3D finite element modelling of ultrasonically assisted turning of high-strength alloy

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3D FINITE ELEMENT MODELLING OF ULTRASONICALLY ASSISTED TURNING OF HIGH STRENGTH ALLOY

by Naseer Ahmed

A Doctoral Thesis

Submitted in partial fulfilment of the requirements for the award of Doctor of Philosophy of Loughborough University

April 2007

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Abstract

Ultrasonically assisted turning (UAT) is a novel machining technique, where high-frequency vibration is superimposed on the movement of a cutting tool. This technique allows significant improvements in processing intractable materials. This thesis reports research into the development of a coupled thermomechanical 3D finite element model of UAT.

In the case of UAT high-frequency vibrations are introduced in the cutting tool and it is important to use techniques capable of recording this fast movement of the cutting tool and its impact on both the workpiece and cutting tool. High speed (HS) filming is performed to study the chip formation, development of different shear zones and the material's response to different vibrations. Nanoindentation studies are performed to get an insight into the development of a hardness zone near the machined surface in the workpiece. The results obtained are used to calculate residual stresses in specimens of both UAT and conventional turning (CT). Surface analysis studies are conducted to prove the benefits of UAT – as compared to CT – in improving the surface finish.

A novel 3D elasto-plastic thermomechanically coupled finite element model of both UAT and CT are developed. These 3D models are used to study the effects of cutting parameters (cutting speed, feed rate and depth of cut, ultrasonic vibration, ultrasonic frequency, lubrication, rake angle and tool nose radius). Comparative studies are performed for the developed CT and UAT models and are validated by results from experiments conducted on the in-house prototype and the literature.

A series of simulations is also conducted on a realistic model of UAT for a range of variables in order to recommend an optimum set of parameters.

Keywords: Turning, Ultrasonic vibration, INCONEL 718, High speed filming, Thermomechanics, 3D Finite element analysis, Ultrasonic frequency
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I owe many thanks to my fellow researcher, Dr. Alexander Mitrofanov, since our joint research efforts have contributed much to this thesis. He had always provided me with a chance to discuss and implement different ideas.

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Finally, this thesis is dedicated to my parents for their lifelong commitment and enthusiasm toward my education.
## Notation and abbreviations

### Symbols

<table>
<thead>
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<th>Symbol</th>
<th>Meaning</th>
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<tbody>
<tr>
<td>$a$</td>
<td>Amplitude of vibration, $\mu m$</td>
</tr>
<tr>
<td>$b_f$</td>
<td>Burr foot width, mm</td>
</tr>
<tr>
<td>$b_g$</td>
<td>Burr thickness, mm</td>
</tr>
<tr>
<td>$d$</td>
<td>Depth of cut, mm</td>
</tr>
<tr>
<td>$D$</td>
<td>Workpiece diameter, mm</td>
</tr>
<tr>
<td>$E$</td>
<td>Young’s modulus, GPa</td>
</tr>
<tr>
<td>$E'$</td>
<td>Young’s modulus of indenter, GPa</td>
</tr>
<tr>
<td>$E^*$</td>
<td>Reduced Young’s modulus, GPa</td>
</tr>
<tr>
<td>$f$</td>
<td>Frequency of vibration, Hz</td>
</tr>
<tr>
<td>$F$</td>
<td>Cutting force, N</td>
</tr>
<tr>
<td>$F_t$</td>
<td>Main cutting force, N</td>
</tr>
<tr>
<td>$F_p$</td>
<td>Thrust force, N</td>
</tr>
<tr>
<td>$F_r$</td>
<td>Feed force, N</td>
</tr>
<tr>
<td>$h$</td>
<td>Convective heat transfer coefficient, W/m$^2$K</td>
</tr>
<tr>
<td>$h_o$</td>
<td>Burr height, mm</td>
</tr>
<tr>
<td>$H$</td>
<td>Contact heat conduction coefficient, W/m$^2$K</td>
</tr>
<tr>
<td>$k$</td>
<td>Shear yield strength, MPa</td>
</tr>
<tr>
<td>$K$</td>
<td>Thermal conductivity, W/m K</td>
</tr>
<tr>
<td>$l_C$</td>
<td>Contact length, mm</td>
</tr>
<tr>
<td>$n$</td>
<td>Rotational speed, rev/min</td>
</tr>
<tr>
<td>$r$</td>
<td>Chip thickness ratio</td>
</tr>
<tr>
<td>$r_f$</td>
<td>Burr foot radius, mm</td>
</tr>
<tr>
<td>$R_a$</td>
<td>Average roughness, $\mu m$</td>
</tr>
<tr>
<td>$R_n$</td>
<td>Tool nose radius, mm</td>
</tr>
<tr>
<td>$s$</td>
<td>Feed rate, mm/rev</td>
</tr>
<tr>
<td>$t$</td>
<td>Uncut chip thickness, mm</td>
</tr>
<tr>
<td>$t_C$</td>
<td>Deformed chip thickness, mm</td>
</tr>
</tbody>
</table>
\( T \)  
Temperature, °C

\( T_{\text{melt}} \)  
Melting temperature, °C

\( T_{\infty} \)  
Ambient temperature, °C

\( V_C \)  
Cutting speed, m/min

\( V_T \)  
Critical cutting speed, m/min

\( \alpha \)  
Rake Angle, °

\( \gamma \)  
Clearance angle, °

\( \lambda \)  
Burr angle, °

\( \dot{\varepsilon} \)  
Strain rate, s\(^{-1}\)

\( \dot{\varepsilon}_p \)  
Equivalent plastic strain

\( \dot{\varepsilon}_p \)  
Plastic strain rate, s\(^{-1}\)

\( \varphi \)  
Shear angle, °

\( \mu \)  
Friction coefficient

\( \rho \)  
Mass density, kg/m\(^3\)

\( \sigma_R \)  
Residual stress, MPa

\( \bar{\sigma} \)  
Equivalent stress, MPa

\( \nu \)  
Poisson’s ratio

\( \nu' \)  
Poisson’s ratio of the indenter

\( \omega \)  
Angular frequency, rad/s

**Abbreviations**

<table>
<thead>
<tr>
<th>Abbreviation</th>
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<tbody>
<tr>
<td>ALE</td>
<td>Arbitrary Lagrangian Eulerian</td>
</tr>
<tr>
<td>CT</td>
<td>Conventional turning</td>
</tr>
<tr>
<td>DOF</td>
<td>Degree of freedom</td>
</tr>
<tr>
<td>FE</td>
<td>Finite element</td>
</tr>
<tr>
<td>FEA</td>
<td>Finite element analysis</td>
</tr>
<tr>
<td>FEM</td>
<td>Finite element modelling</td>
</tr>
<tr>
<td>HS</td>
<td>High-speed</td>
</tr>
<tr>
<td>SED</td>
<td>Strain energy density</td>
</tr>
<tr>
<td>SEM</td>
<td>Scanning electron microscope</td>
</tr>
<tr>
<td>UAT</td>
<td>Ultrasonically assisted turning</td>
</tr>
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</table>
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1 INTRODUCTION

1.1 Background

Metal cutting is one of the most widely used manufacturing process where the desired dimensional accuracy and surface finish is achieved by carving away excess material from a bulk form. As the development of machining progressed, new techniques were adopted to improve the cutting techniques, precision of cutting and rate of production. Special tools were developed for machining of high strength materials.

Turning, a type of metal cutting, is the process, in which a single-point tool is used to remove unwanted material from a rotating part to produce a desired product, and is performed on a lathe. Modern turning techniques have been improved considerably to achieve easy machining of difficult-to-cut materials and better surface finish. Methods such as high-speed turning have been in use now for a considerable time. However machining of high-strength aerospace alloys, composites and ceramics causes high tool temperature and fast wear of cutting edges, lacks dimensional accuracy and requires a considerable amount of coolant. These deficiencies of conventional turning (CT) necessitate the development of new cutting techniques.

Ultrasonic assisted turning (UAT) has proved to be a significant process in the machining of hard-to-cut alloys. It is an advanced machining technique, where high-frequency vibration (frequency $f \approx 20$ kHz, amplitude $a \approx 15 \mu m$) is superimposed on the movement of the cutting tool. Compared to conventional turning, this technique allows significant improvements in processing intractable materials. A multifold decrease in cutting forces, as well as an improvement in surface finish can be achieved by the use of UAT [1].

Though being of such significance, this technique has not yet been widely introduced in the industry. Problems such as instability of the cutting process that resulted in poor finish prevented the full implementation of this process. The development of an autoresonant control system added stability to the system by making the vibrations regular [2]. However, thermomechanics of the tool-workpiece interaction, which is especially important for the regime with multiple microimpacts in the process zone in UAT, has not been fully understood. Since analytical
approaches cannot be applied to solve the problem due to its complicated geometry, numerical simulations are employed.

The finite element method (FEM) is a main computational tool used to model the process zone and tool-workpiece interaction in metal cutting. Both experimental and FEM techniques have been applied to study behaviour of material during UAT. A 2D model for the UAT had already been developed, which helped in understanding the mechanics of tool-chip interaction, model stresses and strains distributions in the cutting region, heat transfer in the workpiece material and in the cutting tool and also the cutting forces [3].

A need for the 3D analysis was strongly sensed because of the following limitations in the 2D model:

- it does not account for a thickness parameter of the workpiece and cutting tool;
- it does not allow modelling vibrations in the lateral direction;
- it does not allow simulating non-orthogonal/oblique chip formation;
- it does not account for the effects associated with the insert mounting;
- it does not account for the flow of material in the third dimension.

