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EXPERIMENTAL STUDIES AND NUMERICAL SIMULATIONS OF CONTINUOUS AND DISCONTINUOUS CHIP FORMATION DURING ORTHOGONAL CUTTING

by

XIAODONG SONG

A Thesis
Submitted to the Faculty of Graduate Studies and Research through Engineering Materials in Partial Fulfillment of the Requirements for the Degree of Master of Applied Science at the University of Windsor

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The research performed in this thesis has been focused on the experimental and numerical studies of continuous chip formation of commercially pure copper and aluminum (1100 Al). Serrated chip formation of vacuum high-pressure die cast aluminum A380 alloy was also investigated.

To study deformation structures developed during continuous chip formation, metallographic studies were performed on commercially pure copper and aluminum workpieces subjected to dry orthogonal cutting experiments under different feed rates, from 0.25 mm/rev to 0.8 mm/rev, while other machining parameters were kept constant. Equivalent plastic strains in the primary and secondary deformation zones in the material ahead of the tool tip were determined by measuring displacements of copper grain boundaries and the orientation change of flow lines in aluminum samples. The variations of local flow stresses were estimated from the microhardness measurements. A single Voce type exponential stress-strain curve for each material was determined for different feed rates. An Eulerian type finite element formulation in the explicit FE code LS-DYNA was used to predict strain and stress distributions in the material ahead of the tool tip. The simulations were based on an elastic-plastic hydrodynamic material model. Experimentally determined Voce type stress-strain relationship was used in the development of the material model.

For discontinuous chip formation, metallographic studies were performed on A380 samples. Dry orthogonal cutting experiment were done at a constant feed rate of 0.30 mm/rev and a cutting speed of 0.4 m·s⁻¹. Equivalent plastic strains were determined by measuring the orientation changes of the alignment of fractured particles in the
material ahead of the tool tip. The variations of local flow stresses were estimated from the microhardness measurements. Local temperature increases due to deformation were calculated by estimated plastic strains and flow stresses in the workpiece.

Metallographic observations on the cross-sections of continuous chips and numerical predictions showed that the chip thickness, the depth of the deformed layer, and the widths of the primary and secondary deformation zones increased with the increase of the feed rate. Parametric analyses showed that the cutting force and the energy consumption also increased with the feed rate. Formation of adiabatic shear bands was shown to be the main reason for serrated chip formation during cutting of A380 alloy. Flow localization in serrated chip formation may be attributed to the destabilizing effects of thermal softening, which can outweigh the effects of strain and strain rate hardening in a deforming region.
To my parents
and
my husband Qingwu and my daughter Amy
for their understanding, support, and encouragement
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NOMENCLATURE

\( \alpha \)  Rake angle: the angle between the rake face of the tool and the direction perpendicular to the cutting direction

\( \gamma \)  Clearance angle: angle between the clearance face of the tool and the cutting direction

\( \mathbf{f} \)  Feed rate: the lateral distance traveled by the tool during one revolution during turning process

\( v_c \)  Cutting speed: the surface speed of the lathe rotation in a turning operation

\( t_c \)  Chip thickness: distance between the tool rake face and the free surface of the chip

\( t \)  Depth of cut or uncut chip thickness: distance between the machined surface exposed in the current tool pass and previous tool pass

\( w \)  Width of the chip that equals to the depth of cut in turning process

\( \phi \)  Shear (plane) angle: angle between the cutting direction and the shear plane

\( \mu \)  Coefficient of friction between the tool and the workpiece

\( R \)  Resultant tool force acting at the tool cutting edge

\( F_c \)  Cutting force: force measured along the cutting direction

\( F_t \)  Thrust force: force measured along a direction perpendicular to the cutting direction

\( F_s \)  Shear force measured along the shear plane

\( F_f \)  Friction force measured along the rake face of the tool

\( N_s \)  Normal force measured along a direction perpendicular to the shear plane

\( N_f \)  Normal friction force measured along a direction perpendicular to the rake face

\( \beta \)  Mean chip-tool friction angle on tool face and given by \( \arctan \left( \frac{F_f}{N_f} \right) \)

\( \sigma_s \)  Normal stress at the shear plane
\( \tau_s \)  \quad \text{Shear stress at the shear plane}

\( \tau_t \)  \quad \text{Shear stress at the tool-chip interface}

\( \sigma_t \)  \quad \text{Normal stress on the rake face}

\( \sigma_{\text{max}} \)  \quad \text{Maximum normal stress at the tool tip}

\( \gamma \)  \quad \text{Shear strain}

\( \dot{\gamma} \)  \quad \text{Shear strain rate}

\( P \)  \quad \text{Total energy consumption during the metal cutting process}

\( \dot{W}_p \)  \quad \text{The rate of plastic deformation work done in the primary shear zone}

\( \dot{W}_s \)  \quad \text{The rate of plastic deformation work done in the secondary shear zone}

\( l \)  \quad \text{Chip-tool contact length}

\( \rho \)  \quad \text{Density}

\( E \)  \quad \text{Young's modulus}

\( G \)  \quad \text{Shear modulus}

\( \nu \)  \quad \text{Poisson's ratio}

\( H \)  \quad \text{Hardness}

\( \bar{\varepsilon}^p \) or \( \bar{\varepsilon} \)  \quad \text{Equivalent plastic strain}

\( \dot{\bar{\varepsilon}} \)  \quad \text{Equivalent plastic strain rate}

\( \theta \)  \quad \text{Deformation angle}

\( \sigma \)  \quad \text{Flow stress accumulated in a material}

\( \sigma_s \)  \quad \text{Saturation stress: stress at which the work hardening becomes zero}

\( \sigma_0 \)  \quad \text{Yield stress at which the work hardening becomes zero}

\( \varepsilon_c \)  \quad \text{Material constant necessary to complete the Voce equation}
$C_p$ Specific heat capacity of a material

$k$ Thermal diffusivity of a material

$T$ Temperature

$T_{\text{melt}}$ Melting temperature of a material

$T_{\text{amb}}$ Ambient temperature

$\eta$ The fraction of plastic work converted to heat

$S_{ij}$ Deviatoric stress tensor

$D^p_\eta$ Plastic component of the deformation rate tensor

$C$ Bulk sound speed

$S_1, S_2, S_3$ Linear, quadratic, and cubic coefficients relating material shock velocity to particle velocity respectively

$\gamma_0$ Grüneisen gamma

$a$ The first order volume correction to $\gamma_0$

$d_s$ Shear displacement within the shear band

$t_s$ Thickness of the shear band
CHAPTER 1

Introduction

1.1 Objectives of the Studied Problem

Metal cutting is a manufacturing process by which unwanted material is removed to obtain the desired shape, dimensions and surface roughness. Although extensive research has been carried out on the development of a general theory of the machining process, only a few of the output parameters of practical machining operations, such as cutting force and power consumption, could be quantitatively predicted because of the complexity of the interactions between various parameters. The cutting tool, and workpiece properties as well as the combinations of operation parameters exert different influences on the workpiece-tool-cutting conditions. These in turn determine the efficiency of a given machining process as well as the quality of the surface finish and dimensional accuracy. Cutting fluids have often been utilized in the machining process to increase tool life and improve surface finish. Due to increasing government demands for environmentally safe handling of cutting fluids, dry machining is becoming an increasingly important way to seeking benefits of cutting fluids cost savings and environmental safety.

Metallographic studies on the plastic deformation behaviour of selected workpiece materials during the chip formation process have been conducted by Zhang [1] and Elmadaglì and Alpas [2]. A method using the microstructural information was developed to determine the shear angles and to calculate the strain and stress distributions during cutting in these studies. Considering the dependency of the cutting process on the
machining parameters, previous work has been extended and parametric studies were performed experimentally and numerically for the first time in this research.

The direct experimental approach to study the metal cutting process is expensive and time consuming, especially when a wide range of parameters needs to be studied, such as tool geometry, materials, cutting conditions. Therefore, modelling of cutting process using finite element analysis programs has become an effective alternative. The versatility of the finite element method is its ability to take into account large deformation, strain rate effect, tool-chip contact and friction, local heating and temperature effect, different boundary conditions, and other phenomena encountered in metal cutting problems. The challenge in modelling metal cutting processes is that although various studies have been made to investigate the material behaviour in the machining process, there is still no satisfactory materials constitutive model as a function of strain, strain rate, and temperature to properly describe the material behaviour during the cutting process. The majority of the research has utilized the Lagrangian formulation to model metal cutting process. However, during modeling of ductile materials, severe mesh distortion occurs due to large strains and strain rates generated during metal cutting resulting in a degradation of the simulation accuracy, chip separation criterion and mesh adaptivity are among the common techniques used to compensate for mesh distortion and disintegration, which is not an accurate representation of the actual behaviour of material deformation. In the Eulerian approach, the material can “flow” through the stationary Eulerian mesh. Therefore, no severe mesh distortion occurs and chip separation criterion is not needed. It has been successfully used to predict the deformation state of copper ahead of the tool tip subjected to orthogonal cutting by Raczy [3].
The objective of this study is to extend previous experimental and numerical work to investigate the role of one of the most important parameters (feed rate) affecting cutting force and energy consumption at the tool tip. This research were focused on experimental and numerical studies of continuous and discontinuous chip formation in selected workpiece materials subjected to orthogonal machining. This study has been undertaken to understand details of metal deformation state and energy expenditure due to localized plastic deformation, which is essential to find optimum conditions in machining. The methods used in the study of discontinuous chip formation provide some new insights in the orthogonal cutting process.

1.2 Outline of the Thesis

The thesis is organized into seven chapters. Chapter I provides an introduction to the problem studied and the layout of the thesis.

Chapter II reviews previous studies on metal cutting processes. Theoretical investigations, experimental and numerical studies on chip formation, cutting force measurement, strain, stress, and temperature distributions in the workpiece material ahead of the tool tip are reviewed. Research on the investigation of the parameters that influence cutting process is also introduced.

Chapter III describes the experimental studies and numerical simulations of the effect of feed rate on the deformation state of copper subjected to orthogonal cutting. Equivalent plastic strains in the primary and secondary deformation zones were determined by measuring displacements of grain boundaries in the material ahead of tool tip. The variation of local flow stresses were estimated from the microhardness
measurements. A single Voce type relationship between flow stress and equivalent plastic strain generated in the deformed material ahead of tool tip under three different feed rates was established. An Eulerian finite element formulation that uses the explicit FE code LS-DYNA was adopted for numerical simulations of the metal cutting process. Numerical results, which included predicted strain and stress distributions, cutting forces, and energy consumptions are presented and compared with experimental measurements in this chapter.

Chapter IV presents the experimental studies and numerical simulations of the effect of feed rate on the deformation state of commercial purity aluminum (1100 Al) subjected to orthogonal cutting. The procedures and method performed on aluminum are the same as copper.

Chapter V describes the experimental studies of discontinuous (serrated) chip formation of A380 alloy, which contains 7.5-9.5 wt % Si particles. Equivalent plastic strains were determined by measuring the orientation changes of the alignment of fractured particles in the material ahead of the tool tip. The variations of local flow stresses were estimated from the microhardness measurements. Deformation induced temperature distributions ahead of the tool tip were calculated by estimated plastic strains and flow stresses in the workpiece. The deformed material was also studied by SEM and characteristics in discontinous chip formation in A380 alloy are also presented.

Chapter VI is an overall discussion of the results. It presents a general discussion of the stress-strain relationship of ductile materials subjected to orthogonal cutting. In this chapter, chip thicknesses, strain and stress distributions, cutting forces and energy consumptions are presented and compared with experimental measurements in this chapter.
consumptions of copper and aluminum under three different feed rates are compared and discussed.

The conclusions drawn from experimental and numerical observations are presented in Chapter VII. The original contributions of this work are emphasized and future work is suggested.
CHAPTER 2

Literature Review

Machining covers a large collection of manufacturing processes to obtain the desired shape, dimension and surface roughness. During machining, unwanted material is removed, usually in the form of chips. Machining is a highly non-linear deformation process coupled with thermomechanical events. Most of the mechanical work is converted into heat through the plastic deformation during chip formation as well as frictional work between the tool, chip and workpiece. A good understanding of machining process is very important in order to control the product dimensions, the workpiece surface characteristics and the tool life. In this section, the literature on the theoretical models and mechanics of chip formation is presented. Research work on the finite element modelling of metal cutting is introduced. The previous studies on machining parameters and their influences on cutting process are also reviewed.

2.1 Basics of Machining Operations and Theories

2.1.1 Types of Machining Processes

The three most widely used machining operations are: i) turning, ii) milling, and iii) drilling [4]. Turning is a process that uses a single point tool to remove unwanted material to produce a surface of revolution. The operation is accomplished on a lathe. Milling is a process for producing flat and curved surfaces using multipoint cutting tools by a milling machine. Drilling operation is a complex process and uses a rotating drill to make a hole in the workpiece. Since lathe turning is the process studied in this research, the principles of this operation will be described in detail.
A schematic of the turning process is shown in Figure 2.1. The two dimensional orthogonal cutting process is the most widely used method in machining research because of its simplicity. Orthogonal cutting uses a single cutting edge. The most important characteristic of this configuration is that the velocity of the workpiece is orthogonal to the cutting edge. Models of the orthogonal machining are useful for understanding the basic mechanics of machining and can be extended to modelling of the production processes. These models will be reviewed in section 2.1.4.

Figure 2.1 (a) Schematic diagram of the turning process, and (b) workpiece and tool geometry during the process of orthogonal metal cutting, where $t_c$ is the chip thickness, $\alpha$ is the rake angle, $\gamma$ is the clearance angle, $v_c$ is the cutting speed, and $t$ is uncut chip thickness.

### 2.1.2 Deformation Zones in the Workpiece

The basic mechanism involved in metal cutting is that of a localized shear deformation of the work material immediately ahead of the cutting edge of the tool [5]. The relative motion between the tool and the workpiece during cutting compresses the work material near the tool and induces a shear deformation (called the primary
deformation zone, PDZ) at the base of the chip. The chip passes over the rake face of the cutting tool and receives additional deformation (called the secondary deformation zone, SDZ) because of the shearing and sliding of the chip against the tool [5] (Figure 2.2), large strains and strain gradients are generated in these areas.

Figure 2.2 Schematic diagram of deformation zones on a cross-sectioned workpiece undergoing orthogonal cutting, where $\phi$ is the primary shear plane angle.

2.1.3 Principal Chip Types

2.1.3.1 Continuous Chips

This type of chip tends to generate in ductile material such as brass, low carbon steels, or ductile aluminum alloys machined at high speeds and small feeds and depths of cut [4,5]. Chips of this type are severely deformed and have a characteristic “curly” geometry. These long chips are difficult and dangerous to handle because they may wrap around a tool or workpiece causing scratches and reducing the finishing quality surface.
2.1.3.2 Continuous Chip with Built-up Edge

A built-up edge (BUE) forms when some work material that is deposited on the rake face near the cutting edge. A BUE modifies the geometry of the tool and also causes generation of high localized temperatures at the tool - chip interface. A BUE tends to grow until it reaches a critical size and then passes off with the chip. Build-up edge formation can often be eliminated or minimized by reducing the depth of cut, increasing the cutting speed, using positive rake tools, or applying coolant [4,5,10].

2.1.3.3 Discontinuous Chips

Discontinuous chips are characterized by the chip breaking into small segments. Fracture is a dominant mechanism in forming discontinuous chips. As a result, chips continuously break and re-form because the material is unable to undergo large amounts of plastic deformation. It occurs in brittle materials, such as cast iron, machined at low cutting speed or at high tool-chip friction and large feed and depth [4,5].

Serrated chips are partially discontinuous chips in which cracks do not penetrate the chip thoroughly, or completely break the chip. It is the consequence of localization shear into shear bands (also called adiabatic shear, see section 2.1.8), which results from competition of thermal softening and strain hardening mechanisms in the primary shear zone. This type of chips are likely to form during the machining of materials with poor thermal properties and high hardness, such as titanium alloys, and some hardened alloy steels [4].
2.1.4 Theoretical Models of Chip Formation

There have been extensive attempts in the past to derive a complete chip formation theory from different points of view. Unfortunately, there is no theory that can accurately predict this complicated process because various oversimplified assumptions that had to be made. Among them, the single shear plane theory of Ernst and Merchant [6,7,8], and the slipline theory of Lee and Shaffer [9, 10] are most commonly referred to in the literature.

2.1.4.1 Ernst and Merchant Theory

Ernst and Merchant developed a cutting force diagram [6] with the assumption that strains are localized on a single shear plane (Figure 2.3). In their analysis, the chip is assumed to behave as a rigid body held in equilibrium by the action of the forces transmitted across the chip-tool interface and across the shear plane in Figure 2.3. R is the resultant tool force acting at the tool cutting edge. It is resolved into two components: i) the cutting force, $F_c$, and ii) the thrust force, $F_t$. R is also resolved into $F_s$ and $N_s$ as the shear and normal forces on the shear plane, and $F_f$ and $N_f$ as the friction and normal forces on the rake face. $\phi$ is the shear angle and $\alpha$ is the rake angle of the cutting tool. $\beta$ is the mean chip-tool friction angle on the tool face and given by $\arctan F_f/N_f$.

The Ernst and Merchants’ theory suggested that the shear angle, $\phi$, would take up a value that would reduce the work done in cutting to a minimum. The required value is given by:

$$2\phi + \beta - \alpha = \frac{\pi}{2} \quad (2-1)$$
Merchant also assumed that the dependence of the shear stress, $\tau_s$, on the normal stress, $\sigma_s$, at the shear plane by a relationship as given by:

$$\tau_s = \tau_0 + k\sigma_s \quad (2-2)$$

which indicates that the shear strength of the material $\tau_s$ increases linearly with an increase in the normal stress $\sigma_s$, on the shear plane. $\tau_0$ is the shear flow stress at zero plastic strain. It is also assumed that $k$ and $\tau_0$ are constants for the particular work material. Incorporating the force relations and after some conversions, the resulting expression is

$$2\phi + \beta - \alpha = C \quad (2-3)$$

where $C$ is given by $\arccot k$ and is a constant for the work material.

![Figure 2.3](image)

Figure 2.3 Force diagram developed by Ernst and Merchant [6], with the assumption of a single shear plane.
2.1.4.2 Lee and Shaffer Theory

The theory of Lee and Shaffer [9,10] was the result of studies aimed at applying the plasticity theory to the problem of orthogonal metal cutting. The following assumptions were made: 1) the material behaves as an ideal plastic, which does not strain-hardened; 2) the shear plane represents a direction of maximum shear stress.

The slip-line field proposed by Lee and Shaffer for orthogonal cutting of a continuous chip is shown in Figure 2.4. It can be seen that like Merchant, Lee and Shaffer [9, 10] have employed the idealized single shear plane model of cutting, where all the deformation takes place in a plane extending from the tool cutting edge to the point of intersection of the free surfaces of the work and chip. The triangular region ABC is plastically rigid and subjected to a uniform state of stress, which equals to the yield strength of the workpiece material. Both sets of slip lines are straight in the stress field, being parallel with AB and DC respectively. If the boundaries of this triangular zone are considered, it is clear that the shear plane AB must give the direction of one set of slip lines since the maximum shear stress must occur along the shear plane. Also, since no forces act on the chip after it has passed through the boundary AC, no stresses can be transmitted across this boundary. Thus, AC can be regarded as a free surface, and since the directions of maximum shear stress always meet a free surface at $\pi/4$, the angle CAB is equal to $\pi/4$. Finally, assuming that the stresses acting at the chip-tool interface are uniform (an unreasonable assumption, as shown in section 2.1.5.2), the principal stresses at the boundary BC will meet this boundary at angle $\beta$ and $\beta + \pi/2$. Directions of maximum shear stress lie at $\pi/4$ to the directions of principle stress, and thus the angle BCD is given by $\pi/4 - \beta$. It now follows from figure 2.4 that
\[ \frac{\pi}{4} + \beta - \alpha = \frac{\pi}{2} \]  
\[ (2-4a) \]

Thus,

\[ \phi + \beta - \alpha = \frac{\pi}{4} \]  
\[ (2-4b) \]

which is the required shear angle solution.

Because the workpiece was assumed to be rigid and perfectly plastic, it can not reflect the reality that the yield strength changes with changes in strain, strain rate, and temperature.

Figure 2.4 Lee and Shaffer's slip-line field model for orthogonal cutting [9, 10]

2.1.5 Mechanics of Chip Formation

2.1.5.1 Cutting Forces

\( F_c \) and \( F_t \) can be measured by a dynamometer mounted in the tool holder [5], then the relationship between the various forces can be easily derived from the force equilibrium diagram assumption [7] (Figure 2.3).
Shear force on shear plane:

\[ F_s = F_c \cdot (\cos\phi) - F_t \cdot (\sin\phi) \]  \hfill (2-5)

Normal force on shear plane:

\[ N_s = F_c \cdot (\sin\phi) + F_t \cdot (\cos\phi) \]  \hfill (2-6)

Friction force on rake face:

\[ F_r = F_c \cdot (\sin\alpha) + F_t \cdot (\cos\alpha) \]  \hfill (2-7)

Normal force on rake face:

\[ N_r = F_c \cdot (\cos\alpha) - F_t \cdot (\sin\alpha) \]  \hfill (2-8)

These results are valid for the perfect orthogonal cutting process only, where the radial force is zero, only the cutting force is exerted on the sharp tool, and the forces along the shear plane are uniformly distributed.

