Some Investigations of Scaling Effects in Micro-Cutting

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Dedicated to all my teachers . . .

parents, my first teachers
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LIST OF ABBREVIATIONS AND SYMBOLS

\[ \begin{align*} 
E & \quad \text{Young’s modulus} \\
F_c & \quad \text{Cutting force} \\
F_t & \quad \text{Thrust force} \\
F_{\text{const}} & \quad \text{Constant component of } F_c \\
F_{\text{dec}} & \quad \text{Decreasing component of } F_c \\
F_{\text{inc}} & \quad \text{Increasing component of } F_c \\
G & \quad \text{Shear modulus} \\
K & \quad \text{Stress intensity factor} \\
K_I & \quad \text{Stress intensity factor in mode I crack} \\
K_c & \quad \text{Critical stress intensity factor or fracture toughness} \\
K_{IIc} & \quad \text{Critical stress intensity factor in mode II crack} \\
K_{II} & \quad \text{Stress intensity factor in mode II crack} \\
K_{Ic} & \quad \text{Critical stress intensity factor in mode I crack; also called fracture toughness} \\
P & \quad \text{Total power consumed in machining} \\
R & \quad \text{Fracture toughness} \\
SD & \quad \text{Standard Deviation} \\
V & \quad \text{Cutting speed} \\
V_s & \quad \text{Shear velocity} 
\end{align*} \]
Parameter in Atkins’ model that makes $\phi$ material dependent

Rake angle

Friction angle

Equivalent normal strain time rate

Equivalent shear strain time rate

Equivalent normal strain

Spatial gradient of normal strain

Shear strain

Poisson’s ratio

Shear angle

Equivalent normal stress

Shear yield stress

Crack length

Width of cut in orthogonal machining

Burgers vector

Chip thickness

Uncut chip thickness; depth-of-cut (DOC) in orthogonal machining

Specific cutting energy

Position along crack
SUMMARY

This thesis studies scaling or “size” effects in micro-cutting; specifically scaling effects in specific cutting energy when micro-cutting ductile metals is explored. This is achieved by interpreting cutting force as consisting of several components, performing unique experiments to isolate one force component, detecting direct evidence of material separation, and numerical modeling of chip formation incorporating ductile fracture.

While many reasons have been given in the literature for explaining the scaling in specific cutting energy. To reconcile these varied reasons, a unified framework is proposed for understanding the scaling in specific cutting energy by viewing the cutting force as a combination of a constant component, and increasing and decreasing force components, the independent variable being the uncut chip thickness or thickness of material removed in orthogonal cutting. Most of the reasons given in the literature can be classified under the increasing component of force. The interest in this thesis is the constant force component. Hence, an attempt is made to isolate the constant force component by performing high rake angle orthogonal cutting experiments. The data shows a trend towards a constant force component as the rake angle is increased. A simple model using dislocation fracture mechanics is presented to predict this constant force component observed at high rake angles.

In order to understand the source of this constant force component the chip-root is investigated. by examining the chip-workpiece interface in a scanning electron microscope. Evidence of ductile tearing ahead of the cutting tool is clearly seen at both low and high rake angles, at high and low cutting speeds, in a range of uncut chip thickness values, with small and large edge radius tools, and in two materials OFHC
Copper and Al-2024 T3. This material separation by ductile fracture is considered to be a source of the constant force component.

A detailed approach to model the cutting process that captures ductile fracture leading to material separation has been developed using the finite element method. The model is implemented in a commercial software ABAQUS® using the explicit formulation. Material separation is modeled via element failure. The model is then validated using the measured cutting and thrust forces and used to study the energy consumed in cutting. As the thickness of layer removed is reduced, the energy consumed in material separation becomes important. Simulations show that the stress state ahead of the tool is indeed favorable for ductile fracture to occur. In addition, simulations performed at varying rake angles show the trend towards a constant force component similar to that observed in experiments. The numerical model is also used to study the difference in energy consumption between a sharp tool and a tool with a non-zero edge radius.

This thesis, by presenting indirect and direct evidences, conclusively shows the presence of ductile fracture leading to chip formation in micro-cutting of ductile metals. It also shows the importance of including this phenomenon in modeling the micro-cutting process and in explaining the scaling of the specific cutting energy.
CHAPTER I

INTRODUCTION

1.1 The Machining Process

Manufacturing a product or a part essentially involves providing the necessary geometry (shape or form and dimensions) and material properties to a given raw material. The cutting or machining process is a top-down approach that achieves this by mechanically removing unwanted material, from the bulk raw material, in the form of shavings or chips. Such brute force material removal is usually associated with large forces and temperatures and thus requires robust machinery. Common examples of this include lathes, milling/drilling machines, and grinding machines. These machines achieve material removal by providing dynamic interference between the raw material, called workpiece, and a cutting tool, while also firmly holding (fixturing) these two. The relative motion between the workpiece and the tool is called the cutting velocity or speed and the interference is characterized by the term, uncut chip thickness. This motion can be provided by moving the workpiece or the tool or both. The basic shape of the cutting tool is very similar to common devices such as a scissor, a knife, or a shaving razor blade, viz., that of a wedge.

The common machining processes such as turning, milling, grinding are fairly complex processes known as oblique cutting where the cutting edge of the tool is oblique (non-orthogonal) to the direction of the cutting velocity. This causes the chips to flow side-ways, often results in non-uniform uncut chip thickness, and also in three dimensional cutting forces. Upon changing the setup so that the length of the cutting edge is always orthogonal to the movement of the tool (cutting velocity), the process becomes two-dimensional and is referred to as orthogonal cutting (see
This thesis deals only with the orthogonal cutting process. Such a process simplifies the study of machining and useful insights obtained from this can then be extended to the oblique cutting process. The orthogonal cutting process is illustrated in Fig. 1.2. The wedge shaped tool (with the cutting edge into the paper) is moving at a cutting speed, \( V \), and is removing a layer of material of thickness \( t_o \), also called the uncut chip thickness, from the workpiece of width \( b \) (into the paper). The face on which the chip slides up the tool is called the rake face and the angle \( \alpha \) is called the
rake angle. The sign of the angle $\alpha$ is also indicated. The more negative this angle, the stronger and more blunt is the wedge, while the more positive it is, the more weak and sharp is the wedge. The chip thickness, $t_c$, is usually larger than $t_o$. The material is seen to intensely deform primarily in two areas in the chip - the primary zone marked A and the secondary zone labeled B. The shear in zone A is often idealized as occurring in a thin shear plane as shown. The strains involved in conventional metal cutting are in the range of 2-4, while the strain rates are in the range of $10^4$-$10^6$ s$^{-1}$ and temperatures can go as high as the melting point of the workpiece material depending on the cutting conditions. In orthogonal cutting all chip and work material particles move in planes parallel to the plane of the paper [Ghosh and Mallik 1985]. There is no component of velocity or motion perpendicular to plane of the paper.

Orthogonal cutting can be achieved on a planing or shaping machine or by axially feeding a tool on a lathe into a tube shaped workpiece with the cutting edge of the tool aligned with the centerline of the spindle.

Three main chip types were noticed [Ernst 1938] in orthogonal cutting: continuous chips without built-up-edge (BUE), continuous chip with BUE and discontinuous chips. These chip types are shown in Fig. 1.3. In this thesis, continuous chip formation without BUE is of primary interest. This type of chip is mainly observed while cutting ductile metals.

![Figure 1.3: Types of chip in metal cutting](Ernst 1938). From left: discontinuous chip, continuous chip, continuous chip with BUE
1.2 Macro and Micro-Cutting

The machining process can be broadly classified into macro and micro-cutting based on the interference between the tool and workpiece (uncut chip thickness as explained before). If the interference is large (> 200µm), meaning that more material is being removed during a given motion or pass of the tool/workpiece, then it is commonly regarded as macro-cutting and if the interference is small (< 200 μm, it is called micro-cutting. Masuzawa and Tonshoff (1997) have defined the range of uncut chip thickness for micro-cutting as 0.1 to 200 μm. Larger interferences are normally used in “rough” machining operations to increase the material removal rate under mass-production environments. Smaller interferences are normally used in finishing operations after roughing operations have removed most of the unwanted material from the bulk workpiece.

The micro-cutting process is getting more attention from researchers and industry due to the increasing need for miniaturization of components, features, and devices. These include micro-holes in circuit boards, micron-sized devices in IC-packages, microscale fuel cells, microfluidic systems, microholes in fuel injection nozzles for automobiles, fiber optics, and high temperature jets, cell and tissue handling devices in bio-technology, inspection and surgical devices needed in the medical industry, micro-molds, mirrors in optical devices, deep X-ray lithography and more (Masuzawa, 2000; Liu et al., 2004). The need to remove smaller amounts of material to create these features, components and devices arise under three conditions:

1. When making tiny parts and components,
2. When making smaller, intricate features on larger parts such as molds, and
3. When making very fine, smooth surfaces on larger, bulk parts

Note that micro-cutting to remove such small amounts of material may not necessarily involve micro-sized tools. The first two conditions listed above largely involve cutting
tools that are of the same dimensions as that of the desired features. Micro-cutting can also be achieved with normal macro-sized cutting tools, especially in the last item listed above.

The mechanics of machining at the microscale is known to be different in several respects from the macro-scale \cite{Liu et al., 2004}. Differences include changes in chip formation mechanics, vibration and process stability, importance of cutting tool edge radius, scaling of specific cutting energy, to name a few. This thesis focuses on the chip formation process and its possible relation to a scaling phenomenon in specific cutting energy. It must be noted that although the findings in this thesis are reported in the context of micro-cutting such findings may be equally applicable, but may not be significant, under macro-cutting conditions. The following section describes the scaling phenomenon seen in specific cutting energy.

### 1.3 Specific Cutting Energy and Scaling

In orthogonal cutting, essentially a two-dimensional process, the tool or workpiece experiences, as a result of the cutting action, two forces: one, along the cutting velocity direction and the other in the perpendicular direction. Since there is motion along the cutting velocity direction and no motion in the other direction, the rate at which energy is consumed in the machining process can be written as,

\[
P = F_c V
\]  

where \( F_c \) is the force experienced by the tool or the workpiece in the direction of the cutting velocity. Most textbooks on machining \cite{Shaw, 1997} decompose this energy consumed in the machining process into the following components:

1. Shear energy - this is the energy consumed in shear as the chip material crosses the shear plane
2. Friction energy - energy dissipated due to rubbing of the chip on the rake face
3. Surface energy - energy required for new surface formation

4. Momentum energy - associated with metal movement as it changes direction and hence velocity after crossing the shear plane

It is commonly regarded that the surface energy is comparable to solid surface energy and hence is negligible in comparison to shear or friction [Shaw, 1997]. The momentum energy is also neglected in developing machining models since it is small compared to shear or friction [Shaw, 1997].

The energy consumed per unit volume of material removed, or specific cutting energy, is often calculated and used for example, as a measure of the machinability of a material. Higher the specific cutting energy, more difficult it may be to cut and remove material. The specific cutting energy, \( u \), is calculated as,

\[
\frac{P}{MRR} = \frac{F_cV}{Vbt_o} = \frac{F_c}{bt_o}
\]  

(1.2)

where MRR is the material removal rate and \( b \) is the width of cut. The volume of metal removed in unit time and the width of cut are illustrated in Fig. 1.4. The

**Figure 1.4:** Volume of material removed in unit time

specific cutting energy is thus a measure of the amount of effort (energy) needed to
remove a unit amount of material. The smaller the amount of material removed, proportionally smaller should be the total energy. So, for a constant width of cut $b$ one expects a plot of $u$ versus $t_o$ to be a horizontal straight line. In other words, one would normally expect that the cutting force decreases proportionally with decrease in thickness of cut. Experimental evidence has however shown that this is not the case i.e. $u$ is seen to scale non-linearly with decreasing $t_o$. An example of this scaling is shown in Fig. 1.5 which shows data from cutting plain carbon steel at several uncut chip thicknesses. It can be seen from the experimental data that $u$ does increase with decrease in $t_o$. Similar scaling effects are also seen in other machining processes such as grinding (Shaw [1996]). A unique explanation for this behavior of $u$ has challenged several researchers for well over 50 years.

The scaling in $u$ simply means that more amount of energy has to be expended, and hence more amount of cutting force has to be exerted, than normally expected, in removing smaller amounts of material. Different explanations for why this extra energy is needed have been proposed in the literature. These include:

![Figure 1.5: Scaling in specific cutting energy (Kopalinsky and Oxley [1984])](image)
1. Material strengthening because of lack of defects at the small scales \cite{Shaw1950}, because of reduced temperature softening effect \cite{Kopalinsky1984}, because of strain-rate increase \cite{Larsen-Basse1973}, and because of strain-gradient effects \cite{Dinesh2001, Joshi2004, Liu2006}.

2. Finite edge radius of the tool resulting in plastic flow (ploughing) around the tool tip \cite{Lucca1993, Komanduri1998},

3. Sub-surface deformation \cite{Nakayama1968, Liu1976}, and

4. Fracture energy consumed in material separation \cite{Atkins1974, Atkins2003, Atkins2006}.

The first explanation has been well studied and reported in literature. The second explanation involves a blunt tool. In reality cutting tools are never sharp but have a finite cutting edge radius (Fig. 1.6). Note that the radius $r$ is between the rake and flank faces of the tool. The extra plastic flow associated with a non-zero edge radius has long been considered as a reason for the size effect \cite{Lucca1993, Komanduri1998, Schimmel2002}. There are a few works reported for the third explanation. The fourth explanation is a relatively recent one; it disputes some basic assumptions widely accepted by machining researchers in chip formation while cutting ductile metals and it is this explanation that is of interest in this thesis work.

The exact nature of material separation leading to chip formation in machining of ductile metals is unclear with literature reporting the following mechanisms: (a) shear or plastic flow \cite{Merchant1945, Finnie1956, Madhavan2000}, and (b) fracture \cite{Atkins2003}. Chip formation as an indentation process has been justified from polished cross-sectional micrographs of the chip-root showing plastic flow around the tool edge \cite{Madhavan2000}. Very little proof of actual material separation
Figure 1.6: (a) Sharp tool (b) Tool with edge radius

by fracture (cracks) is available in ductile machining. However, many researchers (Shaw, 1997, pages 384, 386) have acknowledged that there has to be some form of localized fracture in chip formation leading to material separation. Some researchers (Komanduri and Brown, 1972; Iwata and Ueda, 1976) have indeed shown the presence of cracks ahead of the tool in machining low carbon steel and other ductile metals. While the presence of cracks is shown, it is not clear if chip formation occurred by material fracture. In other words, cracks or fracture may have occurred in the chip after it was formed. Among the proponents of chip formation by fracture in machining is Atkins (2003), who has argued the applicability of modern ductile fracture mechanics in modeling the cutting process. He has shown that inclusion of a fracture toughness term in the energy expended in machining can explain hitherto inexplicable observations such as the dependence of shear angle on material properties. It is also shown to account for the scaling in specific cutting energy.

1.4 Open Questions

The observation of scaling in the specific cutting energy and the several explanations offered in literature gives rise to several questions. Given that these explanations offered for this phenomenon seem logical and reasonable, is there a common framework, viewed from which, all of these explanations can be reconciled? Also, the
recent work (Atkins, 2003), which proposes that material separation energy cannot be neglected as in traditional machining theory, does not give direct evidence of the presence of ductile fracture. Can we actually find evidence of such material separation or any other evidence of its presence? If indeed material separation does occur from ductile fracture and if such fracture is occurring under extreme strain, strain-rate, and temperature under normal machining conditions, what appropriate model can be adopted to simulate such chip formation in machining? Can such a model be used to estimate the energy consumed and to study its relation to the scaling effect seen in the specific cutting energy? Also, what is the effect of the cutting edge radius on such ductile fracture occurring ahead of the cutting tool? This thesis tries to address some of these questions and the approach taken is given in the following section.

1.5 Research Approach and Thesis Outline

The research begins by trying to formulate a framework that will reconcile all the explanations offered for the size effect in specific cutting energy (Chapter 3). The framework involves viewing the cutting force as being composed of a constant component, an increasing component, and a decreasing component, with decrease in uncut chip thickness, \( t_o \). Experimental methods are then devised that minimize the shear and friction components of the energy going into cutting, and thus, establish the presence of the constant force component (Chapter 3). Experiments are then carried out to look for evidence of ductile fracture during chip formation. An oblique view of the unpolished chip root in a scanning electron microscope (SEM) is adopted for this purpose (Chapter 4). The chip root is obtained by quick-stopping the spindle at low cutting speeds and using a hammer-type quick-stop device at higher cutting speeds. Based on firm experimental evidence of direct material failure ahead of the tool, a finite element model that simulates failure by a sacrificial layer involving sequential
element deletion is developed (Chapter 5). The model is then used to study the energy consumed in material separation and its relation to the scaling in specific cutting energy (Chapter 5). Finally, cutting is also performed with cutting tools of known edge radii and the effect of this edge radius on ductile fracture is studied using a quick stop device designed and built for this purpose (Chapter 6). The main conclusions of this thesis are drawn in Chapter 7 and suggestions for future work are given.
CHAPTER II

LITERATURE REVIEW

This chapter reviews some of the relevant literature work on size-effect in cutting, material separation, numerical modeling, and the use of ductile fracture in modeling of machining.

2.1 Size Effect in Cutting

The size-effect phenomenon in specific cutting energy i.e., an unbounded increase in the specific cutting energy with decrease in uncut chip thickness in micro-cutting (such as was shown earlier in Fig. 1.5), has been reported and investigated by several researchers over the past fifty years.

2.1.1 Experimental Reports of Size-Effect

The first known report of the size-effect in specific cutting energy was given by Backer et al. (1952). They performed tests on three different processes: turning, milling and grinding. They performed turning tests on SAE1112 steel machined in the form of a thin-walled tube. The plot of specific cutting force (same as specific cutting energy for a constant width of cut) as the uncut chip thickness varies from 2 to 40µm can be clearly seen. Other researchers have also reported size effect in cutting of mild steels (Parsen:1973, Kopalinsky:1984).

Similar size effects were also found in other several copper alloys. Experimental evidence of the size-effect in cutting of Brass is shown by Nakayama and Tamura (1968). Fig. 2.2 shows the plot of specific cutting force (same as specific cutting energy for a constant width of cut) as the uncut chip thickness varies from 2 to 40µm.
Figure 2.1: Size effect observed in SAE1112 steel (Backer et al. 1952) μm. Data was obtained at a very low cutting speed of 0.1 m/min. Size effect in

Figure 2.2: Size effect observed in Brass (Nakayama and Tamura 1968)

OFHC-Cu (Oxygen Free High Conductivity Copper) and Te-Cu alloy has also been reported (Lucca et al. 1991; Lucca and Seo 1993). The data for Te-Cu is reported with a cutting tool with edge radius of 250 nm and uncut chip thickness ranging from 10 nm to 20 μm at a cutting speed of 7.6 m/min, while the data for OFHC Cu is reported with cutting tool edge radius of 100-300 nm and uncut chip thickness ranging from 25 nm to 25 μm at cutting speeds ranging from 6 to 108 m/min.
Figure 2.3: Size effect in TeCu and OFHC-Cu (Lucca et al. 1991; Lucca and Seo 1993)

Size effect in several materials including PMMA, Calcium Fluorite and Germanium (Fig. 2.4) has been reported by Furukawa and Moronuki (1988). Cutting tests on these materials were performed at a cutting speed of 100 mm/s and uncut chip thickness values ranging from 0.5 to 10 \( \mu \text{m} \).

Scaling of specific cutting energy has also been reported in Aluminum alloys. Liu and Melkote (2006) have reported size effect in Al5083 (Fig. 2.5). Experiments on Al5083 are reported at cutting speeds 10 m/min and 200 m/min at uncut chip thickness ranging from 0.5 to 10 \( \mu \text{m} \) at the lower speed and 20 to 200 \( \mu \text{m} \) at the higher speed. The cutting tool used at lower speed was a single crystal diamond with edge radius of 65-100 nm. Ng et al. (2006) have reported it in Al7075 (Fig. 2.6) at uncut chip thickness values ranging from 10 nm to 2 \( \mu \text{m} \) using a tool with edge radius of 65 to 100 nm at a cutting speed of 10 m/min and 150 m/min.