The current thesis encapsulates the work conducted for 3D FE modelling of both UAT and CT processes; CT cases are modelled in parallel for a comparison purpose.

1.2 Aims and objectives

Aim:

The aim of the project was to develop a coupled thermomechanical 3D-finite-element model of UAT in order to investigate the different deformation and thermal processes as well as stress distributions in the tool-chip interaction zone and to suggest optimal cutting parameters for ultrasonically assisted turning processes of advanced engineering materials.

Objectives:

The objectives of the project were:

1. to study the major features of UAT;
2. to analyse stress, strain and temperature distributions in the cutting region;
3. to study cutting forces;
4. to study the chip morphology under the effect of different friction conditions;
5. to study heat transfer in the workpiece material and into the cutting tool;
6. to perform experimental studies of CT and UAT in order to verify and validate numerical simulations;
7. to perform a detailed comparison of UAT vs CT on the basis of different variables of turning such as a cutting speed, feed rate and friction coefficient;
8. to suggest a set of parameters of cutting variables for successful implementation of UAT.

All the results for the features and parameters of UAT, obtained in 3D simulations, will be compared with those from simulations of CT.

1.3 Outline of the report

The present work incorporates seven other chapters, the summary of which is the following:

Chapter 2 deals with basic mechanics of cutting, chip shapes, the forces involved during the cutting, experimental methods to estimate the chip shape and the forces.

Chapter 3 presents a general theory of FEM followed by the literature review of the development of FE models for cutting throughout history.

Chapter 4 explains the development of the UAT technology in recent years, the basic components of a UAT system and reviews publications on ultrasonic turning.

Chapter 5 presents some of more recent experimental results including high-speed filming and experimental turning force measurement.

Chapter 6 gives details of the general features and development of simulation models for both UAT and CT of INCONEL 718. The modelling strategy for various cases - from a simple orthogonal model to the more complex incremental turning models - is presented. The development of a real-life turning model is also presented.

Chapter 7 presents a detailed discussion of the results obtained from FEM models. This discussion is focused on the comparison of cutting forces, the effect of friction, temperature and stress distributions, the chip shape, feed rate and effects of ultrasonic amplitude and frequency.

Chapter 8 suggests a combination of parameters for successful implementation of UAT of INCONEL 718 on the basis of a sequence of simulations performed for
different conditions of turning such as the cutting speed, feed rate, coefficient of friction and the amplitude, frequency and direction of vibration.

Chapter 9 summarizes the outcome of the research, presents conclusions drawn on the basis of the experimental results and numerical simulations, and recommends possible further work on the topic.
2 MECHANICS OF TURNING

Material removal plays a major role in reshaping objects in many manufacturing processes. A conventional cutting operation involves the removal of macroscopic chips from the workpiece. The four widely used cutting operations are:

- turning;
- grinding;
- milling;
- drilling.

The focus of the current research is the turning process and thus the following sections will present details about this type of cutting operation only.

2.1 Turning

The machine, on which this operation is performed, is the lathe [4]. The variables adjusted by an operator in turning are the cutting speed \( v \), the feed rate \( s \) and the depth of cut \( d \) (Figure 2-1 (a)).

The cutting speed \( v \) is the rate at which the uncut surface of the workpiece passes the cutting edge of the tool. The feed rate \( s \) is the distance covered by a tool in an axial direction at each revolution of the workpiece. The depth of cut \( d \) is the thickness of material removed from the workpiece [5].

2.2 Cutting tool geometry

The shape and position of the tool, relative to the workpiece, have an important effect on turning [4]. The tool surface, over which the chip flows, is known as a rake face, and is inclined at an angle to the axis of workpiece. The rake angle \( \alpha \) (Figure 2-1 (a)) is measured from a line parallel to the axis of rotation of the workpiece. The cutting edge is formed by the interaction of the rake face with the clearance face of the tool, and the angle that this clearance face makes with the cut surface is the clearance angle \( \gamma \). The clearance angle prevents the tool from rubbing against the freshly cut metal surface. The third face of the tool (Figure 2-1 (b)), the end clearance face, is inclined
at an angle to avoid rubbing against the freshly cut surface. The intersection of all three faces is called the nose of tool. The transition between two clearance faces has a radius of curvature and is called the nose radius $R_n$.

![Diagram](image)

**Figure 2-1** (a) Parameters and geometry of cutting (b) faces of cutting tool [6]

### 2.3 Types of chip

Cutting conditions and the type of material being cut can strongly influence the type of chip formed. This and the following sections present the details of different types of chips and the key factors behind their formation [4].

*Discontinuous or segmented chips* are produced when brittle metal such as cast iron and hard bronze are cut or when some ductile metals are cut under poor cutting conditions like low cutting speeds, coarse machining feeds and small rake angle on cutting tools. Generally, a poor surface is produced as a result of cutting with discontinuous chips.

A *continuous chip* is produced when the flow of metal next to the tool face is not greatly restricted by a built-up edge or friction at the chip tool interface. The continuous chip is considered ideal for an efficient cutting action because it results in better finishes.

Due to high temperature, high pressure, and high frictional resistance to the flow of the chip along the chip-tool interface, small particles of metal can adhere to the edge of the cutting tool. As the cutting progresses these fragments break off and most of which stick to the chip. Adherence of these fragments to the machined surface results
in the formation of continuous chip with a built-up edge (BUE), which results in a poor finish [7].

The shear localized chip can be formed when the yield strength of the workpiece decreases with temperature. Under the proper conditions the rapidly heated material in a narrow band in front of the tool can become much weaker than the surrounding material, leading to localized deformation [8]. This type of chip is obtained when cutting stainless steels and titanium alloys at high cutting speeds.

2.4 Chip formation

This section presents the details of chip formation mechanics [4]. Chip formation requires concentrated shear along a distinct shear plane. The deformation of metal starts after it reaches the primary shear zone (Figure 2-2). The metal then undergoes a substantial amount of simple shear as it crosses this thin zone. There is essentially no further plastic flow as the chip proceeds along the tool face. Here \( t \) is the uncut chip thickness. Uncut chip thickness is a term closely related to feed, but is used from the point of view of the chip formation process.

2.4.1 Forces involved in chip formation

When the chip in the orthogonal process is presented as a free body, only two forces are considered – the force between the tool face and the chip (\( R \)) and the force between the workpiece and the chip along the shear plane (\( R' \)). For equilibrium these must be equal:
The forces $R$ and $R'$ are conveniently resolved into three sets of components as shown in Figure 2-3:

- the horizontal and vertical components, $F_p$ and $F_Q$;
- along the perpendicular to the shear plane, $F_S$ and $N_S$;
- along and perpendicular to the tool face, $F_C$ and $N_C$.

If the forces $R'$ and $R$ are plotted at the tool point instead of at their actual points of application along the shear plane and tool face, a convenient and compact diagram is obtained (Figure 2-4). Here $R$ and $R'$ (which are equal and parallel) are coincident and are making the diameter of the dotted reference circle shown. Then, by virtue of the geometrical fact that lines that terminates at the ends of diameter $R$ and intersect at a point on the circle will form right angles, there is a convenient means for graphically resolving $R$ into orthogonal components in any direction.
Figure 2-4 Composite cutting force circle (after [4])

Analytical relationships may be obtained for the shear and friction components of the force in terms of the horizontal and vertical components ($F_p$ and $F_Q$) which are the components normally determined experimentally by a dynamometer. From Figure 2-5 it is evident that

$$F_s = F_p \cos \varphi - F_Q \sin \varphi,$$  \hspace{1cm} (2-2)

$$N_s = F_Q \cos \varphi + F_p \sin \varphi = F_s \tan(\varphi + \beta - \alpha).$$  \hspace{1cm} (2-3)

Similarly from Figure 2-5,

$$F_c = F_p \sin \alpha + F_Q \cos \alpha,$$  \hspace{1cm} (2-4)

$$N_c = F_p \cos \alpha - F_Q \sin \alpha.$$  \hspace{1cm} (2-5)

The tool face components are of importance since they enable the coefficient of friction on the tool face $\mu = \tan \beta$, where $\beta$ is the friction angle shown in Figure 2-5 to be determined:
\[
\mu = \frac{F_c}{N_c} = \frac{F_p \sin \alpha + F_q \cos \alpha}{F_p \cos \alpha - F_q \sin \alpha} = \frac{F_q + F_p \tan \alpha}{F_p - F_q \tan \alpha}.
\] (2-6)

**Figure 2-5** Resolution of resultant cutting forces (a) Shear Plane. (b) Tool face
(after [4])

### 2.4.2 Stresses involved in chip formation

The shear-plane components of the force are necessary to determine the mean shear and normal stresses on the shear plane. Thus,

\[
\tau = \frac{F_s}{A_s},
\] (2-7)

where \(A_s\) is the area of the shear plane, and

\[
A_s = \frac{bt}{\sin \phi},
\] (2-8)

where \(b\) is the cutting edge engagement length and \(t\) is the depth of cut. From equations for \(F_s\), \(N_s\), \(\tau\) and \(A_s\) follows

\[
\tau = \frac{(F_p \cos \varphi - F_q \sin \varphi) \sin \varphi}{bt}.
\] (2-9)

Similarly,

\[
\sigma = \frac{N_s}{A_s} = \frac{(F_p \sin \varphi + F_q \cos \varphi) \sin \varphi}{bt}
\] (2-10)
2.4.3 Shear angle

Shear angle can be obtained using a cutting ratio \( r \), which is the ratio of the depth of cut \( t \) to the chip thickness \( t_C \). This ratio may be determined by directly measuring \( t \) and \( t_C \).