2.1.5.2 Stresses in the Workpiece

(1) Stresses on the Shear Plane

The shear plane is generally modeled to have uniform distributions of both shear and normal forces over its entire area. Thus, the shear stress \( \tau_s \) and normal stress \( \sigma_s \) can be computed as [5]:

\[ \tau_s = \frac{F_c \cdot \cos\phi - F_t \cdot \sin\phi}{t \cdot w / \sin\phi} \]  \hfill (2-9)

\[ \sigma_s = \frac{F_c \cdot \sin\phi + F_t \cdot \cos\phi}{t \cdot w} \]  \hfill (2-10)

where \( t \) is the uncut chip thickness (see Figure 2.1) that equals to the feed rate, and \( w \) is the width of the chip that equals to the depth of cut in turning process. The results of the traditional stress analyses are limited in predicting the workpiece material behaviour in
cutting processes, especially when the strain is higher than 1 and the strain rate is around \(10^3\) to \(10^5\) s\(^{-1}\) [11].

(2) Stresses Distributions on the Rake Face

The nature of the tool/chip interface and the distribution of the shear and normal stresses are critical in understanding the cutting process and the performance of cutting tools. The high stresses, coupled with the high temperatures and large strains in the chip adjacent to the tool face, make the secondary shearing process difficult to model.

A typical analysis of the forces and stresses on the rake face assumes that Columbic sliding friction is present, and that the stresses are uniformly distributed [5]. The coefficient of friction is velocity dependent: increasing speeds result in lower friction. The shear stress at the interface, \(\tau_f\), and the normal stress on the rake face, \(\sigma_f\), can be written as [5]:

\[
\tau_f = \frac{F_c \cdot \sin \alpha + N_s \cdot \cos \alpha}{w \cdot l} \quad (2-11)
\]

\[
\sigma_f = \frac{F_c \cdot \cos \alpha - N_s \cdot \sin \alpha}{w \cdot l} \quad (2-12)
\]

where \(l\) is the length of sliding contact. A value of 215 MPa normal stress on the rake face was calculated in Zhang’s work [12] for a 6061 aluminum sample. The measured cutting force and thrust force were 410 N and 240 N respectively. The rake angle was \(-5^\circ\) and the shear angle was \(15^\circ\). The length of sliding contact was 200 \(\mu\)m, and the width of the chip was 3.0 mm.

Above models have been found to be useful approximations of the behaviour of the chip as it slides over the tool. However, when cutting metals and alloys, over a large part of the interface, the work material and the tool are interlocked and atomically bonded.
to such an extent that normal sliding cannot occur. This condition is termed "seizure" [9].

In most modern industrial machining operations, high compressive stress, very large amounts and high rates of strain, the cleanliness of the work material in contact with the tool leads to the prediction that the seizure at the interface could not be avoided [13]. Such studies have revealed two major regions on the rake face with respect to flow: seized region and sliding region (Figure 2.5). Because of the high interface temperatures i.e. 650-900 °C when cutting steel [14], and pressures, i.e. 1000-1500 MPa [14], the material adjacent to the tool surface is almost stationary, and relative shearing takes place in the chip. As originally developed by Zorev [15], the variation of normal stress $\sigma_f$ along the rake face is determined by a power law:

$$\sigma_f = \sigma_{\text{max}} \left(\frac{x}{l}\right)^n$$  \hspace{1cm} (2-13)

where $\sigma_{\text{max}}$ is the maximum normal stress at the tool tip (i.e. $x = 1$), $l$ is the total length of contact of the chip on the tool, $x$ is the distance from the point at which the chip leaves the tool to the point of interest, and $n$ is the exponent which dependent on the material and tool properties.

It was suggested that the shear stress $\tau_f$ has a constant value over the seizure region because the chip material shears internally. Over the sliding region, the shear and normal stresses are related by the Coulomb law of friction [16]:

$$\tau_f = \mu \cdot \sigma_{\text{max}} \left(\frac{x}{l}\right)^n$$  \hspace{1cm} (2-14)
2.1.5.3 Strains and Strain Rates

Strain and strain rates generated during metal cutting were first calculated by Merchant [17] (Figure 2.6). The results of the analysis are as follows:

Shear angle: $\phi = \tan^{-1}\left(\frac{r \cdot \cos \alpha}{1 - r \cdot \sin \alpha}\right)$ \hspace{1cm} (2-15)

Where $r = \frac{t}{t}$

Shear strain: $\gamma = \tan(\phi - \alpha) + \cot\phi$ \hspace{1cm} or \hspace{1cm} $\gamma = \frac{\cos \alpha}{\sin \phi \cdot \cos(\phi - \alpha)}$ \hspace{1cm} (2-16)

Shear strain rate: $\dot{\gamma} = \frac{\cos \alpha}{\cos(\phi - \alpha)} \cdot \frac{V_c}{\Delta y}$ \hspace{1cm} (2-17)

where $\Delta y$ is the thickness of the shear band (narrow band that produce a lamellar structure in the chip [5]). The values of strain rates strongly depend on the shear angle. For special
cutting tool, the rake face angle $\alpha$ is a constant. $\Delta y$ depends on the material properties and the stress distribution. The cutting speed $v_c$ is a constant. Under this circumstance, strains and strain rates are monolithic functions of shear angle $\phi$. However the strain and strain rate remain constant only if the shear angle is assumed to have a fixed value. This is not the case from the experimental work of Zhang and Alpas [18] and Elmadagli and Alpas [2]. The shear strains generated during cutting of the 6061 Al in the chip formation area exhibit nonlinear distributions [18] (Figure 2.7). In the material immediately ahead of the tool tip, the equivalent strain has a value of 2.5. The strains gradually decreased from the tool tip value of 2.5 to 0.8 at the chip root. Therefore, all the stress, strain and strain rate are actually nonlinearly distributed in the deformation areas.

Figure 2.6 Schematic representation of shear strain in metal cutting process [17].
Figure 2.7 Equivalent strain distribution in the material (6061 Al) ahead of the tool tip, obtained from the work of Zhang and Alpas [18].

2.1.5.4 Energy Consumption

The total power $P$, or energy, required to complete the machining process is the combination of energy required 1) to deform the material, 2) to create new fresh surfaces, and 3) to move the chip along the rake face of the cutting tool. The conventional method to analyze energy consumption rate is usually calculated by the product of cutting speed $v_c$ and cutting force $F_c$ [5]:

$$ P = F_c \cdot v_c $$  \hspace{1cm} (2-17)

From equation (2-17), it can be seen that the power consumption is directly related to the cutting force. Therefore the process should be geared to minimize this force.
According to Wright's work, the rate of plastic deformation work done in the primary shear zone, $\dot{W}_p$ (assuming a discrete parallel-sided shear zone) can be calculated as [19]:

$$\dot{W}_p = k \cdot u_p \cdot s_p$$  \hspace{1cm} (2-18)

where $k$, $u_p$ and $s_p$ are the yield strength of the material, the shear velocity along the zone and the length of the primary shear zone, respectively. Similarly, in the secondary shear zone, the rate of plastic deformation energy is [19]:

$$\dot{W}_s = \lambda \cdot k \cdot u_c \cdot l$$

where the shear strength is multiplied by a constant $\lambda<1$ to account for the fact that some sliding occurs at the end of the contact length, and $u_c$ is the shear velocity or chip formation speed and $l$ is the length of the chip-tool contact length.

Wright [19] has shown that the total power curve ($\dot{W}_p + \dot{W}_s$) has a minimum value corresponding to a specific shear angle (Figure 2.8). If $\alpha = 6^\circ$, the minimum power of primary shear $\dot{W}_p$ arises when $\phi$ is away from $48^\circ$, provided that the tool is frictionless, i.e. $\dot{W}_s = 0$. However, as $\dot{W}_s$ is increased as schematically shown, the minimum in the total power curve ($\dot{W}_p + \dot{W}_s$) moves to lower $\phi$ values.
2.1.6 Temperature Increase and Its Effects on Machining

It was found that the cutting mechanism varies widely with the workpiece temperature. The amount of heat that causes the temperature rise depends on the operation parameters as well as the physical, mechanical properties of the workpiece and the tool materials. During the process of machining, heat is generated from three sources [20]:

1) Primary shear zone where plastic deformation by shearing occurs.
2) Secondary shear zone where the newly formed chip suffers from secondary deformation by friction between chip and tool rake face.

3) Secondary friction zone where the friction is between the worn surface on the tool flank face and the freshly machined surface.

All these heat components may transfer away from the heat sources, either by the moving chip, by conduction into the bulk of the workpiece, or into the cutting tool. The heat transferred and dissipated depends on the physical and thermal properties of the tool and the workpiece such as the density, \( \rho \) and the specific heat, \( C_p \). In the machining of metals, most of the heat is dispersed by the moving chip (about 80% of current cutting operation [20]).

Elmadagli and Alpas calculated strain distribution in commercially pure copper subjected to orthogonal cutting by shear angle measurements as well as temperature distribution from estimating the conversion of deformation energy to heat within the workpiece [2] (Figure 2.9 and Figure 2.10). The temperature increased from 25 °C to 137 °C at the base of the chip along the primary shear plane. The highest temperature calculated was 857 °C, which occurred immediately ahead of the tool tip.

The increased temperature can reduce the strength of the metal at the chip-tool interface, causing the decrease of cutting force. High temperature can cause the metal in the chip experience recovery and recrystallization or phase transformation in some alloys [21], affecting the chip formation. Recrystallization studies on interrupted cut specimens by Ramalingan and Black [22] showed that chip formation occurred by a process dominant in shear in a thin shear front, as soon as a balance is attained between strain hardening and dynamic recovery immediately ahead of the shear front.
Figure 2.9  The work of plastic deformation per unit volume of Cu workpiece material (MJ·m⁻³) between each increment of equivalent strain, obtained from the work of Elmadagli and Alpas [2].

Figure 2.10  Temperature distribution of Cu workpiece subjected to orthogonal cutting, obtained from the work of Elmadagli and Alpas [2].

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2.1.7 Deformation Microstructures of Workpiece Subjected to Dry Machining

It is important to understand the development of the deformation microstructures in the workpiece material in order to rationalize the energy expenditure process during machining operations. TEM (Transmission electron microscopy) and SEM (Scanning electron microscopy) investigations on the plastically deformed zone at the root of the chips in steels and non-ferrous materials were conducted by Ramalingam and Black [23]. This provided evidence for the formation of dense dislocation tangles in the primary deformation zone, which were oriented in a direction parallel to the direction of shear. Ni, Elmadagli, and Alpas [24] also used TEM to investigate the deformation structures developed in the workpiece (1100 aluminum) ahead of the tool tip when subjected to dry machining. The undeformed grains were equiaxed with relatively dislocation free interiors and had an average size of 4.6 ± 0.5 μm. The deformation microstructure in the primary deformation zone was characterized by the formation of an elongated subgrain structure. The average thickness of the subgrains was 380 nm, and their width was 730 nm. Grain growth occurred in the secondary deformation zone, resulting in an equiaxed grain structure of 1210 nm in diameter (Figure 2.11 (a) and (b)).
Figure 2.11  TEM micrographs of 1100 Al workpiece subjected to dry orthogonal machining: (a) TEM micrograph showing the microstructure formed in the primary deformation zone; (b) TEM micrograph showing the microstructure formed in the secondary deformation zone. Obtained from the work of Ni, Elmadagli, and Alpas [24].

2.1.8 Mechanics of Discontinuous (Serrated) Chip Formation

Most of the previous investigations on metal cutting were focused on continuous chip formation because it is relatively stable and simple to analyze. Discontinuous or serrated chip formation that involves adiabatic shear deformation has been observed in certain difficult to machine materials such as the titanium base and the nickel base superalloys, which have poor thermal properties and high strength at elevated temperature. Also other materials, such as the hardened AISI 4340 steel at high speed [25] show discontinuous chip formation.

Adiabatic shear deformation is a term used to describe the localization of plastic flow that occurs in many metals when they are deformed at high strain rates to large
plastic strains, for example in ballistic impact and machining. It usually manifests itself as zones of intense shear deformation and/or microstructural modification of the original material up to hundreds of micrometers wide, interspersed between regions of relatively lower and homogeneous deformation [26] (Figure 2.12).

![Serrated chip due to the shear localization in chips](image)

Figure 2.12 Serrated chip due to the shear localization in chips [26].

The material behaviour during the formation of shear bands in the chip is very complicated and there has been no confirmed model to describe the behaviour of workpiece material during the machining process to date. A schematic representation of shear localization and the onset of shear band formation are presented in Figure 2.13 [27]. Labelled points on the curve are associated with the various stages of deformation of the block of material. The material begins to strain (a), and then exhibits uniform deformation (B, C) up to the instability (D), after which shear bands begin to form (E).
Figure 2.13  Schematic representations of the shear localization process and an accompanying stress-strain curve [27].

Nakkalil [28] studied the localized adiabatic shear band formation in an eutectoid steel subjected to high strain rate compression. Shear bands in different metals could be
classified as either "transformed" or "deformed" on the basis of their appearance in metallographic section, see Figure 2.14 [29]. It was shown that the deformed and transformed shear bands rather than being two separate phenomena are only an outcome of the extent of adiabatic strain localization occurring during deformation; the deformed bands forming with lesser localized flow and the transformed bands forming with extensive localized flow.

The general tendency of several metals to form either "transformed" or "deformed" shear zone was reviewed and presented by Timothy [29] in Figure 2.15. Solid symbols represent metals tend to form "transformed" shear bands (region I); half open symbols represent metals tend to form "deformed" shear bands (region II); and open symbols represent metals do not tend to form discrete shear bands (region III). It can be seen that copper alloys and aluminum alloys tend to form deformed shear zones, while pure aluminum and copper rarely form discrete shear bands.

![Image](a) Transformed shear band in hot rolled titanium alloys, and (b) deformed shear band in a 7039 aluminum alloy [29].

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Figure 2.15 Thermal diffusivity at room temperature of different metals plotted against their resistance to adiabatic shear localization [29].

Orthogonal cutting studies on Fe -18.5 Ni - 0.52 C tempered martensitic steel were performed by Lemaire and Backofen [30]. The reversion of martensite to austenite suggested that discontinuous chips can result from adiabatic instability in the shear zone. The appearance of austenite was found to be conditional upon three events: a) deformation must be confined to a shear zone; b) temperature in the shear zone must reach a level at which the criterion for adiabatic instability is met; c) rapid shearing during instability must further elevate the shear zone temperature beyond $A_s$ (austenite start temperature).

Shaw et al. [31] suggested that chip serration of titanium alloys is due to the onset of instability in the cutting process which results from competing thermal softening and strain-hardening mechanisms in the primary shear zone. Shaw [32] also suggested that the formation of concentrated shear (also called adiabatic shear) bands was due to the poor
thermal properties (the low thermal conductivity and the low specific heat) of those alloys and the consequent concentration of thermal energy in these bands.

Recht [33] developed a model for catastrophic shear instability in metals under dynamic plastic conditions. Catastrophic shear occurs at a plastically deforming region within a material when the slope of the true stress-strain curve becomes zero. The local rate of change of temperature has negative effect on strength (thermal softening) which is equal to or greater than the positive effect of strain hardening. A simple criterion was formulated for catastrophic slip in the primary shear zone based on the thermophysical response of the work material under the conditions of cutting as

\[
0 \leq -\frac{\partial \tau}{\partial \epsilon} \left( \frac{\partial \tau}{\partial T} \right) \left( \frac{dT}{d\epsilon} \right) \leq 1.0
\]  

(2-19)

where \( \tau, \epsilon, \) and \( T \) refer to the shear stress, shear strain, and temperature respectively. Material will shear catastrophically when this ratio lies between 0 and 1; catastrophic shear will be imminent when the ratio is equal to 1. No catastrophic slip occurs when this ratio is greater than 1, in which case the material will strain-harden more than it will thermal soften.

Another thermal instability model was modified by Semiatin and Jonas [34]. This model has led to a simple equation relating the critical shear strain to localization \( \gamma \) during high strain rate deformation to other experimentally determined material parameters

\[
\gamma_i = -\frac{\rho C_p n}{\eta \left( \frac{\partial \tau}{\partial T} \right) \gamma, \gamma^*}
\]  

(2-20)
where $\rho$ is the density, $C_p$ is the specific heat, $n$ is the work hardening exponent, $\tau$ is the shear flow stress, $T$ is the temperature, $\gamma$ is the shear strain, $\dot{\gamma}$ is the shear strain rate, and $\eta$ is the fraction of plastic work converted to heat.

2.2 Finite Element Modelling

2.2.1 Introduction

In the last two decades, the finite element method has been applied to study and simulate metal cutting processes and various finite element simulation techniques have been developed (an example using finite element method is briefly demonstrated in appendix A through the analysis of a bar structure). The versatility of the finite element method allows it to take into account large deformation, strain rate effect, tool-chip contact and friction, local heating and temperature effect, different boundary and loading conditions, and other phenomena encountered in metal cutting problems. The challenge to modelling metal cutting process is that although various studies have been made to investigate the material behaviour in the machining process, there is still no satisfied materials constitutive model as a function of strain, strain rate, temperature, etc. to properly describe the material behaviour during cutting process.

From the viewpoint of numerical formulation, the analyses used in metal cutting may be divided into three major approaches: 1) Lagrangian approach, 2) Eulerian approach, and 3) Arbitrary Lagrangian-Eulerian (ALE) approach.
2.2.2 Comparison of Lagrangian, Eulerian, and ALE Element Formulations

Followings are the comparisons of Lagrangian, Eulerian, and ALE Element Formulations from LSTC (Livermore Software Technology Corporation)’s fluid-structure interaction modelling tutorial, Figure 2.16 [35].

Figure 2.16 Comparison of Lagrangian, Eulerian, and ALE mesh [35].

- In the Lagrangian approach, the finite element mesh consists of material elements that cover the region of analysis exactly. These elements are attached to the material and are deformed with the deformation of the workpiece. In the Eulerian mesh, the mesh can be considered as two overlapping meshes, one is a background mesh which is fixed in space and the other one is attached to the material which can “flow” through the fixed mesh. This may be visualized in 2 steps: First, the material is deformed in a...
Lagarangian step just like the Lagrangian formulation. Then, the element state variables in the Lagrangian elements are mapped or advected onto the fixed Eulerian mesh.

- The ALE mesh is similar to the Eulerian mesh. The main difference between the pure Eulerian and ALE method is different amounts of material being advected through the meshes due to the reference (fixed) mesh positions (see Figure 2.16).

### 2.2.3 Researches on Metal Cutting Using Finite Element Modelling

#### 2.2.3.1 Lagrangian Element Formulation

Lagrangian formulation is the most commonly used approach to dealing with finite element modelling of metal cutting processes by a number of researchers. As illustrated in section 2.2.1, in the Lagrangian approach, the finite element mesh consists of material elements that cover the region of analysis exactly. These elements are attached to the material and deformed with the deformation of the workpiece. Severe mesh distortion due to large strains and strain rates during metal cutting results in a degradation of the simulation accuracy, chip separation criterion \[36, 37, 38, 39, 40, 41, 42, 43\] and mesh adaptivity \[44, 45\] are the common techniques to compensate its disadvantages.

The separation criteria, which assume cutting is supposed to take place at the line representing the undeformed chip thickness, give a condition upon which nodes along this pre-determined line split or separate. Several types of conditions have been applied in the past research, each based on physical, and/or geometrical constraints. The geometric criteria relate to the distance between the overlapping nodes and the tool tip while the physical criteria include strain energy density, effective plastic strain and stress.
Lin and Lo [38] applied a geometry criterion to simulate ultra-precision orthogonal cutting for oxygen-free high-conductivity copper, with validation by numerical-experimental comparison in terms of cutting forces. The chip and the workpiece were connected by twin nodes along a predefined separation path OB. The chip will be separated when the distance D between the leading node and the tool edge was equal to or smaller than a given value $D_c$ (Figure 2.17). Johnson-Cook Constitutive equation [46], which takes the flow stress of workpiece material as a function of strain, strain rate and temperature was used in order to reflect realistic behaviour in metal cutting:

$$\sigma = (A + B\bar{\varepsilon}^n)(1 + C\dot{\varepsilon})(1 - \frac{\bar{T}}{T_m})$$

(2-21)

Where $\sigma$ is the flow stress in MPa; $A = 90$ MPa; $B = 292$ MPa (the strain-hardening coefficient); $C = 0.025$ (the strain-rate coefficient); $m = 1.09$; $n = 0.31$ (the strain-hardening exponent); $\bar{\varepsilon}$ is the equivalent plastic strain; $\dot{\varepsilon}$ is the plastic strain rate; $T_m$ is the melting temperature, $T_{room}$ is the room temperature.

In this study, temperature was found the most dramatic impact on the flow stress of OFHC copper among temperature, strain, and strain rate. When temperature effects were neglected, the stress in the chip varied from 200 to 500 MPa, but accounting for temperature effects decreased the stresses in the chip to between 200 and 400 MPa.

The same geometry criterion was used by Mamalis et al. [39] for the plane strain orthogonal cutting simulation of mild steel with 0.18 % C. The workpiece material was modeled as isotropic elastic-plastic, with isotropic strain-hardening. Strain, strain rate and temperature effects were also included in the flow stress constitutive equation:

$$\sigma = \hat{f}(T, \dot{\varepsilon})(\frac{\dot{\varepsilon}}{1000})^{0.0195}\varepsilon^{0.21}$$

(2-22a)
\[ f(T, \varepsilon) = 1394e^{-0.00118T} + 339e^{-0.0000184[T-(943+23.5\ln(\varepsilon/1000))]^2} \] (2-22b)

A sticking and a sliding region were considered at the tool/chip interface. Contours of equivalent plastic strain and stress were obtained, which revealed a maximum value of 1.86 of the plastic strain and a maximum value of 933 MPa of equivalent stress at the chip-tool interface (Figure 2.18). A divergence of about 11% between the predicted and the experimental cutting force was presented in this study.