It can thus be seen from the literature that scaling in specific cutting energy has been reported while cutting several materials at varied cutting conditions of speeds, uncut chip thickness values and edge radii (Table 2.1). The following section explores the reasons for the size effect given by researchers in the literature.
<table>
<thead>
<tr>
<th>Reference</th>
<th>Cutting Speed</th>
<th>DOC Range</th>
<th>Edge Radius</th>
<th>Material</th>
</tr>
</thead>
<tbody>
<tr>
<td>Backer et al. (1952)</td>
<td>137 m/min</td>
<td>50-300µm (turning)</td>
<td></td>
<td>SAE1112 Steel</td>
</tr>
<tr>
<td></td>
<td>1630 m/min</td>
<td>5-13 µm (milling)</td>
<td></td>
<td>SAE1112 steel</td>
</tr>
<tr>
<td>Nakayama and Tamura (1968)</td>
<td>0.1 m/min</td>
<td>2-42 µm</td>
<td>3-4 µm</td>
<td>Brass</td>
</tr>
<tr>
<td>Larsen-Basse and Oxley (1973)</td>
<td>6.1 m/min</td>
<td>25-2500µm</td>
<td></td>
<td>low carbon steel</td>
</tr>
<tr>
<td>Kopalinsky and Oxley (1984)</td>
<td>420 m/min</td>
<td>10-200µm</td>
<td></td>
<td>Steel</td>
</tr>
<tr>
<td>Lucca et al. (1991)</td>
<td>6-108 m/min</td>
<td>25nm-20µm</td>
<td>100-300 nm</td>
<td>OFHC Cu</td>
</tr>
<tr>
<td>Lucca and Seo (1993)</td>
<td>7.6 m/min</td>
<td>10nm-25µm</td>
<td>250 nm</td>
<td>Te-Cu</td>
</tr>
<tr>
<td>Furukawa and Moronuki (1988)</td>
<td>6 m/min</td>
<td>0.5-10µm</td>
<td></td>
<td>PMMA, CaF$_2$, Germanium</td>
</tr>
<tr>
<td>Ng et al. (2006)</td>
<td>10, 150 m/min</td>
<td>10nm-2µm</td>
<td>65-100 nm</td>
<td>Al-7075</td>
</tr>
<tr>
<td>Liu and Melkote (2006)</td>
<td>10, 200 m/min</td>
<td>0.5-10µm</td>
<td>65-100 nm</td>
<td>Al-5083</td>
</tr>
</tbody>
</table>
Figure 2.4: Size effect observed in several materials (Furukawa and Moromuki 1988)

Figure 2.5: Size effect observed in Al5083 alloy (Liu and Melkote 2006)
2.1.2 Explanations for the Size Effect

Extensive work in this area has resulted in many explanations for this phenomenon. These explanations can be broadly categorized as follows: (a) Material strengthening effect due to strain, strain-rate, strain-gradient etc., (b) Tool edge radius effect, (c) Sub-surface plastic deformation, and (d) Material separation effect. These factors and their location in the cutting zone is illustrated in Fig. 2.7. The explanation of material strengthening mainly pertains to the area of intense deformation (location 1) in the primary and secondary shear zones. The area close to the tip of the tool has been cited as a reason for two reasons: (a) geometry of the tool (location 3) in the form of the finite edge radius, and (b) material separation (location 4) leading to chip formation is expected to happen in this area. The fourth reason pertains to the finished machined surface, particularly the sub-surface (location 2) deformation undergone in this area.

2.1.2.1 Material Strengthening

As mentioned earlier (Fig. 1.2) machining is associated with intense deformation of the material cut immediately ahead of the cutting tool in the primary and secondary
Figure 2.7: Factors attributed to size-effect and their location in the cutting zone
shear zones. Naturally, the energy associated with this large plastic deformation can be suspected to be involved in the scaling of the specific cutting energy. Since stresses are associated with this plastic deformation, any factor that can increase the stresses in the material can be associated with increase in energy consumed in plastic deformation and hence can be used to explain the scaling phenomenon. Note that the plastic deformation energy per unit volume is calculated as:

\[ E = \int \sigma d\epsilon \]  \hspace{1cm} (2.1)

Stress is known to be a function of several variables:

\[ \sigma = f(\epsilon, \dot{\epsilon}, T, \eta) \]  \hspace{1cm} (2.2)

where \( \sigma \) is the equivalent normal stress, \( \epsilon \) the equivalent normal strain, \( \dot{\epsilon} \) the time-rate of the normal strain, \( T \) the temperature, and \( \eta \) the spatial gradient of the normal strain. If it can be shown that any of these factors favor the increase in stress response of the material as the uncut chip thickness is decreased, then it would explain the size effect.

The size effect has been attributed to strain hardening and crystallographic defects such as grain boundaries, missing and impurity atoms, etc (Shaw, 1950; Backer et al., 1952). They argued that since a significantly reduced number of imperfections are encountered when deformation takes place in a small volume, the material strength would be expected to increase and approach the theoretical strength (Fig. 2.8).

Larsen-Basse and Oxley (1973) explained the scaling phenomenon in machining in terms of the strain-rate sensitivity of the workpiece material. Their reasoning is based on empirical data drawn from experiments on plain carbon steel, over a range of cutting speeds (17 to 817 ft/min) and uncut chip thickness (0.005 in to 0.0108 in), which suggest that the maximum shear strain rate, \( \dot{\gamma} \), within the primary shear zone is inversely proportional to the uncut chip thickness and is given as:

\[ \dot{\gamma} = C \frac{V_s}{t_o} \]  \hspace{1cm} (2.3)
Figure 2.8: Theoretical strength is approached at small \( t_o \) \[\text{Backer et al. 1952}\] where \( V_s \) is the shear velocity, \( C \) a material-dependent constant, and \( t_o \) the uncut chip thickness. Therefore, a decrease in the uncut chip thickness will leave the strain occurring in the shear zone unchanged but the strain rate will increase inversely with the uncut chip thickness \( t_o \). For most metals, an increase in the strain rate causes an increase in the flow stress with the strain-rate sensitivity of flow stress increasing rapidly in the range applicable to machining processes. This could therefore explain the increase in specific cutting energy with reduction in uncut chip thickness.

\[\text{Kopalinsky and Oxley 1984}\] took into account the effect of temperature in later work and attributed the size effect in the specific cutting force to a decrease in the shear plane angle due to decrease in the tool-chip interface temperature. This, they contended, leads to an increase in the shear strength of the workpiece material. Furthermore, they acknowledge that the temperature effect does not explain the size effect observed at uncut chip thickness less than 50 \( \mu \text{m} \), which is possibly because...
of the increasing sensitivity of flow stress to strain rate within this range (Fig. 2.9). Marusich (2001) also offers a similar explanation based on finite element simulations of orthogonal cutting at very high cutting speeds. Fang (2003) recently presented a complex slip line model for orthogonal machining and attributed the size effect to the material constitutive behavior of varying shear flow stress with uncut chip thickness.

Recently, Dinesh et al. (2001) linked the size effect observed in micro/nano indentation to that in machining. The increase in hardness of a metallic material with decrease in indentation depth is a consequence of the dependence of the flow stress of the metal on the spatial gradient of strain. They suggested that the size-effect in machining can also be explained by the theory of strain-gradient plasticity since strain gradients in machining are very intense. Strain-gradient plasticity suggests that when deformation is large and is constrained spatially to a narrow region, the stress not only depends on strain at a point but also upon the strains in the region surrounding that point. Building upon this work Dinesh et al. (2001), Joshi and Melkote (2004) presented an analytical model for orthogonal cutting that incorporates a material constitutive law with strain gradient effects. However, their model

**Figure 2.9:** Left: Predicted temperatures and strain-rates at tool/chip interface; Right: Predicted temperatures, strains and strain-rates (Kopalinsky and Oxley, 1984)

21
only considers strain gradient produced in the primary shear zone. More recently, strain gradient incorporated into the flow stress equations has been implemented in a finite element model to simulate machining by Liu and Melkote (2006). This work incorporates strain gradient in all aspects of deformation in the chip and not just the primary deformation zone. Liu and Melkote (2006) have shown (Fig. 2.10) that strain gradient plasticity based model of orthogonal micro-cutting is able to capture the size effect in specific cutting energy for the aluminum alloy Al5083-H116 examined. Strain gradient strengthening contributes significantly to the size-effect at low cutting speed (< 10 m/min) and small uncut chip thickness (< 10µm).

Figure 2.10: Importance of strain gradient in explaining size-effect (Liu and Melkote, 2006)

2.1.2.2 Tool Edge Radius

The extra plastic flow associated with a non-zero edge radius has also been considered as a reason for the size effect (Lucca et al., 1991; Komanduri et al., 1998; Schimmel et al., 2002). Cutting tools commonly used in machining operations are never ideally sharp but always have some bluntness to them. The bluntness can often be approximated to be of the form of a circular radius between the flank and the rake faces of the tool as shown earlier in Fig. 1.6. The tool edge being the closest to the root
of the developing chip, the nature of the edge geometry can be expected to play a significant role in affecting the processes occurring at the chip-root. This effect of the edge radius has been studied and reported by several researchers. The presence of edge radius is said to cause, what is now commonly called, ploughing to occur ahead of the tool tip. This is described by Albrecht (1960) and illustrated in Fig. 2.11 as the pushing of the material in front of the edge radius predominantly into the chip and some into the workpiece. This conclusion, from a basic understanding of cross-sectional micrographs, implicitly assumes that the chip formation is simply one of plastic flow around the tool edge and that there is no explicit material separation that occurs.

Based on this understanding of ploughing, several researchers have come up with analytical models for machining (Connolly and Rubenstein, 1968; Wu, 1988; Endres et al., 1995). Slip line models incorporating the ploughing-type of metal flow have also been reported (Waldorf et al., 1998; Fang, 2003). This ploughing mechanism has also been attributed to the increase in machining forces seen and attempts have been made to isolate this ploughing force by extrapolating forces to zero uncut chip

Figure 2.11: Ploughing around the edge radius of the tool [Albrecht 1960]
thickness (Thomsen et al., 1953). The effect of tool edge geometry on force and energies have been reported by Lucca et al. (1991). It was seen that at lower uncut chip thickness values, closer to the edge radius of the tool, the energy consumed in the shear zone alone was unable to account for the size effect in specific cutting energy. Sliding at the tool-work interface and ploughing was attributed to explain this anomaly. The energy consumed in machining is assumed to be expended in chip formation, sliding and ploughing (Fig. 2.12). The energy dissipated in ploughing

![Figure 2.12: Energy dissipation in metal cutting (Lucca et al., 1991)](image)

is considered important as $t_o$ approaches the edge radius, in its effect on workpiece surface integrity and sub-surface damage and in determining the minimum uncut
chip thickness. In another experimental study by Lucca and Seo (1993), it was shown that the nominal rake angle and the edge profile significantly affect the forces. Edge radius effects have also been cited by Armarego and Brown (1962) as contributing to size-effect by decreasing the effective rake angle. While practical experiments with a perfectly sharp tool are not feasible, simulations can be performed under sharp conditions. Molecular dynamic simulations performed by Komanduri et al. (1998) have indicated that the size effect does not seem to occur until one approaches close to atomic dimensions (Fig. 2.13). Hence, it is concluded that size-effect at the micro-meter range may be attributed to change in rake angle and/or tool wear.

Figure 2.13: Molecular Dynamics simulation setup (left); Specific cutting energy results from simulations (right) (Komanduri et al., 1998)

2.1.2.3 Sub-Surface Damage

Nakayama and Tamura (1968) have attributed size-effect to two reasons: (a) the energy expended in sub-surface plastic deformation of the workpiece, and (b) the edge radius. Thus, they too have shown that apart from the edge radius effect, the sub-surface plastic flow, which is observed to be disproportional to the uncut thickness (Fig. 2.14), can contribute to size-effect. The effect of the sub-surface plastic flow is incorporated into a machining model as an extended shear plane.
Figure 2.14: Measure of sub-surface plastic flow (left); plastic flow is not proportionate to $t_o$ (right) [Nakayama and Tamura 1968]

2.2 Chip Formation and Material Separation

Experimental observations of the formation of chips in metal cutting has led to the interpretation and development of the theory of chip formation. Based on the experimental observations, continuous chip formation without BUE (see Fig. 1.3) is classically explained by the Piispanen model [Piispanen 1937, 1948] of deck of cards (see Fig. 2.15). This model was also independently developed by Merchant [1945]. According to this model, the uncut chip region is likened to be a set of lamellae (or cards) of metal that get sheared and slide up the rake face of the tool as the cutting action proceeds. In reality the lamellas are thin and the chip’s underside appears continuous and is of uniform thickness.

The question of whether chip formation in ductile metal cutting involves material separation has been a controversial one in literature. There are two schools of thought. One school of thought regards machining as a special case of wedge indentation process and that the chip is generated without any fracture. The other school of thought
The idea that chip formation occurs without material separation has its roots mainly in the observation of cross sectional areas of the chip. The argument is that there is no evidence of cracks ahead of the cutting tool in such micrographs (Fig. 2.16). If material separation occurs then one should be able to see cracks running ahead of

![Figure 2.15: Model of chip formation. Left: Piispanen model (Piispanen, 1937). Right: Merchant model (Merchant, 1945)](image)

the tool, much like that observed in a hatchet splitting a piece of wood (Fig. 2.17).
This lack of evidence of crack in front of the tool gave place to the idea of plastic deformation or shear as causing the formation of the chip. Several researchers have made a case for treating machining as a wedge indentation process. By using sequences from motion pictures of the indentation of paraffin block by a wedge-shaped tool pressed into the middle of the block, and the cutting action initiated when the tool is pressed into the block close to an edge, Ernst was able to conclude that machining is equivalent to asymmetric indentation with an inclined wedge. Similarities between machining and indentation were also shown by Bhattacharyya. A case for machining as a wedge indentation is made by comparing texture or flow lines in any deformation process with that in cutting and the argument is made as follows in the next paragraph.

Texture or flow lines form as a result of elongation of the grains of the material in the direction of maximum tensile strain. These lines can be further stretched, compressed and rotated by subsequent strains imposed on the material. The flow lines formed by indentation (Fig. 2.18(a)) are not ruptured by the indenter but bent around the tip of the indenter. In machining, the flow lines (Fig. 2.18(b)) form as a consequence of the shear deformation occurring along the primary shear zone. These lines are then stretched and rotated due to the secondary...
Figure 2.18: (a) Indentation (b) Machining (Madhavan et al., 2000)

deformation taking place along the rake face of the tool. The flow lines are also seen to bend around the tip of the tool in machining as in indentation. Also, it is argued by Madhavan et al. (2000) that the rupture in flow lines predicted in indentation of a semi-infinite solid with a wedge is similar to the rupture in flow lines in machining (Fig. 2.16). Further evidence of similarities between machining and wedge indentation is cited from a study of the deformation produced in mild steel surfaces by the oblique impact of square plates made of hardened tool steel (Hutchings, 1977). A regime of deformation intermediate between normal wedge indentation and cutting was observed in these experiments. This is consistent with the orientation and direction of motion of the indenter in the experiments being intermediate between normal indentation and machining. An argument is also made from a calculation of stresses ahead of the cutting tool (Madhavan et al., 2000). From consideration of the distribution of hydrostatic stresses along the shear zone, given by Roth and Oxley (1972), it is seen that the hydrostatic stress close to the cutting edge is compressive and hence is not conducive to tensile rupture of the work material. It must be mentioned that this compressive stress distribution ahead of the tool is controversial in literature with other researchers indicating the possibility of tensile
stresses ahead of the tool edge (Connolly and Rubenstein, 1968; Palmer and Oxley, 1959).

The second school of thought regards chip formation as occurring via material separation due to fracture mechanisms. Among the proponents of fracture in machining is Atkins who suggests that the machining process adjusts itself to simultaneous actions of cracking and plastic flow with friction playing a superimposed role (Atkins, 1974, 2003, 2006). Some researchers have speculated the presence of fracture leading to chip formation. In a report on discontinuous chip formation, Cook et al. (1954) have acknowledged that even in the perfectly continuous chip some fracture of the material is involved leading to the development of new surfaces. It is noted, albeit by few (Cook et al., 1954; Atkins, 2006), that for the card number 7 (in Fig. 2.15(a)) to slide up the rake face, it has to be first separated from the base workpiece at region X-Y and hence material separation has to occur here. From the direction of the required fracture (in the direction of cut) it would seem that it must result from a tensile stress acting at the tool point (Cook et al., 1954). The argument for this is made by Cook et al. (1954) as follows.

No matter how carefully a tool may be ground, there will always be a finite radius of curvature at its point. If the metal cut consisted of a number of plates then the shear surface might be expected to be as shown in Fig. 2.19. The rake angle

![Figure 2.19: Stack of plates cut with tool (Cook et al., 1954)](image-url)
associated with plate 1 is very small and hence the corresponding shear angle is also expected to be small. The chip from plate 1 would be bent toward the tool by plate 2. The shear angle would increase for subsequent plates inasmuch as the rake angles associated with them increase. This variation in shear angle would occur up to point A beyond which the rake angle and hence the shear angle should remain constant. As a consequence, chips 1, 2, 3, and 4 would be of different lengths. In practice, the metal cut does not consist of separate plates and the chips cannot be of different lengths. Hence, it is argued that the chip in the vicinity of the tool face must deform plastically in tension. The region subjected to tension will extend up to point A. The tensile field of stress that is developed as a result of the curvature of the tool point can thus produce the crack necessary in the development of the new surface. The material beyond point A, it is argued, is subjected to large compressive stresses and the crack will be quenched upon reaching this region in case of continuous cutting. Hence, it is argued (Cook et al., 1954) that the possibility of observing cracks in front of a tool increases as the radius of curvature at the tool point is increased. However, it is concluded that the presence of such cracks is of negligible importance with regard to the analysis of the shear process and the mechanics of cutting.

A very similar argument in the presence of a finite edge radiused tool is posed by Connolly and Rubenstein (1968). It is argued that in the presence of a finite edge radius, all the material below point D (see Fig. 2.20) must pass below the tool and be compressed. All material above D eventually moves parallel to the rake face. Thus, if a vertical element of material is considered to approach D in the direction of cutting, that part of the element above the horizontal through D will move upwards while that part below the horizontal through D will move downwards and hence the element will be subject to a tensile stress. It is not speculated however that fracture can occur because of this tension, nor do Connolly and Rubenstein (1968) include fracture in their analysis.
The role played by microcracks and fracture under high deformation conditions seen in machining can be seen in the works of Walker and Shaw (1969) and Komanduri and Brown (1972). The behavior of steels under high shear strain was studied by Walker and Shaw (1969). They found experimentally that a material that has positive work hardening characteristic at low strains can have a negative work hardening trend at high shear strains under high hydrostatic compressive stress. This behavior was attributed to the formation of microcracks. The compressive stress is speculated to prevent coalescence of the microcracks and deformation proceeds by a mechanics of microcrack formation and rewelding (caused by the compressive stress). The presence of such microcracks in machining a low carbon steel, ahead of the cutting tool, has been shown by Komanduri and Brown (1972) (see Fig. 2.21).

That the machined surface is a newly generated surface is generally accepted (Merchant, 1945; Shaw, 1997). Generation of new surfaces means that at some point ahead of the cutting edge material separation has to occur. Material separation implies that fracture has to be involved and energy has to be consumed in this process. In Merchant’s analysis, it is specifically stated that the work required to separate the chip from the metal at the very cutting edge, thus producing two new surfaces, has been
Figure 2.21: Presence of microcracks in cutting low carbon steel (Komanduri and Brown, 1972). (a) Deformation region mid-way between the tool edge and free surface. (b) A SEM micrograph of the deformation near tool edge for the cut in (a).

neglected (Merchant, 1945). Lately, this assumption has been increasingly being questioned (Atkins, 2003, 2006). The traditional argument to neglect this energy is as follows (Merchant, 1945; Shaw, 1997). When a new surface is generated in a solid substance, sufficient energy must be supplied to separate the ions at the interface. A certain energy is thus associated with the formation of new surface called the surface energy of the substance (analogous to surface tension of liquids). This value of surface energy for most metals is very small, about $1 \times 10^{-3}$ N/mm, and hence the energy associated is considered negligible. Merchant goes on to say that only when the dimension $t_o$ becomes of the order of 0.25 $\mu$m or less would the surface energy begin to become significant. He also goes on to add that this estimate of energy required to separate the chip from the metal at the very cutting edge would be increased by considering the work expended in the local plastic flow of the metal adjacent to the newly created surfaces on the chip and work. However, Merchant speculated that even after including this plastic work it would still be negligibly small compared to
the total work. *It is to be noted that upon inclusion of this plastic work, the energy needed for material separation may become important at \( t_o \) values much larger than what Merchant speculated earlier as 0.25 \( \mu \)m.* In a related work, [Thomsen et al. (1953)](1953) have argued that deformation during chip formation by the action of the tool is similar to piercing a hole using a punch. There is some material displacement or pushing before separation occurs. They acknowledge though that material separation does occur. Thus, they argue that there is always a constant force involved in the pushing action, regardless of the uncut chip thickness, that gives rise to the scaling of the specific cutting energy.