It was found experimentally that when the metal is cut there is no change in its density and hence,

\[
tbl = t_cbcl_c, \quad (2-11)
\]

where \( b, l \) are the width of cut and length of cut respectively, and a subscript \( C \) refers to the corresponding measurement on the chip. If \( \frac{b}{l} \geq 5 \), as was experimentally found, the chip width is the same as that of the workpiece and hence,

\[
\frac{t}{t_c} = \frac{l_c}{l} = r. \quad (2-12)
\]

The relationship between the cutting ratio \( r \) and the shear angle \( \phi \) may be obtained with the aid of Figure 2-6.

Here it is evident that

\[
r = \frac{t}{t_c} = \frac{AB \sin \phi}{AB \cos(\phi - \alpha)}, \quad (2-13)
\]

or, solving for \( \phi \):

\[
\tan \phi = \frac{r \cos \alpha}{1 - r \sin \alpha}. \quad (2-14)
\]
Although analytical techniques are used to find the forces and stresses in a turning process their use is limited to simple processes. Real-life cases of turning are more complicated and hence require the use of other techniques for finding the forces, stresses and temperatures involved. The following section presents the details of experimental techniques adopted by researchers to study chip shape, forces, surface roughness and temperatures involved in turning.

2.5 Experimental methods

Researchers throughout history have been using different types of experimental techniques to study different effects of turning, e.g. chips shapes, forces, temperature etc. This section presents some of the more broadly used techniques adopted for turning.

2.5.1 Chip formation

Direct observations as well as in depth analysis are required to analyze a chip formation mechanism. High-speed imaging is now used to study the chip formation during cutting more closely. Both light microscopy and scanning electron microscopy (SEM) can be used for a posteriori external observations of chip microstructure. All
these techniques require special setup and arrangement and still they do not provide in
depth analysis of the tool-chip interface due to the small scale of the deforming region
and usually high cutting velocities.

2.5.2 Measurement of forces acting on the cutting tool

In order to put the analysis of the metal cutting operation on a quantitative basis,
certain observations must be made before, during and after a cut. The determination
of cutting force components is one of the most important measurements carried out
during the cutting process. The force measurement involves mounting a tool on a
dynamometer (Figure 2-8), which responds to the forces by creating electrical signals
in proportion to them. Generally, the dynamometer is required to be rigid, sensitive,
and accurate.

![Figure 2-7 A schematic diagram of the experimental setup](image)

To keep recorded forces uninfluenced by machine vibrations, the natural frequency
of the dynamometer should be kept large compared to the frequency of the exciting
system. The dynamometer must measure at least two components of force [4]. For
convenience, the forces are mostly measured relative to a set of rectangular
coordinates (x, y, z). In most dynamometers the force is applied to some sort of
spring, and the deflection produced is measured.
2.5.3 Temperature measurement

Different techniques had been applied to cutting operations to study a temperature rise in the tool-workpiece interaction zone. The tool work thermocouple technique is one of them and is based on the fact that an electromotive force (EMF) is generated at the interface of two dissimilar metals when the temperature of the interface changes. The applied form of the thermoelectrical circuit is given in Figure 2-9 which shows a lathe equipped for measuring tool-face temperatures [4].

![Figure 2-8 Schematic diagram showing temperature measurement of tool-chip interface by means of thermoelectric technique (after [4])]()

The chip and tool junction H constitutes the hot junction, while A and B (the cold junction) remain at room temperature [4]. The mercury contact is used to make electrical connection with the rotating member without introducing extraneous voltages that frequently accompany the use of slip rings. The wires C and D may be ordinary copper wires and since both ends of these wires are at the same temperature, they have no influence on the EMF generated at H, as measured with the potentiometer P. While the tool-workpiece thermocouple technique is relatively simple to apply, it is not without its limitations. The method estimates the temperature over the entire contact area between chip and tool. Oxide layer formation resulting from low-melting non-metallic inclusions may change the static calibration of a tool-workpiece thermocouple if present.

Another technique used to measure temperature in the cutting zone is based on a thermocouple embedded in the cutting tool [9]. Figure 2-9 shows an example of the embedded thermocouple technique for mapping temperatures in a cutting tool used in
the turning operation. The hole in which the thermocouple is placed represents a disturbance and may appreciably change the temperature field being measured unless it is very small. Mapping the temperature field by the use of a thermocouple is extremely tedious procedure involving many tools with thermocouples mounted at different points. Also the sharp corners of the holes may be the cause of steep temperature gradients around them, which may lead to errors in calculations.

![Diagram of embedded thermocouple technique](image)

**Figure 2-9** Embedded thermocouple technique for mapping temperatures in a cutting tool (after [9])

A non-contact technique of measuring temperature from the surface of the tool and workpiece is thermal imaging. The use of this technique allows measurements of the temperature from the exposed areas of the material only. The more advanced form of this technique is the use of thermal imaging cameras, which contain an infrared detector, optical subassemblies, a power source, a signal processor, and a standard video monitor. These cameras display the temperature of the object in the form of color gradients, but an accurate calibration of these instruments prior to use is always required.

Although analytical relations are a useful means of finding the forces and stresses in the turning process the applications are only limited to simple geometries. Real turning processes are additionally complicated by various factors, e.g., presence or
absence of lubricant and introduction of ultrasonically assisted tooling, effectively excluding an analytical solution. This leaves numerical simulations especially finite element techniques as the only tool. The next chapter will present the details about the developments in finite element methods to solve such complicated problems and to predict the forces, stresses, temperatures and chip shapes resulting from changes in turning parameters. Experimental methods will be used to validate and verify the numerical simulations.
3 FEM IN CUTTING

In metal cutting, large deformations take place in the vicinity of a tool-workpiece interaction zone, and cutting conditions affect considerably strain rates and temperatures. Thermomechanics of the tool-workpiece interaction and other specific features of the cutting process cannot be fully estimated from analytical solutions and by simplified experimental methods. FE modelling is a main computational tool for simulation of the cutting process and of tool-workpiece interaction in metal cutting that can provide detailed information on forces, stress, temperature, chip shape and also can predict the optimum set of parameters for an efficient turning process.

3.1 Theory of FEM

Finite Element Method (FEM) is a numerical analysis technique used by engineers, scientists and mathematicians to obtain a solution to the differential equations that describe, or approximately describe a wide variety of physical (and non-physical) problems. Physical problems range in diversity from solid, fluid and soil mechanics, to electromagnetism or dynamics. The underlying premise of the method states that a complicated domain can be sub-divided into a series of smaller regions in which the differential equations are approximately solved. By assembling the set of equations for each region, the behaviour over the entire problem domain is determined [10].

Finite element analysis (FEA) is a computer simulation technique that uses FEM to solve complex engineering problems. It employs a number of finite points (nodal points) covering the domain of a function to be evaluated. The subdomains within these nodes are called finite elements [11]. Thus, the entire domain is a set of elements connected at their boundaries without gaps, each element is identified by its nodes. Instead of seeking a solution covering the whole space, an approximate solution is obtained just for the nodes. Governing equations associated with geometry, velocity, and forces are applied to the nodes and then all equations are solved numerically. An approximate solution is obtained by interpolation between the nodes. These steps are all covered in three phases in most of the computer-aided software: (i) pre-processing where the finite element model and different boundary conditions are defined, (ii) solving where the problem is solved on the basis of applied boundary
conditions and the material properties, and finally (iii) post-processing where results are obtained from the software using different visualization tools.

The FEM has the following six steps, and its success is due in part to the simplicity of these steps and the way that a FEA program implements these steps [12].

**Step 1. Shape Functions:** The FEM expresses the unknown field, \( u(x) \), in terms of the nodal point unknowns, \( a \), by using the shape functions, \( N(x) \), over the domain of the element, \( \Omega^e \), as

\[
 u(x) = N(x) a^e .
\]  

\[ (3-1) \]

**Step 2. Material Loop:** The FEM expresses the dependent flux fields, \( \varepsilon(x) \), such as the strain (stress) or heat flux, in terms of the nodal point unknowns as,

\[
 \varepsilon(x) = L[u(x)] = B a ; \\
 \sigma = \varepsilon(x) = D \varepsilon(x) .
\]  

\[ (3-2) \]

\[ (3-3) \]

Here \( L \) is the matrix of partial differential operators, \( D \) is the matrix of material constants, normally obtained through experiments and \( B \) is the strain matrix.

**Step 3 Element Matrices:** The FEM equilibrates each element with its environment which can be expressed as

\[
 K^e a^e + f^e = 0 ,
\]  

\[ (3-4) \]

where the element matrices, \( K^e \) and \( f^e \) have consistently lumped all physical significance of the element at its nodes, here:

\[
 K^e = \int_B^{T} D b dV , \\
 -f^e = \int_{\Omega^e} N(x)^T b dV + \int_{\Gamma^e} N(x)^T t dS + F ,
\]  

\[ (3-5) \]

\[ (3-6) \]

\( K^e \) represents physical properties such as stiffness or conductivity, superscript \( T \) denotes transpose and \( f^e \) represents loads experienced by the element \( e \). These loads may be body loads, \( b \), such as weight or internal heat generation in volume \( \Omega^e \); surface loads, \( t \), such as pressure or convection on surface \( \Gamma^e \); or concentrated loads, \( F \).