Figure 2.17 Geometrical separation method: (a) before node separation; (b) after node separation, obtained from Lin and Lo’s work [38].

Figure 2.18 Contours of equivalent plastic strain and stress in simulating orthogonal machining of mild steel, obtained from the work of Mamalis [39].

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Lin and Lo [40] also used a strain energy density criterion to investigate the deformation state of mild steel under different low cutting velocities. During the cutting process, the node at the tool front end whose strain energy density had reached its critical point was judged suitable for separation into two points. The strain energy density was expressed as:

\[
\frac{dW}{dv} = \int_0^{\varepsilon_{ij}} \sigma_{ij} d\varepsilon_{ij}
\]

where \( W \) is the total work stored in an element, consisting of elastic deformable energy and plastic flow work, \( v \) is the volume of the element, \( \sigma_{ij} \) and \( \varepsilon_{ij} \) are the stress and strain components, respectively. The flow stress of the workpiece was expressed as a function of strain, strain rate and temperature. Experimental-numerical comparisons were made on the basis of cutting forces. The experimental results showed that the cutting force decreased from 1686 N to 1470 N as the cutting velocity increased from 16.5 m/min to 36 m/min, while the numerically calculated cutting force decreased from 1640 N to 1446 N.

The model developed by Xie et al. [41] applied an effective strain criterion to model the chip formation and the shear localization phenomena in the metal cutting process. The effective plastic strain \( \bar{\varepsilon}^P \) based on the von Mises criterion is calculated for every node at each time step. When the value of the effective plastic strain at a nodal point reached a prescribed critical value \( \bar{\varepsilon}_0^P \), that is,

\[
\bar{\varepsilon}^P \geq \bar{\varepsilon}_0^P
\]

the "tie" at this nodal point was broken and material separation occurred. In order to model the shear banding during chip formation, a strain-hardening thermal-softening...
model for the flow stress was used. In this investigation, cutting forces, distribution of effective stress on tool rake face, maximum shear stress and plastic strain fields in the workpiece material were predicted. However, the lack of thrust force (see definition in section 2.1.4.1) in FEA modelling may bring in some errors to the predicted cutting forces.

In stress based chip separation criterion, chip separation occurred when a critical stress state is achieved at a specified distance ahead of the tip of the cutting tool [42]:

\[ f = \sqrt{\left(\frac{\sigma_n}{\sigma_f}\right)^2 + \left(\frac{\tau}{\tau_f}\right)^2}, \text{ where } \sigma_n = \max(\sigma_2, 0) \]  \hspace{1cm} (2-25)

In the above, \( \tau \) and \( \sigma_n \) are the shear and normal stress components at a specified distance in front of the tool tip along the cutting path, and \( \sigma_f \) and \( \tau_f \) are the failure stresses of the material under pure tensile and shear loading conditions respectively. A critical combination is considered reached if the stress index \( f \) attains a value of 1.0. Shi et al. [42] used this chip separation technique to conduct a finite element study to address the issue of the effect of friction along the tool-chip interface in orthogonal metal cutting and to seek insight into possible ways of calibrating the friction behaviour along the tool-chip interface. The workpiece material was taken to be AISI 4340 steel and temperature dependent material properties were employed in the analysis. This was done by incorporating temperature-dependent Young’s modulus values and temperature-dependent elastic-plastic behaviour of the workpiece material in the numerical modelling. To account for the effect of friction along the contact surfaces between the chip and the cutting tool, contact surfaces were defined where a modified Coulomb friction law was applied. It was found that shear straining is localized in the primary shear zone while the
material near the tool tip underwent the largest plastic strain rate. The maximum temperature, the contact length, the shear angle, and the cutting force are found to depend strongly on the coefficient of friction (Figure 2.19).

Figure 2.19 Effect of friction on the cutting force and maximum temperature for various rake angle values, obtained from the work of Shi, Deng, and Shet. [42].

In order to gain an understanding of the reliability of the existing separation criteria for orthogonal metal cutting, different criteria, which included the effective plastic strain, strain energy density, the normal failure stress and the distance between the separation element node and tool tip, were examined by Zhang [47] using LS-DYNA3D finite element analysis software package. A simple finite element model under plane-strain deformation was used to investigate the mechanics of cutting from the incipient stage. The workpiece materials studied were elastic-perfectly plastic and elastic-plastic with work hardening. The study showed that since no single threshold of separation existed for different cutting conditions, none of the existing criteria is universal. A more comprehensive criterion needs to be established to provide consistent and reliable FEM simulation.
Johnson-Cook failure model [48,49,50] is a more recently used model in metal cutting modelling. The strain at failure $\varepsilon^f$ which was assumed to be dependent on a non-dimensional plastic strain rate, a pressure-deviatoric stress ratio $P/\sigma_{eff}$ (where $P$ is the hydrostatic pressure stress and $\sigma_{eff}$ is the effective stress) and the temperature terms. Ng and Aspinwall [48] utilized this criterion to simulate continuous and segmental chip formation when machining AISI H13 tool/die steel with polycrystalline cubic boron nitride (PCBN) tools. Experimental involving chip morphology and cutting forces were used to validate the model. Temperature and stress field distribution were investigated in the study. The temperature generated in the shear zone was higher with the segmented chip (up to 700 °C) than with the continuous chip (up to 250 °C), for the same machining parameters. The stress field distributions in the shear zone were very different. With the segmental chip, the magnitude of the stress was 1.2-1.4 GPa and was highly localized, while for continuous chip formation, the stress magnitude was substantially lower in the range 0.7-0.9 GPa and was distributed over a wide region as show in Figure 2.20. However, the difficulty in using this failure model is that the failure constants in this model need to be experimentally determined in advance.

Chip separation criterion, which is a key component of Lagrangian metal cutting procedures, approximates material fracture rather than the actual behaviour of material deformation. In order to deal with the difficulties associated with deformation induced element distortion, adapting the mesh has been incorporated in Lagrangian approach by some researchers. The elements are subdivided into smaller elements when original elements of the mesh lose accuracy due to mesh distortion. A new less distorted mesh is created and all the parameters like stresses, strains, damages are updated into the new
Ceretti et al. [44,45] used the commercial code DEFORM 2D to simulate the cutting process of AISI 1045 steel using both mesh smoothing and remeshing algorithms. The workpiece material was assumed to be strain, strain rate, and temperature dependent by defining different flow stress curves at various temperatures and strain rates. In this investigation, continuous chip formation was simulated by changing the default remeshing procedure while the segmented chip formation was simulated by deleting the elements close to the tool tip that had been subjected to high deformation and stress. This required a very dense mesh close to the tool tip in order to minimize volume loss during the simulation. The elements separated when a criterion, based on the accumulate damage was satisfied. This damage value was determined from a uniaxial tensile test. The influence of cutting speed, rake angle, and depth of cut on chip geometry and cutting forces were investigated in this study. The shortcomings of the model were lack of experimental verification and the damage value obtained from uniaxial tensile test can’t represent the real damage characteristic under high strain and strain rate during cutting process.

Figure 2.20  Stress distributions (MPa) during (a) continuous and (b) segmental chip formation, obtained from the work of Ng and Aspinwall[48].

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Marusich and Ortiz [51] also applied continuous remeshing in modelling high speed machining of AISI 4340 steel. The model accounted for dynamic effects, heat conduction, variable contact friction, and crack propagation leading to fracture. Mesh refinement was applied only to elements, which exceeded a critical value of plastic power density. In order to model the discontinuities in chip formation of AISI 4340 steel, a fracture model, which was a critical effective strain dependent on the fracture toughness of the material, was implemented which allowed for arbitrary crack initiation and propagation in the regime of shear localized chips. Average steady state temperatures along the tool-chip interface were near 1000 °C and drop off rapidly into the interior. The effective plastic strain attained values in the range 2-3 within the localized zone and 6-12 along the tool face. The only validation of the model was that numerical chip morphologies were in approximate range of experimental observations.

During mesh adaptive process, the mesh would become finer because of the increase of the element number. Therefore, the major disadvantage of continuous mesh adaptivity is the high cost in computation time due to small incremental time steps required for proper mesh adaptivity.

2.2.3.2 Eulerian Element Formulation

The application of Eulerian formulation used to be limited to fluid-structure interaction problems and only applied to solid mechanics problems recently. Not much literature on metal cutting modelling using Eulerian method can be found. Since the mesh is constructed on a fixed domain, the primary advantages of the Eulerian formulation are that no explicit material failure criterion is required and that the element cannot become
overly distorted. Therefore, Eulerian formulation captures the continuous flow of material around the tool, enabling a physically realistic model of machining of ductile materials, without remeshing or node splitting. But it requires custom software development and is less well suited to modelling discontinuous chip formation [52].

Caroll and Strenkowski [53] used both updated Lagrangian and Eulerian methods, and modelled the workpiece material as viscoplastic to investigate the cutting process of aluminium 2024-T361 at slow speed. The model by Eulerian method is more accurate and computationally less intensive than an updated Lagrangian approach. The disadvantage is that the final shape of the chip cannot be easily predicted. Strenkowski and Moon [54] improved the model by including a free surface algorithm to determine the final chip geometry. The workpiece material in this study was aluminum alloy 6061-T6 and the cutting speed ranged from 0.25 to 3 m/s. The model incorporated a procedure for predicting the chip geometry and the contact length. With this information, the thermal conduction path between the chip and the tool could be determined, which allowed for calculation of the tool temperatures. In addition, the cutting forces, stress, strain rate distributions in the workpiece were also determined.

Eulerian formulation was also successfully employed by Raczy et al. [55] to predict the stress and strain distributions in the material (copper) subjected to orthogonal cutting. Material behaviour of the workpiece was studied using an elastic plastic hydrodynamic material model incorporated with a Voce-type stress-strain relationship. An alternative material model was based on the Johnson-Cook constitutive equation. Both of the simulation results were validated by experimental results of strain, stress, and cutting force. According to the hydrodynamic material model, the equivalent strain was
3.50 in the material 50 μm directly ahead of the tool tip, which compared well with the experimentally measured strain 3.65. Numerical and experimental stress and strain distributions correlated well in terms of both magnitudes and distributions. By using Johnson-Cook equation, the predicted strain distribution ahead of tool tip fairly accurate, while the predicted tool tip stresses were higher than those from the hydrodynamic material model due to the power-law nature of Johnson-Cook equation.

2.2.3.3 ALE Element Formulation

Owing to the large deformations and very high strain rates and temperatures involved in cutting process, the numerical modelling presents significant numerical and analytical challenges. An Arbitrary Lagrangian-Eulerian (ALE) formulation, which combines the advantages of both Lagrangian and Eulerian formulations in a single description, has been developed and applied to model high deformation problems, such as metal forming and machining. Benson [56,57] was one of the pioneers to introduce the concept of the ALE element formulation and illustrate the characteristics of the formulation. The algorithms for the remesh step for Eulerian and ALE is the same. Both Eulerian and ALE formation may have elements containing more than one material, while Lagrangian formulation virtually assumes that each element is restricted to a single material.

Altintas [58] applied ALE formulation to predict cutting variables such as temperature, strain, strain rate, stress distribution and residual stresses on the finish surface when machining P20 mold steel by chamfered Carbide and CBN tools. The FE predicted maximum temperature of about 1200 Celsius on the rake face when the cutting
speed and feed rate were 240m/min and 0.06mm/rev, respectively. Olovsson et al. [59] used ALE formulation in a two dimensional finite element model to simulate the orthogonal cutting process of steel. The shape of the chip and the plastic strain distribution were illustrated in Figure 2.21. But detailed conclusions were difficult to draw regarding the quality of the results.

![Effective plastic strain](image)

**Figure 2.21** Deformed mesh and distribution of effective plastic strain using ALE formulation during numerical simulation of steel, obtained from the work of Olovsson, Nilsson, and Imonsson. [59].

The ALE approach has also been employed by Movahhedy et al.[60] to model the metal cutting process. An initial chip geometry is assumed in this study. During the analysis, part of the mesh is Eulerian (fixed in space), while the motion on free
boundaries was of Lagrangian nature. An elastic-plastic analysis with linear strain-hardening is performed on a material (not specified in the paper) with a Young’s modulus of 200 GPa and an initial yield stress of 414 GPa. The strain rate and temperature effects on material properties were neglected. Contours of effective stress in the primary and secondary deformation zone were presented in the paper, but the numerical results were not validated by experiment measurements. O. Pantalé et al. [61] presented a two-dimensional and three-dimensional finite element model of unsteady state metal cutting performed with the Abaqus/Explicit finite element code. The yield stress was taken as a function of strain, strain rate and temperature. In stead of using one of the separation criterions, a Johnson-Cook damage law, which took into account strain, strain rate, temperature and pressure, was used in the model to better represent the reality. Cutting force results agreed with experimental result very well. Von Mises stresses and temperature distributions are shown at different stages of the cutting process (Figure 2.22).
Figure 2.22 Simulation results from Pantalé [61] representing cutting 42CrMo4 steel using ALE formulation: (a) Chip formation and temperature field in 2D modelling; Von Mises stresses distribution at time $t = 0.4$ ms and time $t = 1.5$ ms in 3D modelling.

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2.3 Machining Parameters and Their Influences on Cutting Process

The machining operation is highly dependent on the machining parameters, which fall into 3 categories: 1) the workpiece properties, which include the mechanical, physical, chemical, and thermal properties of the workpiece material; 2) the cutting tool properties, which involve the tool geometry and the material properties of the cutting tool material; and 3) the cutting parameters, which are cutting speed, feed rate, and depth of cut. It is very important that optimized cutting parameters be selected in controlling the quality required for surface finish. The most common factors investigated in metal cutting research are the rake angle of the cutting tool, cutting speed and feed rate. Cutting force, energy consumption and built-up edge (BUE) formation are the most often used factors to evaluate their influences.

2.3.1 Rake Angle

As illustrated in Figure 2.1, rake angle is the angle between the rake face of the tool and the direction perpendicular to the cutting direction. Williams et al. [62] studied the machining behaviour by doing cutting experiments of a range of metals and alloys. A greatly increased cutting force was observed with a 6° rake angle compared with 35° rake angle when machining aluminium, iron and copper at a low speed. For copper, the cutting force at 6° rake angle was almost as three times big as that from 35° rake angle. The chip thickness was also much larger than for cutting with a 35° rake angle. The iron chips became discontinuous with the increased strain, while copper chips showed considerable cracking. Lo [36] and Shih [63] investigated the effect of tool rake angle on cutting force, chip formation and equivalent stress and strain distributions in the workpiece ahead of the
tool tip when cutting low carbon steel and OFHC copper by finite element analysis. They found that the cutting force decreased with the increase of the rake angle, which results in a reduced equivalent stress and strain distribution in the primary and secondary deformation zones (Figure 2.23). The specific power requirements also decreased with the increase of the rake angle [5] because of the decreased cutting force.

2.3.2 Cutting Speed

Cutting speed is the surface speed of the lathe rotation in a turning operation. A decreasing cutting force with increasing cutting speed was observed by Williams [62]. For iron, copper, and aluminum, the cutting force at 800 f.p.m dropped to 25-50 % of the 5 f.p.m. value. This decrease was associated with a reduction in the strength of the metal at the chip-tool interface, presumably due to the higher temperatures attained at higher cutting speeds. Two and three-dimensional cutting simulations with different cutting speeds conducted by Lin and Lo [40], and Lin and Yarng [64] gave similar results. Both the normal stress on the tool rake face and the residual stress of machined workpiece decreased with increase in cutting velocity. Hua and Shivpuri [65] performed orthogonal cutting simulation of Ti-6Al-4V at three different cutting speeds. Different chip morphologies were observed and the distance between chip serration decreased with the increase of cutting speed (Figure 2.24).

The cutting speed has a significant effect on the specific power and/or energy because the coefficient of friction on the rake face is speed dependent. Increasing speeds decrease the friction (up to a point), thus decreasing the specific power through the frictional component of specific power [5].

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Figure 2.23  Plastic strain (1) and effective stress (2) contours during steady-state orthogonal cutting process under different rake angles: (a) \( \alpha = -2^\circ \), (b) \( \alpha = 0^\circ \), (c) \( \alpha = 5^\circ \), (d) \( \alpha = 15^\circ \), obtained from the work of Shih [63].

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Figure 2.24 Chip morphology: (a) experimental results; (b) simulation results, obtained from the work of Hua and Shivpuri [65].

2.3.3 Feed Rate

Feed rate is the lateral distance traveled by the tool during one revolution during turning process. It’s often related to power consumption and finished surface during cutting process. Not much research effort has been made in exploring the effects of the feed rate on the cutting process. Thomas and Beauchamp [66] statistically analyzed cutting force, tool vibration, and tool-modal-parameter data generated by lathe dry turning of mild carbon steel samples at different speeds, feeds, depths of cut, tool nose radii, tool lengths and workpiece lengths. Manna and Bhattacharayya [67] studied the machinability of Al/SiC composites under different cutting speeds, feed rates and depths of cut. They both discovered that the cutting force increased with the increase of the feed rate which means more power is needed to complete the cutting operation. The chance of formation of BUE was also discussed in their studies. BUE is generally formed between the chip and the rake face of the cutting tool. It changes the actual rake angle, which may
change the direction of chip flow and may directly affect the cutting force [67]. A low feed rate, which reduces the forces and increases damping, allows the reduction of tool vibration and a good surface roughness to be obtained, when the cutting speed is high enough for avoiding BUE formation [66].

From literature review, it can be seen that metal cutting is a complex process with non-linear and coupled thermomechanical characteristics. The combinations of operation parameters determine the efficiency of a given machining process as well as the surface quality of the workpiece. Parametric studies are very essential in order to have a good understanding for this operation.
CHAPTER 3

Experimental and Numerical Studies on the Deformation State of Copper (the Effect of Feed Rate)

3.1 Test Materials and Orthogonal Cutting Tests

For these tests, commercial purity electrolytic tough pitch copper (C11000) received in the form of a hot extruded rod with 25.4 mm diameter was used. The rod was then machined into a tubular shape with 3.0 mm wall thickness before the orthogonal cutting tests. The mean grain size of Cu was measured as 18.5 ± 5.0 μm according to ASTM E112-96 [68]. The average microhardness of the material, measured by a Knoop indenter using a load of 25 gf, was 90 ± 10 kg·mm⁻².

Orthogonal cutting tests were performed on a Harrison M3000 lathe without using metal removal fluids. A rapid action foot controlled brake with an electrical disengagement mechanism was used in the cutting process to stop the cutting action. The cutting tool insert used was a SiAlON grade silicon nitride based ceramic cutter (Kennametal CNGA-432) with a negative 5° rake angle (α). The cutting speed was kept constant at 0.6 m·s⁻¹. The three feed rates investigated were 0.25, 0.50 and 0.80 mm/rev.

3.2 Experimental Determination of Deformation State of the Workpiece

3.2.1 Deformation Microstructures of the Workpiece Material Ahead of the Tool Tip

Machined samples were sectioned for metallographic examination of their deformation microstructures as illustrated in Figure 3.1 (a). The distance between the machined surface and the cutting line (t) is defined as depth of cut, which equals to the
value of the feed rate in each revolution (Figure 3.1 (b)). Samples were prepared using the standard metallographic preparation techniques and etched with a solution of two parts of HNO₃ and acetic acid, and one part of H₃PO₄. Optical micrographs showing the deformation microstructures of the machined samples under three different feed rates are presented in Figures 3.2 (a-c). The extent of plastic deformation is revealed by the significant shape change of the grains. It can be seen that the grains that had an equiaxed shape in the bulk material but then became elongated in the deformation direction when they entered the deformation zone at the root of the chip (the primary deformation zone). The grains within the primary deformation zone continued to align their long axes with the direction of deformation (as depicted by the lines drawn on Figure 3.2 (a). At a depth below the machined surface, the morphologies of the grains were unaffected by the cutting process and this depth can be regarded as the depth of the plastically deformed layer. The orientation changes of the grain boundaries due to plastic deformation ahead of the tool tip are illustrated in Figure 3.1 (b).
Figure 3.1 (a) Schematic diagram of metallographic section taken from specimens for microstructural analysis, and (b) Section A-A showing the workpiece and tool geometry during the process of orthogonal metal cutting, where $\alpha$ is the rake angle and $t$ is the uncut chip thickness.
Figure 3.2 Optical cross-sectional microstructures of workpiece material (Cu) ahead of the tool tip, which have undergone orthogonal cutting: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 3.2 Continued
3.2.2 Strain and Stress Measurements Ahead of Tool Tip in the Workpiece Material

The shear angle (\(\phi\)), which is defined as the value of the angle of a straight line tangent to the flow lines at each point within the workpiece was used to estimate the direction of the plastic flow at that point (Figure 3.1 (b) and Figure 3.2 (a)). The values of local equivalent strains \(\bar{\varepsilon}^p\) in the material ahead of the tool tip were estimated using [2,69,70,71,72]:

\[
\bar{\varepsilon}^p = \frac{\sqrt{3}}{3} \tan \theta
\]  

(3-1)

where \(\theta (90^\circ - \phi)\) is the deformation angle (the complement of the shear angle).