In more recent work [Atkins (2003)](2003) has argued for the applicability of modern ductile fracture mechanics in modeling the cutting process. He cites the work associated with the chip separation criterion in Finite Element simulations of machining to be orders of magnitude different from that associated with surface energy or surface tension, and comparable to fracture toughness values. The work done in cutting is considered to be expended in (a) plasticity along the shear plane, (b) friction at the tool-chip interface, and (c) formation of new surfaces. From this, it is seen that if shear and friction can be minimized, then for a unit width of cut the fracture energy term naturally gives rise to a constant component of the cutting force viz., \( R \), the fracture toughness. However, when Atkins applied this theory to data in the literature, the values of \( R \) needed to get a good fit of data were different from that obtained using standard fracture tests. This model has been extended to oblique cutting in a more recent publication [Atkins (2006)](2006). In this recent work, an argument is made for why chip formation in machining cannot be just by plastic deformation. In plastic flow, elements of material that are neighbors before permanent deformation are the same neighbors after flow. In machining, this would mean that elements just above, and just below the parting line at the tool tip would still have to be neighbors afterward [Atkins (2006)](2006). That is, elements on the underside of the chip are still “joined”
to elements on the machined surface, however far away from one another they may have traveled. This is implausible and suggests that chip formation is more than just plastic flow.

Thus, only indirect evidences are presented for the presence of ductile fracture in metal cutting leading to chip formation. In addition to such indirect evidence, more direct evidence is needed to able to proceed with this new approach to machining modeling, particularly at small $t_o$ values.

### 2.3 Numerical Modeling of Cutting Process

The choice of finite element models for machining problems involves rigid-plastic or elastic-plastic models with the latter being more realistic; Eulerian or updated Lagrangian flow treatments (Fig. 2.22); structured or adaptive meshes; chip/work separation criterion needed or not needed; and coupling to thermal calculation models or not (with the latest computational models thermo-mechanical coupling is the preferred method). In the Eulerian approach the mesh is fixed in space and material flows through it, while in the Lagrangian approach the mesh flows with the material. It is common sense that a finer mesh is needed where problem variables vary strongly with position than where they do not. This poses no problem in the Eulerian approach where a choice is made where to refine the mesh and by how much. However for computational efficiency with a Lagrangian mesh, there is a

![Figure 2.22: Eulerian versus Lagrangian treatments (Childs et al. 2000)](image-url)
need to refine and then coarsen how the material is divided into elements as it flows into and out of plastic shear zones (Childs et al. 2000).

This problem is particularly acute near the cutting edge of a tool, where the work material flow splits into flow under the cutting edge and flow into the chip. For purposes of numerical modeling of chip formation, both material plastic flow around the tool edge (no material separation, highly adaptive meshing) and chip formation by material separation have been adopted in the literature. The latter involves some criterion for separating the chip from the base material, while the former avoids this criterion and regards chip formation as an indentation process with the tool acting as a punch.

The method of indentation leading to chip formation has been numerically modeled using mesh rezoning and dynamic remeshing techniques (Marusich and Ortiz 1995; Ozel and Altan 2000; Liu and Melkote 2006). There is no pre-defined parting line and therefore the shape of the chip is not pre-determined. There is also no node separation or element removal involved in this method. Instead, as the tool advances, nodes of the workpiece move on the tool surface and the elements may deform strongly close to the tool tip. The distorted mesh is replaced by remeshing the new deformed geometry at certain intervals and mapping solutions from the old mesh to the new mesh. The criterion for triggering remeshing is arbitrarily chosen. Such a model inherently ignores any material separation that may be involved in the chip formation process.

Numerical modeling by chip separation has been achieved using different types of chip separation techniques and they basically fall into two categories (Liu 2005). Node separation technique is geometry-based. A predefined parting line is used to separate the chip layer from the workpiece. At each point on the parting line, two nodes are tied together initially and share the same degrees of freedom. When the tool approaches the tied pair of nodes, the nodes separate when a pre-specified criterion
is met (Fig. 2.23). The commonly used criteria are the tool node distance of Zhang and Bagchi (1994), strain energy density by Lin and Lin (1992), and critical effective plastic strain by Strenkowski and Carroll (1985). For a detailed review of these methods see Huang and Black (1996). Element deletion technique of Tian and Shin (2004) is also a geometry-based technique in which the chip layer is predetermined by a sacrificial element layer. This sacrificial element layer is positioned at the bottom of the chip layer. When the tool approaches a sacrificial element, the element is deleted based on a criterion such as the critical effective plastic strain (Tian and Shin, 2004). There are two commonly stated criticisms of this modeling approach: one, it is not physically based since no evidence of material separation (as opposed to flow) in ductile metal cutting has been reported in literature, and two, introducing a pre-defined parting line or layer artificially constrains the chip formation process. In the current work, sufficient experimental evidence is presented to show that this modeling approach of using a sacrificial layer and element deletion is a valid one for ductile metals.
2.4 Use of Ductile Fracture Criteria in Machining

Experimental evidence of other phenomena relating to ductile fracture in metal cutting such as segmented or discontinuous chip formation and burr formation have been observed and reported. To capture these effects, several researchers have developed numerical models that incorporate ductile fracture criterion in metal cutting.

Iwata et al. (1984) have used an Eulerian formulation to model steady state metal cutting and have incorporated the Cockroft-Latham criterion to predict crack formation in the plastic deformation zone in the chip. Since the model is based on an Eulerian formulation no chip separation criterion is needed. Marusich and Ortiz (1995) have incorporated a ductile crack initiation and propagation criterion to study segmented chip formation. They have also used mesh rezoning and dynamic remeshing methods in their model. However, chip separation in front of the tool edge could be caused by either crack initiation and propagation or by dynamic remeshing. But, they do not report any cracks in front of the tool edge in their publication. Hence chip formation is merely by dynamic remeshing. Obikawa and Usui (1996) have used the critical strain method to study crack initiation and propagation. Crack propagation is assumed to occur in the direction of the maximum shear stress. However, the chip separation itself, in front of the tool edge, was modeled using a geometric node-release criterion and not using a ductile fracture model. It is noted that the critical strain-to-fracture (for crack propagation) is considered to be a function of the strain-rate, pressure, and temperature.

A similar approach has been used in Obikawa et al. (1997) where two types of node separation are specified: one, near the tool tip for chip separation, and the other for crack nucleation and growth in chip segmentation. Ng et al. (2002) have also used separate criteria for chip separation and for crack initiation and propagation in the chip. The chip separation is modeled by a critical damage criterion based on
analytical expressions derived from Oxley’s parallel-sided zone theory (for calculating the plastic strain, strain rate, pressure, and temperature). It is not mentioned how the so-called ‘damage’ strain itself is evaluated. The crack initiation and propagation in the chip is again modeled using a critical strain criterion with the ‘fracture’ strain calculated using the Johnson-Cook (J-C) damage equation. The difference between the ‘damage’ strain, $\epsilon_d$ and ‘fracture’ strain, $\epsilon_f$ is not distinguished in the paper nor is there an explanation for why two separate criteria were used.

More recently, Rhim and Oh (2006) claim that chip separation was simulated using the Cockroft-Latham criterion in a commercial software DEFORM®. However, the commercial software uses a Lagrangian formulation with mesh-rezoning and dynamic remeshing. So, again the actual chip separation from the base material can occur because of remeshing and not because of material failure ahead of the tool upon satisfying a fracture criterion.

A multi-material mixture theory based Eulerian contact formulation is developed by Benson (1997) and the model is illustrated by application to the metal cutting process and study of incipient chip formation. A J-C damage model is implemented to model contact separation. It is shown that without the use of the damage model no chip formation occurs in OFHC Copper; the material just builds up in front of the tool. Segmented chip formation in 4340 steel was also shown using this model. However, fully developed chip formation was studied, and neither experimental validation nor energy analysis was performed with the developed model.

Tian and Shin (2004) have used an accumulated damage model with a constant strain-to-fracture to delete elements in a sacrificial layer elements that then leads to chip formation. Typically, the strain-to-fracture is not constant and is a function of pressure, strain-rate and temperature. Guo and Yen (2004) have reported the use of the J-C damage model in an arbitrary Lagrangian-Eulerian (adaptive mesh) numerical model. Chip separation criterion is not mentioned the J-C damage model.
is used merely to study segmentation leading to discontinuous chips. Pantale et al. (2004) claim to have used the J-C damage model for chip separation without the use of a parting layer. It is not mentioned how this was implemented nor is there any evidence shown of actual material failure in the tool edge vicinity leading to chip formation.

It is thus clear that barring the work of Benson (1997) there seems to be no work that has modeled chip separation using a fully implemented ductile fracture model. Benson tested the application of the Eulerian mixture theory contact formulation he developed to machining and did not further analyze the model results, especially as they relate to energy consumed in material separation and its role on size effect in the specific cutting energy. Benson also did not experimentally validate the model.

2.5 Summary

It is clear from the literature survey that:

• There are many different reasons attributed to the size-effect observed in the specific cutting energy. Are all applicable? Is there a common framework in which to view all of these reasons?

• There is no direct evidence of ductile fracture ahead of the tool leading to chip formation. Barring few works, literature speculates that there is no such separation.

• There is very little focus in the literature on the role of the energy consumed in material separation in the scaling effect observed in the specific cutting energy.

• Numerical simulation methods for chip formation that use a node separation or element deletion are under criticism for not being physically-based. Also, use of fracture models appropriate to the physically observed phenomenon would make a stronger case for using such models.
The energy distribution in metal cutting with decreasing uncut chip thickness, and specifically the energy going into material separation has not been studied, particularly from a ductile fracture standpoint and at small uncut chip thickness levels.

The rest of this work attempts to address these questions.
CHAPTER III

COMMON FRAMEWORK AND CONSTANT FORCE COMPONENT

This chapter attempts to reconcile the several reasons attributed in the literature to the scaling in specific cutting energy by developing a framework from which to view the cutting force as composed of several components. Then, special experimental conditions are devised to isolate the constant cutting force component and compare this with a preliminary fracture mechanics model.

3.1 Common Framework

The specific cutting energy is a calculated parameter rather than an observed or measured one such as the cutting force. Observing, modeling, and interpreting the trends in the cutting force, a directly measured parameter, may give better insight into the phenomenon of size-effect. As mentioned previously, the scaling in \( u \) simply means that the cutting force did not decrease as much as expected with the decrease in the uncut chip thickness. This could mean either that some component of the cutting force remained constant or that certain component of the cutting force increased a little, thus causing the net force to not decrease as much.

Recall that in orthogonal cutting the specific cutting energy is simply the ratio of the cutting force \( F_c \) to the product of the uncut chip thickness \( t_o \) and cut width \( b \),

\[
u = \frac{F_c}{bt_o} \tag{3.1}\]

Note that the cutting force, \( F_c \), is the force component along the cutting speed direction. For a unit width of cut, this ratio can be considered as consisting of a numerator
(cutting force) and a denominator (uncut chip thickness), and will increase with decreasing denominator under three conditions:

1. the numerator increases as the denominator decreases

2. the numerator decreases at a lower rate than the denominator.

3. the numerator remains constant

Thus the cutting force can be viewed as the sum of three components:

$$F_c = F_{\text{const}} + F_{\text{inc}} + F_{\text{dec}}$$  \hspace{1cm} (3.2)

where $F_{\text{const}}$ is that part of the cutting force that does not change with $t_o$, $F_{\text{inc}}$ increases and $F_{\text{dec}}$ decreases with $t_o$. $F_{\text{const}}$ is that part of the cutting force that will always be needed to remove material irrespective of how much material is removed, and this could be related to parameters such as fracture toughness. $F_{\text{inc}}$ could be due to reported factors such as an increase in the strength of the material with decrease in uncut chip thickness, which in turn could be due to factors such as high strain rates, dependence of strength on strain gradient, or due to inhomogeneities in the material. $F_{\text{dec}}$ is the normal decrease in cutting force one would expect to see as the area of cut decreases with $t_o$. The next section explores how this framework reconciles the several reasons given in literature.

### 3.2 Reconciling Reasons Given in Literature

It can now be shown that all the reasons attributed to the scaling in $u$ given in literature fall under one of the two components $F_{\text{const}}$ or $F_{\text{inc}}$. The decreasing chance of encountering defects at low uncut chip thickness, as mentioned by Shaw (1950) leads to a higher strength of the material thus increasing the force needed to cut it. Thus this can be classified under $F_{\text{inc}}$. Similarly strain-rate increase at lower uncut chip thickness [Larsen-Basse and Oxley] (1973) increases the strength of the
material leading to an increase in force needed ($F_{inc}$). Also, as the uncut chip thickness decreases the temperature in the chip is not as high $[Kopalinsky\ and\ Oxley\ 1984]$, leading to a decrease in softening of the material thus indirectly increasing its strength ($F_{inc}$). Now, stress not only depends on strain at a point, but also depends on the distribution of the strain around it spatially or the strain gradient. Thus, gradients of strain in the chip can also affect the strength. This has been shown to be the case $[Dinesh\ et\ al.\ 2001\ Liu\ and\ Melkote\ 2006]$ thus contributing to increase in force needed ($F_{inc}$). The presence of a finite edge radius increases the plastic flow around the tool tip thus consuming more energy in the process; this extra energy is supplied by an increased cutting force ($F_{inc}$). Similarly, it has been shown by $Nakayama\ and\ Tamura\ 1968$ that the sub-surface plastic deformation is not proportional to the uncut chip thickness and the extra work done in this has to be supplied by the cutting force ($F_{inc}$). $Thomsen\ et\ al.\ 1953$ argue that there is always a pushing

Table 3.1: Classifying the reasons for $u$ from literature

<table>
<thead>
<tr>
<th>$F_{inc}$</th>
<th>$F_{const}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lack of defects</td>
<td>Tool pushing</td>
</tr>
<tr>
<td>Strain-rate</td>
<td>Fracture</td>
</tr>
<tr>
<td>Decrease in temperature</td>
<td></td>
</tr>
<tr>
<td>Strain Gradient</td>
<td></td>
</tr>
<tr>
<td>Edge radius ploughing</td>
<td></td>
</tr>
<tr>
<td>Sub-surface deformation</td>
<td></td>
</tr>
</tbody>
</table>

involved by the cutting tool similar to that of a punch shearing a sheet metal, thus attributing this constant force involved to the scaling effects ($F_{const}$). If fracture is the reason, as suggested by $Atkins\ 2003$, for material separation leading to chip formation then there is likely to be a constant effort needed for material separation and hence contributing to the constant force needed for cutting ($F_{const}$). Table 3.1 summarizes the reasons given in literature and classifies them accordingly to $F_{inc}$ or $F_{const}$.
Now, how can these components be identified, measured and analytical methods developed to understand them? Are there conditions (workpiece material, cutting parameters, and tool geometry) under which one or more of these components will dominate and can be measured and isolated? Extensive work has already been done in understanding the increasing component $F_{inc}$. Hence, this work focuses more on $F_{const}$, particularly as it relates to fracture, since this disputes the prevalent understanding of chip formation in machining. The following sections consider the first component, $F_{const}$, and attempt to isolate and model it.

### 3.3 $F_{const}$, Chip Formation and Fracture

Recall that $F_{const}$ is that component of the cutting force that is independent of the uncut chip thickness. In other words, regardless of how much material is removed, this component of force is always required. An obvious explanation of this could be the energy involved in creation of a new surface. The surface energy of a solid (analogous to surface tension in liquids) is the energy needed to separate the ions at the interface. However, it has been previously shown by [Shaw](#) (1997) that this energy and hence the force needed to supply it seems to be negligibly small. Another possible source for $F_{const}$ could be fracture energy involved in creating the chip and hence the newly machined surface to be formed. Recently, doubts have been raised about the validity of using the surface energy for new surface creation rather than the fracture energy [Atkins](#) (2003).

Machining involves chip formation, which is essentially separation of material. There seem to be different reasons in the literature on how chip formation occurs. Merchant’s theory assumes that the process is one of shear. Literature also reports chip formation as an indentation process [Madhavan et al.](#) (2000), while some consider it as a result of fracture just ahead of the tool [Atkins](#) (1974, 2003). Some researchers have shown the presence of cracks in machining ductile metals [Komanduri](#).
and Brown (1972) although it is not clear if material separation and chip formation resulted from these cracks. Also, Shaw (1997) has acknowledged that there has to be some form of localized fracture in chip formation leading to material separation but presented no direct evidence. It has also been reported by Shaw (1997) that the region just in front of the tool is under high compressive stresses from the movement of the tool. Under these conditions, it is likely that any crack in front of the tool, if present, will be closed and cannot be observed in a micrograph (Atkins, 1974). Among the proponents of fracture in machining is Atkins (1974) who suggests that the machining process adjusts itself to simultaneous actions of cracking (Fig. 3.1) and plastic flow with friction playing a superimposed role. In recent works (Atkins, 2003, 2006), Atkins has argued the applicability of modern ductile fracture mechanics in modeling the cutting process. In this work, he cites the work associated with the chip separation criterion in Finite Element Method (FEM) simulations to be orders of magnitude different from that associated with surface energy or surface tension, and comparable to fracture toughness values. The work done in cutting is expended in (a) plasticity along the shear plane, (b) friction at the tool-chip interface, and (c) formation of new surfaces. This results in the following equation (Atkins, 2003)

\[ F_c V = (\tau_y \gamma) (t_o b V) + [F_c \sec (\beta - \alpha) \sin \beta] \frac{V \sin \phi}{\cos \phi - \alpha} + RbV \]  

(3.3)

where the last term represents the energy involved in the formation of the new surface.
\( R \) is the specific work of surface formation (fracture toughness), \( b \), the width of cut, and \( V \), the cutting speed. From this it can be seen that if the other components of the energy can be ignored, then for a unit width of cut the fracture energy term naturally gives rise to a constant component of the cutting force viz., \( R \). However, when Atkins applied this theory to data in the literature, the values of \( R \) needed to get a good fit of data were different from those obtained using standard fracture tests (Atkins, 2003). If appropriate machining conditions can be devised under which the energies expended in shear and friction are minimal, then this constant cutting force can be isolated, observed and modeled using fracture mechanics based approach.

### 3.4 Isolating \( F_{\text{const}} \)

It would be appropriate to recall comments of Drucker (1949) here that metal cutting is not cutting in the usual sense, but is instead removal by brute force. There are large stresses and deformations in the chip, while in true cutting, bonds are severed in a small region of the material taken away, and little energy is needed. Consider an analogy of peeling an adhesive tape off a surface (Fig. 3.2). While peeling the tape

![Peeling tape analogy - peeled tape has not been unduly deformed](image)

**Figure 3.2:** Peeling tape analogy - peeled tape has not been unduly deformed

just enough work is done to separate the tape from the surface. No additional work is performed to unduly deform, twist or squeeze the tape during or after peeling it off. In contrast, machining involves not only removing the layer of material but also shear straining it to a large extent. Thus, the chip formed is strain hardened and
has different properties than the workpiece material. The energy to strain the chip is wasted energy and is a natural consequence of the geometry of the tool as explained next.

It is well known that the deformation occurring in the primary zone or the shear plane contributes significantly to the energy needed in the cutting process. It is also known that the rake angle of the tool plays an important role in determining the amount of this deformation. The main purpose of the rake angle is to provide structural integrity to the cutting tool. This is the reason why hard materials such as in hard turning are cut with PCBN tools with negative rake angles. The higher this rake angle, the smaller will be the primary zone/chip deformation as can be seen qualitatively in Fig. 3.3 Quantitatively, the higher this rake angle, the smaller will be the shear strain in the primary zone/chip deformation as can be seen in the plot (Fig 3.4) of shear strain versus rake angle. As the rake angle is increased there is less shearing and more cutting that takes place. In the ideal case of a razor thin cutting edge (90° rake angle), pure slicing will take place and one can expect only $F_{\text{const}}$ to be present, i.e. the force can be expected to settle at a constant value. Thus, under conditions of high rake angles one can expect to isolate the constant cutting force component $F_{\text{const}}$. Under high rake angles the chip formation or material separation

![Figure 3.3](image-url)
Figure 3.4: Shear strain decreases with rake angle (steel) (Shaw, 1997)
is expected to be similar to the peeling tape analogy mentioned earlier (Fig. 3.2).

Literature contains some supporting data that show force and specific cutting
ergy trends at higher rake angles. Bitans and Brown (1965) report orthogonal
cutting of wax at higher positive rake angles of up to 80°. However the mechanical
properties and behavior of wax is vastly different from ductile metals. The highest
rake angle reported in cutting of metals is 50° (Zorev, 1966). Variation in cutting
force with rake angle in cutting steel (Shaw, 1997) is shown in Fig. 3.5. It can be
seen that at higher rake angles the decrease in cutting force with uncut chip thickness
is less than that at smaller rake angles. Similar cutting force trends are evident in
data given by Zorev (1966) (Fig. 3.6). Thus, the literature supports enough trends
to warrant further investigation of even higher rake angle tests to detect and confirm
the presence of the proposed constant cutting force component.