**Step 4. Assembly:** The FEM assembles all elements to form a complete structure in such a manner so that to equilibrate the structure with its environment, which requires
\[ K = \sum_{e} K^e, \]  
\[ f = \sum_{e} f^e, \]

and to insure continuity of nodal point unknowns, \( a = a^e \). Therefore, a finite number of system equations results as,

\[ Ka + f = 0 \]  

**Step 5. Solution of Equations:** The FEM specifies the boundary conditions, namely, the nodal point values on the boundary and the system equations are partitioned as

\[
\begin{bmatrix}
K_{uu} & K_{us} \\
K_{su} & K_{ss}
\end{bmatrix}
\begin{bmatrix}
a_u \\
a_s
\end{bmatrix} =
\begin{bmatrix}
f_a \\
f_r
\end{bmatrix}
\]

where, \( a_u \) are the unknown nodal values; \( a_s \) are the specified nodal values; \( f_a \) are the applied nodal loads; \( f_r \) are the nodal point reactions. Hence the solution becomes

\[ a_u = -K^{-1}_{uu}(f_a + K_{us}a_s), \]

\[ f_r = -(K_{su}a_u + K_{ss}a_s). \]

**Step 6. Recover:** The FEM recovers the stresses by substituting the unknown nodal values found in Step 5 back into Step 2 to find dependent flux field such as strain, stress and heat flux.

### 3.2 Nonlinearity

A nonlinear system represents systems, which behaviour is not expressible as a sum of the behaviours of its descriptors. In structural analysis there are four sources of a nonlinear behaviour. The corresponding nonlinear effects are identified by the terms material, geometric, force boundary conditions and displacement boundary conditions.

The change in geometry as the structure deforms is taken into account in geometric nonlinearities by setting up the strain-displacement and equilibrium equations [13]. The situation can occur when the deformation can become large during a loading process. In such cases it is necessary to distinguish between the reference configuration, where the initial shape of the body to be analysed is known, and the current of deformed configuration after loading is applied. When the deformation of a body is sufficiently large to invalidate the assumptions inherent in the small-strain
theory, the finite deformation theory is then used. In a finite deformation problem, loads can be introduced relative to the deformed configuration.

Material nonlinearity occurs when the material behaviour depends on current deformation state and possibly past history of the deformation. Other constitutive variables (prestress, temperature, time, moisture, electromagnetic fields etc.) may be involved. If the material does not fit the standard elastic constitutive equation that relates stresses and strain \((\sigma = Ce)\), generalization of this equation are then necessary.

Transient problems arise when some parameters are changing with time and thus the steady state equations are no longer applicable. The Newton-Raphson (N-R) method, analyzed below following [12], forms the basis of the most practical scheme to solve the transient problems.

For a solution step, \([K][a] = [F]\) or \(I - E = 0\) should be solved [12]. For a linear case, Guass elimination can be applied directly, but for nonlinear equation both the stiffness and external forces can be the functions of the nodal displacements:

\[
I(a) - F(a) = 0.
\]  
(3-13)

To solve this type of equations both the full or modified N-R method, which is an iterative method, can be applied. For a general nonlinear equation, \(f(a) = 0\), and at a known point \(a_i\) a correction \(\Delta a_{i+1}\) is calculated as follows

\[
\Delta a_{i+1} = -\frac{f(a_i)}{f'(a_i)}
\]  
(3-14)

with

\[
a_{i+1} = a_i + \Delta a_{i+1}
\]  
(3-15)

by defining a tangent stiffness as

\[
f'(a_i) = K_i^T(a_i) = \frac{\partial}{\partial u} (I(a_i) - F(a_i))
\]  
(3-16)

and the residual as

\[
f(a_i) = R(a_i) = I(a_i) - F(a_i).
\]  
(3-17)

N-R can then be written in a more familiar form

\[
K_i^T(a_i)\Delta a_{i+1} = R(a_i),
\]  
(3-18)

and the Guass elimination technique can be used to solve this set of equations for \(\Delta a_{i+1}\).
With each iteration the residual should decrease. If it does, the method converges to the correct solution. If the extent of the non-linearity is too great (loads are too large) the method may diverge, or simply not converge. For this reason the load is applied gradually or incrementally.

### 3.3 Eulerian vs Lagrangian analysis

In FE modelling, there are two main types of analysis, in which a continuous medium can be described: Eulerian and Lagrangian analysis [14]. In Eulerian analysis the region of flow is divided into elements, which are fixed in space, and material is allowed to flow through them and the attention is drawn to the way parameters vary from element to element. Whereas in Lagrangian analysis elements are fixed to the flowing material so that they convect with the material, and attention is focused on how the parameters of the particular element vary with time [15].

Eulerian analysis is more common in fluid mechanics than in solid mechanics because a fluid's properties vary less with deformation than those of a solid [16]. The Eulerian type of formulation is recommended for cases where the shape of elements does not change. However, in the case of machining, in which determining the location of free-surfaces of the chip is a part of the problem to be solved, it is not clear where the elements should be drawn. It is necessary to develop the free surface boundaries of the element mesh by iteration. The Lagrangian formulation is therefore a preferable way to model cutting processes. It allows changes in the shape of the elements with flow. The state of the material is fixed in an element. However, the element shape change and requires an update of the strain matrix. This leads to geometric non-linearities in addition to material non-linearities in the finite element equations. In extreme cases it may become necessary to simplify a distorted element shape by remeshing. There is a further complication. An element most likely rotates as well as distorts as it passes through a flow. After a while, its local x and y directions will differ from those of other elements. However, a common set of axes is required for the transformation of individual element equations to a global assembly. Counter-rotating the local element coordinate system as well as updating the strain matrix is repeatedly required.
3.4 Remeshing

In simulations of high-deformation problems, the mesh in the proximity of tool-workpiece interaction zone becomes too distorted and is thus no longer capable of performing the intended job. It is therefore required to replace the distorted mesh with that of the right shape. With Eulerian meshes a pre-choice can be made regarding the area and extent of refining the mesh and the intended sizes can be implemented easily. However, for computing efficiency with a Lagrangian mesh, there is a need to refine and then coarsen the mesh during the simulation as the material is divided into elements as it flows into and out of plastic shear zones with high shape distortions.

No matter whether it is automatic or manual, the procedures followed in remeshing are similar. During the remeshing procedure, the process is divided into many loading steps. The mesh is observed after each loading step and if distorted elements are present in the meshed domain, all the information related to nodes is stored in matrices. The elements in the vicinity of and inside the distortion zone are deleted and new elements with better a shape ratio are introduced. The stored nodal data are mapped back onto the nodes. The process continues as shown in Figure 3-1.

Figure 3-1 Remeshing cycle
3.5 **Historical developments in simulation of metal cutting**

FE simulations have been applied to the machining processes during the last three decades. This section presents the contribution of different researchers to the analysis of the metal cutting process.

### 3.5.1 Chip formation mechanism

The first attempts to model chip formation were by Carroll and Strenkowski [17] when they developed two computer models that treated orthogonal cutting. The models were based on the finite element method, which was used to discretize a portion of the workpiece in the vicinity of a cutting tool. The first model was based on a specially modified version of a large updated Lagrangian code which employed an elastic-plastic model (Figure 3-2). The second model treated the region in the vicinity of the cutting tool as an Eulerian flow model (Figure 3-3); the material passing through the field was modelled as viscoplastic. From the models, the detailed stress and strain fields in the chip and workpiece, chip geometry and tool forces were determined. The updated Lagrangian model linked all displacements and rotations to a fixed coordinate reference frame. Elements were attached to, and deformed with, the workpiece and chip. The model required an explicit material failure criterion and parting algorithm to control the chip formation process. In contrast, the Eulerian-based model used good elements to define a control volume, through which element flowed. Results obtained from both models showed excellent agreement when compared with measured tool forces for slow speed cutting of aluminium.

Another FE model to simulate a plane-strain orthogonal metal cutting process with continuous chip formation was developed by Shih and Yang [18]. A prescribed line of cut in front of a tool tip, parallel to the cutting plane, was used. Element-separation criteria were based upon the assumption that elements separate when the plastic strain at the node ahead of the tip of the cutting tool was greater than a critical value. An adaptive mesh re-zoning criterion was employed, which was capable of adding, refining, combining and deleting element columns. The elements and nodal data such as stresses, temperatures were interpolated, deleted, added or translated during mesh re-zoning. Although Remeshing and rezoning were introduced in author's work, the implementation was still along a predefined line.
A finite element model was developed for the computational machining of titanium alloy Ti-6Al-4V in [19]. A two-dimensional elastic plastic analysis was formulated in the updated Lagrangian form. The geometrical nonlinearity and the reduction of the
constraint of incompressibility on the plastic deformation of an element were taken into account. The friction at the rake face and the complicated flow stress characteristic of the titanium alloy at high strain rates and high temperature were also considered. A ductile fracture criterion on the basis of strain, strain rate, hydrostatic pressure and temperature was applied to the crack growth during the chip segmentation along a pre-defined line. The temperature field in the flowing chip and workpiece as well as the fixed tool was calculated simultaneously by an unsteady state thermal conduction analysis and the re-meshing of tool elements. Serrated chips predicted in simulations of machining showed striking resemblances in the shape and irregular pitch to those obtained by actual cutting. The mean cutting forces and the amplitude of cutting force vibration in the computational machining were in good agreement with those in the actual machining. The paper contributed to development in chip separation by the introduction of multiple pre-defined lines, separation along which resulted in serrated chip formation but still global remeshing and rezoning were not used.