Microhardness measurements were done in order to estimate local flow stress values in the workpiece ahead of tool tip. The measurements were taken at regular intervals within the deformation zone as shown in Figures 3.3 (a-c) by using a load of 25 gf. Two additional measurements were made at 30 \(\mu\)m above and below each indentation point at the intersection of the grid points in Figures 3.3 (a-c), and it is the mean value of these measurements that is marked as local microhardness on these figures. Local flow strength values of the material were estimated using the following equation [73]:

\[
\sigma = \frac{H}{C}
\]  

(3-2)

where \(\sigma\) is the flow stress, \(H\) is the microhardness, and \(C = 3.0\) [73] is a constant.
Figure 3.3  The variation of the microhardness of the material (Cu) ahead of the tool tip under three different feed rates: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.5 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 3.3 Continued

(b) $f = 0.8 \text{ mm/rev}$

(c) $f = 0.5 \text{ mm/rev}$

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3.2.3 Determination of Strain and Stress Distributions in the Workpiece Material Ahead of the Tool Tip

The experimentally determined strain distributions in the workpiece material ahead of the tool tip under different feed rates are shown in Figures 3.4 (a-c) and similarly the local flow strength values are shown in Figure 3.5 (a-c). General trends emerge in local strain values that are common to tests done at all three feed rates. The maximum strain occurred ahead of the tool tip (at \( x = 50 \, \mu m, \ y = 0 \, \mu m \)) in the range of 3.2-3.6. The value of maximum strain increased slightly from 3.2 at \( f = 0.25 \, \text{mm/rev} \) to 3.4 at \( f = 0.50 \, \text{mm/rev} \) and to 3.6 at \( f = 0.80 \, \text{mm/rev} \). The strain decreased from the maximum value at the tool tip to the chip root along cutting line (see Figure 3.1(b)) and also decreased with the increasing distance from the cutting line. The strains in the chip reached a constant value of 1.3 - 1.5 of a given distance above the cutting and remain constant in the remainder of the chip. At the rake face, secondary deformation occurred due to the contact with the tool, resulting in an increase in strain larger than 3.

Although the strain distribution in the workpiece ahead of the tool tip under the three different feeds was similar, there were some differences: the depth of the plastically deformed layer (the region between the cutting line and \( \varepsilon^p = 0.1 \) contour, see Figure 3.1(b)) increased with the feed rate, which were approximate 300 \( \mu m \) (at 0.25 mm/rev), 400 \( \mu m \) (at 0.50 mm/rev), and 500 \( \mu m \) (at 0.80 mm/rev). Another observation is that for the same magnitude of strain, the distributed distance from the tool tip increased with feed rate, which means that the plastic deformation penetrates more when the feed rate is bigger. For example, if compare the strain value of 1.1 along the cutting line, this distance increased from 600 \( \mu m \) (at 0.25 mm/rev) to 1000 \( \mu m \) (at 0.80 mm/rev). The thickness of
the primary deformation zone, defined as the area between the location of the iso-strain contour of $\varepsilon^p = 0.1$ and the strain contour where constant chip strain was reached, increased with the feed rate. The primary deformation zone thicknesses were around 350 $\mu$m (at 0.25 mm/rev), 650 $\mu$m (at 0.50 mm/rev), 800 $\mu$m (at 0.80 mm/rev). The width of the secondary deformation zone, defined by the iso-strain contour of $\varepsilon^p = 2$, also became wider as the feed rate increased, which were estimated as 80 $\mu$m (0.25 mm/rev), 100 $\mu$m (0.50 mm/rev), and 130 $\mu$m (0.80 mm/rev).

![Diagram](image)

Figure 3.4 Strain distributions in the workpiece material (Cu) ahead of the tool tip under three different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 3.4 Continued
The stress distributions in the primary deformation zone under 3 different feed rates were also very similar. The maximum flow stress, which in the range of 443 - 448 MPa, was located ahead of the tool tip (at $x = 50 \, \mu m$, $y = 0 \, \mu m$) which corresponded to the location of the maximum strain. It then decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. Along the cutting line, the stress decreased from the maximum value of 443 ~ 448 MPa to around 360 MPa at the chip root.

![Stress distributions in the workpiece material(Cu) ahead of the tool tip under three different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.](image)

Figure 3.5 Stress distributions in the workpiece material(Cu) ahead of the tool tip under three different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 3.5 Continued
3.2.4 Stress-Strain Curve for the Cu Workpiece in Orthogonal Cutting

Stress and strain values determined at each point in the workpiece ahead of the tool tip under different feed rates by matching local stress and strain values on Figures 3.4 and 3.5 are plotted in Figure 3.6 to determine the stress-strain relationship for the workpiece. The workpiece material shows rapid strain hardening at the initial section of the flow curve and reaches a saturation stress for strains larger than 2. It is seen that the stress and strain data obtained at different feed rates can be combined to form a single flow curve. Thus a unique stress-strain relationship for Cu under the orthogonal cutting condition can be obtained independent of feed rate. A regression analysis showed that the experimental flow stress and equivalent plastic strain values obeyed in a Voce type exponential relationship [74] as shown in Figure 3.6 (a):

\[
\sigma = \sigma_s - (\sigma_s - \sigma_0) \exp \left( -\frac{\bar{\varepsilon}^p}{\varepsilon_c} \right) \quad (3-3)
\]

where \( \sigma \) is the value of the equivalent flow stress; \( \bar{\varepsilon}^p \) is the corresponding equivalent strain; \( \sigma_s \) (448 MPa) is the saturation stress at which the work hardening rate becomes zero; \( \sigma_0 \) (312 MPa) is the flow strength of the material; and \( \varepsilon_c \) (0.7) is a constant.

An alternative way to express the Voce equation is to use its differential form as:

\[
\frac{d\sigma}{d\bar{\varepsilon}^p} = \frac{\sigma_s}{\varepsilon_c} (1 - \frac{\sigma}{\sigma_s}) \quad (3-4)
\]

where \( (d\sigma/d\bar{\varepsilon}^p) \) is the work hardening rate. The work hardening rate versus the flow stress curve was plotted in Figure 3.6 (b), which again shows that the Voce equation is a good representation of the experiment data, with saturation stress \( \sigma_s = 448 \) MPa (at \( \bar{\varepsilon}^p = 0 \)).
Figure 3.6 (a) Stress-strain relationship of commercial purity copper subjected to orthogonal cutting, and (b) Variation of the work hardening rate with the flow stress.
It should be noted that the saturation stress for the orthogonal cutting of Cu obtained in this study (448 MPa) is in good agreement with the saturation stress value in the literature for the same material during large strain metal forming process. For example, Stevillano, Houtte and Aemoudt [75] reported that ETP copper samples deformed by wire drawing plus torsion exhibited a saturation stress of 450 MPa at effective strains larger than 2.5.

3.3 Finite Element Simulations

3.3.1 Geometry and Finite Element Mesh

All finite element simulations were completed using LS-DYNA version 970 [76] release 3858 (Fundamentals of LS-DYNA are given in Appendix B) on a personal computer utilizing dual 2.0 GHz Athlon processors with 1 GB of RAM. Each simulation had a termination time of 20 ms and the processing time for a typical simulation was approximately 150 hrs. The solution results were then viewed and analyzed in the post-processing software package LS-POST.

An illustration of the geometry of specific parts of the finite element model used in this investigation is given in Figure 3.7. The cutting speed and the rake angle are specified as 0.6m/s and -5° respectively. Depths of cut were specified as 0.25 mm, 0.50 mm and 0.80 mm respectively corresponding to the feed rates used in the experiments. To reduce the computation time, a subset of the experimental configuration was considered. The workpiece, airmesh, and cutting tool were discretized between two planes of XY symmetry as illustrate in Figure 3.7. One 48th of the thickness of the experimental configuration was modelled spanning a distance of 0.0625 mm.
The workpiece, airmesh, and cutting tool were the three parts used in the numerical model. The workpiece was discretized using finite element of Lagrangian formulation, while an Eulerian element formulation was selected for the workpiece and airmesh. Since the material associated with an Eulerian FE formulation is not constrained to the original mesh, an airmesh was constructed to allow the material to flow out of the workpiece mesh and deform throughout the duration of the simulation.

There were 25471 elements (which include 25171 single-point integration Eulerian elements with single material and void for the workpiece and airmesh and 300 Belytschko-Tsay Lagrangian elements for the cutting) and 51680 nodes in each simulation.

A summary of the FE input files for copper are attached in Appendix C.
Figure 3.7 Schematic diagram of the finite element model of the tool and the workpiece (Cu): (a) 3D view showing the geometry of specific parts of the finite element model and xy symmetry planes; (b) cross-sectional view showing element distribution

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3.3.2 Workpiece and Tool Material Modeling

An elastic plastic hydrodynamic material model in LS-DYNA (*MAT_ELASTIC_PLASTIC_HYDRO, material type 10) was used. This material model utilizes the von Mises yield criterion. The effective stress is defined in terms of the deviatoric stress tensor, \( S_{ij} \), as:

\[
\bar{\sigma} = \left( \frac{3}{2} S_{ij} S_{ij} \right)^{1/2}
\]

and effective plastic strain by:

\[
\bar{\varepsilon}^p = \int_0^t \left( \frac{2}{3} D^p_{ij} D^p_{ij} \right)^{1/2} dt
\]

where \( t \) denotes time and \( D^p_{ij} \) is the plastic component of the rate of deformation tensor.

Sixteen data points (presented in Table 3.1) were taken from experimentally determined stress-strain relation of Cu workpiece to describe the material yield behaviour.

The Grüneisen equation of state (*EOS_GRUNEISEN [76]) was utilized to describe the pressure-volume relationship of the workpiece. It defines pressure for compressed materials by relating the material shock velocity and particle velocity through a cubic equation:

\[
P = \frac{\rho_0 C^2 \mu \left[ 1 + \left( 1 - \frac{\gamma_0}{2} \right) \mu - \frac{a}{2} \mu^2 \right]}{\left[ 1 - (S_1 - 1) \mu - S_2 \frac{\mu^2}{\mu + 1} - S_3 \frac{\mu^3}{(\mu + 1)^2} \right]} + (\gamma_0 + a\mu)E
\]  

(3-7a)

and

\[
P = \rho_0 C^2 \mu + (\gamma_0 + a\mu)E
\]  

(3-7b)
for expanding materials. In equation 3-7a and 3-7b, C is the bulk sound speed; \( S_1, S_2 \) and \( S_3 \) are the linear, quadratic, and cubic coefficients relating material shock velocity to particle velocity respectively; \( \gamma_0 \) is the Grüneisen gamma; \( a \) is the first order volume correction to \( \gamma_0 \); and \( \mu = \rho / \rho_0 - 1 \) (where \( \rho \) is current density and \( \rho_0 \) is initial density). The constants required for input in the Grüneisen equation of state obtained from the work of Steinberg [77] were: \( C = 0.394 \text{ cm} \cdot \mu \text{s}^{-1}, \quad S_1 = 1.489, \quad S_2 = 0, \quad S_3 = 0, \quad \gamma_0 = 2.02, \) and \( a = 0.47. \)

Table 3.1 Flow stress versus equivalent plastic strain data points used in hydrodynamic material model

<table>
<thead>
<tr>
<th>Equivalent Strain, ( \bar{\varepsilon}^p )</th>
<th>Flow Stress ( \sigma ), MPa</th>
<th>Equivalent Strain, ( \bar{\varepsilon}^p )</th>
<th>Flow Stress ( \sigma ), MPa</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>312.2</td>
<td>1.6</td>
<td>433.92</td>
</tr>
<tr>
<td>0.2</td>
<td>345.87</td>
<td>1.8</td>
<td>437.34</td>
</tr>
<tr>
<td>0.4</td>
<td>371.18</td>
<td>2.0</td>
<td>439.92</td>
</tr>
<tr>
<td>0.6</td>
<td>390.20</td>
<td>2.2</td>
<td>441.85</td>
</tr>
<tr>
<td>0.8</td>
<td>404.49</td>
<td>2.5</td>
<td>443.89</td>
</tr>
<tr>
<td>1.0</td>
<td>415.23</td>
<td>3.0</td>
<td>445.84</td>
</tr>
<tr>
<td>1.2</td>
<td>423.30</td>
<td>4.0</td>
<td>447.25</td>
</tr>
<tr>
<td>1.4</td>
<td>429.36</td>
<td>11.0</td>
<td>447.70</td>
</tr>
</tbody>
</table>

The airmesh was assigned the same material and equation of state properties as the workpiece. Since the deformation of the tool is negligible compared to the workpiece, the tool material behaviour was modelled as rigid. The density and shear modulus of the workpiece were specified as 8.89 Mg·m⁻³ and 47.0 GPa, respectively. The properties of the cutting tool were specified as follows: the density \( \rho = 3.20 \text{ Mg} \cdot \text{m}^{-3} \), Young’s modulus \( E = 300.0 \text{ GPa} \), and Poisson’s ratio \( \nu = 0.28. \)
3.3.3 Workpiece-tool Contact

Contact between the tool (Lagrangian) and workpiece (Eulerian) was implemented through a penalty based contact algorithm defined as *CONSTRAINED _ LAGRANGE _ IN _ SOLID in LS-DYNA. Eulerian and Lagrangian contact coupling was conducted by employing a 3×3×3 point grid to represent the virtual nodes associated with the workpiece (Eulerian) material, which were checked for penetration into the tool.

The values of the static and dynamic coefficients of friction for contact between the workpiece and the tool were specified as zero, since it was assumed that the experimentally observed material response of the workpiece was related to the contact between itself and the cutting tool. Under this assumption, the experimentally determined stress-strain material response of the workpiece would incorporate the frictional effects of the workpiece/tool contact.

3.3.4 Boundary Conditions

A velocity of 0.6 m/s in the negative X direction was assigned to the cutting tool. The bottom elements of the workpiece and air mesh were fully constrained from motion. Nodes of the air mesh lying on the XY-planes of symmetry were restricted to move only within those planes.

3.4 Predictions of Workpiece Properties by Numerical Simulations and Comparisons with Experiments

3.4.1 Chip Thickness Predictions by Numerical Simulations

The chip geometries from the numerical simulations are presented in Figure 3.8. These are compared with the experimentally measured chip thickness values in Figure 3.2.
and the results are shown in Figure 3.9. According to both simulations and experimental observations, the chip thickness increased with the feed rate (or depth of cut). The experimentally measured chip thicknesses were 1.5 mm at 0.25 mm/rev, 1.9 mm at 0.50 mm/rev and 2.5 mm at 0.80 mm/rev. According to the numerical simulations, the chip thicknesses were 0.95 mm at 0.25 mm, 1.7 mm at 0.50 mm and 2.4 mm at 0.80 mm respectively. The good agreement between metallographically measured and calculated chip thickness especially at high feed rates is clear from Figure 3.9.

Figure 3.8 Cross-sectional chip (Cu) geometry under different depths of cut at time of 15 ms: (a) depth of cut \( t = 0.25 \) mm; (b) depth of cut \( t = 0.5 \) mm; (c) depth of cut \( t = 0.8 \) mm. Chip thicknesses are (a) 0.95 mm, (b) 1.7 mm, and (c) 2.4 mm respectively.
3.4.2 Numerical Simulation Results of Strain and Stress Distributions in the Workpiece Material Ahead of the Tool Tip

The numerically computed strain distributions (from LS-POST) in the workpiece material ahead of the tool tip under different depths of cut are shown in Figures 3.10 (a-c) and similarly the stress distribution are shown in Figures 3.11 (a-c). In order to clearly demonstrate the strain and stress distributions in the workpiece material ahead of the tool tip, iso-strain and iso-stress contours from LS-POST were traced by a digitizing software package namely UN-SCAN-IT. The converted data points were then imported in Microsoft Excel to get the final contours as illustrated in Figures 3.12 (a-c) and Figures 3.13 (a-c).

General trends emerge in strain values are similar to that from experimental measurement. The maximum strain occurred at the tool tip in the range of 4.1 - 4.6. The maximum strain increased slightly from 4.1 at t = 0.25 mm to 4.6 at t = 0.50 mm, then
decreased to 4.2 at $t = 0.80$ mm. The numerically calculated strain in the material ahead of the tool tip (at $x = 50$ $\mu$m, $y = 0$ $\mu$m) increased from 2.4 at $t = 0.25$ mm to 2.9 at $t = 0.50$ and 0.80 mm. The strain decreased from the maximum value at the tool tip to the chip root along cutting line and also decreased with the increasing distance from the cutting line. The strain in the chip reached values ranging from 1.5 to 2.0 at $t = 0.25$ mm, 1.8 to 2.0 at $t = 0.50$ mm, and 1.3 to 2.0 at $t = 0.80$ mm. At the chip root, the strain values were around 0.5 at $t = 0.25$ mm, 0.7 at $t = 0.50$ mm, and 1.3 at $t = 0.80$ mm.

The stress distributions in the primary deformation zone under 3 different depths of cut were also very similar. The maximum flow stress (448 MPa), was located at the tool tip which corresponded to the location of the maximum strain. It then decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. Along the primary shear plane, the stress decreased from the maximum value of 448 MPa to 380 MPa (at $t = 0.25$ mm), 400 MPa (at $t = 0.50$ mm), and 430 MPa (at $t = 0.80$ mm) at the chip root. The stress in the chip reached values ranging from 430 to 440 MPa at all the three depths of cut.
Figure 3.10 Strain contours in the workpiece material (Cu) ahead of the tool tip under different depths of cut from LS-POST when simulation time was 15 ms: (a) depth of cut = 0.25 mm; (b) depth of cut = 0.5 mm; (c) depth of cut = 0.8 mm
C11000: E-P-H MATERIAL WITH FC=0.0, FEE

Time = 0.014998
Contours of Effective Plastic Strain
max ipt. value
min=-9.77001e-021, at elem# 5387
max=4.10038, at elem# 7008

Figure 3.10 Continued

C11000: E-P-H MATERIAL WITH FC=0.0, FEE

Time = 0.014998
Contours of Effective Stress (v-m)
max ipt. value
min=0, at elem# 10377
max=447307, at elem# 10122

Fringe Levels

4.473e+005
4.026e+005
3.578e+005
3.131e+005
2.684e+005
2.237e+005
1.789e+005
1.342e+005
8.946e+004
4.473e+004
0.000e+000

Figure 3.11 Stress (in kPa) contours in the workpiece material (Cu) ahead of the tool tip under different depths of cut from LS-POST when simulation time was 15 ms: (a) depth of cut = 0.25 mm; (b) depth of cut = 0.5 mm; (c) depth of cut = 0.8 mm.
Figure 3.11 Continued
Figure 3.12 Strain distributions in the workpiece material (Cu) ahead of the tool tip under different depths of cut from simulation predictions: (a) depth of cut = 0.25 mm; (b) depth of cut = 0.5 mm; (c) depth of cut = 0.8 mm.
Figure 3.12 Continued
Figure 3.13 Stress distributions in the workpiece material (Cu) ahead of the tool tip under different depths of cut from simulation predictions: (a) depth of cut = 0.25 mm; (b) depth of cut = 0.5 mm; (c) depth of cut = 0.8 mm.
Figure 3.13 Continued
3.4.3 Comparisons of Computed Strain Distributions with Experimental Strains

According to computations, the maximum values of strain (4.1 at \( t = 0.25 \text{mm} \), 4.6 at \( t = 0.50 \text{mm} \) and 4.2 at \( t = 0.80 \text{mm} \)) occurred immediately ahead of the tool tip. It then decreased with the increasing distance from the tool tip to values in the range of 0.7 (at \( t = 0.25 \text{mm} \)) -1.3 (at \( t = 0.80 \text{mm} \)). If compared along the primary shear plane, the strain in the material ahead of the tool tip (at \( x = 50 \mu\text{m}, y = 0 \mu\text{m} \)) from experimental measurements was 3.2 while the simulation result was about 2.4 when depth of cut was 0.25 mm, which the difference was around 25 %. In the middle part of the primary plane, the experimentally found equivalent strain was 1.4, while the numerically predicted value was 0.9. The strain at the chip root was found 1.3 from experimental measurements and 0.5 from the numerical simulation. When depth of cut was 0.50 mm, the strain in the material ahead of the tool tip (at \( x = 50 \mu\text{m}, y = 0 \mu\text{m} \)) from experimental measurements was 3.4 while the simulation result was about 2.9, which the difference was around 15 %. In the middle part of the primary plane, the experimentally found equivalent strain was 1.4, while the numerically predicted value was 1.1, a difference of 21.4 %. The strain at the chip root was found 1.1 from experimental measurements and 0.7 from the numerical simulation. It was observed that the strain distributions under \( t = 0.8 \text{mm} \) have the similar difference between experimental and simulation results (Figure 3.14 (c)). The reason as to why experimentally observed \( \varepsilon^p \) were always larger than those determined by simulation could be attributed to the influence of the machining history on the actual samples. During the turning process, the workpiece in fact endured more than one pass of cutting before it was interrupted during the experiment. Residual strains (and stresses) on the
previously machined surface would accumulate in the workpiece and resulted in higher strain values in the workpiece during the subsequent revolution.