3.5 High Rake Angle Cutting Procedure and Results

While cutting a ductile metal at high rake angles, the tool tip is extremely weak and
is susceptible to bending and edge chipping. The challenge at high rake angles is to
perform cutting carefully without breaking or bending the edge of the tool. To this
end, several workpiece materials, cutting conditions, and several tool edge geometries were investigated. Simple orthogonal tube cutting (Fig. 3.7) operation was performed on a Hardinge T42SP super precision lathe.

The cutting tools were made of M2 grade high speed steel (HSS) blanks. The necessary geometry of the edge was carved out of the HSS blank using the wire-EDM process. A Brother HS-3100 wire-EDM machine was used for this purpose. The tool has a fixed clearance angle of 5°. Lower clearance angles resulted in poor cutting action on the ductile metals examined.
After several iterations on workpiece material, oxygen free high conductivity (OFHC) copper was chosen. OFHC copper is almost pure copper, is very ductile, has documented properties of fracture toughness [Nyilas et al. 2002], and is a relatively simple material for future analytical modeling. The outer diameter and wall thickness of the OFHC Cu tube are 38.1 mm and 1.1 mm respectively. Among other materials tried were brass and aluminum alloy Al-2011. A quartz 3-component Kistler dynamometer (Type 9257B) was used to measure the cutting forces.

Initial investigations were conducted with up-sharp tools (edge as created using wire-EDM cutting). Table 3.2 summarizes the experimental conditions. The results of initial tests with upsharp tools and depths of cut (DOC) ranging from 10 – 100µm are shown in Fig. 3.8. As can be seen from this figure the trend in cutting force with decreasing rake angle is similar to what has been reported in literature.

At rake angles $\geq 50^\circ$ severe edge chipping of the tool occurred even at very low depths of cut. The chipped edge was studied under a microscope (Fig. 3.9) to determine an appropriate edge geometry to strengthen the tool. After several iterations it was determined that a chamfer edge geometry of $20^\circ \times 25\mu m$ would be
Table 3.2: Experimental Conditions

<table>
<thead>
<tr>
<th></th>
<th>Initial</th>
<th>Final</th>
</tr>
</thead>
<tbody>
<tr>
<td>Workpiece</td>
<td>OFHC Cu</td>
<td>OFHC Cu</td>
</tr>
<tr>
<td>Tool</td>
<td>HSS M2 grade</td>
<td>HSS M2 grade</td>
</tr>
<tr>
<td>rake angles</td>
<td>20° – 50°</td>
<td>30 – 60°</td>
</tr>
<tr>
<td>Cutting speed</td>
<td>23.9 m/min</td>
<td>1.2 m/min</td>
</tr>
<tr>
<td>DOC</td>
<td>10-100 µm</td>
<td>75-200 µm</td>
</tr>
<tr>
<td>Cut width</td>
<td>1.1 mm</td>
<td>1.1 mm</td>
</tr>
<tr>
<td>Edge rad</td>
<td>Up-sharp</td>
<td>20° × 25µm chamfer</td>
</tr>
</tbody>
</table>

Figure 3.8: Preliminary results - cutting force versus depth of cut under initial cutting conditions

Figure 3.9: Rake face view of chipped edge under a microscope
the best. Another parameter of interest that determines the stiffness of the tool at such high rake angles is the length \( L \) of the rake face (Fig. 3.10). After several iterations of testing for repeatability, the length \( L \) of the rake face was fixed at 0.5 mm. With this new edge geometry of 25\( \mu \)m chamfer the range of the uncut chip thickness had to be revised since depths of cut below 25\( \mu \)m will result in cutting action with a different rake angle. Hence, the lowest uncut chip thickness must be higher than this chamfer to avoid the chamfer effects. The new range of uncut chip thickness was determined to be 75 – 200\( \mu \)m. Note that the lower end of the uncut chip thickness range is three times the chamfer height.

Also, a low cutting speed had to be used for the following reason. In typical orthogonal tube cutting, the tool is fed towards the tube at a feed equal to the desired uncut chip thickness. The tool is typically fed starting at a certain distance away from the rotating tube. The initial engagement of the tool into the workpiece causes severe stresses on the tool. In preliminary testing, these stresses caused the tool edge to break despite having the edge geometry discussed above to strengthen it. To minimize the effect of this the spindle speed was reduced to 10 RPM.

At this reduced spindle speed, a feed of 75\( \mu \)m per revolution generates a very low feed speed (10rpm \( \times \) 75\( \mu \)m/rev = 0.75mm/min), which is beyond the range of conventional machines. As a result, a new method of cutting was devised as follows.

![Figure 3.10: Tool geometry](image-url)
A chip was first initiated with a low rake angle tool. This low rake tool was then withdrawn and the high rake angle tool brought into cutting position. Only then was the spindle turned on without feeding the tool axially to continue the cutting action for just one revolution of the spindle with the high rake angle tool. Consequently, chip formation occurs only for one spindle revolution during which force data was collected. This procedure is illustrated in Fig. 3.11. Following such a procedure, chips were initiated with a 45° rake tool at a cutting speed of 1.2 m/min. To maintain uniformity in experimentation, even the low rake angle cutting was performed similarly with chip initiation, pause in cutting, then cutting continued for one complete spindle revolution.

![Figure 3.11: Steps involved in cutting at high rake angles](image)

A sample force signal is shown in Fig. 3.12. The force signal has reached steady state well within the time interval of data collection. It is noted that chip formation (material separation) was observed even at this high rake angle.

To confirm that shear in the chip is indeed lower at 70° than at 30°, cross-sections of the chip-workpiece at the two rake angles were taken and observed in an SEM without any polishing and in the optical microscope after polishing and etching. The
results shown in Fig. 3.13 confirm that the shear in the chip is indeed smaller at a rake angle of 70°.

![Sample force signal](image)

Figure 3.12: Sample force signal

The complete results of the set of experiments described earlier are shown in Fig. 3.14.

It can be inferred from this figure that the force trend towards a constant value continues at high rake angles. The cutting force trend for 60° rake exhibits a lower slope than that at 50° rake and substantially lower than that at 30° rake. Tests
Figure 3.14: Cutting force versus uncut chip thickness under revised cutting conditions

were repeated at 60° and 70° since the cutting tool is very weak at these rake angles and is susceptible to bending leading to a possibility of erroneous trends. The one-sigma error bars are also shown in this figure. The plot of slopes of the cutting force with uncut chip thickness at different rake angles is shown in Fig. 3.15. Note that this data set does not have repeatability tests at 30, 40, and 50 degree rake angles. The quadratic fit shown in the figure indicates that the slope goes to zero at about 75°, much before than at the anticipated value of 90°. This could be due to lack of repeatability data at the lower rake angles. A plot of the cutting force data as a

Figure 3.15: Slope versus uncut chip thickness with 95% confidence band
function of rake angle for various $t_o$ values is shown in Fig. 3.16. Similar conclusions can be drawn from this figure.

**Figure 3.16:** Cutting force versus rake angle plot

Efforts to cut at even higher rake angles (e.g. $80^\circ$) to further confirm this trend have been unsuccessful to date due to lack of adequate strength of the cutting edge. However, the cutting force trends shown indicate that the force approaches a constant value. Based on the foregoing arguments for the origin of $F_{\text{const}}$, the next section considers a dislocation based fracture mechanics approach to analytically model the constant force component.

### 3.6 Modeling $F_{\text{const}}$ Using Dislocation Fracture Mechanics

At high rake angles the chip undergoes minimal deformation. Under these conditions chip formation is essentially a phenomenon of localized ductile fracture leading to material separation and chip formation.

For a preliminary analysis one can assume the following:

- Linear elastic fracture mechanics can be applied
• Any plastic yielding zone near the tool tip is small enough to be negligible
• Quasi-static crack propagation occurs

Note that the above assumptions involve very rough approximations of the process. Plastic yielding conditions can be severe and span large areas in the chip. Only under extremely slow cutting conditions one can ignore dynamic crack propagation. The following section describes a model using the above assumptions.

As one of the most important types of defects in crystalline solids, dislocations have played an important role in understanding fracture \cite{Liebowitz1968}. It is well known that the crack tip has high plastic deformation associated with it. From a dislocation point of view, the region just ahead of the crack tip has a large pile-up of dislocations. The presence of a dislocation causes a stress field to be setup in and around it, which can be calculated from dislocation mechanics. The sum of stress fields due to the large number of dislocations causes the stress to rise near the crack tip \cite{Weertman1996}. Dislocation based fracture mechanics considers a distribution of dislocations along the length of the crack. The problem then is one of finding the interaction of the dislocations taking into account the effect of the externally applied stress which causes the crack to grow.

Figure \ref{fig:crackmodes} shows two of the three possible crack modes. Mode I is a tension based crack opening mode, while Mode II is a shear based crack propagation mode. Assuming that there is a crack in front of the tool, this crack can grow because of both Modes I and II. Mode I growth occurs because of the geometry of the tool as the tool continuously moves into the crack opening. Mode II growth can occur because of the pushing action of the tool causing shearing to occur parallel to the crack length. Mode III cracking, where the crack is sheared perpendicular to its length, will not occur in simple orthogonal cutting.

The dislocation crack extension force is the net force exerted on all the dislocations and in the direction of crack growth. This force is found using the distribution of
Figure 3.17: Possible crack modes

dislocations and considering their interactions. The distribution of dislocations is found from the condition that the crack faces should be traction free [Weertman 1996]. The distribution of dislocations, $B(x)$ is given as [Weertman 1996],

$$B(x) = K^2 \frac{1 - \nu}{G} \frac{x}{\sqrt{a^2 - x^2}}$$

where, $K$ is the stress intensity factor, $G$ the shear modulus, $a$ the crack length, $x$ the position along the crack, and $\nu$ the Poisson’s ratio. This equation is integrated between $x = 0$ and $x = a$ to obtain the magnitude of the net Burgers vector $b_T$.

The product of the net Burgers vector and the applied stress, gives the total crack extension force $F_E$ (per unit width of crack) exerted on all the dislocations and is given as,

$$F_E = b_T \sigma = \frac{2(1 - \nu)\sigma^2 a}{G} = \frac{2(1 - \nu)K^2}{\pi G}$$

where $\sigma$ is the externally applied stress. In the case of a crack in front of the cutting tool, the crack is one-sided and there is no negative set of dislocations to exert their influence. Depending on which crack mode operates, $K = K_I$ or $K = K_{II}$. If both
Table 3.3: OFHC Copper properties

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_{Ic}$</td>
<td>56 MPa√m</td>
</tr>
<tr>
<td>$G$</td>
<td>44 GPa</td>
</tr>
<tr>
<td>$\nu$</td>
<td>0.3</td>
</tr>
</tbody>
</table>

modes are present then the force is the sum of two terms and is given as,

$$F_E = \frac{2(1 - \nu)K_I^2}{\pi G} + \frac{2(1 - \nu)K_{II}^2}{\pi G}$$

(3.6)

In steady state cutting at slow cutting conditions, it is assumed that crack growth occurs under steady state as well. Thus, assuming quasi-static crack propagation conditions, as a first approximation (Kanninen and Popelar, 1985), the stress intensity factor is equal to the critical stress intensity value, $K_{Ic}$ (fracture toughness). Some of the relevant properties of OFHC Cu are listed in Table 3.3. Typical values for $K_{IIc}$ are not available in the literature for OFHC Cu. Hence, in this analysis, only Mode I cracking is considered in machining crack growth. Based on the above mentioned theory of dislocation based fracture mechanics, the force necessary to extend the crack, which acts parallel to the crack in the direction of propagation, is the proposed constant cutting force $F_{const}$ and is given as,

$$F_{const} = F_E b = \frac{2(1 - 0.3)56^2}{\pi 44}1.1 \approx 35.0 \text{ N}$$

(3.7)

where $b$ is the width of cut. The cutting forces for different rake angles from the experiments mentioned earlier have been replotted in Fig 3.18 along with the analytical model result based on crack extension force. It can be seen that the analytical model result is of the same order of magnitude as what maybe expected by extrapolating the experimental data to 90° rake angle. There are several reasons why there could be a discrepancy between the analytical model and experimental extrapolation:

1. Mode II crack and its contribution has not been included.
Figure 3.18: Comparison of experimental and analytical results

2. Friction between the chip and the tool has not been eliminated and some energy is spent in overcoming this.

3.7 Further Confirmation at Lower Rake Angle and Lower Uncut Chip Thickness

The analysis shown above confirms the presence of the constant force component at higher rake angles as considered in the Atkins model. The uncut chip thickness values had to be higher because of the presence of the chamfer on the tool. In order to test the applicability of the model at lower rake angles and at lower uncut chip thickness a further set of tests was performed and compared with the model.

3.7.1 Experimental Conditions

A simple orthogonal tube cutting operation was performed again on the Hardinge T42SP super precision lathe. Wedge shaped cutting tools were made from M2 grade high speed steel (HSS) blanks. The necessary geometry of the edge was carved out of the HSS blank using the wire-EDM process. The rake angle was fixed at 30° and the clearance angle at 5° (Fig. 3.19). Lower clearance angles resulted in poor cutting action on the ductile metal examined. The workpiece material again was oxygen free.
high conductivity (OFHC) copper. The outer diameter of OFHC Cu tube was 38.1 mm and 1.1 mm thick, as before. A quartz 3-component Kistler dynamometer (Type 9257B) was used to measure the cutting forces, as before. Table 3.4 summarizes the experimental conditions. The tool edge radius was measured using an SEM (Fig. 3.20) and determined to be $\sim 7\mu m$. Note that the smallest uncut chip thickness is twice the edge radius, thus minimizing edge radius effects on the measured forces. The chip thickness was measured at three different places along the length of the chip using a micrometer with a least count of 2.5$\mu m$. All experiments were repeated three times.

### 3.7.2 Force and chip thickness results

The experimental results are shown in Table 3.5. The table gives the average values of the cutting force ($F_c$) and its standard deviation $SD_{F_c}$, thrust force ($F_t$) and its
standard deviation $SD_{F_t}$, and chip thickness ($t_c$) and its standard deviation $SD_{t_c}$. Also shown is the calculated specific cutting energy $U$ along with its standard deviation $SD_U$, and the coefficient of friction calculated from the measured forces. The size-effect can be clearly seen in the specific cutting energy (also see Fig. 3.23). The coefficient of friction is also seen to increase as $t_o$ decreases.

Table 3.5: Experimental Results

<table>
<thead>
<tr>
<th>$t_o$ ($\mu$m)</th>
<th>$F_c$ (N)</th>
<th>$SD_{F_c}$ (N)</th>
<th>$F_t$ (N)</th>
<th>$SD_{F_t}$ (N)</th>
<th>$t_c$ (mm)</th>
<th>$SD_{t_c}$ (mm)</th>
<th>$u$ (MPa)</th>
<th>$SD_u$ (MPa)</th>
<th>$\mu$</th>
</tr>
</thead>
<tbody>
<tr>
<td>15</td>
<td>21.06</td>
<td>1.98</td>
<td>13.06</td>
<td>1.37</td>
<td>0.043</td>
<td>0.006</td>
<td>1207.04</td>
<td>119.82</td>
<td>1.08</td>
</tr>
<tr>
<td>25</td>
<td>24.99</td>
<td>2.91</td>
<td>14.33</td>
<td>2.36</td>
<td>0.057</td>
<td>0.006</td>
<td>854.32</td>
<td>105.78</td>
<td>1.04</td>
</tr>
<tr>
<td>35</td>
<td>30.13</td>
<td>2.28</td>
<td>16.01</td>
<td>2.31</td>
<td>0.066</td>
<td>0.006</td>
<td>753.17</td>
<td>59.21</td>
<td>1.01</td>
</tr>
<tr>
<td>50</td>
<td>37.8</td>
<td>0.63</td>
<td>16.87</td>
<td>0.31</td>
<td>0.086</td>
<td>0.004</td>
<td>680.72</td>
<td>11.44</td>
<td>0.94</td>
</tr>
<tr>
<td>60</td>
<td>41.63</td>
<td>0.92</td>
<td>16.58</td>
<td>0.75</td>
<td>0.099</td>
<td>0.004</td>
<td>635.45</td>
<td>13.91</td>
<td>0.90</td>
</tr>
<tr>
<td>70</td>
<td>48.04</td>
<td>0.41</td>
<td>18.26</td>
<td>0.58</td>
<td>0.119</td>
<td>0.007</td>
<td>623.14</td>
<td>5.34</td>
<td>0.89</td>
</tr>
</tbody>
</table>
The remaining sections of this chapter explore the Atkins model of machining, which as seen before inherently incorporates the constant force component, and evaluate its applicability to the experimental data obtained. The model is first explained in detail in the following section.

### 3.7.3 Atkins Model of Machining

As mentioned earlier the Atkins machining model explicitly accounts for energies expended in shear, friction and material separation. In order to do this, Atkins (2003) has modified the single shear plane model with Coulombic friction developed by Merchant (1945). Merchant assumes that the work of surface creation is negligible and the energy balance in machining can be obtained from the force equilibrium of tool and chip. Atkins assumes that the work of surface work is significant during steady deformation and obtains his energy not from force equilibrium considerations, although force equilibrium is assumed to be valid. The resulting energy equation is repeated here:

\[
F_c V = (\tau_y \gamma)(t_o b V) + [F_c \sec (\beta - \alpha) \sin \beta] \frac{V \sin \phi}{\cos (\phi - \alpha)} + RbV \tag{3.8}
\]

Each term in this equation corresponds to energy expended in shear, friction, and surface creation. The energy terms are also illustrated in Fig. 3.21.

The shear strain, \(\gamma\), is calculated as \(\cot \phi + \tan(\phi - \alpha)\) in both Atkins’ and Merchant’s model. Using this and some trigonometric manipulation, Equation 3.8 can be rewritten as,

\[
\frac{F_c}{\omega \tau_y t_o} = \frac{\cos(\beta - \alpha)}{\sin \phi \cos(\phi + \beta - \alpha)} + \left[1 + \frac{R \cos(\alpha - \phi) \sin \phi}{\tau_y t_o \cos \alpha}\right] \tag{3.9}
\]

When the second term in the square bracket is zero the energy balance matches that of Merchant’s model. While, the experimental shear angle \(\phi_E\) is calculated from the measured chip thickness value as,

\[
\phi_E = \tan^{-1} \left(\frac{t_o \cos \alpha}{t_c - t_o \sin \alpha}\right) \tag{3.10}
\]
Figure 3.21: Distribution of energy in cutting

and the shear angle $\phi_M$, according to Merchant’s model (obtained by minimizing only the first two terms of Eqn. 3.3), is calculated as,

$$\phi_M = \frac{\pi}{4} - \frac{1}{2}(\beta - \alpha)$$ \hspace{1cm} (3.11)

the shear angle, according to Atkins’ model, is calculated by numerically solving the following equation obtained by minimizing the cutting force [Atkins 2003]:

$$
\left[ 1 - \frac{\sin \beta \sin \phi}{\cos(\beta - \alpha) \cos(\phi - \alpha)} \right] \left[ \frac{1}{\cos^2(\phi - \alpha)} - \frac{1}{\sin^2 \phi} \right] = \left[ \cot \phi + \tan(\phi - \alpha) + Z \right] \left[ \frac{-\sin \beta}{\cos(\beta - \alpha)} \left( \frac{\cos \phi}{\cos(\phi - \alpha)} + \frac{\sin \phi \sin(\phi - \alpha)}{\cos^2(\phi - \alpha)} \right) \right]$$

where $Z = R/\tau_y t_o$. The resulting $\phi$ is seen to depend on the material property $R$, the fracture toughness, via the parameter $Z$. The friction angle $\beta$, in both Merchant’s model and Atkins’ model, is calculated from the measured values of the cutting force $F_c$ and thrust force $F_t$ in an identical manner as follows.

$$\beta = \tan^{-1} \left( \frac{F_t}{F_c} \right) + \alpha$$ \hspace{1cm} (3.13)
Atkins (2003) has provided an elaborate procedure for calculating $R$ and $\tau_y$ to fit the machining model to experimental data (see Fig. 3.22). This method is an iterative procedure of guessing values of $R$ and $\tau_y$ until a proper match to the experimental force curve (intercept and slope) is obtained.

