xp-N[t][2].xp)*N[t][1].xp*N[t][2].yp-
        N[t][1].yp*N[t][2].xp+fabs(N[t][0].xp*(N[t][2].yp-N[t][3].yp)-
        N[t][0].yp*N[t][2].xp-N[t][3].xp)+N[t][2].xp*N[t][3].yp-
        N[t][2].xp*N[t][3].xp);
    TOTA_pr = ELM_area + TOTA_pr;
    ELMP_stn = ELM_area * Max[t][1];
    TOTP_stn = ELMP_stn + TOTP_stn;
    ELMP_tmp = (N[t][0].t+N[t][1].t+N[t][2].t+N[t][3].t)*
    ELM_area / 4.0;
    TOTP_tmp = ELMP_tmp + TOTP_tmp;
}

printf("Tocal area of the Primary Zone = %f\n", TOTA_pr);
fprintf(fout, "Tocal area of the Primary Zone = %f\n", TOTA_pr);
printf("Summation of STRATE x AREA = %f\n", TOTP_stn);
fprintf(fout, "Summation of STRATE x AREA = %f\n", TOTP_stn);

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printf("Summation of AVETMP x AREA = %f\n", TOTP_tmp);
fprintf(fout, "Summation of AVETMP x AREA = %f\n", TOTP_tmp);

/* DETERMINING AVERAGE STRAIN RATE */
AVESTR_pr = TOTP_stn / TOTA_pr;
AVETMP_pr = TOTP_tmp / TOTA_pr;
printf("AVERAGE STRAIN RATE FOR THE PRIMARY ZONE = %f\n", AVESTR_pr);
fprintf(fout, "AVERAGE STRAIN RATE FOR THE PRIMARY ZONE = %f\n", AVESTR_pr);
printf("AVERAGE TEMPERATURE FOR THE PRIMARY ZONE = %f\n\n", AVETMP_pr);
fprintf(fout, "AVERAGE TEMPERATURE FOR THE PRIMARY ZONE = %f\n\n", AVETMP_pr);

/*PART 2: CALCULATION OF AVE TEMP & STRAIN RATE FOR THE SECONDARY ZONE*/
finS = fopen("BCCDEF.DAT", "r");
CT=0;
for (i=0; i<NODE; i++)
{
  fscanf(finS, "%i %i %i", &BCC[i][0], &BCC[i][1], &BCC[i][2]);
  if (BCC[i][2] == -2)
  {
    contnd[CT]=BCC[i][0];
    printf("CONTACTING NODE NUMBER = %i\n", contnd[CT]);
    CT++;
  }
}
printf("NUMBER OF TOTAL NODES IN CONTACT = %i\n", CT);
fclose(finS);
SDZ_ne=0;
for (i=0; i<CT; i++)
  for (j=0; j<ELM; j++)
    for (t=1; t<5; t++)
      if (contnd[i]==E[j][t])
      {
        Elmt=E[j][0];
        if (!ContactList(SDZ_ne, SDZ_E, Elmt))
          {SDZ_E[SDZ_ne] = Elmt;
           printf("CONTACTING ELEMENT NUMBER = %i\n", SDZ_E[SDZ_ne]);
           SDZ_ne++;}
      }
printf("NUMBER OF TOTAL ELEMENTS IN CONTACT = %i\n", SDZ_ne);
for (i=0; i<SDZ_ne; i++)
  for (j=0; j<ELM; j++)
    if (SDZ_E[i]==X[j][0])
    {
      SDZ_stn[i] = X[j][1];
      NS[i][0].no = E[j][1];
      NS[i][1].no = E[j][2];
      NS[i][2].no = E[j][3];
      NS[i][3].no = E[j][4];
    }
/* DETERMINE THE COORDINATES OF FOUR NODES ASSOCIATED WITH EACH ELEMENT IN SECONDARY ZONE */
for (i=0; i<SDZ_ne; i++)
    for (j=0; j<4; j++)
        for (t=0; t<NODE; t++)
            if (NS[i][j].no==W[t][0])
            {
                NS[i][j].xp = W[t][1];
                NS[i][j].yp = W[t][2];
                NS[i][j].t = W[t][4];
            }

/* DETERMINE THE AREA OF EACH ELEMENT AND CALCULATE TOTAL AREA OF SECONDARY ZONE*/
/* DETERMINE THE PRODUCT OF STRAIN RATE x AREA FOR EACH ELEMENT AND TOTAL PRODUCT*/
/* DETERMINE THE PRODUCT OF TEMPERATURE x AREA FOR EACH ELEMENT AND TOTAL PRODUCT*/
TOTA_sd=0;
TOTP_stn=0;
TOTP_tmp=0;
for (t=TIP /* Discard the first TWO elements around the tool tip */)
    {
        ELM_area = fabs(NS[t][0].xp*(NS[t][1].yp-NS[t][2].yp)-
                         NS[t][0].yp*(NS[t][1].xp-NS[t][2].xp)+NS[t][1].xp*NS[t][2].yp-
                         NS[t][1].yp*NS[t][2].xp)+
                 fabs(NS[t][0].xp*(NS[t][2].yp-NS[t][3].yp)-
                         NS[t][0].yp*(NS[t][2].xp-NS[t][3].xp)+NS[t][2].xp*NS[t][3].yp-
                         NS[t][2].yp*NS[t][3].xp);