3.4.4 Comparisons of Computed Stress Distribution with Experimental Stresses

It has been observed that the magnitudes of the numerically calculated stresses were very close to those determined by experimental measurements (Figure 3.15). A maximum stress of 448 MPa occurred immediately ahead of the tool tip, corresponding to the location of the maximum strain (4.1 at \( t = 0.25 \text{mm} \), 4.6 at \( t = 0.50 \text{mm} \) and 4.2 at \( t = 0.80 \text{mm} \)). The stress then decreased from this maximum value along the cutting line and decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. If compared along the primary shear plane, an excellent agreement was found. The stress in the material ahead of the tool tip (at \( x = 50 \mu\text{m}, y = 0 \mu\text{m} \)) obtained from the experimental measurements was 443 MPa while the simulation prediction was 440 MPa for a depth of cut of 0.25 mm, which gives a difference of only 1 %. In the middle part of the primary shear plane, the experimentally found equivalent stress was 425 MPa, while the numerically predicted value was 400 MPa, a difference of 5.9 %. The stress at the chip root was 420 MPa from experimental measurement and 380 MPa from the numerical simulation. The stress distributions under the other two feed rates also had small differences between experimental and simulation results. All the stresses around the tool tip, in the middle and at the chip root were much similar. The differences under the three feed rates were within 7.3 %.
Figure 3.14 Comparison of plastic strain between experimental and simulation results (Cu) under different feed rates (depths of cut) along the primary shear plane: (a) 0.25 mm; (b) 0.50 mm; (c) 0.80 mm.
Figure 3.15 Comparison of flow stress between experimental and simulation results (Cu) under different feed rates (depths of cut) along the primary shear plane: (a) 0.25 mm; (b) 0.50 mm; (c) 0.80 mm. Unit: MPa.
3.4.5 Comparison of the Widths of the Primary Deformation Zones between Experimental and Simulation Results

The differences between the widths of the primary zone determined experimentally and by simulations are presented in Figure 3.16. The widths of the primary deformation zone from both experimental measurements and numerical predictions increased with the increase of feed rate.

The experimentally measured widths of the primary deformation zone under the three different feed rates were 450 μm (0.25 mm/rev), 650 μm (0.50 mm/rev), 800 μm (0.80 mm/rev), while the numerically calculated widths of the primary deformation zone were 300 μm (0.25 mm), 550 μm (0.50 mm), and 700 μm (0.80 mm).

![Figure 3.16](image)

Figure 3.16 Comparison of the width of primary deformation zone calculated from experimental observations and simulation results under three different feed rates (depths of cut)(Cu workpiece).

3.4.6 Variations of the Cutting Force and Energy Consumption with Feed Rate

Figure 3.17 presents the variations of cutting force (F_c) with respect to simulation time from LS-POST. The cutting force increased rapidly at the beginning of the contact...
between the workpiece and the cutting tool. It then increased gradually until reach a steady state. The higher the feed rate, the longer it took for a chip to reach steady state formation and for the force to reach steady state value. When the simulation time was 15 ms, a steady state can be regarded having formed because the cutting force almost kept constant for all the situations. The cutting force was found to increase with respect to increasing depth of cut. At simulation time = 15 ms, the cutting force increased from 820 N to 2551 N when depth of cut increased from 0.25 mm to 0.80 mm, which corresponds to a 211 % increase.

An experimental cutting force of 1177 N was found when cutting an ETP copper with a cutting speed of 0.6 m/s and a feed rate of 0.25 mm/rev [3]. The numerically predicted cutting at the same cutting condition was 820 N. The difference is around 30 %.

To calculate the energy consumption during machining, the product of cutting speed \( v \) and cutting force \( F_c \) (the horizontal cutting force from the resultant force on the chip) is taken [5]:

\[
P = F_c \cdot v_c
\]  \hspace{1cm} (3-8)

Figure 3.18 shows the cutting force and energy consumption as a function of the feed rate obtained from numerical analyses when simulation time was 15 ms. The energy consumption also increased with the feed rate to deform the material, to create fresh surfaces, and to move the chip along the rake face of the cutting tool.
Figure 3.17 The cutting force versus time curves under different depths of cut for copper from simulation results.

Figure 3.18 Variations of the cutting force and energy consumption with depth of cut from simulations (Cu).
If material removal rate $R$ ($R = v_c \cdot f \cdot w$, which is the product of cutting speed, $v_c$, feed rate, $f$, and width of chip, $w$, which equals to the wall thickness 3 mm in this paper) is considered, then the energy consumption per unit volume of material removal or the specific cutting energy $U$ is given as [10]

$$U = \frac{P}{R} = \frac{F_c}{w \cdot f \cdot v_c} = \frac{F_c}{w \cdot f}$$ (3-9)

According to Equation 3-9 the specific cutting energy under three different depths of cut are calculated as 1093 MJ·m⁻³ at $t = 0.25$ mm, 1071 MJ·m⁻³ at $t = 0.50$ mm, and 1063 MJ·m⁻³ at $t = 0.80$ mm from the simulation results, which shows a slightly decrease (~2.8 %). This is consistent with the results for a low-carbon steel [10].

As mentioned above, the energy expended per unit volume during the deformation of the material ahead of the tool tip can also be calculated from the area under the stress-strain curve in Figure 3.6. For each increment of strain, the work of plastic deformation, $W$ (per unit volume) is given as [2]

$$W = \int_{\varepsilon_n^p}^{\varepsilon_{n+1}^p} \sigma \, d\varepsilon$$ (3-10. a)

and from Eq. (3-3),

$$W = \int_{\varepsilon_n^p}^{\varepsilon_{n+1}^p} (\sigma_s - (\sigma_s - \sigma_0) \exp(-\frac{\varepsilon^p}{\varepsilon_c})) \, d\varepsilon$$ (3-10.b)

Equation 3-10b can be used to determine the plastic work expended in the material ahead of the tool tip between iso-strain contours plotted in Figure 4 [2]. The calculated specific work of plastic deformation in primary and secondary deformation zones under three different feed rates are listed in Table 3.2. From Table 3.2, it can be seen that the specific work of plastic deformation in primary deformation zones decreased

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with feed rate, while the specific work of plastic deformation in secondary deformation zones increased with feed rate. The specific work of plastic deformation in primary deformation zones decreased 14.5 % when feed rate increased from 0.25 mm/rev to 0.80 mm/rev, while the specific work of plastic deformation in secondary deformation zones increased 35.2 % when feed rate increased from 0.25 mm/rev to 0.80 mm/rev.

Table 3.2 The calculated specific work of plastic deformation under three different feed rates

<table>
<thead>
<tr>
<th>Feed rate mm/rev</th>
<th>Specific work of plastic deformation in primary deformation zone, MJ·m⁻³</th>
<th>Specific work of plastic deformation in secondary deformation zone, MJ·m⁻³</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.25</td>
<td>587.3</td>
<td>750.9</td>
</tr>
<tr>
<td>0.50</td>
<td>544.8</td>
<td>883.3</td>
</tr>
<tr>
<td>0.80</td>
<td>502.0</td>
<td>1015.5</td>
</tr>
</tbody>
</table>

3.4.7 Temperature Distribution in the Workpiece Material Ahead of the Tool Tip

During the metal cutting process, a large portion of the work done during plastic deformation is transformed into heat. A temperature increase will occur in the workpiece material. The temperature increase (ΔT) due to conversion of deformation energy to heat within a unit volume of material with density ρ, and specific heat capacity, C_p can be expressed as [28]:

\[
ΔT = \frac{\eta}{ρC_p} \int_{\varepsilon_a}^{\varepsilon_{a+1}} \sigma d\varepsilon
\]

(3-11)

where \(\eta\) is the fraction of plastic work converted into heat. It is generally assumed that \(\eta = 0.95\) [28]. For copper, \(ρ = 8.91\) g·cm⁻³, and \(C_p = 385\) J/kg·°C. The temperature 

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distributions in the workpiece material ahead of the tool tip under three different feed rates are presented in Figure 3.19. The temperature distributions in the primary deformation zones under the different feed rates are very similar. Plastic deformation increases the temperature of the material from room temperature (25 °C) to 161-185 °C in the chips above a distance from the cutting line. Large temperature increase was observed in the material adjacent to the rake face. The highest temperature occurred directly ahead of the tool tip (x = 50 μm, y = 0 μm) and increased with the increase of the feed rate. The values of the highest temperature are 393 °C for f = 0.25 mm/rev, 418 °C for f = 0.50 mm/rev, and 442 °C for f = 0.80 mm/rev.

Figure 3.19 Temperature distributions (°C) in the workpiece material (Cu) ahead of the tool tip under three different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 3.19 Continued
CHAPTER 4

Experimental and Numerical Studies on the Deformation State of 1100 Aluminum (the Effect of Feed Rate)

4.1 Test Materials and Orthogonal Cutting Tests

The material investigated was commercial purity aluminum (1100 Al) received in the form of a hot extruded rod. The rod was then machined into a tubular shape with 25.4 mm outer diameter and 3.0 mm wall thickness before the orthogonal cutting tests. The average microhardness of the material, measured by a Knoop indenter using a load of 25 gf, was 43 ± 5 kg·mm⁻².

Orthogonal cutting tests were performed on a Harrison M3000 lathe without using metal removal fluids. A rapid action foot controlled brake with an electrical disengagement mechanism was used in the cutting process to stop the cutting action. The cutting tool insert used was a SiAlON grade silicon nitride based ceramic cutter (Kennametal CNGA-432) with a negative 5° rake angle (α). The cutting speed was kept constant at 0.6m·s⁻¹. The three feed rates investigated were 0.25, 0.50 and 0.80 mm/rev.

4.2 Experimental Determination of Deformation State of the Workpiece

4.2.1 Deformation Microstructures of the Workpiece Material Ahead of the Tool Tip

Machined samples were sectioned for metallographic examination of their deformation microstructures as the same method described in Chapter 3. Samples were prepared using the standard metallographic preparation techniques and etched with a solution of Graff Sargent (84 ml H₂O, 0.5 ml HF, 15.5 ml HNO₃, and 3g CrO₃). Optical
micrographs showing the deformation microstructures of the machined samples under three different feed rates are presented in Figures 3.1 (a-c). The extent of plastic deformation is revealed by the significant orientation of the flow lines (deformation lines formed during previous extrusion process). It can be seen that the flow lines paralleled to the extrusion direction (longitudinal direction of the tubular workpiece) in the bulk material but then became bended in the deformation direction when they entered the deformation zone at the root of the chip (the primary deformation zone). The flow lines within the primary deformation zone continued to align with the direction of deformation (as depicted by the lines drawn on Figure 3.1 (a). At a depth below the machined surface, the direction of flow lines was unaffected by the cutting process and this depth can be regarded as the depth of the plastically deformed layer. The orientation changes of the flow lines due to plastic deformation ahead of the tool tip can be seen from the microstructure pictures.
Figure 4.1 Optical cross-sectional microstructures of workpiece material (Al 1100) ahead of the tool tip, which have undergone orthogonal cutting: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
4.2.2 Strain and Stress Measurements Ahead of Tool Tip in the Workpiece Material

The shear angle (ϕ) was used to estimate the direction of the plastic flow at that point (Figure 4.1 (a)). The values of local equivalent strains $\bar{e}^p$ in the material ahead of the tool tip were estimated by using equation 3-1.

Microhardness measurements were done in order to estimate local flow stress values in the workpiece ahead of tool tip. The measurements were taken at regular intervals within the deformation zone as shown in Figures 4.2 (a-c) by using a load of 25 gf. Two additional measurements were made at 30 µm above and below each indentation point at the intersection of the grid points in Figures 4.2 (a-c), and it is the mean value of these measurements that is marked as local microhardness on these figures. Local flow strength values of the material were estimated by using equation 3-2.

4.2.3 Determination of Strain and Stress Distributions in the Workpiece Material Ahead of the Tool Tip

The experimentally determined strain distributions in the workpiece material ahead of the tool tip under different feed rates are shown in Figures 4.3 (a - c) and similarly the local flow strength values are shown in Figure 4.4 (a - c). General trends emerge in local strain values that are common to tests done at all three feed rates. The maximum strain occurred ahead of the tool tip (at $x = 100 \mu m$, $y = 0 \mu m$) in the range of 3.4 - 3.8. The value of maximum strain increased slightly from 3.4 at $f = 0.25 \text{ mm/rev}$ to 3.6 at $f = 0.50 \text{ mm/rev}$ and to 3.8 at $f = 0.80 \text{ mm/rev}$. The strain decreased from the maximum value at the tool tip to the chip root along cutting line and also decreased with the increasing distance from the cutting line. The strains in the chip reached a constant
value of 1.8 - 2.1 of a given distance above the cutting and remain constant in the remainder of the chip. At the rake face, secondary deformation occurred due to the contact with the tool, resulting in an increase in strain larger than 3.

Although the strain distribution in the workpiece ahead of the tool tip under the three different feeds was similar, there were some differences: the depth of the plastically deformed layer (the region between the cutting line and $\bar{e}^P = 0.1$ contour) increased with the feed rate, which were approximate 600 $\mu$m (at 0.25 mm/rev), 800 $\mu$m (at 0.50 mm/rev), and 1000 $\mu$m (at 0.80 mm/rev). Another observation is that for the same magnitude of strain, the distributed distance from the tool tip increased with feed rate, which means that the plastic deformation penetrates more when the feed rate is bigger. For example, if compare the strain value of 1.8 along the cutting line, this distance increased from 600 $\mu$m (at 0.25 mm/rev) to 1000 $\mu$m (at 0.80 mm/rev). The thickness of the primary deformation zone, defined as the area between the location of the iso-strain contour of $\bar{e}^P = 0.1$ and the strain contour where constant chip strain was reached, increased with the feed rate. The primary deformation zone thicknesses were around 800 $\mu$m (at 0.25 mm/rev), 1100 $\mu$m (at 0.50 mm/rev), 1400 $\mu$m (at 0.80 mm/rev). The width of the secondary deformation zone, defined by the iso-strain contour of $\bar{e}^P = 3.0$, also became wider as the feed rate increased, which were estimated as 150 $\mu$m (0.25 mm/rev), 180 $\mu$m (0.50 mm/rev), and 200 $\mu$m (0.80 mm/rev).
Table 4.2 The variation of the microhardness of the material (1100 Al) ahead of the tool tip cut using different feed rates: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.5 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 4.2 Continued
Figure 4.3  Experimentally determined strain distributions in the workpiece material (1100 Al) ahead of the tool tip cut using three different feed rates: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 4.3 Continued
The stress distributions in the primary deformation zone under three different feed rates were also very similar. The maximum flow stress, which in the range of 284 - 293 MPa, was located ahead of the tool tip (at x = 100 μm, y = 0 μm) which corresponded to the location of the maximum strain. It then decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. Along the cutting line, the stress decreased from the maximum value of 284 - 293 MPa to around 230 - 250 MPa at the chip root.

![Stress Distributions Diagram](image)

**Figure 4.4** Experimentally determined stress distributions in the workpiece material (1100 Al) ahead of the tool tip cut using different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 4.4 Continued
4.2.4 Stress-Strain Curve for the Al Workpiece in Orthogonal Cutting

Stress and strain values determined at each point in the workpiece ahead of the tool tip under different feed rates by matching local stress and strain values on Figures 4.3 and 4.4 are plotted in Figure 4.5 to determine the stress-strain relationship for the workpiece. The workpiece material shows rapid strain hardening at the initial section of the flow curve and reaches a saturation stress for strains larger than 3. It is seen that the stress, and strain data obtained at different feed rates can be combined to form a single flow curve. Thus a unique stress-strain relationship for Al under the orthogonal cutting condition can be obtained independent of feed rate. A regression analysis showed that the experimental flow stress and equivalent plastic strain values obeyed in a Voce type exponential relationship [29] as shown in Figure 6 (a):

\[
\sigma = 302 - (302 - 140)\exp\left(\frac{-\bar{\varepsilon}^p}{1.4}\right)
\]  

(4-1)

where \(\sigma\) is the value of the equivalent flow stress; \(\bar{\varepsilon}^p\) is the corresponding equivalent strain.

The work hardening rate versus the flow stress curve was plotted in Figure 4.5 (b), which again shows that the Voce equation is a good representation of the experiment data, with saturation stress \(\sigma_s = 302\) MPa (at \(\bar{\varepsilon}^p = 0\)).
Figure 4.5  (a) Stress-strain relationship of 1100 aluminum subjected to orthogonal cutting, and (b) Change of the work hardening rate with the flow stress.
4.3 Finite Element Simulations

4.3.1 Geometry and Finite Element Mesh

The geometry of the finite element model used for 1100 Al is the same as that used for copper, except the height of the airmesh increased from 5.84 mm to 6.76 mm. Each simulation has a termination time of 20 ms and the processing time for a typical simulation was approximately 190 hrs.

There were a total of 29356 elements (which include 29026 single-point integration Eulerian elements with single material and void for the workpiece and airmesh and 330 Belytschko-Tsay Lagrangian elements for the cutting tool) and 59480 nodes in each FE model.

A summary of the FE input files for aluminum are attached in Appendix D.

4.3.2 Workpiece and Tool Material Modeling

An elastic plastic hydrodynamic material model in LS-DYNA (*MAT_ELASTIC_PLASTIC_HYDRO, material type 10) was used, which is the same model in the analysis of copper.

Sixteen data points (presented in Table 4.1) were taken from experimentally determined stress-strain relation of Al workpiece to describe the material yield behaviour. The Grüneisen equation of state (*EOS_GRUNEISEN) was also utilized to describe the pressure-volume relationship of the workpiece. The constants required for input in the Grüneisen equation of state obtained from the work of Steinberg [77] were: $C = 0.5386 \text{ cm} \cdot \mu \text{s}^{-1}$, $S_1 = 1.339$, $S_2 = 0$, $S_3 = 0$, $\gamma_0 = 1.97$, and $a = 0.48$. 

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Table 4.1 Flow stress versus equivalent plastic strain data points used in hydrodynamic material model

<table>
<thead>
<tr>
<th>Equivalent Strain, $\varepsilon^p$</th>
<th>Flow Stress $\sigma$, MPa</th>
<th>Equivalent Strain, $\varepsilon^p$</th>
<th>Flow Stress $\sigma$, MPa</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>140.2</td>
<td>2.5</td>
<td>274.9</td>
</tr>
<tr>
<td>0.2</td>
<td>161.7</td>
<td>3.0</td>
<td>283.0</td>
</tr>
<tr>
<td>0.4</td>
<td>180.4</td>
<td>3.5</td>
<td>288.7</td>
</tr>
<tr>
<td>0.6</td>
<td>196.6</td>
<td>4.0</td>
<td>292.7</td>
</tr>
<tr>
<td>0.8</td>
<td>210.6</td>
<td>6.0</td>
<td>299.8</td>
</tr>
<tr>
<td>1.0</td>
<td>222.8</td>
<td>8.0</td>
<td>301.5</td>
</tr>
<tr>
<td>1.5</td>
<td>246.6</td>
<td>10.0</td>
<td>301.9</td>
</tr>
<tr>
<td>2.0</td>
<td>263.2</td>
<td>12.0</td>
<td>302.0</td>
</tr>
</tbody>
</table>

The airmesh was assigned the same material and equation of state properties as the workpiece. Since the deformation of the tool was negligible compared to the workpiece, the tool was modelled as a rigid material. The density and shear modulus of the workpiece were specified as 2.71 Mg·m$^{-3}$ and 27.0 GPa, respectively. The properties of the cutting tool were specified as follows: the density $\rho = 3.20$ Mg·m$^{-3}$, Young’s modulus $E = 300.0$ GPa, and Poisson’s ratio $\nu = 0.28$.

4.3.3 Workpiece-tool Contact

Contact between the tool (Lagrangian) and workpiece (Eulerian) was implemented through a penalty based contact algorithm defined as *CONSTRAINED _ LAGRANGE _ IN _ SOLID in LS-DYNA. Eulerian and Lagrangian contact coupling was conducted by employing a 3×3×3 point grid to represent the virtual nodes associated with the workpiece (Eulerian) material, which were checked for penetration into the tool.
The values of the static and dynamic coefficients of friction for contact between the workpiece and the tool were specified as 0.0, since it was assumed that the experimentally observed material response of the workpiece was related to the contact between itself and the cutting tool. Under this assumption, the experimentally determined stress-strain material response of the workpiece would incorporate the frictional effects of the workpiece/tool contact.

4.3.4 Boundary Conditions

A velocity of 0.6 m/s in the negative X direction was assigned to the cutting tool. The bottom elements of the workpiece and air mesh were fully constrained from motion. Nodes of the air mesh lying on the XY-planes of symmetry were restricted to move only within those planes.

4.4 Predictions of Workpiece Properties by Numerical Simulations and Comparisons with Experiments

4.4.1 Chip Thickness Predictions by Numerical Simulations

The chip thickness values calculated using the numerical simulations are presented in Figure 4.6. These are compared with the experimentally measured chip thickness values in Figure 4.1 and the results are shown in Figure 4.7. According to both simulations and experimental observations, the chip thickness increased with the feed rate (or depth of cut). The experimentally measured chip thicknesses were 1.8 mm at 0.25 mm/rev, 2.2 mm at 0.50 mm/rev and 3.0 mm at 0.80 mm/rev. According to the numerical simulations, the chip thicknesses were 1.15 mm at 0.25 mm, 2.0 mm at 0.50 mm and 2.9 mm at 0.80 mm respectively. The good agreement between
metallographically measured and calculated chip thickness especially at high feed rates is clear from Figure 4.7.

Figure 4.6 Cross-sectional chip (Al 1100) geometry under different depths of cut at time of 15 ms: (a) depth of cut $t = 0.25$ mm; (b) depth of cut $t = 0.5$ mm; (c) depth of cut $t = 0.8$ mm.