### 3.7.4 Fitting Atkins Model

Atkins machining model explicitly accounts for energies expended in shear, friction and material separation as shown in Eq. 3.3. The procedure given by Atkins
is little different from a regression fit, a least square fit method is adopted in this work. With $R$, and $\tau_y$ as parameters, the model is fitted to the data using least squares minimization of the measured and modeled cutting forces. The resulting fit is shown in Figure 3.23. This fit was obtained for $R = 5.4 \times 10^3$ J/m$^2$ and $\tau_y = 148.0$

![Figure 3.23](image)

**Figure 3.23:** Measured and fitted values of specific cutting energy

MPa. The values of specific energy in shear, friction and material separation (calculated from Atkins model, Eqn. 3.3) are given in Table 3.6. It can be seen that while

<table>
<thead>
<tr>
<th>$t_o$ (µm)</th>
<th>$U$ (GJ/m$^3$)</th>
<th>$U_{\text{shear}}$ (GJ/m$^3$)</th>
<th>$U_{\text{friction}}$ (GJ/m$^3$)</th>
<th>$U_{\text{separation}}$ (GJ/m$^3$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>15</td>
<td>1157.73</td>
<td>373.82</td>
<td>0.47</td>
<td>0.36</td>
</tr>
<tr>
<td>25</td>
<td>905.80</td>
<td>323.25</td>
<td>0.36</td>
<td>0.22</td>
</tr>
<tr>
<td>35</td>
<td>806.83</td>
<td>297.66</td>
<td>0.33</td>
<td>0.15</td>
</tr>
<tr>
<td>50</td>
<td>681.30</td>
<td>267.93</td>
<td>0.29</td>
<td>0.11</td>
</tr>
<tr>
<td>60</td>
<td>623.25</td>
<td>254.74</td>
<td>0.27</td>
<td>0.09</td>
</tr>
<tr>
<td>70</td>
<td>580.95</td>
<td>249.27</td>
<td>0.27</td>
<td>0.08</td>
</tr>
</tbody>
</table>

the specific energy of shear is highest, the specific energy in material separation is comparable to that of friction particularly at small values of $t_o$. Hence, neglecting
this component of energy can cause substantial error in the machining model when applied to micro-cutting.

The shear angle values calculated from the Atkins model (Eq. 3.12) were then compared to those obtained by measurements of the chip thickness $t_c$ (Eq. 3.10), along with those obtained by Merchant’s model (Eq. 3.11). The graph comparing this comparison is shown in Figure 3.24. It can be seen that the Atkins model predicts the trends in the shear angle better than the Merchant model. The utility of the Atkins model in predicting shear angle is more apparent when comparing shear angle predictions in different work materials since material dependence is captured via the fracture toughness term $R$, while Merchant’s model contains no material dependence in the shear angle expression. The rest of the paper compares the fitted values of $R$ and $\tau_y$ to that available in the literature for OFHC Copper.

**Figure 3.24**: Shear angle ($\phi$) variation with uncut chip thickness - experimental, Merchant model and Atkins model
Assuming a Dugdale type of cohesive zone model for the fracture zone ahead of the tool, the fracture toughness \( R \) can be estimated as,

\[
R = \frac{1}{2} \frac{K_c^2 (1 - \nu^2)}{E}
\]

The factor of two in the denominator comes in because only one-half of the crack is considered. Using values of \( K_c = 56 \text{ MPa}\sqrt{m} \) \cite{Nyilas2002}, \( E = 124 \text{ GPa} \) and \( \nu = 0.34 \) \cite{Johnson1985b}, the value of fracture toughness, for OFHC Copper, can be determined as \( R = 11.2 \times 10^2 \text{ J/m}^2 \). The fitted value of \( R = 5.4 \times 10^2 \text{ J/m}^2 \) and the fracture toughness value are of the same order of magnitude.

The yield stress in shear for OFHC copper can be determined from studying its flow stress curves available in the literature \cite{Johnson1985b}. The flow stress is plotted for different strain rates in Figure 3.25. It is seen from this figure that the flow stress approaches a constant value at higher strains. At strains above
0.5 the material yields without much change in stress, which is constant at about 425 MPa. Also, as shown by the stress-strain curves, the flow stress for OFHC Cu exhibits only a weak dependence on the strain rate at high strains. Hence, at low or high strain-rate the yield stress is relatively unchanged. Using the Tresca yield criterion, the yield stress in shear is then half this value of 212.5 MPa. This is of the same order of magnitude as the fitted value of $\tau_y = 148.0$ MPa. Note that here only an order-of-magnitude comparison is made. The complex nature of stress ahead of the tool may not permit a simple fracture model as assumed. $R$ may not necessarily be constant but vary with strain, strain-rate and temperature. The error can be reduced by introducing more complex fracture models.

The $R$ values obtained by fitting the model to experimental data are average values at best. It is expected that $R$ will change at lower uncut chip thickness values. The reason for this is as follows. In machining the ductile fracture ahead of the tool is very close to the free surface of the workpiece. At very low uncut chip thickness values, the plastic zone ahead of the crack tip will start to interact with the free surface and the fracture toughness is then expected to change.

It is thus seen that the fitted values of the two parameters, fracture toughness and shear yield stress, compare well on an order of magnitude basis. At low cutting speeds where strain-rates and temperature effects are small, the Atkins model of machining appears to explain the trends in machining data and phenomenon fairly well.

### 3.8 Summary

In this chapter a framework was developed to view the scaling effects from a view point of cutting forces. It was pointed out that scaling effect was occurring since cutting forces were not decreasing as expected with decrease in uncut chip thickness. The forces were viewed as consisting of a constant component, an increasing component, and a decreasing component. The different reasons given in literature could then
be attributed to these components. Extensive work has already been reported in analyzing the increasing force component with very little work on the constant force component. Hence, attention was focused more on the constant force component. High rake angle experiments were conducted, where the shear energy and friction energy terms were minimized, thereby facilitating the observation and measurement of the constant force component. The measured values were seen to be within orders of magnitude of the crack extension force modeled using dislocation fracture mechanics. Further tests conducted at lower rake angle and lower uncut chip thickness were used to further evaluate the Atkins model. At the lower cutting speed, the Atkins model was seen to fit fairly well with experimental data.

The above experiments and modeling comparison with dislocation fracture mechanics, in addition to the indirect evidence given by Atkins (2003), provide stronger proof of the possibility of fracture leading to material separation and chip formation in ductile metal cutting. Conclusive evidence of such ductile tearing at the chip root can irrefutably prove this. The following chapter presents such evidence.
CHAPTER IV

EVIDENCE OF DUCTILE TEARING

The previous chapter established the presence of a constant force component at high rake angles, thus providing additional evidence that material separation by fracture could be the cause of chip formation. This chapter describes the investigations made to detect the direct presence of ductile fracture ahead of the cutting tool. Instead of viewing the root of the chip in the traditional way of taking a cross-section, polishing it, and etching it, the chip-root is observed obliquely without any polishing in a scanning electron microscope (SEM). Evidence of ductile tearing at the interface between the chip and finished workpiece surface is shown.

4.1 Experimental Setup

4.1.1 Workpiece and Tool Materials

Tests were conducted on two workpiece materials. In addition to the OFHC Copper described in Chapter 3, an additional ductile material was chosen in order to establish that any ductile fracture occurring is not restricted to just one material. An aluminum alloy, Al-2024 with a T3 temper was chosen. The composition and mechanical properties are listed in Table 4.1 and Table 4.2 respectively.

The cutting tool was made of high speed steel square (7.94 mm; 0.3125 in) blanks. Details on the geometry of the cutting tool and the method is preparation are given in the following sections.
Table 4.1: Nominal composition of Al-2024 and OFHC Cu (wt. %)

<table>
<thead>
<tr>
<th></th>
<th>Al-2024</th>
<th>OFHC-Cu</th>
</tr>
</thead>
<tbody>
<tr>
<td>Al</td>
<td>93.5%</td>
<td>Cu 99.99%</td>
</tr>
<tr>
<td>Cr</td>
<td>0.1% max</td>
<td>Pb 0.001%</td>
</tr>
<tr>
<td>Cu</td>
<td>3.8-4.9%</td>
<td>Zn 0.0001%</td>
</tr>
<tr>
<td>Fe</td>
<td>0.5 max</td>
<td>P 0.0003%</td>
</tr>
<tr>
<td>Mg</td>
<td>1.2-1.8%</td>
<td></td>
</tr>
<tr>
<td>Mn</td>
<td>0.3-0.9%</td>
<td></td>
</tr>
<tr>
<td>Si</td>
<td>0.5% max</td>
<td></td>
</tr>
<tr>
<td>Ti</td>
<td>0.15% max</td>
<td></td>
</tr>
<tr>
<td>Zn</td>
<td>0.25% max</td>
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</table>

Table 4.2: Some Mechanical Properties of Al-2024 and OFHC Cu

<table>
<thead>
<tr>
<th></th>
<th>Al-2024</th>
<th>OFHC Cu</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tensile strength</td>
<td>407.5 MPa</td>
<td>483 MPa</td>
</tr>
<tr>
<td>Yield strength</td>
<td>399 MPa</td>
<td>345 MPa</td>
</tr>
<tr>
<td>Elongation (in 2 in.)</td>
<td>7.0%</td>
<td>18%</td>
</tr>
<tr>
<td>Hardness</td>
<td>63-65 Rockwell T</td>
<td>120 BHN</td>
</tr>
</tbody>
</table>

4.1.2 Set-Up for Chip-Root Observation

Observing a partially formed chip reveals details concerning the cutting process. Traditionally such an observation of the chip has been done in the following manner (Shaw, 1997). During the course of the cutting the tool is brought to a sudden stop. Then the tool is removed leaving the partially formed chip attached to the workpiece. The section of the metal in the vicinity of the partially formed chip is cut from the workpiece and mounted in an epoxy-polymer base for convenient handling. The mounted chip is then ground and polished to produce a very smooth surface. This surface is subsequently etched using chemical solutions (such as 2% Nital for steels) that are likely to bring out the structure of the workpiece material and then photographed in an optical microscope. This procedure is illustrated in Fig. 4.1. It is from such micrographs that researchers have previously declared that no cracks were
observed and hence there is no ductile fracture leading to material separation and chip formation in machining of ductile metals.

A different method is adopted here to observe the chip-root. After quick-stopping action to freeze the chip, the chip roots were sectioned (Fig. 4.2) out of the workpiece tube and made suitable in size for viewing in a scanning electron microscope (SEM) using the wire EDM process. The chip root was mounted in the SEM in an orientation suitable for viewing the chip-workpiece interface directly (Fig. 4.2). The sectioned chip is mounted on a conducting tape and then onto a sample holder as illustrated in Fig. 4.3. The position of the sample holder inside the SEM chamber is shown in Fig. 4.4.

The scanning electron microscope is used in lieu of the optical microscope for the
The rest of this chapter discusses results obtained from three sets of experiments.
Figure 4.4: Sample holder inside SEM chamber. The electron beam comes from the top vertically down towards the sample.

In the first two sets the uncut chip thickness is kept constant. One set of experiments is performed a low cutting speed and the other at a higher cutting speed. In the third set of experimental results presented the presence of ductile fracture at lower uncut chip thickness is explored at a low cutting speed.

4.2 Low Cutting Speed Experiments

4.2.1 Cutting Conditions

Orthogonal tube cutting (Fig. 3.7) experiments were performed on a Hardinge T42SP super precision lathe at a cutting speed of 3 m/min. The workpiece material was OFHC Copper. The cutting tool used was high speed steel (HSS) as explained earlier (section 3.5) using the wire-EDM process. The rake angles used were 30° and 70°. Cutting was performed without application of coolant. The uncut chip thickness was fixed at 105 µm. The spindle was quickly stopped in the middle of cut to freeze cutting action and the tool withdrawn. The chip-roots were section as explained in the previous section.
4.2.2 Results

To understand the nature of material separation leading to chip formation, the chip-workpiece interface was observed using a SEM. The SEM images for the 30° case are shown in Fig. 4.5. Images of increasing magnification can be seen from left to right. The oblique view of the chip attached to the workpiece, with a view of the newly formed surface of the chip facing the reader can be seen from the left image. The interface region exhibits interesting features. The middle image shows a higher magnification picture of the interface area. Evidence of strands of copper attached from the newly formed to the chip can be seen. The grooves running along the back side of the chip are a result of the chip rubbing against the rough surface of the rake face. An even closer view of the interface area is seen in the right image. The strands of copper are clearly visible. It is as if copper is torn in a ductile manner between the two surfaces. The SEM images for the 70° case are shown in Fig. 4.6. Similar evidence of copper material torn between the chip and the newly forming surface can be seen in these pictures.

OFHC copper is known to undergo ductile fracture via void formation (Johnson...
Figure 4.6: SEM image of chip-workpiece interface (70° rake angle)

and Cook [1985b]. Ductile fracture through void formation and void growth is illustrated in Fig. [4.7]. Plastic extension causes voids to enlarge, leading to material between voids to neck down, resulting in adjacent material being subjected to intense shearing [Engel and Klingele 1981]. Eventually, with continued extension, all that remains are thin ridges marking the separation between the holes. The strands seen in Figs. [4.5] and [4.6] are such thin ridges about to be separated.

As discussed earlier, a chamfer was incorporated in the tool used in the experiment to strengthen the cutting tool edge especially at high rake angles. In order to ensure that the tearing seen in the interface zone is not a result of this chamfer, cutting was performed with a tool of 30° rake angle without the chamfer, and where the rake and
flank faces were finished by grinding. The SEM images of the resulting chip-workpiece interface are shown in Fig. 4.8. These images also reveal similar strands between the chip underside and the newly formed surface confirming that ductile tearing is indeed occurring ahead of the tool.

**4.3 High Cutting Speed Experiments**

**4.3.1 Cutting Conditions**

To determine the presence of ductile tearing just ahead of the tool at higher cutting speeds, a quick-stop device (QSD) was used to perform orthogonal cutting experiments on Al2024-T3 and OFHC Copper. The quick-stop device used is shown in Fig. 4.9. The successful use of this device has been documented before in the literature (Joshi et al., 2001; Ponkshe, 1967). It is of the shear-pin-hammer type where the cutting tool is released by breaking a shear-pin (B) using the blow of a hammer. A compressed spring assists in quickly pulling the tool out of the cutting path. A sketch of the device showing the quick stop action can be seen in Fig. 4.10.

The performance of this device approximately approaches that of a similar device reported by Black and James (1981). The only difference between these two devices is that while the hammer blow is given manually in the QSD device of Fig. 4.9, the device used by Black and James uses by a hammer mounted on the rotating workpiece.
Black and James report the successful use of their device at cutting speeds of up to 1800 m/min with the cutting tool being accelerated to $3 \times 10^7$ m/s$^2$. Hence, the similar QSD device used in this study is expected to perform satisfactorily and provide chip roots that are representative of steady-state cutting.

Orthogonal tube-cutting experiments at high cutting speed were performed on both Al2024-T3 and OFHC Copper. The cutting tool used was high speed steel (HSS) made by grinding commercially available blanks. The required geometry (rake and clearance angles) was made by grinding the rake and flank faces. The rake angle
ground was 20° and the clearance angle was 5° (Fig. 4.11). The cutting speed was fixed at 150 m/min and the uncut chip thickness was 110 µm. Cutting was performed under dry conditions. The chip-roots were sectioned as discussed before (Fig. 4.2).

4.3.2 Results

The chip-workpiece interface of Al2024-T3 at a cutting speed of 150 m/min is shown in Fig. 4.12. The picture on the left shows the chip-workpiece interface with the underside of the chip and the machined surface marked. The picture in the middle clearly shows that the interface is not continuous from the machined surface to the underside of the chip. There seems to be a zone of material separation between the

Figure 4.11: Tool geometry

Figure 4.12: Ductile tearing in Al2024-T3
chip and workpiece (see Fig. 4.14). The picture on the right shows a close-up view of this zone and strands of material separating can be seen. This is characteristic of ductile tearing (Fig. 4.7). The tearing is seen to be limited to a narrow zone. The chip-workpiece interface of OFHC copper at a cutting speed of 150 m/min is shown in Fig. 4.13. Here the crack showing separation of chip material from the workpiece

![Figure 4.13: Ductile tearing in OFHC Copper](image_url)

is clearly visible in the picture in the middle (see also Fig. 4.14). A single strand separating from the workpiece by stretching and necking can be seen in the picture on the right. This evidence is similar to that for OFHC copper cut at a slower cutting speed of 3.0 m/min by using quick-stopping of the lathe spindle and discussed in the previous section.
4.4 Ductile Tearing at Lower $t_o$

In order to confirm the presence of ductile tearing at lower uncut chip thickness values, two of the samples from the tests described in section 3.7 were observed in an SEM. The two cases were those of 25$\mu$m and 50$\mu$m uncut chip thickness values. The results are shown in Figs. 4.15 and 4.16. Each figure has three images with increasing magnification from left to right. Some evidence of strands of copper attached to the underside of the chip and the newly formed machined surface can be seen. OFHC copper is known to fail in a ductile manner by void formation and coalescence ([Johnson and Cook 1985b]). The strands at the interface suggest a failure by ductile tearing leading to chip formation ([Engel and Klingele 1981]).

![Figure 4.15: SEM images of chip-workpiece interface for $t_o=25\mu m$](image)

![Figure 4.16: SEM images of chip-workpiece interface for $t_o=50\mu m$](image)
4.5 Summary

This chapter deviated from viewing the root of the chip in the traditional way of taking a cross-section, polishing it, and etching it. Instead, the chip-root is observed obliquely without any polishing in a scanning electron microscope. Evidence of ductile tearing at the interface between the chip and finished workpiece surface is shown at several cutting conditions of speed (low and high) and uncut chip thickness values (low and high). At higher cutting speeds chip roots were obtained using a quick-stop device. The SEM micrographs establish without any doubt that material separation by ductile fracture occurs in front of the cutting edge when cutting ductile metals. Such material separation occurs not only in OFHC copper, but is also seen in another ductile material AL2024-T3. It is also seen that the ductile separation occurs in a narrow zone ahead of the cutting tool edge spanning the width of cut. It is also a reasonable assumption from these micrographs that the ductile “crack” or fracture path is roughly straight ahead in front of the tool edge.
CHAPTER V

MODELING MATERIAL SEPARATION

The stress state in the vicinity of the tool edge is fairly complex. In order to effectively model the ductile fracture ahead of the tool, the stress state can be determined effectively using a finite element (FE) model. Many commercial finite element packages are available to simulate the machining process. These include DEFORM® and ThirdWave®. However, both these packages avoid explicit modeling of the chip separation process due to ductile fracture and instead model machining as an indentation process with chip formation induced by pure plastic flow around the cutting edge. The cutting tool is treated as a punch, and the advancing geometry of the tool is accommodated by frequently remeshing the workpiece to reduce interference and allow penetration. In order to analyze the energy consumed in material separation leading to chip formation a fully coupled thermo-mechanical finite element model incorporating a pre-defined separation layer of elements whose failure is based on a ductile fracture criteria leading to chip formation is adopted in this work.

5.1 Finite Element Model Development

The explicit dynamics procedure is used in this work to build a numerical model and analyze the machining process in this work. This is an effective method to solve a wide range of non-linear problems in a geometric and material sense in structural and solid mechanics. Explicit algorithms use a large number of very small time increments to attain a solution. However, the computational cost of each increment is very small, as opposed to implicit algorithms where increment sizes may be comparatively large with correspondingly large computational costs.
Explicit dynamics solution procedures were originally developed for high-speed dynamic events, where materials are subjected to thermo-mechanical changes in very short times. This necessitates the use of very small time increments, making the computational cost very large. Explicit dynamic methods are also very useful in the solution of complex contact problems and highly non-linear quasi-static events. Another facet of explicit procedures is their ability to handle material degradation and failure \cite{ABAQUS2003}. Simulation of these events often leads to significant convergence difficulties in implicit methods, whereas explicit codes can handle them with ease. This is of importance in the context of the present work, as chip formation has been modeled by material failure. In addition, coupled temperature-stress analyses can be carried out using explicit algorithms. This functionality, coupled with the advantages stated above over implicit methods, makes the explicit FE formulation a natural choice for the simulation of highly non-linear processes involving complex contact interactions, such as machining.

One such software package which offers all the above functionalities is ABAQUS\textsuperscript{®}/Explicit v6.4. This is a commercially available software package, which utilizes descriptive commands in an input file to drive the FE solution procedure. The input file contains a description of the problem in terms of geometry, nodal and elemental definitions, material property information and details of interfacial contact. In addition, it also offers post-processing abilities that make the analysis of results an easy task. The following sections describe the FE model development in ABAQUS\textsuperscript{®}/Explicit (v6.4). More details on the explicit formulation can be obtained from literature \cite{ABAQUS2003}.