A better model for 2D FE modelling was presented in [20], which employed adaptive re-meshing being capable of eliminating the bulk of deformation-induced element distortion. The mesh was constructed by Delauny triangulation and was adapted at regular intervals by adding new corner nodes at the midside of elements targeted for refinement. Re-meshing was triggered based upon a critical distance and critical effective plastic strain criterion. The simulation demonstrated chip morphologies consistent with experimental observations.
An FE model for simulation of serrated chip formation was later presented in [21], which was capable of simulation material breakage by deleting the mesh elements of the workpiece material when their damage was greater than a defined critical value. Figure 3-4 presents the remeshing module followed during the simulations.

The advantage of the approach was that the FE model of the workpiece was not bound to a predefined cutting line and thus chips were undergoing remeshing even when they were not in the tool-chip interaction zone, and thus chip shapes were better presented (Figure 3-5). The research extended the application of global remeshing criteria to serrated chip formation which, in earlier attempts, was modelled using predefined crack paths.
A criterion based on controlled crack propagation for the simulation of chip formation was used in [22]. Selection of a crack length-time relationship was based on the movement of the cutting tool and a reference point on the machined surface. A pre-defined cutting line was defined in the workpiece, which was re-zoned with increasing strain. The need for re-zoning was established by monitoring the extent of distortion of elements from the deformed configuration plots, and re-zoning was implemented by extracting the current model geometry from data in the result file. After forming a new mesh, the analysis was continued, using the previous results as new initial conditions. A two-step interpolation technique was used to transform the old solution to the new mesh. First, all the values of the variables were obtained at the nodes of the old mesh by extrapolating the data from the integration points to the nodes of each element. The values were then averaged over the elements adjacent to the old mesh, and the variables were interpolated from the nodes of the element in the old mesh to the locations in the new mesh. After all of the solution's values were transformed, the analysis proceeded on the new mesh. A typical simulation of the orthogonal machining process required many re-zonings before achieving convergence. For all the cases in the study, rezoning had to be done after the tool moved approximately 0.05 mm into the workpiece. Distributions of strain, strain rate, stress and temperature along the shear zone were presented for machining conditions corresponding to those considered experimentally, and the respective average values were in good agreement with those obtained from the experiments.
Simulations of high-speed flat-end milling using FE methods were presented in [23]. The chip formation simulations were conducted with DEFORM™ (commercial FE software), which was capable of automatically separating a chip from the workpiece around the tip, based on metal flow, material properties and tool geometry. Both automatic re-meshing and re-zoning were used in simulations that were conducted for the axisymmetric deformation model, from which 3D cutting results were approximated. A special methodology was developed to determine simultaneously both the flow stress of a workpiece material and the friction conditions at the chip-tool contact surface. The basic concept of the methodology was the use of orthogonal cutting experiments and FEM simulations in order to determine the flow stress and friction conditions used for the range of high-speed cutting. Therefore a limited number of orthogonal end turning experiments on a P20 mild steel disk were conducted using uncoated tungsten carbide tooling. From the experiments, two components of cutting force \( (F_c \text{ and } F_t) \), chip thickness \( (t_c) \), and chip-tool contact length \( (l_c) \) were measured. In addition, microscopic images of chips were collected to identify chip formation. The acquired data was used to calibrate the model that simulated the process. The unknown friction coefficients were determined in another iterative scheme until the difference between predicted and measured thrust forces was less than 10%. Finally, cutting force predictions from process simulations, using the determined flow stress and friction models, were compared with experiments and good agreements were obtained. Although automatic remeshing and rezoning features of DEFORM™ resulted in chips that matched experimental results, the research was still limited to only 2D simulation and prediction of 3D results from 2D was a possible source of error in the results.

Simulations of metal cutting using the iterative convergence method (ICM) were discussed in [24]. ICM develops a steady-state chip formation from the initial state of a fully formed chip loaded against a tool. ICM relies for its accuracy on the assumption that its simplified loading path coincides with the real developed flow at the end of simulation. The flow chart of the ICM is shown in Figure 3-6.
In all previous models, that followed the chip loading path, the start of crack propagation from one element to the next was a serious issue. Normally, the critical displacement, strain and strain energy criteria were used. But in ICM, the tool displacement, required to reach the fully loaded state, is typically only 5% of the uncut chip thickness.

Simulations of the machining process with mesh adaption were studied in [25]. A 2D simulation was used with the large-strain theory and updated Lagrangian formulation. Re-meshing and re-zoning were employed to simulate the cutting procedure based upon tool advancement in the workpiece and element distortion. Local remeshing criteria rather than global remeshing was used in the research that helped in reducing the computational time.
Simulation of 2D chip formation in orthogonal metal cutting based upon the updated Lagrangian formulation was presented in [26]. A chip- or element-separation criterion was implemented into MARC (commercial FE software) FE code.

Figure 3-7 Schematic diagram of geometric separation criterion: (a) before element separation, \( D > D_c \); (b) after element separation, \( D \leq D_c \) [26]

The criterion for separation of nodes in front of the tool tip was based on a geometrical consideration that when the tool tip approached a node within a critical distance, that node separated from the workpiece and became part of the chip (Figure 3-7).

Figure 3-8 Shape of the deformed chip after a tool path of 2.58 mm [26]

Figure 3-8 shows the successful implementation of the separation criterion. The model was capable of predicting the stress, strain, strain-rate and temperature distributions in the chip, workpiece and tool as well as developed cutting forces. The research was not relaying on a predefined path but still did not incorporated for
division of element into two and thus was only creating a crack along the element boundary. The approach, therefore, required defining a very dense mesh in the workpiece, along the expected tool path.

The mechanics of chip formation in grinding was investigated based on thermo-elastic-plastic finite element simulations of orthogonal cutting with large negative rake abrasive-grits in [27]. A thermomechanical coupled model with strain-rate-dependent flow stress characteristics of a work material SK-5 (0.93%C carbon steel) was formulated. The calculated shape of chip was affected by the cutting speed and the undeformed chip thickness. In high-speed cutting, serrated chip formation caused by adiabatic shear, which was usually observed experimentally under the cutting conditions in the grinding region, was obtained analytically without any consideration of crack propagation. Temperature and flow stress calculated in the primary shear zone varied periodically according to the segmentation of a serrated chip. Cutting experiments with a large negative rake tool or a single abrasive grain showed that a stagnant region formed at the tool tip as shown in Figure 3-9 (a). A finite element mesh used for the cutting analysis is shown in Figure 3-9 (b). The stagnant region was placed on the rake face of the tool in advance. A part of the mesh around the tool tip is shown in detail. Although, the model was relaying on an already developed method of chip separation (the predefined line in the workpiece along which the chip was separated) but the concept of introducing a stagnant region on the cutting tool provided some interesting results. In the turning zone of the serrated chips, the equivalent stress reached the maximum first, and then the strain rate had the maximum during work softening. The rapid work softening decreased the equivalent stress by 70%.

A bi-dimensional numerical thermomechanical Lagrangian model in finite transformations including contact with friction, and an independent prototype software dedicated to such simulation was presented in [28]. A thermo-elasto-visco-plastic material behaviour was used to develop a model, which was able to represent the material behaviour in extremely hard conditions found in cutting in terms of plastic strain, the strain rates and temperature gradients. An incremental material behaviour including an elastic prediction with plastic correction scheme was used. The remeshing presented in this paper used a topological description for the geometric discretization (Figure 3-10). The mesh was taking into account the anisotropy which was occurring at high shear strains areas due to the cutting processes.
Figure 3-9 Finite element mesh for machining with a tool with a stagnant region: (a) schematic of typical chip formation with a stagnant region; (b) finite element mesh and detail around tool tip with a stagnant region [27]
A chip breaking mechanism of the grooved rake face tools was simulated by means of the thermo-elastic finite element method [29]. The shape, temperature and flow stress of the deformed chip in the initial model of simulation were obtained from the finite element analysis of the steady state metal cutting mechanism. In the chip breaking simulation, a stress-dependant fracture criterion was used (Figure 3-11). The chip breakability and the force change at the chip breaker edge were obtained (Figure 3-12). The simulation model presented was capable of demonstrating chip breaking but the crack was still created along element boundaries, and no criteria was included for division of elements.

Figure 3-10 Chip formation simulation using ABAQUS [28]

Figure 3-11 Schematic representation of node separation with crack propagation [29]
Later, in [30] modelling of precision hard cutting using implicit FE methods was presented. The limitations of previous model [26], were overcome by introducing a remeshing criteria based on element distortion and represented the amount of plastic strain of the element in front of the tool radius. The element-distortion criterion was based upon examining the angles of elements at the end of every increment. Additionally, mesh refinement was applied at the outer edges of the workpiece causing subdivision of each element into four, and a mesh coarsening, combining four elements into one inside the workpiece. The chip criterion used allowed for the tool nose radius effect to be taken into account. The capability of the model to divide elements in front of tool edge radius allowed the use of coarse mesh in the workpiece and thus reduced the computational time. The mesh was subdivided, and thus made denser, only in the proximity of the tool-workpiece interaction zone.

Heat generation in single-point metal cutting and the direct/indirect technique is employed in [31] to measure cutting temperature. A FE model was presented using the FORGE 2® software package to simulate cutting forces and distribution for orthogonal turning of a hardened hot work die steel, AISI H13 (52 HRS), with polycrystalline cubic boron nitride (PCBN) tooling. The FE model was based on the assumptions that the orthogonal machining process was steady-state, the tool was a rigid body, the workpiece material was elastic-viscoplastic and that a continuous chip was produced. Unlike the previous models, this model was capable of automatically remeshing and rezoning the workpiece when the elements in the tool-workpiece interaction zone were too distorted (Figure 3-13).
The model predicted the highest temperature at the tool-chip interface. Both the experimental and modelling results showed that cutting forces were reduced with the increasing cutting speed. The FE model was able to predict the trends in temperature and cutting force changes due to cutting tool thermal conductivity and edge preparation. Although the modelled results showed similar trends in terms of cutting forces when compared with experimental data, the model underestimated the magnitude of cutting force due to limited data on the sensitivity of workpiece material to strain hardening and the strain rate sensitivity at elevated temperature.