Figure 4.7 Comparison of chip (1100 Al) thickness between experimental and simulation results under different feed rates (depths of cut).
4.4.2 Numerical Simulation Results of Strain and Stress Distributions in the Workpiece Material Ahead of the Tool Tip

The numerically computed strain distributions (From LS-POST) in the workpiece material ahead of the tool tip under different depths of cut are shown in Figures 4.8 (a - c) and similarly the stress distribution are shown in Figures 4.9 (a - c). The iso-strain and iso-stress contours are illustrated in Figures 4.10 (a - c) and Figures 4.11 (a - c).

General trends emerge in strain values are similar to that from experimental measurement. The maximum strain occurred at the tool tip in the range of 5.5 - 5.9. The maximum strain increased slightly from 5.6 at t = 0.25 mm to 5.9 at t = 0.50 mm, then decreased to 5.5 at t = 0.80 mm. The numerically calculated strain in the material ahead of the tool tip (at x = 100 μm, y = 0 μm) increased from 3.4 at t = 0.25 mm to 3.6 at t = 0.50 mm, then decreased to 3.4 again at t = 0.80 mm. The strain decreased from the maximum value at the tool tip to the chip root along cutting line and also decreased with the increasing distance from the cutting line. The strain in the chip reached values ranging from 1.8 to 2.5 at all the three depths of cut. At the chip root, the strain values were around 1.1 at t = 0.25 mm, and 1.3 at t = 0.50 and 0.80 mm.

The stress distributions in the primary deformation zone under 3 different depths of cut were also very similar. The maximum flow stress (298 - 300 MPa), was located at the tool tip which corresponded to the location of the maximum strain. It then decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. Along the primary shear plane, the stress decreased from the maximum value of 300 MPa to 230 MPa at t = 0.25 mm, and 240 MPa at t = 0.50 and 0.80mm at the chip root. The stress in the chip reached values ranging from 250 to 280 MPa at all the three depths of cut.
Figure 4.8 Strain contours in the workpiece material (1100 Al) ahead of the tool tip under different depths of cut from LS-POST when simulation time was 15 ms: (a) $t = 0.25$ mm; (b) $t = 0.50$ mm; (c) $t = 0.80$ mm.
Figure 4.8 Continued

Figure 4.9 Stress (in kPa) contours in the workpiece material (1100 Al) ahead of the tool tip under different depths of cut from LS-POST when simulation time was 15 ms: (a) $t = 0.25$ mm; (b) $t = 0.50$ mm; (c) $t = 0.80$ mm.
Figure 4.9 Continued
Figure 4.10  Numerically determined strain distributions in the workpiece material (1100 Al) ahead of the tool tip under different depths of cut: (a) t = 0.25 mm; (b) t = 0.50 mm; (c) t = 0.8 mm.
Figure 4.10 Continued
Figure 4.11 Numerically predicted stress distributions in the workpiece material (1100 Al) ahead of the tool tip under different depths of cut: (a) $t = 0.25$ mm; (b) $t = 0.50$ mm; (c) $t = 0.80$ mm.
Figure 4.11 Continued
4.4.3 Comparisons of Computed Strain Distributions with Experimental Strains

According to computations, the maximum values of strain (5.6 at t = 0.25 mm, 5.9 at t = 0.50 mm and 5.5 at t = 0.80 mm) occurred immediately ahead of the tool tip. It then decreased with the increasing distance from the tool tip to values in the range of 1.1 (at t = 0.25 mm) - 1.3 (at t = 0.80 mm). If compared along the primary shear plane, the strain in the material ahead of the tool tip (at x = 100 \, \mu m, y = 0 \, \mu m) was 3.4 from both experimental measurements and numerical predictions when depth of cut was 0.25 mm. In the middle part of the primary plane, the experimentally found equivalent strain was 2.0, while the numerically predicted value was 1.5, which the difference was 25 %. The strain at the chip root was found 1.3 from experimental measurements and 1.1 from the numerical simulation. When depth of cut was 0.50 mm, the strain in the material ahead of the tool tip (at x = 100 \, \mu m, y = 0 \, \mu m) was 3.4 from both experimental measurements and numerical predictions. In the middle part of the primary plane, the experimentally found equivalent strain was 1.8, while the numerically predicted value was 1.5, a difference of 15 %. The strain at the chip root was found 1.4 from experimental measurements and 1.3 from the numerical simulation. It was observed that the strain distributions under t = 0.8 mm have the similar difference between experimental and simulation results (Figure 4.12 (c)). The reason as to why experimentally observed $\bar{\varepsilon}_p$ were always larger than those determined by simulation could be attributed to the influence of the machining history on the actual samples. During the turning process, the workpiece in fact endured more than one pass of cutting before it was interrupted during the experiment. Residual strains (and stresses) on the previously machined surface would accumulate in the
workpiece and resulted in higher strain values in the workpiece during the subsequent revolution.

4.4.4 Comparisons of Computed Stress Distribution with Experimental Stresses

It has been observed that the magnitudes of the numerically calculated stresses were very close to those determined by experimental measurements (Figure 4.13). A maximum stress of 298 - 300 MPa occurred immediately ahead of the tool tip, corresponding to the location of the maximum strain (5.6 at t = 0.25mm, 5.9 at t = 0.50 mm and 5.5 at t = 0.80 mm). The stress then decreased from this maximum value along the cutting line and decreased with increasing distance from the cutting line until below a certain depth, where it became close to the yield strength of the material. If compared along the primary shear plane, an excellent agreement was found. The stress in the material ahead of the tool tip (at \( x = 100 \mu m, y = 0 \mu m \)) obtained from the experimental measurements was 284 MPa while the simulation prediction was 280 MPa for a depth of cut of 0.25 mm, which gives a difference of only 1.4 %. In the middle part of the primary shear plane, the experimentally found equivalent stress was 260 MPa, while the numerically predicted value was 240 MPa, a difference of 7.7 %. The stress at the chip root was 240 MPa from experimental measurement and 230 MPa from the numerical simulation. The stress distributions under the other two feed rates also had small differences between experimental and simulation results. All the stresses around the tool tip, in the middle and at the chip root were much similar. The differences under the three feed rates were within 7.7 %.
Figure 4.12 Comparison of plastic strain between experimental and simulation results (1100 Al) under different feed rates (depths of cut) along the primary shear plane: (a) 0.25 mm; (b) 0.50 mm; (c) 0.80 mm.
Figure 4.13 Comparison of flow stress between experimental and simulation results (1100 Al) under different feed rates (depths of cut) along the primary shear plane: (a) 0.25 mm; (b) 0.50 mm; (c) 0.80 mm. Unit: MPa
4.4.5 Comparison of the Widths of the Primary Deformation Zones between Experimental and Simulation Results

The differences between the widths of the primary zone determined experimentally and by simulations are presented in Figures 4.14. The widths of the primary deformation zone from both experimental measurements and numerical predictions increased with the increase of feed rate.

The experimentally measured widths of the primary deformation zone under the three different feed rates were 800 µm (0.25 mm/rev), 1100 µm (0.50 mm/rev), 1400 µm (0.80 mm/rev), while the numerically calculated widths of the primary deformation zone were 500 µm (0.25 mm), 750 µm (0.50 mm), and 1100 µm (0.80 mm).

![Figure 4.14](image_url)  
Figure 4.14 Comparison of the width of primary deformation zone (PDZ) calculated from experimental observations and simulation results under three different feed rates (depths of cut) (workpiece: 1100 Al).

4.4.6 Variations of the Cutting Force and Energy Consumption with Feed Rate

Figure 4.15 presents the variations of cutting force (F_c) with respect to simulation time from LS-POST. The cutting force rose rapidly at the beginning of the contact
between the workpiece and the cutting tool. It then increased gradually until reach a steady state. The higher the feed rate, the longer it took for a chip to reach steady state formation and for the force to reach steady state value. When the simulation time was 15 ms, a steady state can be regarded having formed because the cutting force almost kept constant for all the situations. The cutting force was found to increase with respect to increasing depth of cut. At simulation time = 15 ms, the cutting force increased linearly from 708 N to 1874 N when depth of cut increased from 0.25 mm to 0.8 mm, which corresponds to a 165 % increase.

![Figure 4.15 The cutting force versus time curves under different depths of cut for 1100 aluminum from simulation results.](image)

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Figure 4.16 Variations of the cutting force and energy consumption with feed rate (depth of cut) from simulations (workpiece: 1100 Al).

The energy consumption during cutting can be calculated by using equation 3-8. Figure 4.16 shows the cutting force and energy consumption as a function of the feed rate obtained from numerical analyses when simulation time was 15 ms. The energy consumption also increased with the feed rate to deform the material, to create fresh surfaces, and to move the chip along the rake face of the cutting tool.

The specific cutting energy under three different depths of cut calculated by equation 3-9 are 944 MJ-m\(^{-3}\) at \(t = 0.25\) mm, 874 MJ-m\(^{-3}\) at \(t = 0.50\) mm, and 781 MJ-m\(^{-3}\) at \(t = 0.80\) mm from the simulation results, which shows 17.3 \% decrease. This is consistent with the results for a low-carbon steel [10].

As mentioned above, the energy expended per unit volume during the deformation of the material ahead of the tool tip can also be calculated from the area under the stress-strain curve in Figure 4.5. The plastic work expended in the material ahead of the tool tip
between iso-strain contours plotted in Figure 4.3 [22] by using equation 3-10.b. The calculated specific work of plastic deformation in primary and secondary deformation zones under three different feed rates are listed in Table 4.2. From Table 4.2, it can be seen that the specific work of plastic deformation in primary deformation zones decreased with feed rate, while the specific work of plastic deformation in secondary deformation zones increased with feed rate. The specific work of plastic deformation in primary deformation zones decreased 11.5 % when feed rate increased from 0.25 mm/rev to 0.80 mm/rev, while the specific work of plastic deformation in secondary deformation zones increased 46.5 % when feed rate increased from 0.25 mm/rev to 0.80 mm/rev.

Table 4.2 The calculated specific work of plastic deformation under three different feed rates

<table>
<thead>
<tr>
<th>Feed rate mm/rev</th>
<th>Specific work of plastic deformation in primary deformation zone, MJ·m⁻³</th>
<th>Specific work of plastic deformation in secondary deformation zone, MJ·m⁻³</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.25</td>
<td>458.2</td>
<td>362.0</td>
</tr>
<tr>
<td>0.50</td>
<td>431.8</td>
<td>446.2</td>
</tr>
<tr>
<td>0.80</td>
<td>405.6</td>
<td>530.5</td>
</tr>
</tbody>
</table>
4.4.7 Temperature Distribution in the Workpiece Material Ahead of the Tool Tip

Equation 3-11 was utilized to calculate temperature increase in the workpiece during cutting process. For aluminum, density \( \rho = 2.71 \text{ g-cm}^{-3} \), and specific heat capacity \( C_p = 904 \text{ J/kg·°C} \). The temperature distributions in the workpiece material ahead of the tool tip under three different feed rates are presented in Figure 4.17. The temperature distributions in the primary deformation zones under the different feed rates are very similar. Plastic deformation increases the temperature of the material from room temperature (25 °C) to 181-201 °C in the chips above a distance from the cutting line. Large temperature increase was observed in the material adjacent to the rake face. The highest temperature occurred directly ahead of the tool tip \( (x = 100 \text{ µm, } y = 0 \text{ µm}) \) and increased with the increase of the feed rate. The values of the highest temperature are 341 °C for \( f = 0.25 \text{ mm/rev} \), 365 °C for \( f = 0.50 \text{ mm/rev} \), and 377 °C for \( f = 0.80 \text{ mm/rev} \).
Figure 4.17 Temperature distributions (°C) in the workpiece material (1100 Al) ahead of the tool tip under three different feed rates from experimental results: (a) feed rate = 0.25 mm/rev, (b) feed rate = 0.50 mm/rev, and (c) feed rate = 0.80 mm/rev.
Figure 4.17 Continued
CHAPTER 5
Experimental Studies of Discontinuous Chip Formation in A380 Alloy

5.1 Orthogonal Cutting Tests and Metallographic Sample Preparation

The material under investigation on discontinuous chip formation studies was vacuum high-pressure die cast aluminum silicon A380 alloy (Table 5.1). The samples were divided into two groups. One group was in the as-cast condition, the other group was solution treated at 480 °C for 12 hours and water quenched in 60 °C water. Both groups of samples were machined into a tubular shape with 12.0 mm outer diameter and 2.5 mm wall thickness before the orthogonal cutting tests. Microhardness testing was performed on a Buehler Microhardness Tester (Model MICROMET II). The average microhardness values of the as-cast and solution heat treated material measured by a Vicker indenter using a load of 10 gf were 65 ± 5 kg·mm⁻², and 58 ± 5 kg·mm⁻² respectively.

Orthogonal cutting tests were performed using the Harrison M3000 lathe. No metal removal fluid was used during these tests. The cutting tool insert was a diamond-tip cutter (SANDVIC Coromant vcmw 332 FPC010) with a positive 2° rake angle. The cutting speed was 800 m/min (0.4m·s⁻¹) and the feed rate was 0.30 mm/rev.

Cross sections of machined samples were prepared using the standard metallographic preparation techniques and etched with Graff Sargent (84ml H₂O, 0.5ml HF, 15.5ml HNO₃, and 3g Cr₂O₃).

Table 5.1 Chemical compositions of A380 alloy (wt %) [78]

<table>
<thead>
<tr>
<th></th>
<th>Si</th>
<th>Cu</th>
<th>Fe</th>
<th>Mn</th>
<th>Mg</th>
<th>Ni</th>
<th>Zn</th>
<th>Sn</th>
<th>others</th>
<th>Al</th>
</tr>
</thead>
</table>
|   |  7.5-9.5 | 3.0-4.0 | 1.3 |  0.50 |  0.10 |  0.50 |  3.0 |  0.35 |  0.50 | Bal.

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5.2 Metallographic Analysis

5.2.1 Phase Identification

A380 samples in the as cast condition and after solution treatment did not show different phases but the morphology of silicon particles changed as a result of solution treatment. A JSM-5800LV scanning electron microscope (SEM) with EDS (Energy Dispersive Spectroscopy) attachment was employed in a backscattered model to characterize microstructures of specimens. Six phases were identified in the A380 alloy by SEM/EDS. These were:

1) the aluminum matrix, the matrix is actually a solid solution;
2) an eutectic silicon phase;
3) an \( \text{Al}_2\text{Cu} \) (in light gray);
4) an iron-rich phase with a composition of \( \text{Al}_{15}(\text{Fe}, \text{Mn})_3\text{Si}_2 \);
5) an iron-rich phase with a composition of \( \text{Al}_{12}(\text{Fe}, \text{Mn}, \text{Cr})_3\text{Si}_2 \) (in light grey colour);
6) Fe-Cu-Si phase (in needle shape).

SEM images and EDS spectrum of A380 can be found in Figure 5.1 and Figure 5.2.
Figure 5.1 (a) and (b) Back scattered SEM images and phases of A380
Figure 5.2 EDS spectrum of A380: (a) EDS spectrum of the aluminium matrix; (b) EDS spectrum of silicon phases; (c) EDS spectrum of Al$_2$Cu in light gray color; (d) EDS spectrum of an iron-rich phase with a composition of Al$_{15}$(Fe, Mn)$_3$Si$_2$; (e) EDS spectrum of an iron-rich phase with a composition of Al$_{12}$(Fe, Mn, Cr)$_3$Si$_2$ in light gray colour; (f) EDS spectrum of Fe-Cu-Si phase in needle shape.
5.2.2 Deformation Microstructure of the Workpiece

Chip deformation microstructures of as cast and solution treated A380 workpieces are shown in Figure 5.3 and Figure 5.4. In the sample machined in the as-cast condition, discontinuous chips with built-up edge (BUE) were formed (see Figure 5.3). The BUE formation may be attributed to the higher hardness of the workpiece: higher cutting force is needed to separate the chip from the machined surface, consequently, the chip material welds itself to the tool face. From Figure 5.3, it can be seen that fractured particles were concentrated in a triangular area and aligned in the direction of the deformation in the sample ahead of the tool tip (see the area surrounded by the dotted line). The dotted line was drawn by identifying fractured particle flows. In the area below this line, no obvious fractured particle flow was found. No secondary deformation zone was found.

For the solution treated sample, a serrated chip morphology was formed during orthogonal cutting. The built up edge at the tool tip was so small that it could be neglected. Like the former sample, fractured particles were also concentrated in a triangular area and aligned in the direction of the deformation in the sample ahead of the tool tip (see the area surrounded by the dotted line in Figure 5.4) while no obvious particle fracture was found in other areas. No secondary deformation zone was found. However, the direction of the fractured particle flow was different in the two samples. In the solution treated sample, the fractured particle flow direction was very similar to that formed in copper and aluminum with continuous chip formation (Figure 5.5 (b)). In the as cast sample, the fractured particle flow direction was in the opposite direction, i.e. the alignment of the fractured particles was towards the rake face instead of being aligned away from the rake face (Figure 5.5 (a)). It can be inferred that the BUE formation at the
tool tip in the sample tested in the as-cast condition caused a "disturbed material flow" that led to the difference in the direction of fractured particle flow.

A notable phenomenon that occurred was the adiabatic shear bands that were generated as a result of plastic strain localization in the solution treated sample (Figure 5.4). The shear bands have been formed in as cast samples and chips were formed by the separation of the shear bands. The shear bands divided the chips into discrete segments. The lengths of the segment varied in the range of 300 - 350 µm (Figure 5.4). The direction of the fractured particle flow at each side of the shear bands was reversed (Figure 5.5 (b)).

Figure 5.6 (a) shows the typical morphology of silicon phase of A380 in the as-cast condition. In the as-cast microstructure, silicon particles were present as acicular needles. The needle shaped particles could act as crack initiators and lower the mechanical properties [79]. Figure 5.6 (b) gives the morphology of silicon particles in the sample that was solution treated at 480 °C for 12 hours and then water quenched. It appeared that the most of the silicon particles were spheroidized. The serrated chip formation instead of completely broken chip formation (discontinuous chip) may be attributed to the ductility improvement by spheroidization of silicon particles during the solution treatment.

The adiabatic shear band is among the most interesting phenomena observed in this study. Therefore, the investigation of discontinuous chip formation was focused on the sample with serrated chip formation. In order to estimate the thickness of the shear band, two lines were drawn paralleled to the shear bands. The flow direction of the fractured particles between the two lines was parallel in this direction (Figure 5.7). The
estimated distance between the two lines, 40 μm, was regarded as the thickness of the shear band.

Figure 5.3 Optical cross-sectional microstructure of A380 (as-cast condition) ahead of the tool tip, which have undergone orthogonal cutting, where cutting speed was 800 m/min, and feed rate was 0.30 mm/rev.
Figure 5.4 Optical cross-sectional microstructure of A380 (solution treated at 480 °C for 12 hours and water quenched) ahead of the tool tip, which have undergone orthogonal cutting, where cutting speed = 800 m/min, and feed rate = 0.30 mm/rev.
Figure 5.5 Optical images of workpieces at the tool tip, which show the particle flows in the workpiece at the tool tip: (a) A 380 (as-cast); (b) A380 (solution treated at 480 °C for 12 hours + water quench).
Figure 5.6 (a) Optical micrograph of A380 as-cast condition microstructure (aspect ratio = 3.7, sphericity = 0.35)
(b) Optical micrograph of A380 microstructure solution treated at 480 °C for 12 hours and water quenched (aspect ratio = 2.3, sphericity = 0.54)
Figure 5.7 Optical (a) and SEM (b) images of shear band formed in A380 (solution treated) subjected orthogonal cutting at cutting speed = 80m/min, and feed rate = 0.25 mm/rev. Pictures were take from the elliptic area in Figure 5.5.
5.3 Strain Measurements and Distribution

The value of the shear angle ($\phi$) at each point within the workpiece was used to estimate the direction of the plastic flow at that point (Figure 5.8). By following the direction of the particle flow, the values of local equivalent strains $\bar{\varepsilon}_p$ in the material ahead of the tool tip were estimated using Equation 3-1.

According to Turley and Doyle [80], the shear strain $\gamma_c$ in the shear band can be estimated by the shear displacement within the shear band $d_s$ divided by the thickness of the shear band $t_s$ (Figure 5.9):

$$\gamma_c = \frac{d_s}{t_s}$$  \hspace{1cm} (5-1)

The average length of $d_s$ was measured as 480 $\mu$m, and the width of the shear band estimated from optical and SEM micrograph was 40 $\mu$m. Therefore, according to Equation 5-1, the estimated shear strain in the shear band was 12.

The metallographically determined strain distribution in the material outside the shear band (using Equation 3-1) is shown in Figure 5.10. Unlike the case of pure Cu and Al that form continuous chips, no secondary deformation zone was found near the rake face. The strain in the chip was confined to the shear band. Within the segments between the shear bands, very little deformation occurred. Above the cutting line, the values of strain increased from 0.2 at the tool tip to a value of 1.5 near the shear band. The strain in the primary deformation zone was in the range of 0.1 to 0.8.
Figure 5.8 Schematic drawing of equivalent plastic strain measurement.

Figure 5.9 Schematic drawing of the shear displacement within the shear band $d_s$ and the thickness of the shear band $t_s$. 
5.4 Flow Stress Measurements and Distributions

Microhardness measurements were done in order to determine local flow stress values in the workpiece ahead of the tool tip. The measurements were taken at the intersection points of an imaginary grid within the deformation zone by using a load of 10 gf. Two or three additional measurements were taken around this point by avoiding placing the indenter on the silicon particles. Average value of the measurements obtained from the Al matrix was taken as the microhardness value at that point. Then the flow stress was calculated as described in Chapters 3.2.2 and 4.2.2 by using equation 3-2.