5.1.1 Model Setup

Plane strain elements (CPE4RT) were used to model the workpiece and the tool. These are 4-noded quadrilateral elements with reduced integration and hourglass
control. The active degrees of freedom in these elements are displacements in the X and Y directions, and temperature. The mesh was generated in ANSYS®. Varying mesh densities were used to minimize the total number of elements and nodes for computational efficiency. The mesh and boundary conditions are shown in Fig. 5.1. Below the layer of the chip the workpiece is 0.3 mm deep and 1.0 mm long. Depending on the uncut chip thickness, the number of elements ranges from 4000 up to 7000. The number of nodes is also in the same range. The workpiece is constrained on the left, bottom, and right sides, to move horizontally towards the fixed tool at the cutting speed. The cutting tool top, right, and bottom (close to the tool tip) are constrained in all directions. The bottom of the tool is constrained to simulate rigid tool conditions without running into noise related to numerical contact. In the initial set of simulations the cutting tool is assumed to be sharp since the simulations will be compared with experiments conducted with cutting tools with edge radius much

**Figure 5.1:** Finite Element model mesh and boundary conditions
Table 5.1: Workpiece (Al2024-T3) Properties (Lesuer, 2000; Kay, 2003)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>2770 kg/m³</td>
</tr>
<tr>
<td>Specific Heat</td>
<td>875.0 J/Kg.K</td>
</tr>
<tr>
<td>Thermal Conductivity</td>
<td>121.0 W/m.K</td>
</tr>
<tr>
<td>Coefficient of Thermal Expansion</td>
<td>24.7x10⁻⁶/°C</td>
</tr>
<tr>
<td>Young’s Modulus</td>
<td>73.0 GPa</td>
</tr>
<tr>
<td>Poisson’s Ratio</td>
<td>0.33</td>
</tr>
<tr>
<td>Melting Temperature</td>
<td>502°C</td>
</tr>
<tr>
<td>Johnson-Cook Strength Model</td>
<td>A = 369.0 MPa, B = 684.0 MPa, n = 0.73</td>
</tr>
<tr>
<td></td>
<td>C = 0.0083, m = 1.7</td>
</tr>
<tr>
<td></td>
<td>D₁ = 0.13, D₂ = 0.13, D₃ = −1.5, D₄ = 0.011</td>
</tr>
<tr>
<td></td>
<td>D₅ = 0.0</td>
</tr>
</tbody>
</table>

smaller than the uncut chip thickness.

5.1.2 Material Property

The workpiece modeled is Al2024-T3 and the tool is modeled as a tool steel. Among the available forms of material models, the Johnson Cook (J-C) form is widely used and is chosen in this work. There is both a strength component and a damage component in the J-C model. The constitutive equation for the strength of the material is given as (Johnson and Cook, 1985a):

\[
\sigma = (A + B\varepsilon^n)(1 + C \ln \dot{\varepsilon}^*)(1 - T^*)
\]  

(5.1)

where \(\sigma\) is the stress, \(\varepsilon\) is the plastic strain, \(\dot{\varepsilon}^* = \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\) the ratio of rate of strain to a reference strain-rate, and \(T^*\) is the homologous temperature. The tool is modeled as an elastic tool-steel. The various properties are listed in Tables 5.1 and 5.2. The JC model parameters were obtained using data from split-Hopkinson pressure bar tests (Lesuer, 2000) and from ballistic limit tests (Kay, 2003).

The inelastic heat fraction for the workpiece material was fixed at 0.9 i.e., 90% of the energy dissipated by plastic deformation is converted into heat.
Table 5.2: Tool Properties (Tool Steel)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>7830 kg/m³</td>
</tr>
<tr>
<td>Specific Heat</td>
<td>477.0 J/Kg.K</td>
</tr>
<tr>
<td>Thermal Conductivity</td>
<td>386.0 W/m.K</td>
</tr>
<tr>
<td>Coefficient of Thermal Expansion</td>
<td>32.0x10⁻⁶ /°C</td>
</tr>
<tr>
<td>Young’s Modulus</td>
<td>680 GPa</td>
</tr>
<tr>
<td>Poisson’s Ratio</td>
<td>0.3</td>
</tr>
</tbody>
</table>

5.1.3 Material Separation and Damage Model

Chip formation is assumed to occur along a narrow zone ahead of the cutting tool edge. The path of ductile fracture is assumed to be straight ahead of the cutting edge. As discussed before, these assumptions are close to what is observed in the SEM micrographs. A narrow line of sacrificial elements is modeled and separates the chip region from the work region (Fig. 5.2). When the element in the sacrificial layer immediately ahead of the tool reaches a predetermined critical damage value, the element is deemed to have “failed”. It is then removed from the mesh, and the process is repeated for the duration of the cut.

![Sacrificial Layer](image.png)

Figure 5.2: Sacrificial layer separates the chip from the workpiece

The failure of the element is assumed to occur based on a ductile fracture criterion. The use of this criterion is valid given the experimental evidence of fracture shown before. Several fracture models for ductile fracture in metals are available in the
literature (Bao and Wierzbicki 2004; Wierzbicki et al. 2005).

One of the earliest models is the void nucleation and growth model by Gurson (1977). The model has been subsequently improved upon by several researchers (Tvergaard 1981, 1982; Tvergaard and Needleman 1984). However, the improved model contains over 10 parameters to be calibrated, some of them being strongly coupled. The cohesive zone model (Barenblatt 1962; Hillerborg et al. 1976) is also widely used in numerical fracture modeling through the use of interface elements. Commercial softwares such as ABAQUS® have incorporated this model. However, the model effectiveness depends on the determination of the traction-separation law that is fairly difficult to determine experimentally and no universal function has been established. A number of other simple criteria for ductile fracture have been developed based on the postulate that fracture occurs when the accumulated plastic strain reaches a critical value:

\[
\int_0^{\bar{\epsilon}_f} f(\text{stress state}) \ d\bar{\epsilon} = C
\]

where \( f \) is a weighting function, \( \bar{\epsilon} \) is the equivalent strain, \( \bar{\epsilon}_f \) is the equivalent strain-to-fracture and \( C \) is a material constant. Many successful criteria developed in literature belong to this form. McClintock (1968) developed a fracture criterion using enlargement of cylindrical holes accounting for load history effects. Another criteria is by Rice and Tracey (1969), where they introduced a stress triaxiality parameter defined by,

\[
\eta = \frac{\sigma_m}{\overline{\sigma}}
\]

where \( \sigma_m \) is the mean stress and \( \overline{\sigma} \) is the equivalent stress, to study the enlargement of a spherical void. Some models of the form of Eqn. 5.2 are implemented in commercial softwares such as DEFORM®.

A comparative study by Bao and Wierzbicki (2004) recently reported that none of the models of this form gave consistent results in a series of fracture tests. Fracture models, including those of the form given in Eqn. 5.2 implemented in commercial
codes were compared in another recent study [Wierzbicki et al. 2005]. This study
did not consider the effects of strain-rate or temperature in evaluating the models.
Two of the models compared are similar in form to Eqn. 5.2 with \( f = \bar{\varepsilon}_f \) and with
strain-to-fracture as a function of several variables. One model is the J-C fracture
model and the other is the Xue-Wierzbicki (X-W) model. In the J-C model, the
strain-to-fracture is a monotonic function of only the stress triaxiality parameter\( (\eta) \).
In the X-W model the strain-to-fracture is a function of both \( \eta \) and the deviatoric
state parameter, \( \xi \),
\[
\xi = \frac{27 J_3}{2 \sigma^3}
\] (5.4)
\( J_3 \) being the third invariant of the stress deviators. The study by Wierzbicki et al.
(2005) reports that the X-W fracture criterion performed the best in the tests. However,
the X-W fracture criterion and the J-C damage models coincide for \( \xi = 0 \). For
the case of plane strain, \( \xi = 0 \), and hence the two models are identical. Also, the J-C
ductile fracture model is readily available in ABAQUS®/Explicit. The J-C fracture
model also incorporates strain-rate and temperature effects, important factors to be
considered in machining. Hence the J-C model is adopted in this work for numerically
modeling material separation leading to chip formation.

The strain-to-fracture in the J-C damage model is given as [Johnson and Cook
1985b):
\[
\bar{\varepsilon}_f = [D_1 + D_2 e^{(D_3 \eta)}][1 + D_4 \ln \dot{\varepsilon}^*][1 + D_5 T^*]
\] (5.5)
The values of the constants are given in Table 5.1. The JC model parameters
were obtained using data from split-Hopkinson pressure bar tests [Lesuer 2000] and
from ballistic limit tests [Kay 2003]. This J-C fracture model is implemented in
ABAQUS®/Explicit as a shear failure mechanism. The shear failure mechanism
depends on a damage parameter evaluated at element integration points. When this
parameter exceeds a certain value, the element integration point is considered to have
failed, and is removed from the model. The damage parameter is defined as,

$$\omega = \sum \frac{\Delta \epsilon}{\epsilon_f}$$

(5.6)

When the shear failure criterion is met at an integration point, all the stress components are set to zero and the material point fails.

It should be pointed out that apart from a few researchers [Benson 1997], any separation criterion to cause element deletion in a sacrificial layer was never implemented with the intention of applying a fracture criterion for material separation leading to chip formation. [Ramesh 2002]; [Tian and Shin 2004] have used an accumulated damage model with a constant strain-to-fracture criterion to delete elements in a sacrificial layer of elements that then leads to chip formation. However, in the complete J-C damage model [Johnson and Cook 1985b], the strain-to-fracture is not constant but is a function of pressure, strain-rate and temperature. A multi-material mixture theory based Eulerian contact formulation is developed by [Benson 1997] and the model is illustrated by application to the metal cutting process using the complete J-C damage model to model contact separation for incipient chip formation. However, complete chip formation and separation is not simulated.

The numerical model presented here utilizes a complete J-C damage model, that is stress, strain-rate and temperature dependent, to simulate complete chip formation and is used to study the energy consumed in material separation. The use of a damage model has been substantiated before by experimental evidence of ductile fracture ahead of the cutting tool.

5.1.4 Chip-Tool Interaction

The friction characteristic at the tool chip interface is difficult to determine since it is influenced by many factors such as cutting speed, contact pressure, and temperature. The friction model in [Zorev 1963], which reveals that two distinct regions of sliding and sticking on the interface exist, is widely accepted. In the sliding region, the shear
stress, \( s \), is a fraction of the normal contact pressure, \( p \). As the shear stress reaches a limiting shear stress value, \( \tau^* \), sticking occurs and the shear stress equals the limiting shear stress value regardless of the normal contact pressure (Eq. 5.7). The extended Coulomb friction model, expressed in terms of the frictional shear stress, appears to fit the machining problem adequately and has been used successfully by several researchers and is chosen in this work to model the tool-chip interaction.

\[
s = \mu p \quad \text{when } \mu p < \tau^* \quad \text{(sliding)} \tag{5.7}
\]
\[
s = \tau^* \quad \text{when } \mu p \geq \tau^* \quad \text{(sticking)}
\]

5.1.5 Heat Transfer

In the finite element model, heat generation due to plastic deformation and friction at the tool-chip interface is modeled as a volume heat flux. Heat conduction is assumed to be the primary mode of heat transfer, which occurs within the workpiece material and at the tool-chip interface. The governing equation of heat transfer is as follows:

\[
K \frac{\partial^2 T}{\partial x^2} + K \frac{\partial^2 T}{\partial y^2} = \rho_m C_p \left( u \frac{\partial T}{\partial y} + v \frac{\partial T}{\partial x} \right) + \dot{Q} \tag{5.8}
\]

where \( K \) is the thermal conductivity of the workpiece material, \( \rho_m \) is the mass density, \( C_p \) is the specific heat capacity, \( u \) is the velocity in the \( x \) direction, \( v \) is the velocity in the \( y \) direction, \( \dot{Q} \) is the volume heat flux. The heat flux term includes both plastic dissipation and frictional heating. The fraction of dissipated energy converted into heat due to plastic deformation and friction is assumed to be 0.9. Heat generated due to friction is distributed via a weighting factor of 0.5 between the two contact surfaces.

A sample ABAQUS® code used for the simulation is given in Appendix B.

5.2 Model Validation

The finite element model developed was validated using cutting and thrust forces obtained from orthogonal tube-cutting experiments (Fig. 3.7). Experiments were
Table 5.3: Cutting Conditions

| Cutting speed | 180 m/min |
| Uncut chip thickness | 50, 60, 75, 90, 105 µm |
| Cutting Tool | HSS (20° rake, 5° clearance) |
| Workpiece | Al2024-T3 tube 0.89 mm wall thickness |

performed at a cutting speed of 180 m/min on a superprecision lathe (Hardinge T42SP). The workpiece was a tube made of Al2024-T3 with 0.89 mm wall thickness. The cutting and thrust forces were measured using a piezoelectric force dynamometer (Kistler 9257B). The cutting conditions used are given in Table 5.3.

Simulation trials were conducted at several values of $\tau^*$, $\mu$ and the critical damage parameter. Consistent chip formation with failure just ahead of the cutting edge were observed for values of $\tau^*$=20 MPa, $\mu$=0.2, and when failure occurs as the damage parameter exceeds a value of 4.0. These values are considered acceptable for the following reasons. Chip-tool interface temperatures as high as 293°C are seen and at these temperatures the shear flow stress reduces to 33 MPa. Hence, the value of $\tau^*$=20 MPa is considered reasonable. It is also clear from the results reported by Johnson and Cook (1985b) that the damage model does not work very well with very ductile materials such as OFHC Copper. Hence, it is possible that the damage model coefficients for Al2024 T3 also have errors leading to failure prediction well beyond the expected damage value of 1.0. A mesh convergence study was performed and shows that a 4 µm mesh is sufficient to provide good force results. An 8 µm mesh under-predicts the cutting and thrust force while a 2 µm mesh give no better result than the 4 µm mesh.

The force plots comparing the experimental and simulated forces are shown in Fig. 5.3 and the values are given in Table 5.4. Note that all force values are for a width of cut of 1 mm.

The average error in cutting forces is 1%, maximum error being 2%. The average
error in the thrust force is 8% while the maximum error is 16%. Note that the use of constant frictional conditions at different uncut chip thickness could be a contributing factor to the errors seen. The errors are considered reasonable for the current discussion and the model is considered to be sufficiently validated. The model will now be used to study the energy consumed in the material separation due to ductile fracture and its influence on the size effect in specific cutting energy.

### 5.3 Some Results and Discussion

#### 5.3.1 Stresses Ahead of Tool

The stresses obtained from the simulations can be examined to study the state of material immediately ahead of the tool. Specifically, it can be inferred if conditions...
favorable for ductile fracture are present. One of the important variables on which the strain-to-fracture depends on is the hydrostatic pressure $p$. Note that $p$ is defined as:

$$p = \frac{1}{3} (\sigma_{11} + \sigma_{22} + \sigma_{33}) \quad (5.9)$$

where $\sigma_{ii}$ represents the normal stress component acting in the $i^{th}$ direction. A negative pressure indicates compressive conditions while a positive pressure indicates existence of tensile conditions. For a sample case of $t_o=50 \, \mu m$, the distribution of this pressure is shown in Fig. 5.4. The plot shows the regions of the workpiece and chip where pressures are positive (dark) and negative (light). The region immediately ahead of the tool, where fracture is seen in the experiments, has positive stresses indicating favorable conditions for fracture to occur. A little ahead of the tool the pressure turns negative indicating compressive conditions that will inhibit any ductile fracture. This may explain why ductile fracture cracks may not be seen well ahead of the tool. The distributions of individual stress components are shown in Figures 5.5, 5.6, and 5.7. The two stress components $\sigma_{11}$ and $\sigma_{33}$ behave in a similar manner as $p$, viz., positive immediately ahead of the tool and negative further ahead of the tool. Stress component $\sigma_{22}$ is positive in a broader area ahead of the tool that reaches even

![Figure 5.4: Contour plot of pressure stress](image)

96
the free surface of the chip. The combined negative effects of $\sigma_{11}$ and $\sigma_{33}$ seem to restrict the overall pressure $p$ to negative values. A plot of the stresses straight ahead of the tool in the sacrificial layer is shown in Fig. 5.8. An element in the sacrificial
Figure 5.7: Contour plot of $\sigma_{33}$

Figure 5.8: Plot of stresses straight ahead of the tool

layer far ahead of the tool is initially in a state of compression and as it approaches the edge of the cutting tool undergoes a change in stress state to a positive value. This then lends conditions favorable to fracture leading to material separation ahead of the cutting tool.
5.3.2 Energy Consumed

The amount of energy going into the cutting process can also be calculated from the simulation results. All simulations were performed for a fixed length of cut $l$ of 0.46 mm. The total energy spent, $E_{tot}$, can be calculated from the simulation. The energy spent as plastic dissipation in the sacrificial layer $E_{sacr}$, in the chip $E_{chip}$, and in the sub-surface $E_{sub}$, as well as energy dissipated in friction $E_{fric}$ can be obtained from the simulation. The sub-surface considered is about three layers of elements below the tool edge and is about 10 µm deep. These energy values are listed in Table 5.5 as absolute values and as a percentage of $E_{tot}$. A plot of the percentage energies is shown in Fig. 5.9. Note that any elastic strain energy is assumed to be negligibly small. The percentage of energy spent as plastic dissipation in the chip ($E_{chip}$) is seen to be highest and decreases as the uncut chip thickness decreases from 105 µm to 50 µm. The next higher consumption of energy is in the sacrificial layer. Contrary to the energy consumption in the chip, the percentage of plastically dissipated energy in the sacrificial layer increases with decrease in uncut chip thickness. The lowest energy is spent in the sub-surface layer. The percentage of energy spent in friction and the sub-surface do not vary significantly with uncut chip thickness. In absolute

![Figure 5.9: Percentage of energy spent in the chip, sacrificial layer, friction, and sub-surface](image-url)
terms, while the energy consumed in the chip is seen to decrease with uncut chip thickness, the plastic dissipation energy in the sacrificial layer ($E_{sacr}$) is seen to not vary substantially. This gives credence to the unified framework for the decomposition of the cutting force into a constant force (due to material separation), increasing and decreasing components. The constant force component could well be attributed to this non-varying energy (see absolute values of $E_{sacr}$ in Table 5.5) associated with material separation.

The energy per unit area needed to form the new surfaces can be calculated from $E_{sacr}$ as,

$$\text{Energy per unit area} = \frac{E_{sacr}}{lw} \quad (5.10)$$

where $w$ is the width of cut (=1 mm). From the values listed in Table 5.5 it can be seen that the energy per unit area ranges from 1.7 to 1.8 kJ/m$^2$. This value is orders of magnitude different from the solid surface energy terms that are a few J/m$^2$. This value is also comparable to that given by Atkins (2003) in his comparison of energy needed for material separation in finite element simulations. The reason it could be a little lower than that reported by Atkins (2003) is that the plastic work in elements other than the sacrificial layer is not included in the present analysis.

The specific cutting energy from experiments and from simulations are compared in Fig. 5.10. It can be seen that the scaling effect is predicted well by the finite element model.
It is also possible to calculate the specific energy associated with each term as the energy consumed per unit volume of material removed:

\[ u = \frac{E}{lt_o w} \]  

(5.11)

where \( l \) is the length of cut (=0.46 mm), \( t_o \) the uncut chip thickness, and \( w \) the width of cut (=1 mm). This value, for each of \( E_{tot}, E_{sacr}, E_{chip}, E_{sub}, \) and \( E_{fric} \) is shown in Table 5.6. The size-effect in micro-cutting can be seen in the table as an increase in \( u_{tot} \) as the uncut chip thickness decreases. The specific energy in the sacrificial layer is seen to more than double (108%) as the uncut chip thickness changes from 105 to 50 \( \mu m \). The specific energy of deformation in the chip, on the other hand, has increased by only 10%. The specific energy of sub-surface deformation rose by 59% (half of the increase in \( u_{sacr} \)) while that of friction by 22% (less than a quarter of the increase in \( u_{sacr} \)) as the uncut chip thickness was decreased.
Table 5.6: Specific cutting energies (in $\frac{N-mm}{mm^3}$)

<table>
<thead>
<tr>
<th>$t_o$ (µm)</th>
<th>$u_{tot}$</th>
<th>$u_{sacr}$</th>
<th>$u_{chip}$</th>
<th>$u_{sub}$</th>
<th>$u_{fric}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>50</td>
<td>1026.1</td>
<td>36.3</td>
<td>864.8</td>
<td>4.9</td>
<td>15.9</td>
</tr>
<tr>
<td>60</td>
<td>976.8</td>
<td>29.3</td>
<td>858.7</td>
<td>4.3</td>
<td>15.6</td>
</tr>
<tr>
<td>75</td>
<td>918.8</td>
<td>23.1</td>
<td>824.4</td>
<td>3.7</td>
<td>14.4</td>
</tr>
<tr>
<td>90</td>
<td>886.5</td>
<td>19.3</td>
<td>807.5</td>
<td>3.9</td>
<td>13.5</td>
</tr>
<tr>
<td>105</td>
<td>863.4</td>
<td>17.5</td>
<td>787.5</td>
<td>3.1</td>
<td>13.0</td>
</tr>
</tbody>
</table>

% Increase 108% 10% 59% 22%

5.3.3 Variation with Rake Angle

Simulations were conducted for two other rake angles, 40° and 60° for the same range of uncut chip thickness. The trend in the cutting force as the rake angle increases can be seen in Fig. 5.11. The plots show a clear trend: the slopes decrease as the rake angle increases. This trend is similar to what was observed in experiments with high rake angle tools. The variation in the energy consumed as a percentage of the total energy expended, as the rake angle increases, is shown in Figures 5.12 and 5.13. The energy consumed in the chip is seen to decrease slightly with rake angle, while the percentage of energy in the sacrificial layer is seen to increase substantially as the rake angle is increased. No such trends are seen in the energy consumed in friction.
or sub-surface deformation.