A comparison of the orthogonal cutting data from experiments with three different FE methods was presented in [32]. Cutting was simulated with three commercial FE codes with implementation of damage criterion in one (Deform2D) and relying on automatic remeshing feature of the other two (MSC.MARC and AdvantEdge). Friction at the chip-tool interface was modelled as constant shear in MSC.MARC and Deform2D, whereas a Coulomb friction model was used in AdvantEdge. It was shown that the damage criterion for chip formation that erases the elements at the tool-tip when a critical damage value was met, was inappropriate for the machining process and that the remeshing model gave better results. It was concluded that the three commercial codes estimated the chip thickness and shear angle quite well if an appropriate friction factor or coefficient was used. And that the estimation of contact length between the chip and rake face of the tool was not in agreement with the experiment for all the codes. Although individual parameters matched with experimental results, all models failed to achieve a satisfactory correlation with measured process parameters. A good comparison of the commercial FE software was
presented and their limitations were studied in chip formation criteria and friction modelling.

The formulation of modelling of discontinuous chip formation in hard machining was given in [33]. A more general formulation - Arbitrary Lagrangian-Eulerian (ALE) - was used in simulations to maintain a high-quality mesh and to prevent analysis from terminating as a result of severe mesh distortion. The mechanism of discontinuous chip formation was simulated (Figure 3-14) by creating an initial crack on the surface and extending it in front of the tool and then meeting with the surface crack.

The work successfully demonstrated that discontinuous chip can be simulated using the general commercial FEA code. The machining simulations demonstrated the importance of having a material damage model as a mechanism for generating new free surface. Johnson-Cook plasticity and damage models together with adaptive meshing and control were capable of simulating chip crack initiation and propagation. Although the modelling was not relying on remeshing and re-zoning it was shown that the use of material damage model along with adaptive remeshing and Arbitrary Lagrangian-Eulerian formulation were capable of simulating chip crack initiation and propagation.

The literary work presented so far in this chapter showed different modelling techniques adopted by researchers for chip formation mechanism using either a pre-defined line or re-meshing technique. The following section will present the literary work that included the effect of friction in modelling of the turning process.

![Figure 3-14](image.png) Progression of discontinuous chip and von Mises stress contour [33]
3.5.2 Friction in turning and its modelling

Measurements of chip radius, shear plane angle and the average coefficient of friction between chip and tool were made and their interrelations were successfully explained in terms of the slip-line field theory in [34]. The friction stress (stresses due to friction force) distribution was determined both by the mechanical strength, under the deformation conditions of the cutting experiment, of the chip material adjacent to the rake face of the tool and by the distribution over the rake face of the ratio of the real to apparent area of contact between the chip and tool. Experiments on orthogonal cutting of brass confirmed that the chip shape was determined by work-hardening properties of the workpiece and the distribution over the rake face of the friction stress between the chip and tool. It was shown that the friction stress distribution between chip and tool depended on two factors, the distribution over the rake face of the ratio of the real to apparent area of contact between chip and tool and the distribution over the rake face of the mechanical strength of the chip material adjacent to the rake face. The disadvantage of the method presented was the accurate experimental analysis of input parameters for the calculation of average friction e.g. temperature and the rate at which the chip material experienced. At low cutting speeds the real to apparent area of contact ratio was the most important factor affecting the friction stress distribution, whereas at high cutting speeds, the mechanical strength of the chip material adjacent to rake face most affected the friction stress distribution. Also, the suggested method was only applied to the experiments performed for brass and no information was provided about the application of suggested method of other materials.

In [35], metal cutting studies were carried out to analyse the nature of friction present at the tool-chip and tool-workpiece contact surfaces in the presence of progressing flank wear. It was found that the friction stresses along the flank-land had a more uniform distribution and were affected by the increase in flank contact in the case of a zero-rake tool. The coefficient of friction along the flank rose uniformly with the distance from the tool nose. The tool-workpiece contact friction was always higher than the tool-chip friction and the higher values resulted from the greater contribution of true friction coefficient. Only the effect of flank wear-land was considered for prediction of the nature of friction in the paper and no information was provided for the effect of other factors that can possibly affect the nature of friction.

In [36] the process of orthogonal cutting was studied analytically. A thermomechanical model of a primary shear zone was combined with modelling of a
contact problem at the tool-chip interface. The friction law was introduced that accounted for temperature effects. The effects of cutting conditions and material behaviour on the temperature distribution along the contact zone and on the mean friction and on the global cutting forces were evaluated. The Coulomb friction law at the tool-chip interface was assumed with a friction coefficient as a decreasing function of temperature:

\[ \mu = 1 - 3.4410^{-1}\frac{T_{int} - T}{T_m - T} \text{ for } 25^\circ C \leq T_{int} \leq 955^\circ C , \quad (3-19) \]

\[ \mu = 0.68 \left( 1 - \frac{T_{int} - T}{T_m - T} \right) \text{ for } 955^\circ C \leq T_{int} \leq 1500^\circ C \quad (3-20) \]

where \( T_{int} \) is the mean temperature, \( T = 955^\circ C , T_m = 1500^\circ C \) (melting temperature).

Experimental trends such as decay of the mean friction coefficient \( \mu \) in terms of the cutting velocity \( V \), the feed \( f \) and the growth of \( \mu \) in terms of the rake angle \( \alpha \) were reproduced by the analysis. Due to the extreme temperature variation at the tool-chip interface observed in machining, \( T_{int} \) was considered as the primary factor and the effect of other factors like secondary shear zone and pressure distribution along the primary shear zone were ignored.

A better approach was used in [23] where a methodology to determine simultaneously the flow stress at high deformation rates and temperatures that are encountered in a cutting zone and the friction at the chip-tool interface was developed. A shear friction model was used for interpretation of friction conditions at the chip-tool contact. The shear flow stress was determined using computed average strain, strain-rate and temperatures in secondary deformation, while the friction coefficient was estimated by minimizing the difference between the predicted and measured thrust forces. By matching the measured values of the cutting forces with the predicted results from FEM simulations, an expression for workpiece flow stress and the unknown friction parameters at the chip-tool contact were determined. An iterative scheme was used for determining the unknown friction coefficients until the difference between the predicted and measured thrust forces was less than 10%.

In [37] a thermo-elastic-viscoplastic model using the explicit finite element code ABAQUS was developed to investigate the effect of tool-chip friction on residual stresses in a machined layer. Chip formation, cutting forces and temperature were also
examined in sequential cuts. A Coulomb friction model was used in the simulations at the tool-workpiece interface:

\[ \mu = c \frac{F_t + F_c \tan \alpha}{F_c - F_t \tan \alpha}, \quad (3-21) \]

where \( F_c \) and \( F_t \) are the measured cutting and thrust forces, \( \alpha \) is rake angle and \( c \) is a coefficient. The value of \( c \) depended on the difference between measured and simulated forces. If the difference was less than 5\%, the friction coefficient was considered acceptable because the variation in measuring cutting and thrust forces was as high as 10-15\%. Otherwise, a modified coefficient value was used and the difference was checked again. The process continued until the acceptable calculated forces were obtained. The findings of the work included that residual stress on the machined surface were sensitive to the friction condition of the tool-chip interface. The advantage of the proposed approach was that measured cutting and thrust forces was relatively easy to obtainable.

The effect of friction on thermomechanical quantities in a metal cutting process using the finite element procedure was studied in [38]. A series of finite element simulations was performed, which used the modified Coulomb friction law to model friction along the tool-chip interface. According to the modified Coulomb friction law, the critical friction stress \( \tau_c \) was determined by

\[ \tau_c = \min(\mu p, \tau_{th}), \quad (3-22) \]

where \( p \) is the normal pressure across the tool-chip interface, \( \mu \) is the coefficient of friction, and \( \tau_{th} \) is a threshold value for the conventional Coulomb friction stress (which is given by the product \( \mu p \)). A friction coefficient ranging from 0.0 to 0.6 was considered in the simulations. For a fixed rake angle, as the friction coefficient increased, the chip curvature and the shear angle decreased (Figure 3-15).

The results of the simulations showed consistency with the experimental observations. It was found that shear straining was localized in the primary shear zone while the material near the tool tip experienced the largest plastic strain rate. The maximum temperature rise and maximum effective plastic strain occurred somewhat away from the tool tip along the tool-chip contact interface. The findings of the paper suggested that devising a practical procedure for establishing a friction model for the
tool-chip contact interface and for calibrating the friction parameters in the model by using a combined experimental/computational approach was possible.

Figure 3-15 Chip formation at time = 0.62 sec, for the case of rake angle $\alpha = 25^\circ$ and various values for the friction coefficient $\mu$ [38]

A review of the experimental evidence of the nature of the friction contact between the chip and tool during continuous chip formation and the historical development of friction models was presented in [39]. The work considered three separate circumstances of turning: at low speeds when lubricants can reduce friction by partial penetration of the chip-tool contact; at high speeds when thermal softening can provide self-lubrication; and at intermediate speeds when in some cases solid-lubricating inclusions from the work material can segregate in the chip-tool contact. An improved friction model was proposed that suggested the replacement of a constant friction coefficient $\mu$ by one, which increases with plastic strain rate:
where \( \mu_0 \) is the coefficient of friction in conditions, in which the hinterland was elastic and \( \alpha \) is a coefficient that was expected to be of the order of the undeformed chip thickness divided by the cutting speed. The \( \mu \) increased to a large multiple \( \mu_0 \) within the plastically deforming secondary shear zone in metal cutting. The paper, through numerical simulations of the turning process, demonstrated the shortcomings of the previously used friction models and proposed an improved formulation.