Figure 5.11 and Figure 5.12 shows the microhardness distribution and stress distribution in the workpiece at the tool tip respectively. Like the strain distribution, the stress distribution was also confined in a triangular area. In the primary deformation zone,
the stress gradually increased from a value of 220 MPa to 285 MPa above the cutting line. A decreased value of 246 MPa was observed in the shear band area, which indicates thermal softening had happened in this area.

Based on experimental measurements and calculations, the equivalent plastic strain of 1.5 near the shear band, the shear strain value of 12 in the shear band, and the flow stress of 285 MPa can be regarded as initial failure prediction values.

Figure 5.11 Microhardness (kg/mm$^2$) of A380 (solution treated) ahead of the tool tip.
5.5 Temperature Distribution

According to Leowen and Shaw [81], the temperature in the shear zone $T_{sz}$ is given by

$$T_{sz} = \frac{\tau \cdot \gamma}{\rho C_p} \left[ \frac{1}{1 + 1.328 \frac{k \cdot \gamma}{v_c \cdot d}} \right] + T_{amb} \quad (5-2)$$

where $T_{amb}$ is the ambient temperature, $\tau$ is the shear stress, $\gamma$ is the shear strain, and $\rho$, $C_p$, and $k$ are density, specific heat, and thermal diffusivity respectively, $v_c$ is the cutting speed and $d$ is the depth of cut which equals the wall thickness of the tube.
The temperature increase (\(\Delta T\)) in regions outside the shear band can be estimated by Equation 3-11. For A380, \(\rho = 2.74 \text{ g/cm}^3\), \(C_p = 963 \text{ J/kg-K}\), and \(k = 36.5 \times 10^6 \text{ m}^2/\text{s}\) [78]. The calculated temperatures were presented in Figure 5.13. From Figure 5.13, it can be seen that not significant temperature increase was observed in the primary deformation zone. The temperatures were less than 100 °C in the primary deformation zone except near the shear band. The highest increase was occurred inside the adiabatic shear band, where the temperature reached 620 °C, which was close to the liquid temperature, 595 °C of A380 [78], (see aluminum-silicon binary phase diagram in Figure 5.14 [82]).

![Figure 5.13 Temperature (°C) distribution of A380 (solution treated) ahead of the tool tip.](image-url)

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Figure 5.14 Al-Si binary phase diagram, the Si content for A380 is 7.5-9.5 %. [82]

5.6 Discussion

Formation of adiabatic shear bands is a commonly observed phenomenon in various classes of materials deformed at high strain rates. The primary mechanism for shear band formation is a thermo-mechanical process. Flow localization in shear is attributed to the destabilizing effects of thermal softening, which can outweigh the effects of strain and strain rate hardening in a deforming region when the local rate of heat generation resulting from the plastic flow itself exceeds its rate of dissipation into the surrounding material. This is an important mode of deformation as the shear zones often become the sites for eventual failure of the material [83].

Thomason [84] reviewed a large number of experimental studies, which conclusively showed that in the initial stages of plastic flow in metals and alloys, inclusions and second-phases particles acted as sites for nucleation of microvoids.
Figure 5.15 shows fractured particles near the tool tip and the shear band. The particles began to show cracks or fracture near the tool tip because of high plastic deformation. Then microvoids were formed at some spots around the particles with the increase of deformation as the workpiece passed along the cutting tool. The voids coalesced and aligned in the direction of plastic deformation. From strain, stress and temperature distributions in the workpiece, it can be inferred that thermal softening accompanied the plastic deformation during this process but was not significant to compensate the effect of strain hardening at this stage. The growth of microvoids continued with increasing plastic strain until a critical condition was reached for the localized plastic instability in the deformed material. This can be confirmed by the observation near the shear band, where the particles had severely cracked and debonded from the aluminum matrix. At this stage, the material suddenly collapsed. This happened in such a small area and a very short time; that the adiabatic shear band became thermally softened.

The general method to model serrated chip formation is by applying some failure criteria [44,48,65] as described in Chapter 2. Serrated chip morphology is formed by deleting elements which reach the failure criterion. Therefore, the Lagarangian formulation is always used in this kind of simulations. Strain, strain rate and thermal effects are also incorporated in the numerical modelling. Mesh adaptivity is applied to compensate severe mesh distortion due to large strains during cutting process. If numerical simulation is going to be conducted on this kind of chip formation in the future, all these factors should be considered.
SEM image showing voids and fractured particles in A380 (solution treatment): (a) near the tool tip, and (b) near the adiabatic shear band (taken from the square area in Figure 5.7).

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CHAPTER 6

General Discussion

A generalization of the stress-strain response of ductile materials subjected to orthogonal cutting is presented in this chapter. Comparisons of the deformation state between the copper and aluminum used in this study are given. The comparisons were focused on the chip thickness, the widths of the primary and secondary deformation zones, and strain and stress distributions in the workpiece ahead of the tool tip during cutting process under different feed rates.

6.1 Generalization of Stress-Strain State of Ductile Material Subjected to Orthogonal Cutting

In this study, commercial purity copper and aluminum subjected to orthogonal cutting under three different feed rates were studied. The deformation behaviour (stress-strain curves in Figure 6.1) for both materials can be represented by the same Voce type equation (equation 3-3) and is independent of the feed rate. The work hardening rate versus the flow stress curves for both of the two materials were plotted in Figure 6.2, which again shows that the Voce equation is a good representation of the experiment data. Therefore, the method followed in this study is a good way to characterize stress-strain relationship at high strains in ductile materials. Namely, the relatively simple metallographic methods described in chapter 3.2 can be used to estimate stress-strain behaviour of the samples subjected to high strains without using sophisticated testing equipment such as Hopkinson bar testing and torsion testing.
Figure 6.1  Stress-strain relationship of commercial purity copper and aluminum subjected to orthogonal cutting.

Figure 6.2  Variation of the work hardening rate with the flow stress for copper and aluminum.
6.2 Deformation State Comparisons between Copper and Aluminum

6.2.1 Comparison of Chip Thickness

Both experimental measurements and numerical predictions have shown that the chip thickness increased with the feed rate (or depth of cut). However, the chip thickness of aluminum is always larger (approx. 20 %) than that of copper under the same cutting conditions. Both experimental measurement and numerical predictions lead to the same conclusion as shown in Figure 6.3.

6.2.2 Widths of the Primary and Secondary Deformation Zones

Experimental and Numerical results have shown that as illustrated in Figure 3.16 and Figure 4.15, the widths of primary deformation zones of aluminum were larger than those of copper. For the primary deformation zone, the experimentally measured widths of aluminum increased 78 %, 69 % and 75 % than that of copper corresponding to feed rates of 0.25, 0.50, and 0.80 mm/rev respectively. The numerically predicted widths of aluminum increased 67 %, 50 %, and 57 % than that of copper (Figure 6.4).

For the same feed rates, the experimentally measured widths of secondary deformation zone of aluminum increased 87 %, 80 % and 54 % than that of copper, corresponding to feed rate of 0.25, 0.50, and 0.80 mm/rev respectively (Figure 6.5).

The differences of chip thickness, widths of primary and secondary deformation zones between copper and aluminum may be attributed to the lower flow stress of aluminum.
Figure 6.3 Comparison of chip thicknesses between copper and aluminum from (a) experimental and (b) simulation results under three different feed rates (depths of cut).
Figure 6.4 Comparison of the width of the primary deformation zone (PDZ) between copper and aluminum from experimental and simulation results under three different feed rates (depths of cut).

Figure 6.5 Comparison of the width of the secondary deformation zone between copper and aluminum from experimental results under three different feed rates.
6.2.3 Strain and Stress

Table 6.1 gives the comparison of plastic strain in the workpiece of copper and aluminum under different feed rates used in the experimental measurements. The plastic strain of Al near tool tip is only 6% larger than that of copper, while approximately 40% larger for the constant chip strain at all the cutting conditions, which indicates larger deformation occurred in aluminum and this is consistent with the larger chip thickness.

The flow strength of bulk material of aluminum (140 MPa) is much lower than copper (312 MPa), see Table 6.2. This could be the reason that caused a higher amount of deformation in aluminum. The flow stress in the workpiece material ahead of the tool tip increased during cutting process because of strain hardening. For copper, the flow stress increased 43% at the tool tip and 36% at the middle of the primary shear plane. While for aluminum, the increase of flow stress at the tool tip and at the middle of the primary shear plane was 106% and 84% respectively (Figure 6.2).

Table 6.1 Comparison of strain values in the workpiece of copper and aluminum ahead of the tool tip

<table>
<thead>
<tr>
<th>Feed rate mm/rev</th>
<th>Strain</th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Ahead of the tool tip approx. 50 µm</td>
<td>Ahead of the tool tip approx. 100 µm</td>
<td>In the chip</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Cu</td>
<td>Al</td>
<td>Cu</td>
<td>Al</td>
</tr>
<tr>
<td>0.25</td>
<td>3.2</td>
<td>3.4</td>
<td>1.5</td>
<td>2.1</td>
</tr>
<tr>
<td>0.50</td>
<td>3.4</td>
<td>3.6</td>
<td>1.4</td>
<td>2.0</td>
</tr>
<tr>
<td>0.80</td>
<td>3.6</td>
<td>3.8</td>
<td>1.3</td>
<td>1.9</td>
</tr>
</tbody>
</table>
Table 6.2 Comparison of flow stress values in the workpiece of copper and aluminum ahead of tool tip

<table>
<thead>
<tr>
<th>Feed rate mm/rev</th>
<th>Flow stress of bulk material, MPa</th>
<th>Flow stress at tool tip, MPa</th>
<th>Flow stress in the chip, MPa</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Cu</td>
<td>Al</td>
<td>Cu</td>
</tr>
<tr>
<td>0.25</td>
<td>312.2</td>
<td>140.2</td>
<td>443</td>
</tr>
<tr>
<td>0.50</td>
<td></td>
<td></td>
<td>445</td>
</tr>
<tr>
<td>0.80</td>
<td></td>
<td></td>
<td>448</td>
</tr>
</tbody>
</table>

6.2.4 Cutting Force and Energy Consumption

Figure 6.6 presents the comparison of cutting force and energy consumption between copper and aluminum. The values reported are obtained from simulation results under three different depths of cut. Due to the lower flow stress of aluminum, the cutting force and energy consumption of aluminum were approximately 16 %, 23 %, and 36 % lower than copper when depth of cut increased from 0.25 mm to 0.50 mm, and 0.8 mm.

The calculated specific work of plastic deformation in primary deformation zones and the secondary deformation zones under three different feed rates in chapter 3.4.6 and chapter 4.4.6 were listed in Table 6.3. From Table 6.3, it can be seen that the specific work of plastic deformation in primary deformation zones decreased with feed rate, while the specific work of plastic deformation in secondary deformation zones increased with feed rate for both copper and aluminum. The specific work of plastic deformation in primary deformation zones for copper decreased approx. 14.5 % for copper, while approx. 11.5 % for aluminum. The specific work of plastic deformation in secondary deformation zones increased approx. 35.2 % for copper, while approx. 46.5 % for aluminum.
Figure 6.6 Comparison of cutting force and energy consumption between copper and aluminum from simulation results under three different depths of cut.

Table 6.3 Comparison of specific work of plastic deformation in the workpiece of copper and aluminum ahead of tool tip

<table>
<thead>
<tr>
<th>Feed rate mm/rev</th>
<th>Specific work of plastic deformation in primary deformation zone, MJ m(^{-3})</th>
<th>Specific work of plastic deformation in secondary deformation zone, MJ m(^{-3})</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Cu</td>
<td>Al</td>
</tr>
<tr>
<td>0.25</td>
<td>587.3</td>
<td>458.2</td>
</tr>
<tr>
<td>0.50</td>
<td>544.8</td>
<td>431.8</td>
</tr>
<tr>
<td>0.80</td>
<td>502.0</td>
<td>405.6</td>
</tr>
</tbody>
</table>
CHAPTER 7
Conclusions and Suggestions for Future Work

7.1 Conclusions

The present study was focused on the experimental and numerical studies of continuous chip formation in two commercial purity workpieces, namely copper and 1100 aluminum. Serrated chip formation in 380 aluminum alloy was also studied experimentally. The experimental tests and numerical simulations conducted in this research have provided information regarding the influence of one of the most important parameters, namely feed rate, on the deformation state during metal cutting process. The damage pattern of the fractured particles during serrated chip formation was also observed. A number of conclusions may be drawn based on the experiments and simulations conducted in this research. The most important conclusions can be summarized as follows:

Continuous Chip Formation:

(1) By characterizing the deformation microstructures of the material ahead of the tool tip in commercial purity copper and 1100 Al samples subjected to orthogonal cutting experiments under different feed rates (between 0.25 mm/rev and 0.80 mm/rev), the strain and stress distributions in the workpiece material were determined. The relationship between the equivalent flow stresses and equivalent strains obtained at different feed rates can be represented by a single Voce type exponential equation. A saturation flow stress of 448 MPa was reached at strain levels were larger than 2.0 for
copper and a saturation stress of 302 MPa was reached at strain levels were large than 3.0 for aluminum.

(2) An Eulerian finite element formulation was used for numerical simulations in LS-DYNA to predict strain and stress distributions in the material ahead of the tool tip. The simulations were based on an elastic-plastic hydrodynamic material model. Experimentally determined Voce-type stress-strain relationship was used in the development of the material model.

(3) For copper, the experimental results showed that the equivalent strains in the material ahead of the tool tip (at x = 50 μm, y = 0 μm) were about 3.2 for f = 0.25 mm/rev, 3.4 for f = 0.50 mm/rev and 3.6 for f = 0.80 mm/rev. The numerical predictions showed that the strains ranged between 2.4 and 2.9, which amounts to a difference within 25%. According to the experimental measurements, the chip root strains decreased to a low value of 1.1 - 1.3. The strains in the chip ranged from 1.3 to 1.5. The numerically calculated chip strains ranged between 1.3 - 2.0, and 0.5 - 1.3 at the chip root.

(4) For copper, the maximum von Mises stress of 448 MPa predicted by numerical simulation under three feed rates occurred directly ahead of the tool tip, which corresponded to the location of calculated maximum strain. At a distance of 50 μm ahead of the tool tip, the experimentally estimated stress were 443 MPa for f = 0.25 mm/rev, 445 MPa for f = 0.50 mm/rev and 448 MPa for f = 0.80 mm/rev, which were in agreement with the numerically predicted value of 440 MPa. According to the numerical model, along the primary shear plane, the high tool tip stress of 448 MPa decreased to 400 - 430 MPa at the middle of the primary shear plane, then
to approximately 400 MPa at the chip root, which correlated well to the experimentally determined stress of 400 - 430 MPa at the middle of the primary shear plane and approximately 420 MPa at the chip root.

(5) For aluminum, the experimental results showed that the equivalent strains in the material ahead of the tool tip (at x = 100 μm, y = 0 μm) were 3.4 for f = 0.25 mm/rev, 3.6 for f = 0.50 mm/rev, and 3.8 for f = 0.80 mm/rev. The numerical strain predictions at the same location were also ranged between 3.4 and 3.6. According to the experimental measurements and numerical calculations, the strains decreased along the primary shear plane, and reached 1.2 - 1.3 at the chip root. The experimentally determined strains in the chip ranged from 1.8 to 2.1.

(6) The maximum von Mises stress of 300 MPa predicted by numerical simulations for samples cut at three different feed rates occurred directly ahead of the tool tip, which corresponded to the location of calculated maximum strains of 5.6 for f = 0.25 mm/rev, 5.9 for f = 0.50 mm/rev and 5.5 for f = 0.80 mm/rev. At a distance of 100 μm directly ahead of the tool tip, the experimentally estimated stresses were 284 MPa for f = 0.25 mm/rev, 293 MPa for f = 0.50 mm/rev and 287 MPa for f = 0.80 mm/rev, which were in agreement with the numerically predicted values of 280 MPa. According to the numerical model, along the primary shear plane, the high tool tip stress of 300 MPa decreased to 240 - 250 MPa at the midsection of the primary shear plane, then to 240 MPa at the chip root, which correlated well to the experimentally determined stress of 260 MPa at the middle of the primary shear plane and 250 MPa at the chip root.
(7) The chip thickness and the width of the primary deformation zone increased with the increase of the feed rate according to both experimental measurements and numerical simulations for both of copper and aluminum.

(8) The increasing cutting force from the simulation results showed that the energy consumption increased with the increase of depths of cut (which were the same values of feed rate). Consequently, more power was required to complete cutting operation when feed rate increased. The increment was about 211 % for copper, while it was about 165 % for aluminum when depth of cut increased from 0.25 mm to 0.8 mm.

(9) Due to lower flow stress of aluminum, the chip thicknesses, plastic strain values in the chip, the widths of primary and secondary deformation zones) of aluminum were approximately 20 % larger than those of copper at the same cutting conditions. The cutting force and energy consumption for aluminum were approximately 16 %, 23 %, and 36 % lower than copper when depth of cut increased from 0.25 to 0.50 and 0.80 mm/rev.

**Discontinuous Chip Formation:**

(1) The formation of adiabatic shear bands was the main reason for serrated chip formation during cutting A380 alloy.

(2) The strain in the chip was confined to the shear bands between the segments with very little deformation within these bands. The strain in the primary deformation zone was only 0.1 – 0.8, while the strain in the shear band reached 12.

(3) Like the strain distribution, the stress distribution in the workpiece ahead of the tool tip was confined in a triangular area. The stress gradually increased above the cutting
line in the primary deformation zone. A decreased value was observed at the shear band area, which indicates thermal softening had occurred in this area.

(4) The particles began to crack near the tool tip. Then microvoids formed at regions around the particles with the increase of deformation as the workpiece passed along the cutting tool. The voids coalesced and aligned in the direction of plastic deformation. The growth of microvoids continued with increasing plastic strain until a critical condition was reached for the localized plastic instability in the deformed material. At this stage, the material suddenly collapsed.

7.2 Suggestions for Future Work

The future work in this area may include experimental studies and numerical simulations of other parameters, such as rake angle and cutting speed. Studies on the cutting process with minimum quantity lubrication will provide important information for practical operation. This work can also be extended to other composite materials, such as aluminum alloys. With the increasing usage of aluminum alloys in automotive industry in order to decrease the vehicle weight and save fuel consumption, investigation focused on cutting mechanism of this kind of material will have more practical applications. Furthermore, considering the large influence of temperature on the tool life and quality of the machined surface, the development of a FE model incorporates strain, strain rate, and temperature should be more helpful for numerical simulations in this area.
REFERENCES


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Appendix A
Introduction to Finite Element Analysis

1. Introduction

Finite element analysis (FEA) is a numerical method for the solution of field problems. A field problem like heat transfer, stress strain analysis can be described by governing differential equations (either ODE: Ordinary Differential Equation or PDE: Partial Differential Equation) and boundary conditions mathematically. However, it is often very difficult or impossible to solve the differential equations theoretically because of the complexity of the problem. In finite element analysis, a structure is discretized into finite elements. In each finite element, the field quantity is approximated by piecewise polynomial interpolation. FEA provides an approximate solution to the problem. The accuracy of a FEA result can be enhanced by improving the mathematical model of the problem and fine discretization of the structure. The development of finite element method is briefly demonstrated in this appendix through the analysis of a bar structure. These introductions would barely scratch the surface of this complex topic. Detailed description of this topic is referred in the reference list [85, 86].

2. Governing Differential Equations and Formulation Techniques Bar analysis:

Consider a bar of length $L$ with a vary section area of $A(x)$ subject to an axial load $q(x)$ and a concentrated load $Q_0$ at the right end as illustrated in Figure A-1.
The governing equilibrium equation is:

\[
\frac{d}{dx} \left[ EA(x) \frac{du(x)}{dx} \right] + q(x) = 0
\]

where, \( E \) is the Young's modulus of material, \( u(x) \) is the displacement at point \( x \).

Supposing the material is elastic, then

\[
\sigma(x) = E\varepsilon(x) \quad \text{And} \quad \varepsilon(x) = \frac{du(x)}{dx}
\]

Boundary conditions:

At \( x = 0 \) \( u(0) = 0 \)

At \( x = L \) \( Q_0 = EA(L) \frac{du}{dx} \) \hspace{1cm} (A-2)

There are several formulation techniques that can be used to get the final structure stiffness equation of \( [K][D] = \{R\} \). Frequently used methods include energy method, virtual work method, Galerkin method and weighted residual methods. In this analysis, Galerkin method will be used to derive the structure stiffness equation.