**Figure 5.12:** % Energy as a function of rake angle: $E_{\text{chip}}$ and $E_{\text{sacr}}$

**Figure 5.13:** % Energy as a function of rake angle: $E_{\text{fric}}$ and $E_{\text{sub}}$

### 5.4 Summary

A finite element of orthogonal cutting using a sharp cutting tool has been developed. The model uses a sacrificial layer whose elements are deleted based on a fracture criterion. Unlike, other models reported in literature, a complete J-C damage model, with dependence on stress, strain-rate and temperature has been implemented. The model has been used to study the energy consumed in material separation and how it varies...
with uncut chip thickness. The analysis of energy consumed in the different areas of machining as presented here highlights the importance of the energy associated with material separation that has hitherto been ignored in the analytical models of machining. In the given analysis, if it can be assumed that the plastic dissipation energy in the sacrificial layer is the energy associated with material separation, then the material separation energy is seen to play a significant role at smaller uncut chip thickness values. Note that this is a conservative estimate since the energy associated with ductile fracture will span more than just the one sacrificial layer considered in this work. The plastic work in the areas surrounding a ductile crack front is very well known in the fracture mechanics literature (Stuwe, 1980). In this conservative estimate, part of this ductile fracture plastic work is attributed to the chip deformation energy ($E_{chip}$) and part of it to the sub-surface deformation energy term $E_{sub}$. It could very well be that the sub-surface deformation is entirely the result of this rather brute-force material separation caused by the advancing tool. It is also seen from the stress distributions ahead of the cutting tool that conditions are favorable for ductile fracture to occur.

In practice cutting tools are never sharp and have a finite cutting edge radius. The next chapter attempts to study the effect of this edge radius on the ductile failure ahead of the cutting tool.
CHAPTER VI

EDGE RADIUS EFFECTS

All the experiments and simulations conducted thus far used sharp cutting tools including the tool with chamfer, which had a sharp edge. In reality cutting tools are never sharp but have a finite edge radius (Fig. 1.6). The presence of the edge radius becomes more important as it becomes comparable to the uncut chip thickness. The extra plastic flow associated with a non-zero edge radius has long been considered as a reason for the size effect in the literature ([Lucca and Seo 1993] Komanduri et al. 1998 [Schimmel et al. 2002]). It has also been considered, from a study of the traditional polished cross-sectional view of the chip root, that there exists a stagnation point of flow on the rounded edge of the tool [Connolly and Rubenstein 1968 Oxley 1989]. Above this stagnation point, material is considered to flow into the chip (Fig. 6.1) while below this stagnation point material is considered to flow into the workpiece to form the new machined surface (see also Fig. 2.20). The literature, however, does not say what causes the material to separate above and below this stagnation point.

A logical extension of this dissertation work would then be to study the chip-root created by tools with finite edge radius. Such a study can clarify the nature of material separation leading to the separation of flow above and below the so-called stagnation point. Hence, this chapter continues the investigation of material separation using tools of finite edge radii.

6.1 Experimental Setup

Orthogonal tube-cutting (Fig. 3.7) experiments were performed by axially plunging a tool whose cutting edge is lined up with the centerline, into a tube shaped workpiece
made of an aluminum alloy (Al2024-T3). The tube is 0.89 mm thick and its outer diameter is 38.1 mm (1.5 inches). A polycrystalline diamond (PCD) grooving insert with the following geometry is used: $+5^\circ$ rake angle, 3.18 mm wide (Kennametal NGP3125R). The tool holder used is Kennametal NEL-123B-NJ3. The inserts have a clearance angle of $11^\circ$. The cutting edge radius of the insert was measured using an edge qualifier system (Niebauer, 2006). A sample edge trace of one of the inserts is shown in Fig. 6.2. Complete set of all traces are shown in the Appendix A. Inserts with three edge radii were used: 38 $\mu$m (case A), 50 $\mu$m (case B), and 75 $\mu$m (case C). For comparison purposes, experiments were also run with an up-sharp tool with an edge radius of 12.5 $\mu$m (case D). The uncut chip thickness ($t_o$) values used for each of these inserts is summarized in Table 6.1. The cutting speed was kept fixed at 150 m/min. The values of uncut chip thickness were chosen to yield $\tau_r$ ratios of 0.67, 1, and 1.33. Cutting forces were measured for all tests in a separate setup without the quick stop device (described below) using a quartz 3-component Kistler 9527B.
Figure 6.2: Sample trace of edge radius (Niebauer, 2006)

Table 6.1: Edge radius test conditions

<table>
<thead>
<tr>
<th>Test</th>
<th>Edge Radius (µm)</th>
<th>Uncut Chip Thickness(µm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>1</td>
</tr>
<tr>
<td>A</td>
<td>38</td>
<td>68</td>
</tr>
<tr>
<td>B</td>
<td>50</td>
<td>90</td>
</tr>
<tr>
<td>C</td>
<td>75</td>
<td>105</td>
</tr>
<tr>
<td>D</td>
<td>Up-sharp</td>
<td>105</td>
</tr>
</tbody>
</table>

dynamometer.

6.1.1 Quick-Stop Device

A quick-stop device was built to perform the quick-stopping required to freeze the chip at high cutting speeds. The device action is similar to the device discussed in
Chapter 4. It is of the hammer-shear-pin type with spring retraction. The device is shown in Fig. 6.3 and sketch of its operation is shown in Fig. 6.4. The quick stop device consists of a U-shaped structural member that houses a swivel pin around

**Figure 6.3:** Quick-stop device: (a) before breaking shear pin (b) after breaking shear pin

**Figure 6.4:** Sketch of quick-stop device operation

device consists of a U-shaped structural member that houses a swivel pin around
which an aluminum tool-holder can pivot and swing. The aluminum tool holder houses the cutting tool holder. The aluminum tool holder is prevented from swinging during machining by a shear pin (housed in a holder), made of brittle cast iron, with a v-notch. The shear pin is located outside of the swiveling tool holder, unlike the device shown before in chapter 4. The shear pin diameter is 4.76 mm (0.1875 inches) and the notch diameter is 2.79 mm (0.11 inches). A spring is attached underneath (unlike the previous device where it was above the toolholder) the aluminum tool-holder and is stretched at the ready-to-machine position. After the machining cut is initiated, a hammer blow imparted to the top of the tool holder is used to break the shear-pin (by fracture at the notch) causing the tool holder to swivel out quickly because of the spring action and the hammer velocity. This “freezes” the cutting action and the chip remains attached to the workpiece. The spindle and feed movements are subsequently stopped.

The performance of the device was characterized using an eddy current proximity sensor. Characterization involves measuring the distance and time over which the tool holder, starting from zero velocity (at rest), attains a linear speed greater than the cutting speed. This is illustrated in Fig. 6.5. In the figure, (a) represents the stage when cutting action is taking place at a cutting speed of $V$ and the tool is at rest. In (b) the QSD has been activated and the tool has started to move. The cutting speed is now less than $V$ and starts to decrease. In (c) the tool has almost attained the speed of $V$ and the cutting speed is now almost zero. Finally, in (d) the tool speed has exceeded $V$ and cutting action has stopped. So in a time $\delta t = t_3 - t_1$ the tool has moved a distance $P$ before the chip was frozen. The faster the acceleration of the receding tool, the smaller $\delta t$ and $P$ will be. It must be noted however that regardless of how fast the tool recedes, except in the case of infinite acceleration of the tool holder, the frozen chip is always formed at a cutting velocity close to zero as illustrated in Fig. 6.5.

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Figure 6.5: Sequence of events leading to frozen chip
In order to measure the receding acceleration of the aluminum tool holder, a sensor was mounted above the tool holder (Fig. 6.6) and measurement was performed without any cutting action. The sensor provides distance measurement as the tool holder recedes away and this can be used to calculate the acceleration. The calibration graph for the sensor is given in the Appendix (Fig. A.5). Note that the sensor is mounted at a distance from the pivot point that is 2/3rds of the distance from the pivot point to the cutting insert. Hence, the acceleration of the insert is 50% higher than that detected by the sensor.

Figure 6.6: Eddy current sensor used to measure acceleration

Three acceleration measurements were made to check for repeatability in performance. A sample displacement curve is shown in Fig. 6.7. The other two tests are given in the Appendix A (Figures A.6 and A.7).

The acceleration can be calculated by differentiating the displacement curve twice. The acceleration test results are summarized in Table 6.2.

After the chip is frozen, the chip root sample is removed from the workpiece using the wire-EDM process and mounted for SEM observation using the same procedure as explained in chapter 4 (Fig. 4.2 Fig. 4.3).
Figure 6.7: Acceleration measurement - test I

<table>
<thead>
<tr>
<th>Test</th>
<th>Initial Acceleration (m/s$^2$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>2.1×10$^3$ m/s$^2$</td>
</tr>
<tr>
<td>2</td>
<td>1.8×10$^3$ m/s$^2$</td>
</tr>
<tr>
<td>3</td>
<td>1.8×10$^3$ m/s$^2$</td>
</tr>
</tbody>
</table>

### 6.2 Chip-root study results

#### 6.2.1 Force measurement results

The measured cutting and thrust forces along with one-sigma error bars (based on three repetitions) are plotted as a function of uncut chip thickness in Fig. 6.8 and Fig. 6.9 respectively. The cutting and thrust forces increase with uncut chip thickness. The maximum differences in forces can be seen for the up-sharp case and 75 µm edge radius (cases C and D) with the latter yielding higher forces. The difference in forces is higher in the thrust direction than in the cutting direction. Less difference in forces, both cutting and thrust, can be seen for samples A and B (38 µm and 50 µm edge radius).
radii). There is also a notable difference in the thrust force between case A (or B) and C or D, more so with case C than with case D.

![Cutting force plot for different edge radii](image)

**Figure 6.8:** Cutting force plot for different edge radii

### 6.2.2 Results of SEM Observations

The chip-root interface was observed in an SEM to study the nature of the interface and to check for the presence of any ductile tearing/fracture. A sketch of the observed chip-root interface with a finite edge radius tool (samples A, B, and C) is shown in Fig. 6.10. There is a zone of metal seen at the interface. Three areas in this zone are depicted by labels L, M and U in the figure. The area marked U refers to the upper edge of the zone that is attached to the underside of the chip. The area marked M is the middle area of the zone and the area marked L is the lower edge of the interface closer to the machined surface.

In the test samples observed, ductile fracture/tearing is observed in all three areas, sometimes even within the same sample. Consider case A of the tests performed with
Figure 6.9: Thrust force plot for different edge radii

Figure 6.10: Sketch of the interface zone seen with edge-radius tool

38 μm edge radius tool. The SEM micrographs for sample A1 ($t_o = 68\mu m$, $r = 38\mu m$) where the uncut chip thickness is higher than the edge radius is shown in Fig. 6.11
The zone of metal in the interface can be clearly seen. The evidence of ductile tearing in area M can be seen in lower set of micrographs. In the upper set of micrographs the lifting of the material in area U can be clearly seen. Underneath this lifted material ductile tearing evidence is observed (see picture in the middle right). There is no evidence of material separation in area U in this sample.

**Figure 6.11:** SEM micrographs of sample A1 \((t_o = 68\mu m, r = 38\mu m)\)

For the case where the uncut chip thickness is comparable to the edge radius, sample A3 \((t_o = 37\mu m, r = 38\mu m)\) the SEM micrographs are shown in Fig. 6.12. Here one can see evidence of material separation in the upper edge, U, and the lower edge L of the interface zone. The interface zone surface in the middle also seems
Figure 6.12: SEM micrographs of sample A3 \( (t_o = 37\mu m, r = 38\mu m) \) to exhibit the characteristics of a surface that has undergone ductile fracture. For the case where the uncut chip thickness is lower than the edge radius, sample A5 \( (t_o = 15\mu m, r = 38\mu m) \) the SEM pictures are shown in Fig. 6.13. Here, there is clear evidence of material separation in the lower edge of the interface zone. In this case also the surface of the interface zone seems to be a torn surface with dimples that are characteristic of ductile fracture.

One obvious question that can be raised is if the interface zone observed is a built-up edge or not. Based on experiences in the literature of cutting aluminum alloys [Trent 1991], built-up edge is observed only at lower cutting speeds \(< 30\) m/min\) and is usually not seen while cutting with a diamond tool. Also, the built-up
edge usually occurs over a region on the rake face much larger than the uncut chip thickness. The interface zone seen in the micrographs is smaller than the uncut chip thickness. In addition, the machined surface also shows strong evidence of built-up edge formation; such evidence was not found in the tests conducted above.

Consider now the samples with the highest edge radius of 75 µm. Evidence of ductile tearing in area M, can be seen in sample C1 \((t_o = 105\, \mu\text{m}, r = 75\, \mu\text{m})\) as shown in Fig. 6.14. The chip (in the first figure on the left) was broken while handling during the wire-EDM process. The interface zone is again clearly visible and ductile separation by tearing can be seen in the right most micrograph. As is evident, no separation seems to occur in the upper edge, U, or the lower edge, L, of the interface. For the

**Figure 6.13:** SEM micrographs of sample A5 \((t_o = 15\, \mu\text{m}, r = 38\, \mu\text{m})\)
Figure 6.14: SEM micrographs of sample C1 ($t_o = 105\, \mu m$, $r = 75\, \mu m$) sample where the edge radius is comparable with the uncut chip thickness, sample C3($t_o = 75\, \mu m$, $r = 75\, \mu m$), the SEM micrographs are presented in Fig. 6.15. It is clear

from these micrographs that material separation occurs in all three regions viz., the lower, middle, and upper edges of the interface zone. Finally, for the sample of uncut
chip thickness smaller than the edge radius, sample C5 ($t_o = 50\mu m$, $r = 75\mu m$), the SEM micrographs are presented in Fig. 6.16. In this case the interface zone is clearly visible. Also, material separation was seen at the lower and upper edges, with one area in the upper edge showing clear of evidence of ridges characteristic of a ductile fractured surface. There was no evidence of material separation in the middle of the interface zone.

Case B was very similar to case A and the micrographs associated with this case are shown in Fig. 6.17. For the case of the up-sharp tool (smallest edge radius of 12.5 $\mu m$), a very small interface zone was seen. This can be seen in the SEM micrographs for samples D1, D3 and D5 shown in Fig. 6.18. Here, the material separation is seen at the bottom of the small interface zone where it seems to be attached to the machined surface. This form of ductile separation in an area close to the machined surface is similar to that shown earlier (chapter 4).

**Figure 6.16:** SEM micrographs of sample C5 ($t_o = 50\mu m$, $r = 75\mu m$)
Figure 6.17: SEM micrographs of case B ($r = 50 \mu m$)
Figure 6.18: SEM micrographs of case D ($r = 12.5\mu m$)
6.2.3 Discussion

It is clear from the micrographs described above that there is ductile fracture and material separation involved in the chip formation process even with a cutting tool of finite edge radius. However, ductile fracture is not seen just in one area as in the “sharp” tool case but in several areas in the interface zone. Based on the SEM evidence of the different ductile fracture areas in the interface zone, a hypothesis is presented here as to how the interface zone may form and why fracture is seen in more than one area. The hypothesis is sketched in Fig. 6.19.

Consider a small unit volume of material approaching the tool edge radius. As it reaches the tool edge, it gets wrapped around the edge. As a result of cutting motion, the material is pulled in two directions - one in the direction of the chip motion, and the other in the direction of the workpiece motion. The stretched material forms the interface zone seen in the SEM micrographs. As the cutting motion continues the stretching action continues in both the directions. There are now three possibilities. One, the pulling action is equal in both directions and as a result the interface zone separates in the middle area, M, as seen in sample C1. Two, the interface zone material is weaker near the upper edge, U, and three, the interface zone is weaker at the lower edge and separates there. As seen in the SEM micrographs all three possibilities can occur simultaneously (as in sample C3) or in combinations thereof. If the material separation happens only in the middle, then this situation is similar to the formation of a stagnation point as hypothesized in the literature (Comolly and Rubenstein [1968] Oxley [1989]). However, as can be seen from the micrographs this may not always be the case.

6.3 Analysis of the Stress State Ahead of the Tool

The stress state in the vicinity of the tool edge is fairly complex. In order to effectively model the ductile fracture ahead of the tool, the stress state needs to be determined
Figure 6.19: Hypothesis of material separation in the interface zone in the presence of finite cutting edge radius
and this can be done using the finite element (FE) method. However, given the observations of the SEM micrographs and the hypothesis of interface zone formation and multiple locations of fracture, it is difficult to simulate this in a finite element based numerical model. The reason for the difficulty arises in dealing with elements that will fail in different parts of the chip in front of the tool. This is illustrated in Fig. 6.20. In the mesh shown on the left elements are allowed to fail anywhere based upon a given criterion. This exactly simulates real world conditions. However, this results in a problem of handling the deleted element and creation of new surfaces.

**Figure 6.20:** Significance of height of sacrificial layer
and importantly how to make them establish contact with the tool surfaces. On the contrary, in the mesh on the right side of Fig. 6.20 element failure is restricted to a layer of elements in the sacrificial layer and the element failure is orderly and takes place one after another. Hence, it is precisely known which set of surfaces will subsequently contact the tool rake face and contact conditions can therefore be established appropriately.

Here, a simplified model similar to the mesh on the right will be used to study the stress state in front of the tool in the presence of a cutting tool with a finite edge radius. The important question of whether the stresses ahead of the tool even permit such ductile fracture to occur in the presence of a finite edge radius can be studied using this model. A numerical model that simulates material separation in the form of a sacrificial layer whose elements fail based on a damage criterion is used in this paper.

### 6.3.1 Model Setup

The model setup is very similar to that used before in chapter 5. The mesh and boundary conditions are shown in Fig. 6.21. Below the chip layer the workpiece is 0.3 mm deep and 1.0 mm long. The number of elements is around 6500 and the number of nodes is about 6700. The model has been validated for cutting with a sharp tool before. It will be assumed that this validation is sufficient for the purposes of studying the stresses and energy distribution. The rest of the chapter presents some simulation results.

### 6.3.2 Simulations and Results

Simulations were performed for a fixed uncut chip thickness of 105 \( \mu \text{m} \) and under two conditions: (i) sharp tool and, (ii) a tool with an edge radius of 38 \( \mu \text{m} \). (Higher edge radius conditions are not simulated since the sacrificial layer elements become too tall and ill-defined and also the coarse mesh in this area will make the simulation
Figure 6.21: Mesh with finite edge radius and boundary conditions inaccurate. Simulations with a 12.5 \( \mu \text{m} \) edge radius did not give results very different from the sharp tool case). The frictional conditions between the chip and rake face of the tool were kept the same for the two conditions. The coefficient of friction \( \mu \) was 0.2 and \( \tau^* \) was 20 MPa. These frictional values are consistent with the validated model under sharp tool conditions. The damage parameter had to be altered to maintain consistent element failure in front of the tool i.e. neither too many elements fail ahead nor is the failure delayed too late before element distortion is severe. The sharp tool and edge radius tool simulations had the same sacrificial layer height.

The contour plots of the von Mises stress for the two sharp tool and edge radius tool are shown in Fig. 6.22 and Fig. 6.23 respectively.