        TOTA_sd = ELM_area + TOTA_sd;
        ELMP_stn = ELM_area * SDZ_stn[t];
        TOTP_stn = ELMP_stn + TOTP_stn;
        ELMP_tmp = (NS[t][0].t+NS[t][1].t+NS[t][2].t+NS[t][3].t) *
            ELM_area / 4.0;
        TOTP_tmp = ELMP_tmp + TOTP_tmp;
    }

printf("\nTotal area of the Secondary Zone = %f\n", TOTA_sd);
fprintf(fout, "Total area of the Secondary Zone = %f", TOTA_sd);
printf("Summation of STRATE x AREA = %f\n", TOTP_stn);
fprintf(fout, "Summation of STRATE x AREA = %f", TOTP_stn);
printf("Summation of AVETMP x AREA = %f\n", TOTP_tmp);
fprintf(fout, "Summation of AVETMP x AREA = %f", TOTP_tmp);
/* DETERMINE AVERAGE STRAIN RATE & TEMP FOR SECONDARY ZONE */
AVESTR_sd = TOTP_stn / TOTA_sd;
AVETMP_sd = TOTP_tmp / TOTA_sd;
printf("AVERAGE STRAIN RATE FOR THE SECONDARY ZONE = %f\n", AVESTR_sd);
fprintf(fout, "AVERAGE STRAIN RATE FOR THE SECONDARY ZONE = %f", AVESTR_sd);
printf("AVERAGE TEMPERATURE FOR THE SECONDARY ZONE = %f\n", AVETMP_sd);
fprintf(fout, "AVERAGE TEMPERATURE FOR THE SECONDARY ZONE = %f", AVETMP_sd);
fclose(fout);
return 0;
APPENDIX D
LEAST SQUARES PARAMETER ESTIMATION AND REPRESENTATION OF
FLOW STRESS EQUATION

Least squares method was used in estimating the unknown coefficients in the
flow stress equation given as:

\[
\bar{\sigma} = K \left( e^{aT} + A e^{b(T-T_0)} \right) \left( \frac{\ddot{e}}{\dot{e}_R} \right)^c \left( \bar{e} \right)^d
\]  \hspace{1cm} (D.1)

While this flow stress model is not linear when its used, by taking a log transform
of Equation D.1, the model becomes:

\[
\log(\bar{\sigma}) = \log K + \log \left( e^{aT} + A e^{b(T-T_0)} \right) + c \log \left( \frac{\ddot{e}}{\dot{e}_R} \right) + d \log(\bar{e})
\]  \hspace{1cm} (D.2)

where \( T_0 = 400 \, ^\circ C \) is used from the initial flow stress model (Eq. 3.40), and \( \dot{e}_R = 10,000 \, \text{sec}^{-1} \) is used for cleaning units.

The linear model with the error associated with each determined flow stress data
\( \delta_i \) included results in:

\[
y_i = \hat{Y}_0 + xZ_i + \delta_i \]  \hspace{1cm} (D.3)

The linear least squares method is aimed at finding estimates of \( \hat{Y}_0 \) and \( \hat{x} \) that
minimize:

\[
\sum_{i=1}^{n} \delta_i^2 = \sum_{i=1}^{n} \left( y_i - \hat{Y}_0 - \hat{x}Z_i \right)^2
\]  \hspace{1cm} (D.4)
The solution can be written using a matrix notation.

\[
Y = \begin{bmatrix} Y_1 \\ Y_2 \\ \vdots \\ Y_n \end{bmatrix}, \quad X = \begin{bmatrix} 1 & X_1 \\ 1 & X_2 \\ \vdots & \vdots \\ 1 & X_n \end{bmatrix}, \quad \Delta = \begin{bmatrix} \delta_1 \\ \delta_2 \\ \vdots \\ \delta_n \end{bmatrix}
\]

(D.5)

The solution is found by differentiating the squared error in Equation D.4 with respect to the model parameters. Setting the resulting linear equations equal to zero leads to final solution:

\[
\hat{\beta} = \left[ \begin{array}{c} Y_0 \\ \hat{x} \end{array} \right] = \left( X^T X \right)^{-1} X^T Y
\]

(D.6)

MATLAB Programs

PROGRAM 1:

% The Ohio State University
% Written by Tugrul Ozel
% Least square parameter estimation for the flow stress equation:
% \( \sigma = A \cdot (T) \cdot \left( \frac{\text{strain-rate}}{10000} \right) \cdot M \cdot \left( \text{strain} \right)^N \)
% Coefficient are solved with least squares method
% \( B = [b_1, b_2, b_3, b_4, b_5, b_6, b_7]' \)
% \( [Y] = [YO] + [x][Z] + E \)

format short e;
load data25.txt;
fs=data25(:,1); % flow stress
Y=log(fs); % log (flow stress)
T=data25(:,2); % temperature
epsdot=data25(:,3); % strain-rate
eps=data25(:,4); % strain
z2=T.*T;
z3=T;
z5=log(epsdot./10000);
z6=log(eps);
Z(1,:)=ones(80,6);
Z(:,2)=z2;
Z(:,3)=z3;
Z(:,4)=z5;
Z(:,5)=z6;
B=(inv(Z'*Z))*(Z'*Y)
PROGRAM 2:
% The Ohio State University
% Written by Tugrul Ozel
% 3-D Representation of initial flow stress curves
clear;
%FLOW STRESS FOR Cr-Mn Steel [Maekawa; 1996]
K=0.000065;
m=0.0039;
M=0.047;
j=0;
k=0;
STRN1=0.1; %Strain values
STRN2=5;
STRN3=10;
STRN4=20;

for T=1:40:2001;
k=k+1;
TEMP(k)=T;
A(k)=1.46*exp(-0.0013*T)+0.196*exp(-0.000015*(T-400)^2)-0.0392*exp(-0.01* (T-100)^2);
N(k)=0.162*exp(-0.001*T)+0.092*exp(-0.0003*(T-380)^2);

SS1(k)= exp(-K*T/N(k))*10e-3*(N(k)/((N(k)-m))/N(k));
SS2(k)= exp(-K*T/N(k))*10e-3*(N(k)/((N(k)-m))/N(k));
SS3(k)= exp(-K*T/N(k))*10e-3*(N(k)/((N(k)-m))/N(k));
SS4(k)= exp(-K*T/N(k))*10e-3*(N(k)/((N(k)-m))/N(k));

for STRR=10:150:100010;
3=3+1;
STRATE(j)=STRR;

FS1(k,j)= A(k)*(STRR*10e-3)*(M*exp(K*T)*(STRR*10e-3)^m +SS1(k);
FS2(k,j)= A(k)*(STRR*10e-3)*(M*exp(K*T)*(STRR*10e-3)^m +SS2(k);
FS3(k,j)= A(k)*(STRR*10e-3)*(M*exp(K*T)*(STRR*10e-3)^m +SS3(k);
FS4(k,j)= A(k)*(STRR*10e-3)*(M*exp(K*T)*(STRR*10e-3)^m +SS4(k);
end
j=j+1;
end
surf(STRATE,TEMP,FS2);
view(50,-10);
ylabel('Temperature, C');
xlabel('Strain-rate, 1/sec');
zlabel('Flow Stress, GPa');
PROGRAM 3:

% The Ohio State University
% Written by Tugrul Ozel
% 3-D Representation of determined flow stress curves

clear;

% DETERMINED FLOW STRESS DATA

% FLOW STRESS FOR P20

% FS = K*{exp (a*T)+A*exp[b*(T-T0)' '2]  )*(STRR/STTR0)^c * STRN^d

K=exp(7.2);
a=-0.0013;
A=0.18268;
b=-0.00001;
T0=400;
c=0.02964;
d=0.0363;
STTR0= 1000;
j=0;
k=0;
STRN1=0.001; %Strain values
STRN2=0.1;
STRN3=10;

for T=1:40:2001;
k=k+1;
TEMP(k)=T;
for STRR=1:1500:100001;
3=3+1;
STRATE(j)=STRR;
FS1(k,j)=K*exp(a*T)+A*exp(b*(T-T0)^2))*(STRR/STTR0)^c*(STRN1^d);
FS2(k,j)= K*exp(a*T)+A*exp(b*(T-T0)^2))*(STRR/STTR0)^c*(STRN2^d);
FS3(k,j)= K*exp(a*T)+A*exp(b*(T-T0)^2))*(STRR/STTR0)^c*(STRN3^d);
end
j=0;
end

figure (1);
surf(STRATE,TEMP,FS2);
view(50,-10);
ylabel('Temperature, C');
xlabel('Strain-rate, 1/sec');
zlabel('Flow Stress, MPa');
title('Strain= 0.1');
axis([0.1 100000 0 2000 0 2600]);

figure (2);
surf(STRATE,TEMP,FS3);
view(50,-10);
ylabel('Temperature, C');
xlabel('Strain-rate, 1/sec');
zlabel('Flow Stress, MPa');
title('Strain: 10');
axis([0.1 100000 0 2000 0 2600]);