Let the bar be divided into \( N \) elements of length \( l \). For each element, axial displacement is approximated by linear function. In a typical two d.o.f.(degree of freedom) element with end nodes 1 and 2, the approximating function is
\[ \bar{u} = [N][d] \] In which \([N] = \begin{bmatrix} \frac{l-x}{l} & \frac{x}{l} \end{bmatrix} \] \( \{d\} = [u_1 \ u_2]^T \)

The Galerkin residual equation is a sum over the \(N\) elements of the structure.

\[
\sum_{j=1}^{N} \int_{0}^{l} \left[ N_i \frac{d}{dx} \left( EA(x) \frac{d\bar{u}(x)}{dx} \right) + q(x) \right] dx = 0 \tag{A-3}
\]

Where index \(i\) ranges over all element shape functions, \(i = 1, 2\) for the bar element.

\[ N_1 = \frac{l-x}{l} \] and \[ N_2 = \frac{x}{l} \] are the weight functions

Integration by parts of the first term in Eq. A-2 yields

\[
\int_{0}^{l} \left[ N_i \frac{d}{dx} \left( EA(x) \frac{d\bar{u}(x)}{dx} \right) \right] dx = \int_{0}^{l} \frac{dN_i}{dx} \left( EA(x) \frac{d\bar{u}}{dx} \right) dx - \int_{0}^{l} \frac{dN_i}{dx} EA(x) \frac{d\bar{u}}{dx} dx \tag{A-4}
\]

Substitute boundary condition (2) and Eq. A-4 into Eq. A-3, we obtain

\[
\sum_{j=1}^{N} \int_{0}^{l} \left[ -N_i \frac{d\bar{u}(x)}{dx} + N_i q(x) \right] dx + \sum_{j=1}^{N} [N][Q]_j = 0 \tag{A-5}
\]

For a typical bar element, we adopt the notation

\[ [B] = \frac{dN}{dx} \] hence \[ \frac{d\bar{u}(x)}{dx} = [B][d] = \begin{bmatrix} -1 \ 1 \end{bmatrix} \begin{bmatrix} u_1 \\ u_2 \end{bmatrix} \]

Thus, after rearrangement, Eq. A-5 becomes,

\[
\sum_{j=1}^{N} \int_{0}^{l} [B]^T EA(x) [B] dx \{d\}_j = \sum_{j=1}^{N} \int_{0}^{l} [N]^T q(x) dx + \sum_{j=1}^{N} [N]^T [Q]_j \tag{A-6}
\]

\[ [K] \]

Eq. A-6 is the standard structure stiffness equation \([K][D] = \{R\}\)
APPENDIX B
LS-DYNA Fundamentals

1. Introduction:

LS-DYNA is a general-purpose finite element code for analyzing the large
deformation dynamic response of structures including structures coupled to fluids. The
main solution methodology is based on explicit time integration [87]. Its fully automated
contact-impact algorithm and error-checking features have enabled users to solve
successfully many complex large deformation problems. More than ten kinds of elements
can be used for structure discretization, which including four node tetrahedron element,
eight node solid element, two node beam element, three and four node shell element,
eight node solid shell element, truss element, membrane element, discrete element, cable
element and rigid body. For each element, there is a variety of element formulation to
choose for different loading situation. Over one hundred material constitutive modes are
available, covering a wide range of material behaviour. In this appendix, some
fundamentals of LS-DYNA, which include time integration techniques, time step control
are briefly introduced, more details can be referred in LS-DYNA Keywords User’s
Manual [76] and Theoretical Manual [87].

2. Time Integration Techniques in LS-DYNA

The general form of the governing equation of motion used in finite element
analysis is:

$$[M]\{\ddot{u}\}+[C]\{\dot{u}\}+[K]\{u\}=[F] \quad (B-1)$$
where \([M]\) is the mass matrix, \(\ddot{u}\) is the nodal acceleration vector, \([C]\) is the damping matrix, \(\dot{u}\) is a nodal velocity vector, \([K]\) is the stiffness matrix, \(u\) is the nodal displacement vector, and \(\{F\}\) is the externally applied force.

There are two kinds of methods for the solution of the equation of motion in FEA: explicit and implicit time integration. The time integration algorithm used for the explicit code is the central difference method [88], while Newmark's method [89] is used for implicit code.

The central difference method assumes linear change in displacement. The governing equation is evaluated at time \(t_n\) and the acceleration and velocity is expressed as:

\[
\ddot{u}_n = \frac{1}{\Delta t^2} \left( u_{n+1} - 2u_n + u_{n-1} \right) \quad \text{(B-2)}
\]

\[
\dot{u}_n = \frac{1}{2\Delta t} \left( u_{n+1} - u_{n-1} \right) \quad \text{(B-3)}
\]

where \(n\) is the mid-point between time steps \(n-1\) and \(n+1\), and \(\Delta t\) is the duration of the time step interval. Therefore, \(\ddot{u}_n\) and \(\dot{u}_n\) are an average of the acceleration and velocity at time step \(n-1\) and \(n+1\). By substituting equations B-2 and B-3 into the equation of motion, the unknown displacement at \(t_{n+1}\) can be solved by:

\[
\left( \frac{[M]}{\Delta t^2} + \frac{2[C]}{\Delta t} \right) \cdot u_{n+1} = [F] - \left( \frac{[K]}{\Delta t^2} \right) \cdot u_n - \left( \frac{[M]}{\Delta t^2} \frac{[C]}{2\Delta t} \right) \cdot u_{n-1} \quad \text{(B-4)}
\]

By lumping the \([M]\) and \([C]\) matrices, the equation can be solved directly (explicitly). No inversion of the stiffness matrix is required. However, very small time steps are required to maintain the stability limit.
3. Time Step Control

The time step size in LS-DYNA roughly corresponds to the transient time of an acoustic wave through an element using the shortest characteristic distance. LS-DYNA checks all elements when calculating the required time step. For stability reasons, a scale factor is typically set to a value of 0.9 (default) or some smaller value to decrease the time step:

\[ \Delta t_e = \alpha \frac{l_e}{c} \]

where \( \Delta t_e \) is the critical time step, \( l_e \) is the smallest finite element length, \( \alpha \) is the scale factor, and \( c \) is the wave propagation velocity defined as a function of material density (\( \rho \)), Young’s modulus (\( E \)), and Poisson’s ratio (\( \nu \)):

**1-D beam and truss elements**

\[ c = \frac{E}{\sqrt[3]{\rho}} \]

**2-D shell elements**

\[ c = \frac{E}{\sqrt[3]{\rho \cdot (1 - \nu^2)}} \]

**3-D solid elements**

\[ c = \frac{E \cdot (1 - \nu)}{\sqrt[3]{\rho \cdot (1 + \nu) \cdot (1 - 2\nu)}} \]
Appendix C

Sample LS-DYNA Input File for Copper Using Eulerian Formulation

*KEYWORD
$----+----1----+----2----+----3----+----4----+----5----+----6----+----7----+----8
$ UNITS: millimeters, kilograms, seconds
$----+----1----+----2----+----3----+----4----+----5----+----6----+----7----+----8
$ (1) TITLE CARD.
$----+----1----+----2----+----3----+----4----+----5----+----6----+----7----+----8
*TITLE
Cull1000: New Material Model-Voce type
$----+----1----+----2----+----3----+----4----+----5----+----6----+----7----+----8
$ (2) CONTROL CARDS.
$----+----1----+----2----+----3----+----4----+----5----+----6----+----7----+----8
*CONTROL_TERMINATION
$ ENDTIM ENDCYC DTMIN ENDENG ENDMAS
0.0200 0 0.0 0.0 0.0
$ CONTROL_TIMESTEP
$ DTINIT TSSFAC ISDO TSLIMIT DT2MS LCTM ERODE MS1ST
0.0 0.90 0
$ DT2MSF
$ CONTROL_ALE
$ DCT NADV METH AFAC BFAC CFAC DFAC EFAC
2 1 2 -1.0
$ START END AAFAC VFAC PRIT EBC PREF NSIDEBC
0.0 100.0 0
$ DATABASE_CONTROL CARDS - ASCII HISTORY FILE
$ DATABASE HISTORY OPTION
$ ID1 ID2 ID3 ID4 ID5 ID6 ID7 ID8
$ OPTION : BEAM BEAM_SET NODE NODE_SET
$ SHELL SHELL_SET SOLID SOLID_SET
$ TSHELL TSHELL_SET
$ DATABASE OPTION
$ DT
$ OPTION : SECFORC RWFORC NODOUT ELOUT GLSTAT
$ DEFORC MATSUM NCFORC RCFORC DEFGEO
$ SPCFORC SWFORC ABSTAT NODFOR BNDOUT
$ RBDOUT GCEOUT SLEOUT MPGS SBTOUT
$ JNTFORC AVSFLT MOVIE
$ DATABASE_GLSTAT
$ DT BINARY
2.00E-05
$ DATABASE_MATSUM
$ DT BINARY
2.00E-05
$ DATABASE_RBDOUT
$ DT BINARY
2.00E-05
$ DATABASE_SLEOUT
$ DT BINARY
2.00E-05

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*DATABASE.psi

$ DT
2.00E-05
$ DBFSI_ID SID SIDTYPE
  1 3 1

$ (5) DATABASE CONTROL CARDS FOR BINARY FILE

*DATABASE_BINARY_D3PLOT
$ DT/CYCL LCDT NOBEAM
3.333E-04

*DATABASE_BINARY_D3THD
$ DT/CYCL LCDT NOBEAM
3.333E-04

$*DATABASE_BINARY_OPTION
$ DT/CYCL LCDT NOBEAM

$ OPTION : D1DRFL D3DUMP RUNRSP INTFOR

$ (6) DEFINE PARTS CARDS

*PART
$ SHEADING
PART PID = 1 PART NAME : WORKPIEC
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
  1 1 1 1 1

$ PART
$ SHEADING
PART PID = 2 PART NAME : AIRMESH
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
  2 1 1 1 1

$ PART
$ SHEADING
PART PID = 3 PART NAME : CUTTER
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
  3 3 3

*INITIAL_VOID_PART
$ PID
2

$ (7) MATERIAL CARDS

*MAT_ELASTIC_PLASTIC_HYDRO
$ 11000 copper material properties
$ MID RO G SIGY EH PC FS
  1 8.899E-6 4.7E+07
$ EPS1 EPS2 EPS3 EPS4 EPS5 EPS6 EPS7 EPS8
  0.0 0.2 0.4 0.6 0.8 1.0 1.2 1.4
$ EPS9 EPS10 EPS11 EPS12 EPS13 EPS14 EPS15 EPS16
  1.6 1.8 2.0 2.2 2.5 3.0 4.0 11.0
$ EPS17 EPS18 EPS19 EPS20 EPS21 EPS22 EPS23 EPS24
  312200.0 345875.0 371181.0 390197.0 404488.0 415227.0 423297.0 429362.0
$ EPS25 EPS26 EPS27 EPS28 EPS29 EPS30 EPS31 EPS32
  433919.0 437344.0 439917.0 441852.0 443890.0 445835.0 447253.0 447699.0

*MAT_RIGID

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$ MID  RO  E  PR  N  COUPLE  M  ALIAS
3  3.24E-6  2.88E+08  0.25
$ CMO  CON1  CON2
1.0  5.0  7.0
$ LCO.OR_A1  A2  A3  V1  V2  V3

$ (8) SECTION CARDS
$ (9) EOS CARDS
$ EOS parameters for Cu 11000 from Steinberg
$ EOSID  C  S1  S2  S3  GAMAO  A  E0
1  3.94E+6  1.489  0.0  0.0  2.02  0.47  0.0
$ V0  1.0

$ (10) PRESCRIBED MOTION CARDS
$ BOUNDARY PRESCRIBED MOTION RIGID
$ DEFINE_CURVE
$ SET PART_LIST
$ NODE  X  Y  Z  TC  RC
1  0.00000000E+00  0.00000000E+00  0.00000000E+00  0.00000000E+00
2  0.00000000E+00  0.00000000E+00  6.25000000E-01

$ (11) EULER/LAGRANGE CONTACT COUPLING
$ CONSTRAINED_LAGRANGE_IN SOLID

$ (12) NODAL POINT CARDS

$ (13) SOLID ELEMENT CARDS

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*ELEMENT SOLID
$ EID PID N1 N2 N3 N4 N5 N6 N7 N8
  1  2  1  83  85  3  2  84  86  4
  2  2  83  165 167  85  84 166 168  86
...(cont'd)

$ (14) SHELL ELEMENT CARDS

*ELEMENT_SHELL
$ EID PID N1 N2 N3 N4
  1  3  52061  52062  52064  52063
  2  3  52063  52064  52066  52065
...(cont'd)

$ (15) BOUNDARY CONDITION CARDS

*BOUNDARY_SPC_NODE
$ NID/NSID CID DOFX DOFY DOFZ DOFRX DOFRY DOFRZ
  1  0  1  1  1  1  1  0
  2  0  1  1  1  1  1  1
  3  0  0  0  1  1  1  0
...(cont'd)

*END

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Appendix D

Sample LS-DYNA Input File for Al 1100 Using Eulerian Formulation

*KEYWORD
$ - - - - -+ - - - - - - - - -! - - - - - - - - -+ - - - - - - - -2 - - - - - - - -+ - - - - - - - -3 - - - - - - -+ - - - - - - - - -4 - - - - - - - -+ - - - - - - - -5 -
$ UNITS: millimeters, kilograms, seconds
$- -+----
1----+----2----+----3---+----4----+----5-
$ (1) TITLE CARD.
$ ( 1 )  TITLE CARD.
$ $ - - - - -+ - - - - - - - - - -+ - - - - - - - - -+ - - - - - - - -2 - - - - - - - -+ - - - - - - - -3 - - - - - - -+ - - - - - - - - -4 - - - - - - - -+ - - - - - - - -5 -
$ TITLE
Al 1100: New Material Model-Voce type
$- -+----
1----+----2----+----3---+----4----+----5-
$ (2) CONTROL CARDS.
*CONTROL_TERMINATION
$ ENDTIM ENDCYC DTMIN ENDMAS
0.0200  0  0.0  0.0  0.0
$CONTROL_TIMESTEP
$ DTINIT TSSFAC ISDO TSLIMT DT2MS LCTM ERCDE MS1ST
 0.0  0.90  0
$ DT2MSF
*CONTROL_ALE
$ DCT NADV METH AFAC BFAC CFAC DFAC EFAC
 2  1  2  -1.0
$ START END AAPAC VFAC PRIT EBC PREF NSIDEBC
 0.0  100.0
$OPTION : BEAM BEAM_SET NODE NODE_SET
$ SHELL SHELL_SET SOLID SOLID_SET
$ TSHELL TSHHELL_SET
$ DATABASE CONTROL CARDS - ASCII HISTORY FILE
$OPTION
$ DATABASE_HISTORY_OPTION
$ ID1 ID2 ID3 ID4 ID5 ID6 ID7 ID8
$ DATABASE_GLSTAT
$ DT BINARY
 2.00E-05
$DATABASE_GLSTAT
$ DATABASE_MATSUM
$ DT BINARY
 2.00E-05
$DATABASE_RBDOUT
$ DT BINARY
 2.00E-05
$DATABASE_SLEOUT
$ DT BINARY
 2.00E-05
*DATABASE_PSI
$ DT
2.00E-05
$ DBFSI_ID SID SIDTYPE
1 3 1
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
$ (5) DATABASE CONTROL CARDS FOR BINARY FILE
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
*DATABASE_BINARY_D3PLOT
$ DT/CYCL LCDT NOBEAM
3.33E-04
*DATABASE_BINARY_D3THDT
$ DT/CYCL LCDT NOBEAM
3.33E-04
*$DATABASE_BINARY_OPTION
$ DT/CYCL LCDT NOBEAM
$ OPTION : D3DRFL D3DUMP RUNRSP INTFOR
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
*DATABASE_EXTENT_BINARY
$ NEIPH NEIPS MAXINT STRFLG SIGFLG EPSFLG RLTFLG ENGFLOG
0 0 3 1 1 1 1 1
$ CMPFLG IEVRFP BEAMP DCOMP SHGE STSSZ N3THDT
0 0 0 0 0 0
$ NINTSLD
1
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
$ (6) DEFINE PARTS CARDS
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
*PART
$SHADING
PART PID = 1 PART NAME : WORKPIEC
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
1 1 1 1
$PART
$SHADING
PART PID = 2 PART NAME : AIRMESH
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
2 1 1 1
$PART
$SHADING
PART PID = 3 PART NAME : CUTTER
$ PID SID MID EOSID HGID GRAV ADPOPT TMID
3 3 3
$INITIAL_VOID_PART
$ PID
2
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
$ (7) MATERIAL CARDS
$--------------2--------------3--------------4--------------5--------------6--------------7--------------8
*MAT_ELASTIC PLASTIC HYDRO
$ 1100 aluminum material properties
$ MID RO G SIGY EH PC FS
1 2.71E-6 2.71E+07
$ EPS1 EPS2 EPS3 EPS4 EPS5 EPS6 EPS7 EPS8
0.0 0.2 0.4 0.6 0.8 1.0 1.5 2.0
$ EPS9 EPS10 EPS11 EPS12 EPS13 EPS14 EPS15 EPS16
2.5 3.0 3.5 4.0 6.0 8.0 10.0 12.0
$ ES1 ES2 ES3 ES4 ES5 ES6 ES7 ES8
140200.0 161700.0 180400.0 196600.0 210600.0 228800.0 246600.0 263200.0
$ ES9 ES10 ES11 ES12 ES13 ES14 ES15 ES16
274900.0 283000.0 288700.0 292700.0 299800.0 301500.0 301900.0 302000.0
*MAT_RIGID
$ MID  RO  E  PR  N  COUPLE  M  ALIAS
3  3.24E-6  2.80E+08  0.25
$ CMO  CON1  CON2
1.0  5.0  7.0
$LCO_OR_A1  A2  A3  V1  V2  V3

(8) SECTION CARDS

*SECTION_SOLID
$ SECID  ELMFORM  AET
1  12

*SECTION_SHELL
$ SECID  ELMFORM  SHRF  NIP  PROFQ  QR/IRID  ICOMP  SETYP
3  2  0.0  0.0  0.0  0.0  0.0

(9) EOS CARDS

*EOS_GRUNEISEN
$ EOS parameters for A1 1100 from Steinberg
$ EOSID  C  SI  S2
1  5.386E+6  1.339  0.0
$ V0
1.0

(10) PRESCRIBED MOTION CARDS

*BOUNDARY_PRESCRIBED_MOTION_RIGID
$ DEFINE_CURVE
$ LCID  SIDR  SPA  SPO  OFFA  OPPO  DATTYP
1  0  1.0  -1.0  0.0  0.0
$ PID  DOF  VAD  LCID  SF  VID  DEATH  BIRTH
3  1  0  1

(11) EULER/LAGRANGE CONTACT COUPLING

*CONSTRAINED_LAGRANGE_IN_SOLID
$ SLAVE  MASTER  SSSTYP  MSTYP  NQUAD  CTYP  DIREC  MCOUP
3  1  1  0  -3  4  2  0
$ START  END  PFAC  FRIC  FRACMIN  NORM  PNORM  DAMP
1  0.20  0.0
$ CQ  HMIN  HMAX  ILIKE  PLEAK

*SET_PART_LIST
$ SID  DAI
1  0.0  0.0  0.0
$ PID1  PID2  PID3  PID4  PID5  PID6  PID7  PID8
1  2

(12) NODAL POINT CARDS

*NODE
$ NODE  X  Y  Z  TC  RC
1  0.00000000E+00  0.00000000E+00  0.00000000E+00
2  0.00000000E+00  0.00000000E+00  0.62500000E-01

...(cont'd)

(13) SOLID ELEMENT CARDS

188
$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
*ELEMENT SOLID
$ EID PID N1 N2 N3 N4 N5 N6 N7 N8
  1  2  1  83  85  3  2  84  86  4
  2  2  83 165 167  85  84 166 168  86
... (cont'd)

$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
$ (14) SHELL ELEMENT CARDS
$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
*ELEMENT SHELL
$ EID PID N1 N2 N3 N4
  1  3  52061 52062 52064 52063
  2  3  52063 52064 52066 52065
... (cont'd)

$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
$ (15) BOUNDARY CONDITION CARDS
$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
*BOUNDARY_SPC_NODE
$ NID/NSID CID DOFX DOFY DOFZ DOFRX DOFRY DOFRZ
  1  0   1   1   1   1   1   1
  2  0   1   1   1   1   1   1
  3  0   0   0   1   1   1   0
... (cont'd)

$ - - - - 1 - - - - 2 - - - - 3 - - - - 4 - - - - 5 - - - - 6 - - - - 7 - - - - 8
*END
Appendix E
Calculation of Experimental Error

1. Equivalent Plastic Strain

In this study, equivalent plastic strains in the deformed material were determined by measuring displacements of copper grain boundaries and the orientation change of flow lines in aluminum samples. These measurements could be influenced by individual’s judgement. The optical microstructures were enlarged to 200 times big during the shear angle measurement. The estimated error of measured shear angles is within ± 0.5°. Therefore, the maximum error of the calculated equivalent plastic strains is within ± 5%.

2. Flow Stress

Flow stresses in the deformed material were estimated from microhardness measurements. For copper, the fluctuation of microhardness values is bigger than that of aluminum. Three measurements were performed on each indentation points. Compared to the average value of each point, the maximum deviation of the measurements is 10 kg/mm² for copper is, and 5 kg/mm² for aluminum. Therefore, the error of the estimated flow stress is within ± 8% for copper, and ± 6% for aluminum.

3. Non-linear Regression of Stress-Strain Curve

The non-linear regression of the stress-strain curves of copper and aluminum was conducted in SigmaPlot 9.0 by curve fitting an “exponential rise to Max” equation. The coefficients of determination (Rsq) of the non-linear regression are 0.9994 for copper and 0.9976 for aluminum.
PUBLICATIONS


PRESENTATION

VITA AUCTORIS

Xiaodong Song was born in 1969 in Henan, China. She received her B.Sc. degree from University of Science and Technology, Beijing, China in 1991 and M.A.Sc. degree from Wuhan University of Science and Technology, China in 1994. From 1994 to 2002, she worked as a process/design engineer in WISDRI Engineering & Research Incorporation Limited, China. From May, 2003, she began her study toward her M.A.Sc. degree in Materials Engineering at the University of Windsor. She is going to obtain her degree in the summer of 2005.