The stress was analyzed in front of the tool in the sacrificial layer to study the differences between the two cases. A plot of the hydrostatic (mean) stress of the elements in the sacrificial layer directly ahead of the tool is shown in Fig. 6.24.
first notable observation is that even in front of the edge radius tool, the mean stress is positive (tensile) indicating that conditions are favorable for fracture to occur. It can also be seen that the mean stress in front of the edge radius tool is consistently
Figure 6.24: Comparison of hydrostatic stress in front of tool lower than in front of the sharp tool. This is to be expected since the blunt tool imparts more of a compressive stress to the material ahead of it than the sharp tool. The compressive stress causes less favorable conditions for fracture to occur. Similar plots of the three stress components, $\sigma_{11}$, $\sigma_{22}$, $\sigma_{33}$, are shown in Figure 27, Figure 28, and Figure 29, respectively. It can be seen that all three components of the

Figure 6.25: Comparison of stress component $\sigma_{11}$ in front of tool
normal stress are consistently lower than those seen in front of the sharp tool. Stress component \( \sigma_{11} \) reaches a negative value (compressive) at around the 15th element, while \( \sigma_{33} \), reaches a negative value a little after the 30th element for both sharp and edge radius tools. Stress component \( \sigma_{22} \) does not become negative until much ahead
Both simulations were performed from roughly the same starting point and for a fixed length of cut of 0.46 mm. It is thus possible to compare the percentage of energy being expended in different aspects of the chip and workpiece. The aspects of interest are: plastic dissipation in the chip itself, plastic dissipation in the sacrificial layer, plastic dissipation in the immediate sub-surface of the workpiece and frictional dissipation. A bar graph of these energies as a percentage of the total energy expended is shown in Fig. 6.28. The plot compares these numbers for the sharp tool and the edge radius tool. It can be seen from this plot that in the case of the edge-radius tool a higher percentage of energy is expended in the chip itself. Also a higher percentage of energy is expended in the sacrificial layer. This could be because of the compressive stresses impeding the occurrence of fracture thus requiring more energy to be used to cause fracture leading to chip formation. There is no considerable difference in energy expended in friction for the two tool edge conditions. There is an increase in percentage of energy expended in sub-surface deformation with the edge radius tool. This can be expected because of the extra effort needed to stretch the material, based on the hypothesis presented earlier, and cause fracture to occur.
6.4 Summary

This chapter investigated the effect of finite edge radius on ductile fracture ahead of the cutting tool leading to chip formation. The basic question of whether such ductile failure occurs was explored by performing a series of experiments with inserts of different edge radii at various uncut chip thickness values ranging from 15 to 105 $\mu$m. Chip roots are obtained in these experiments using a quick-stop device. The chip-roots are then examined in a scanning electron microscope. Clear evidence of material separation is seen at the chip-root even when the edge-radius is large compared to the uncut chip thickness. Based on the observations from these set of experiments, a hypothesis was presented for presence of the interface zone of metal and its different fracture regions. Using a finite element model, that uses a sacrificial layer for material separation based on a J-C damage model, the stresses and energy distribution has been studied. Stresses were seen to be tensile ahead of the tool edge even in presence of a edge radius. The stresses were however lower than that seen with a sharp tool. More energy is consumed in material failure and in sub-surface deformation with a edge radius tool than with a sharp tool.
CHAPTER VII

CONCLUSIONS AND RECOMMENDATIONS

This chapter summarizes the conclusions of this thesis and suggests related areas for further exploration.

7.1 Main Conclusions

Some of the main conclusions of this thesis work are as follows:

7.1.1 Constant force component

The several reasons attributed in the literature to the scaling in specific cutting energy is reconciled by developing a framework from which to view the cutting force as composed of several components. Then, special experimental conditions are devised to isolate the constant cutting force component

- By viewing specific cutting energy as a ratio of two numbers the cause for the size effect can be attributed to an increasing component of the cutting force and/or a constant force component.

- Special experiments at high rake angles can serve to isolate the constant force component.

- Experiments at high rake angles (up to 70°) that minimize the shear in the chip, indicate that the cutting force tends to a constant value at higher rake angles approaching 90°.

- SEM images of the chip-workpiece interface for OFHC copper show strong evidence of ductile fracture ahead of the tool leading to chip formation.
• The crack extension force associated with ductile fracture is shown to be comparable with the constant cutting force observed at high rake angles. Chip formation (material separation) is seen to occur even at high rake angles. With shear in the chip being minimal at such high rake angles, the mechanism of chip formation (material separation) is primarily ductile fracture or ductile tearing ahead of the tool. This mode of chip formation is also seen to occur at lower rake angles.

• The Atkins model of machining, which explicitly includes energy needed for material separation as that due to ductile fracture can phenomenologically explain effects such as the increase in specific cutting energy with decrease in uncut chip thickness.

• At low cutting speeds where strain-rate and temperature effects are minimal, the Atkins model fits experimental data well for values of fracture toughness, coefficient of friction and shear yield stress, that are within an order of magnitude of that reported in the literature.

### 7.1.2 Evidence of material separation

Instead of viewing the root of the chip in the traditional way of taking a cross-section, polishing it, and etching it, the chip-root is observed obliquely without any polishing in a scanning electron microscope (SEM).

• Ductile tearing leading to chip formation occurs at uncut chip thickness as low as 25µm.

• Orthogonal experiments using a quick-stop device show that ductile tearing of material immediately ahead of the tool occurs in both Al2024-T3 and OFHC-Cu at speeds of 150 m/min.
• Ductile tearing occurs in a narrow zone spanning the width of cut and directly in front of the cutting tool edge.

• These observations put to rest criticisms about the use of a pre-defined sacrificial layer in numerical simulations to model chip separation either by node release or element deletion. As long as physically-based fracture models or damage models pertaining to the nature of failure are used to release the nodes or delete elements, it is argued here that this approach is valid.

• The chip-workpiece junction consists of an interface zone of metal when cutting with an edge-radius tool. Such an interface is absent in the case of an up-sharp tool (with edge radius much smaller than the uncut chip thickness).

• The interface zone of metal is seen to fracture at three locations: one, at the upper edge closer to the chip, two, in the middle of the zone, and three, at the lower edge closer to the machined surface.

• In the case of the up-sharp tool, material separation happens only at the lower boundary of the chip where it leaves the workpiece surface.

• Based on observations it can be hypothesized that an element of metal approaching the edge-radius tool tip will get wrapped around the edge and then be stretched in opposing directions: in the direction of the chip-flow and in the direction of the workpiece surface movement. Depending on which area is weaker and how strong these pulling effects are, the element of metal can fracture at the upper, middle and/or the lower edges of the interface zone.

7.1.3 Stress and energy analysis using numerical simulation

A numerical model incorporating sacrificial layers and damage simulated by a J-C damage model is developed using a explicit finite element method to simulate orthogonal cutting of Al2024-T3. The model is validated using cutting forces measured
in orthogonal tube-cutting experiments. The model is used to analyze the energy dissipated and stresses developed.

- A study of the stress state ahead of the cutting tool shows that the pressure is positive closer to the cutting edge and negative further ahead. An element in the sacrificial layer changes its stress state to tensile as it approaches the cutting tool leading to ductile fracture.

- Analysis of the total energy consumed in cutting indicates that the percentage of energy consumed in plastic dissipation in the chip decreases with uncut chip thickness, while that of the sacrificial layer increases. The percentage of energy spent in the sub-surface and in friction remains unchanged with changes in uncut chip thickness.

- In absolute energy terms, the energy consumed in material separation is seen to not vary significantly as the uncut chip thickness is reduced. This could then be attributed to the hypothesis that there is always a constant force component associated with cutting.

- The size-effect in specific cutting energy is observed in Al2024-T3. The specific energy (energy per unit volume) of the sacrificial layer is seen to increase substantially while that of the chip deformation energy is seen to rise by only a small percentage. Both the sub-surface and friction show strong increases in specific energy.

- The mean stresses in front of the tool are positive (tensile) even with an edge radius tip indicating favorable conditions for ductile fracture to occur.

- All stress components are consistently lower with the edge radius tool than with the sharp tool indicating that the sharp tool provides a more favorable condition for fracture to occur.
• A higher percentage of energy is spent in the sacrificial layer and the sub-surface in the case of the edge radius tool than the sharp tool.

7.2 Further Investigations

Related areas for further research can include the following:

• Study the relative roles of material separation, strain-gradient strengthening, temperature effects and strain-rate effects in contributing to the scaling effect in specific cutting energy. This can be attempted by building a finite element model that incorporates strain-gradient and strain-rate into the material model in addition to incorporating material failure.

• Machining of ductile materials and brittle materials pose opposing requirements - the former requires fracture promotion in order to facilitate easy and clean material removal, while the latter requires controlled fracture suppression in order to facilitate machining. Factors such as hydrostatic pressure and use of a blunt tool induces such fracture suppression in brittle materials; new factors have to be designed to promote fracture while machining in ductile materials to get a “cleaner” cut. An example would be to induce tensile residual stress in the surface of the material being cut that will help promote occurrence of fracture.

• Use of several layers of sacrificial elements in numerical simulation in ABAQUS® using 3D elements and the general contact algorithm. This would allow the modeling of a larger fracture area while not compromising the numerical accuracy.

• Extend the validation of the developed finite element model to include other metrics such as chip thickness, cutting temperatures and strains generated in the machined surface. Validation using strain and temperatures have been attempted in the literature and measurement of these parameters is very difficult.
• Frictional interaction at the tool-chip interface is modeled with Coulomb friction using a constant friction coefficient. Other, better friction models more applicable at the micro-scale need to be investigated.

• Use of other ductile fracture models can be investigated.

• Perform quick-stop experiments using a very sharp tool, such as that provided by a tool made of single crystal diamond, to study the occurrence of fracture at \( t_o \) values much smaller than a micron.

• Perform quick-stop experiments using a tool with a finite edge radius, to study the phenomenon of minimum uncut chip thickness, to study if fracture ceases to occur at a certain \( t_o \) and if this is the reason for chip formation to cease.
Figure A.1: Edge trace of insert with edge radius 38 \( \mu \text{m} \)
Figure A.2: Edge trace of insert with radius 50 μm
Figure A.3: Edge trace of insert with radius 75 µm
Figure A.4: Edge trace of up-sharp insert
Figure A.5: Eddy current sensor calibration

\[ y = 744.98x + 502.04 \]
\[ R^2 = 0.9999 \]

Figure A.6: Acceleration measurement - test II
Figure A.7: Acceleration measurement - test III

\[ y = 6E+08x^2 + 2E+06x + 177.03 \]
\[ R^2 = 0.9959 \]
APPENDIX B

SAMPLE ABAQUS CODE

*HEADING
** 2D MACHINING
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*NODE, NSET=NWORK, INPUT=ansys_mesh_105_2F_nwork.inp
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  93, 101, 1
*NSET, NSET=NWORKLEFT1, GENERATE
  1, 6, 1
  465, 475, 1
*NSET, NSET=NWORKLEFT
  NWORKLEFT1, 1290, 1365, 1366, 465
*NSET, NSET=NWORKRIGHT1, GENERATE
  88, 92, 1
  367, 369, 1
*NSET, NSET=NWORKRIGHT
  NWORKRIGHT1, 82
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  1219, 1293, 1
*ELSET, ELSET=EMODEL, GENERATE
  1, 1443, 1
*ELSET, ELSET=ECHIP, GENERATE
  394, 1143, 1
*ELSET, ELSET=EBASEADAPT, GENERATE
  316, 333, 1
  1219, 1443, 1
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*ELSET, ELSET=ECHIPRIGHT, GENERATE
339, 393, 6
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388, 393, 1
1069, 1143, 1
*ELSET, ELSET=ECHIPLEFT, GENERATE
394, 1069, 75
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1144, 1213, 1
*ELSET, ELSET=ESACR1
1218
*ELSET, ELSET=ESACR2
1217
*ELSET, ELSET=ESACR3
1216
*ELSET, ELSET=ESACR4
1215
*ELSET, ELSET=ESACR5
1214
*ELSET, ELSET=ESACRALL
ESACR, ESACR1, ESACR2, ESACR3, ESACR4, ESACR5
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**
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1.0
*SOLID SECTION,MATERIAL=AL2024SACR1,ELSET=ESACR1
1.0
*SOLID SECTION,MATERIAL=AL2024SACR2,ELSET=ESACR2
1.0
*SOLID SECTION,MATERIAL=AL2024SACR3,ELSET=ESACR3
1.0
*SOLID SECTION,MATERIAL=AL2024SACR4,ELSET=ESACR4
1.0
*SOLID SECTION,MATERIAL=AL2024SACR5,ELSET=ESACR5
1.0
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**MATERIAL PROPERTY DEFINITION
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*MATERIAL,NAME=AL2024MODEL
*DENSITY
2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
0.0083, 1.0
*INELASTIC HEAT FRACTION
0.9,
*SPECIFIC HEAT
875.E6, 298.
*CONDUCTIVITY
121.0, 298.
*EXPANSION, ZERO=298
24.7E-6, 298.
**
**
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*DENSITY
2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
0.0083, 1.0
** NOTE: D1 AND D2 VALUES BELOW ARE DEFLATED BY ***
** TO REFLECT THE FACT THE DAMAGE CAN OCCUR EARLIER
*SHEAR FAILURE,ELEMENT DELETION=YES,TYPE=JOHNSON COOK
0.13, 0.13, 1.5, 0.011, 0.
*SPECIFIC HEAT
875.E6, 298.
*CONDUCTIVITY
121.0, 298.
*EXPANSION, ZERO=298
24.7E-6, 298.
*INELASTIC HEAT FRACTION
0.9,
**
**
*MATERIAL,NAME=AL2024SACR2
*DENSITY
2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
0.0083, 1.0
** NOTE: D1 AND D2 VALUES BELOW ARE DEFLATED BY ***
** TO REFLECT THE FACT THE DAMAGE CAN OCCUR EARLIER
*SHEAR FAILURE,ELEMENT DELETION=YES,TYPE=JOHNSON COOK
0.13, 0.13, 1.5, 0.011, 0.
*SPECIFIC HEAT
875.E6, 298.
*CONDUCTIVITY
121.0, 298.
*EXPANSION, ZERO=298
24.7E-6, 298.
*INELASTIC HEAT FRACTION
0.9,
**
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*DENSITY
2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
0.0083, 1.0
** NOTE: D1 AND D2 VALUES BELOW ARE DEFLATED BY ***
** TO REFLECT THE FACT THE DAMAGE CAN OCCUR EARLIER
*SHEAR FAILURE,ELEMENT DELETION=YES,TYPE=JOHNSON COOK
0.2, 0.2, 1.5, 0.011, 0.
*SPECIFIC HEAT
875.E6, 298.
*CONDUCTIVITY
121.0, 298.
*EXPANSION, ZERO=298
24.7E-6, 298.
*INELASTIC HEAT FRACTION
0.9,
**
**
*MATERIAL,NAME=AL2024SACR4
*DENSITY
2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
 369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
 0.0083, 1.0
** NOTE: D1 AND D2 VALUES BELOW ARE DEFLATED BY ***
** TO REFLECT THE FACT THE DAMAGE CAN OCCUR EARLIER
*SHEAR FAILURE,ELEMENT DELETION=YES,TYPE=JOHNSON COOK
 0.3, 0.3, 1.5, 0.011, 0.
*SPECIFIC HEAT
 875.E6, 298.
*CONDUCTIVITY
 121.0, 298.
*EXPANSION, ZERO=298
 24.7E-6, 298.
*INELASTIC HEAT FRACTION
 0.9,
**
**
**
*MATERIAL,NAME=AL2024SACR5
*DENSITY
 2.770E-9,
*ELASTIC,TYPE=ISOTROPIC
 73.10E3, 0.33, 298.0
*PLASTIC,HARDENING=JOHNSON COOK
 369.0, 684.0, 0.73, 1.7, 775.0, 298.0
*RATE DEPENDENT,TYPE=JOHNSON COOK
 0.0083, 1.0
** NOTE: D1 AND D2 VALUES BELOW ARE DEFLATED BY ***
** TO REFLECT THE FACT THE DAMAGE CAN OCCUR EARLIER
*SHEAR FAILURE,ELEMENT DELETION=YES,TYPE=JOHNSON COOK
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*SPECIFIC HEAT
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*CONDUCTIVITY
 121.0, 298.
*EXPANSION, ZERO=298
 24.7E-6, 298.
*INELASTIC HEAT FRACTION
 0.9,
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 NWORKLEFT, 2
 NBASEBOTTOM, 2
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*NSET, NSET=NTOOLBOUND
9003, 9004, 9017, 9020, 9021,
9036, 9039, 9040, 9002,
9006, 9007, 9005, 9009,
9010, 9008

**

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*ELEMENT, ELSET=ETOOL, TYPE=CPE4RT, INPUT=tool_05deg_elements.inp

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9034, 9035,
9043, 9018, 9019, 9024,
9004, 9007
*ELSET, ELSET=ETOOLEDGE
9029

**

**

*NSET, NSET=NFORCES
NTOOLBOUND, NWORKRIGHT, NBASEBOTTOM, NWORKLEFT

**

**

*SOLID SECTION, MATERIAL=HSS, ELSET=ETOOL
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*MATERIAL, NAME=HSS
*DENSITY
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*ELASTIC
680.E3, 0.3
*SPECIFIC HEAT
477.0E6, 298.
*CONDUCTIVITY
386.0, 298.0
*EXPANSION, ZERO=298
32.0E-6, 298.
*BOUNDARY
NTOOLBOUND, ENCASTRE
*INITIAL CONDITIONS, TYPE=TEMPERATURE
NTOOL, 298.
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  9999, 0.15, 0.35, 0.0
*SURFACE, TYPE=SEGMENTS, NAME=STEMP
  START, 0.9799, 0.3002, 0.
  LINE, 0.0, 0.3002, 0.
*RIGID BODY, ANALYTICAL SURFACE=STEMP, REF NODE=NTEMP
*BOUNDARY
  NTEMP, 2, 6
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**NODE, NSET=NTEMP2
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**SURFACE, TYPE=SEGMENTS, NAME=STEMP2
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**LINE, 0.975, 0.3039, 0.
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**BOUNDARY
**NTEMP2, 2, 6
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  ETOOLEDGE, S4
*SURFACE, NAME=STOOLFLANK, TYPE=ELEMENT
  ETOOLEDGE, S1
*SURFACE, NAME=SCHIP, TYPE=ELEMENT
  ECHIPBOTTOM, S1
  ECHIPRIGHT, S2
  ECHIPTOP, S3
  ECHIPLEFT, S4
*SURFACE, NAME=SBASETOP, TYPE=ELEMENT
  EBASETOP, S3
*SURFACE, NAME=SSACRBOTTOM, TYPE=ELEMENT
  ESACRALL, S1
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**
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*NSET, NSET=NSYSTEM
  NWORK, NTOOL
*ELSET, ELSET=ESYSTEM
  ETOOL, EWORK
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STEP1
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*Diagnostics
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*GAP HEAT GENERATION
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  STOOLFLANK, SBASETOP
*SURFACE INTERACTION, NAME=TOOLWORK
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  0.2
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*GAP HEAT GENERATION
**
**
*CONTACT PAIR, INTERACTION=TEMP
  SBASETOP, STEMP
*SURFACE INTERACTION, NAME=TEMP
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**CONTACT PAIR, INTERACTION=TEMP2
**STEMP2, SCHIP
**SURFACE INTERACTION, NAME=TEMP2
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  RF1, RF2
*ENERGY OUTPUT
  ALLFD, ALLWK, ETOTAL
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  ALLPD, ALLSE, ALLIE
*ENERGY OUTPUT, ELSET=ECHIP
  ALLPD, ALLSE, ALLIE
*ENERGY OUTPUT, ELSET=EBASETOP
ALLPD, ALLSE, ALLIE
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*OUTPUT, NUMBER INTERVAL=2000, FIELD
*NODE OUTPUT, NSET=NSYSTEM
U
*NODE OUTPUT, NSET=NFORCES
RF,
*ELEMENT OUTPUT, ELSET=ESYSTEM
S, TEMP, PEEQ, ERV, ENER, PE
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**ADAPTIVE MESH, MESH SWEEPS=3, ELSET=EBASEADAPT
*END STEP
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neering, 42–49.

Sathyan Subbiah was born on 26th May 1976 in the southern Indian city of Tuticorin to V. Subbiah and S. Saradha. Most of his schooling was at Kendriya Vidyalaya-IIT in Madras (Chennai). During his last two years of his high school he prepared rigorously for the IIT-Joint Entrance Exams, which he successfully cleared and joined IIT-Madras in 1993. While studying here, he got interested in the field of manufacturing and also was initiated into research during his part-time work at the Aerospace Engineering department. He also participated actively in planning and conducting the institute’s annual cultural program Mardi Gras (now called Saarang). He obtained his B.Tech in Mech. Engg. in 1997 and had the third highest GPA in a class of 83. He then joined University of Illinois Urbana-Champaign, USA to pursue his M.S. degree, which he completed in 1999. While at UIUC he was also exposed to volunteering with the organization Asha for Education. He then joined Hayes-Lemmerz International, an automotive company in Michigan, as a manufacturing engineer. He saw the company go through bankruptcy, and survived a 11% lay-off in the company. He quit his job in August 2002 despite an offer of increase in salary and promotion. After declining two graduate study offers from UC-Berkeley and MIT, he joined Georgia Institute of Technology for pursuing his Ph.D. While at Georgia Tech he tutored undergrads, topped the Ph.D. qualifying exam, actively conducted several panel discussions, was the recipient of two fellowships, tutored mathematics at local schools, and continued volunteering for Asha for Education. He married Balarohini Rajasekaran on 7th June 2006.