In [40] the influence of friction models on finite element simulations of machining was presented. In this study, the influence of implementing different friction models on prediction of FE simulations was investigated by comparing the predicted variables to experimental results. Measured cutting forces, temperatures, the stress distribution on the tool rake face, the tool-chip contact length and the shear zone in the orthogonal cutting process were obtained from literature. Five different friction conditions were considered; constant shear friction at the entire tool-chip interface (model I), constant shear friction in the sticking region and Coulomb friction in the sliding region (model II), a variable shear friction at the entire tool-chip interface (model III), variable friction coefficient at the entire tool-chip interface (model IV) and variable shear friction in the sticking region and a variable friction coefficient in the sliding region (model V). Evaluation of all models was carried out under the same cutting conditions and tool geometry in order to identify the most suitable friction model in predicting process variables accurately using FE simulations of machining. The predicted process variables (cutting forces \( F_c \), thrust force \( F_t \), chip-tool contact length \( l_c \), shear angle \( \phi \) and maximum temperature at the tool-chip interface \( T_{\text{max}} \)) from the process simulations using five models are presented in Table 3-1.

The measured cutting and thrust forces were believed to be within ±10% accuracy of the experimental conditions. A comparison with experimental results depicted that cutting force, \( F_c \) and tool-chip contact length, \( l_c \) were not in close agreement while other predicted process variables exhibited fair agreement with experimental results. The best agreement between predictions and experiments were obtained for the shear angle and maximum temperature at the tool-chip interface. The friction models (III, IV and V) that were developed from experimentally measured normal and frictional stresses at the tool rake face had been identified as good.
Table 3-1 Comparison of friction model with experimental results at $V_c = 150 \text{ m/min}[40]$

<table>
<thead>
<tr>
<th>Friction Model</th>
<th>$F_c$ (N/mm)</th>
<th>$F_t$ (N/mm)</th>
<th>$l_c$ (mm)</th>
<th>$\phi$ (degree)</th>
<th>$T_{max}$ (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>-</td>
<td>174</td>
<td>83</td>
<td>0.6</td>
<td>18.8</td>
<td>590</td>
</tr>
<tr>
<td>Predicted values from FE simulations</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>I</td>
<td>270</td>
<td>108</td>
<td>0.38</td>
<td>20.9</td>
<td>607</td>
</tr>
<tr>
<td>II</td>
<td>283</td>
<td>126</td>
<td>0.38</td>
<td>21.3</td>
<td>450</td>
</tr>
<tr>
<td>III</td>
<td>265</td>
<td>101</td>
<td>0.34</td>
<td>21.1</td>
<td>600</td>
</tr>
<tr>
<td>IV</td>
<td>272</td>
<td>115</td>
<td>0.47</td>
<td>18.4</td>
<td>620</td>
</tr>
<tr>
<td>V</td>
<td>297</td>
<td>140</td>
<td>0.51</td>
<td>17.8</td>
<td>489</td>
</tr>
</tbody>
</table>

The results showed that the use of various tool-chip interface friction models had a significant influence in predicting chip geometry, forces, stresses on the tool and the temperatures at the tool-chip interface. The results of the proposed study were better correlated with the experimental values when different combinations of friction and material models are considered. Further justifications for the influence of friction models independently of the material model were needed as different combinations of friction and material models may produce better correlation with the experimental values.

The presence of friction between cutting tool and the workpiece and the work of plastic deformation during metal cutting are sources of heat generation resulting in a temperature rise in the tool-workpiece interaction zone. This increase can affect material properties which will influence deformation process and stress evolution in the workpiece. For true estimation of such changes it is important to perform thermomechanical coupled simulations. The following section will present the literary work where coupled thermomechanical FE models of turning were used.

3.5.3 Thermomechanical analysis in metal cutting

The finite element method was applied to calculate the temperatures in orthogonal machining in [41] with account being taken of a finite plastic zone, (a) in which the chip is formed and (b) in which further plastic flow occurs at the tool-chip interface, and also of a shape and thermal properties of the cutting tool. Mathematical models of both primary and secondary deformation were developed. The procedure allowed a complete temperature distribution to be obtained, given only experimental values of
the tool force and chip thickness, and thermal properties of the workpiece and tool. Force and shear angle data for typical machining conditions were obtained from experiments on S1016 steel (0.19% C, 0.61% Mn, 0.016% P, 0.027% S), using a Sandvik S6 grade carbide tool. The data obtained was then implemented to the FE model and temperature distributions were obtained along the plane by numerical integration.

The temperature distribution in the workpiece, tool and chip during orthogonal machining were obtained numerically using the Galerkin approach of the finite element method for various cutting conditions [42]. The effect of a number of process variables such as speed, feed, coolant, rake angle, tool flank wear and tool material on the temperature was investigated. The heat transfer phenomenon occurring during orthogonal machining was governed by the partial differential equation:

\[
K \frac{\partial^2 T}{\partial x^2} + K \frac{\partial^2 T}{\partial y^2} - \rho C_p \left( u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} \right) + Q = 0,
\]

which is subjected to the following conditions:

\[ T = T_b \text{ on part of boundary } S_T, \]

\[ -K \frac{\partial T}{\partial n} = q \text{ on part of boundary } S_q, \text{ and} \]

\[ -K \frac{\partial T}{\partial n} = h(T - T_o) \text{ on part of boundary } S_h, \]

where \( n \) is the outward normal to the boundary, \( K \) is the thermal conductivity, \( h \) is the heat transfer coefficient, \( \rho \) is the density, \( u \) is the velocity in X direction, \( v \) is the velocity in Y direction, \( C_p \) is the specific heat and \( Q \) is the rate of heat generation per unit volume. It was assumed that the distribution of the shear stress on the flank face of the tool was uniform, i.e.

\[
\tau_f = \frac{F_F}{wl_f},
\]

where \( F_F \) is the force component due to the boundary friction parallel to the flank measured experimentally, \( w \) is the width of the cut and \( l_f \) is the length of flank wear land measured along the flank face. The rate of heat generation per unit area over the length of flank wear land due to boundary friction was estimated as

\[
q_f = \tau_f V_f
\]

where \( V_f \) is the velocity along the flank face.
It was concluded that temperatures in the primary shear zone increased progressively from the workpiece-end boundary towards the chip-end boundary and that the average shear plane temperature increased with an increase in the cutting speed and a decrease in the rake angle. A non-uniform temperature distribution was obtained on the rake face of the cutting tool and the maximum temperature always occurred at some distance from the cutting tool. The overall temperature increased with an increase in the cutting speed and/or feed rates. There existed an optimum rake angle for minimum temperature, and the maximum temperature was reduced by between 5% and 10% by using a suitable coolant. Under similar cutting conditions, lower temperatures were generated with a carbide tool than with a high speed steel tool.

The development of a thermo-viscoplastic cutting model using FE methods to analyse mechanics of the steady-state orthogonal cutting process was discussed in [43]. Since a workpiece in machining was made of a thermo-viscoplastic material and hence, the flow stress of the workpiece was a function of strain, strain rate and temperature, the machining process was analyzed with the coupling of the metal flow and heat transfer. It was shown that the steady-state solution of the orthogonal cutting process can be obtained by a direct iteration method. The model developed in this study was predicting the orthogonal cutting process without any cutting experiment if the flow properties and the friction characteristics were known. Orthogonal experiments performed for 0.2% carbon steel and the tool forces were measured. Good correlation between experiments and simulations was found for the principal forces and thrust forces over the entire range tested.

A thermomechanical model of orthogonal cutting was later developed in [22] for 1020 steel. Deformation of the workpiece material was treated as elastic-viscoplastic with isotropic strain hardening, and the numerical solution accounted for coupling between plastic deformation and the temperature field, including treatment of temperature-dependent material properties. A non-linear temperature-displacement solution procedure was used to determine the coupled temperature and stress fields in the workpiece and the chip during material removal. The steady state, two-dimensional form of the energy equation governing the orthogonal machining process was used.
\[ K \left( \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} \right) - \rho C_p \left( u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} \right) + q^{pl} = 0, \]  

(3-27)

where \( q^{pl} \) is the volumetric rate of heat generation.

The temperatures were integrated using a backward difference scheme, and the coupled system was solved using Newton's method. Non-linearities in the numerical model were associated with the material behaviour, the geometry of the elements, and the change in boundary contact conditions. To enhance convergence, minimum and maximum time increments were specified, and within these limits actual increments were automatically adjusted based on the difficulty or ease, with which convergence was achieved. Parametric calculations to determine the effect of uncertainties, in calculating the flow stresses, strains, strain rates and temperatures from the machining experiments, in the constitutive model revealed their significant influence on the stress and temperature distributions. Although the approach considered the effect of frictional heating along the tool-chip interface the phenomenon had little effect on conditions in the primary shear zone.

In [44] a thermo-elastic-plastic 3D finite element model was established to study oblique cutting, the chip deformation process and temperature effects on the strain-energy density, chip flow angle, cutting forces and specific cutting energy that was used to analyse integrity on the machined workpiece surface from the variation of residual stresses along the longitudinal and transverse cross-section and the temperature distribution on it after cutting. The workpiece deformation was assumed to follow the Prandtl-Reuss flow rule and the von Mises yield criterion, possessing isotropically strain hardening characteristics. Then the thermo-elastic-plastic constitutive equation was written as:

for a body in the elastic deformation range
\[ \{d\sigma\} = [D^e] \{d\varepsilon^e\}, \]  

(3-28)

where \( \{d\sigma\} \) is the stress increment, \([D^e]\) the elastic stress-strain relationship matrix and \( \{d\varepsilon^e\} \) the elastic strain increment.

for a body in the plastic deformation range
\[
\{d\sigma\} = [D^{ep}][d\varepsilon] - \{d\varepsilon^T\} + \frac{\left[D^e\right] \left[\frac{\partial f}{\partial \sigma}\right] \left\{\frac{\partial R}{\partial \sigma} d\hat{\varepsilon} + \frac{\partial R}{\partial T} dT\right\}}{H' + \left[\frac{\partial f}{\partial \sigma}\right]^T \left[D^e\right] \left[\frac{\partial f}{\partial \sigma}\right]},
\]

(3-29)

where \([D^{ep}]\) is the elastic-plastic stress-strain matrix and

\[
[D^{ep}] = \left[D^e\right] - \frac{\left[D^e\right] \left[\frac{\partial f}{\partial \sigma}\right] \left\{\frac{\partial f}{\partial \sigma}\right\}^T \left[D^e\right]}{H' + \left[\frac{\partial f}{\partial \sigma}\right]^T \left[D^e\right] \left[\frac{\partial f}{\partial \sigma}\right]},
\]

(3-30)

\([d\varepsilon]\) is the total strain increment, \([d\varepsilon^T]\) is the thermal strain increment, \(H'\) is the strain hardening rate, \(f\) is the plastic potential and \(R\) is the magnitude of yield surface, which is the function of equivalent plastic strain, strain rate and temperature. \(\hat{\varepsilon}\) is the strain rate based on the current area. The simulated specific cutting energy yielded an acceptable error with the specific cutting energy obtained from an orthogonal cutting experiment. The large equivalent stress distribution on the chip surface was found to concentrate at the chip near the tool tip. The further away it was from the tool tip, the lower the stress distribution was. In addition, as the cutting is oblique, the portion of chip surface far away from the tool tip gradually leaves the tool face and the other remained attached to the tool face, the stress at the attached portion was greater. Since there was plastic deformation heat and friction heat in the region of chip-tool contact, the high temperature was observed around the chip-tool interface. However, the further the heat from the tool tip, the lower the temperature distribution. In addition, since the portion of the chip surface far away from the tool tip gradually leaves the tool face, the two portions become unsymmetrical and it induced the uneven temperature distribution. The residual stresses for nodes on the machined workpiece surface along the longitudinal cross section were different. The temperature distribution was not the same along the surface layer of longitudinal cross section. The high temperature located at the place close to the tool tip and the temperature gradually decreased when the place moved away from the tool tip. The validity of the proposed model was verified by checking the consistency with the geometrical and mechanical requirements.

In [45] a FE based computational model was developed to determine temperature distribution in a metal cutting process. The model was based on a multi-dimensional
steady state heat diffusion equation along with heat losses by convection film coefficient at the surface. The models for heat generation within primary and secondary zones and in the rake face due to friction at the tool-chip interface were discussed and incorporated into the FE model. The heat generated in the shear zone and at the rake face was dissipated in the chip and workpiece by conduction and lost to the ambient environment by convection. Those heat transfer phenomena that occurred during metal cutting processes were governed by a heat conduction equation. The following assumptions were made in developing the heat conduction model:

1. The process was steady.
2. The problem was two-dimensional.
3. The heat generation was uniformly distributed over the primary and secondary zone.
4. Convection losses could be approximated by constant convection transfer coefficients.

The mathematical statement of the heat conduction process was given as

\[
K \left( \frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} \right) + Q(x,y) = \rho C_p \left( u \frac{\partial T}{\partial x} + v \frac{\partial T}{\partial y} \right) = 0, \tag{3-31}
\]

which was subjected to the following boundary conditions:

\[
T = T_\infty, \tag{3-32}
\]

\[
-K \frac{\partial T}{\partial n} = h_c (T - T_\infty). \tag{3-33}
\]

The convection film coefficients for the different surfaces had been arrived at by using standard correlation's for free convection and boiling for horizontal, vertical and inclined surfaces. Figure 3-16 shows the heat generation and different convective conditions used on different surfaces of the model.
A temperature increase in the tool, with the maximum temperature occurring at the tool-chip interface, was observed. The model also showed a significant effect of conduction and convection losses in heat dissipation and temperature in the tool. The maximum temperature increased by 9.3% as the convection heat transfer coefficient was increased from 13,000 W/m²K to 25,000 W/m²K. Although the model was accounting for both conduction and convection heat transfer, due to the 2D nature of the model, lateral heat distribution along depth of cut was still based on approximation.

In [46] the temperature distribution in the cutting zone was determined by integrating analytical and simulation thermal models of orthogonal cutting process with uncoated and coated carbide tools. 2D FEM simulations were run to provide numerical solutions for temperatures occurring at different points through the chip/tool contact region and the coating/substrate boundary under defined cutting conditions. The changes of temperature fields resulting from varying heat flux transfer conditions were the main findings of the FEM simulations. The analytically and numerically predicted average temperatures were validated against the tool-workpiece thermocouple-based measurements. The performed finite element simulations yielded
the evidence of existence and localization of the secondary shear zone. It was also reported that for coated tools, areas with the maximum temperatures were localized near the chip and workpiece. The author used a coupled thermo-mechanical model of orthogonal metal cutting to determine the distribution of temperature in the tool and the chip as well as the average tool-chip interface. Also the analytical prediction of the average tool-chip temperature, incorporating equivalent thermal properties for the multilayer coating, was made.

Molinari and Moufki in [47] formulated an analytical approach to model the thermomechanical process of chip formation in a turning operation. The material characteristics such as strain-rate sensitivity, strain hardening and thermal softening, thermomechanical coupling and inertia effects were taken into account in the modelling. The model was capable of giving the local information along the cutting edge such as the temperature distribution. Optimization of the cutting edge geometry was a possible outcome of the analysis.

Although the 2D finite element models of turning are used by many researchers and have played their role in understanding the complex processes in turning, they still have limitations. With the developments in computational power and simulation software, it is now possible to develop 3D numerical schemes of turning. A 3D model allows study of various effects in turning such as non-orthogonal/oblique chip formation, as well as the influence of the tool geometry on process parameters, such as cutting forces and stresses generated in the workpiece material. The 3D FE formulation helps to perform a direct comparison of results of numerical simulations with experimental tests for oblique cutting and require fewer assumptions. Also, the real geometry of the cutting tool can be studied with the 3D model, thus allowing the analysis of the influence of tool sharpness and wear on the cutting process. The following section will present the literary work where 3D FE models of turning were introduced.

3.5.4 3D modelling

A three-dimensional analysis of a cutting process with different cutting velocities was presented in [48]. The model used the updated Lagrangian formulation and an incremental theory approach to develop a 3D elastic-plastic analytical model that examined metal cutting on the tool tip and twin nodes on the machined face (Figure 3-17). The geometric position and the critical value of strain energy density (SED),
combined with twin node treatment were also introduced to serve as the continuous chip separation criterion along a pre-defined line. At the completion of the simulation, a comparison of cutting forces indicated that the average cutting force during the steady state decreased as the velocity increased in the low cutting velocity range. Under low velocity conditions, the cutting forces were greater, which led to a faster accumulation of SED. With a small velocity and large cutting force, the nodes on the machined surface were subjected to relatively greater tensile forces, which resulted in more obvious deformation on the machined surface. The work presented was relaying on a predefined plane along which separation of chip was performed.

**Figure 3-17** Initial division model of finite elements in 3D cutting simulation [48]

The model was further enhanced for simulating oblique turning in [44] (Figure 3-18). Chip separation was based upon the displacement increment criterion. A predefined cutting line was introduced in a workpiece, and nodes along that line were forced to separate when the critical distance criteria, based upon the tool geometric location, was met.
Figure 3-18 Oblique cutting finite element mesh and boundary conditions [44]

Figure 3-19 shows the deformation diagram of chip configuration along with the tool advancement. \( D \) is the total displacement of the tool. It is clear from the diagram that during the oblique cutting process, the flow line formed by the displacement path of chip node did not show the phenomenon of partial node insertion into the tool face. This proved that the equation of three-dimensional tool face geometrical limitation established in that paper could be applied in correcting the chip node and render the simulations of oblique cutting more reasonable.

Figure 3-19 Chip deformation diagram at \( v = 274.8 \text{ mm/s} \) and \( D = 1.30267 \text{ mm} \) [44]
Later in [49] the same authors developed another 3D model for orthogonal cutting with discontinuous chip for 6-4 brass. An additional algorithm for fracture initiation based on strain-energy density was implemented as a creation for crack initiation in the chip. For any one of the elements, the SED function can be expressed as

\[
\frac{dW}{dV} = \int_0^{\varepsilon_y} \sigma_{ij} d\varepsilon_{ij},
\]

where \(\sigma_{ij}\) and \(\varepsilon_{ij}\) are the components of stress and strain, respectively, \(W\) denotes the total stored energy that contained the effects of distortion and dilatation while \(V\) denoted the volume element. The searching algorithm that found the location of initial fracture along the first longitudinal cross-section of the chip was described as following:

- The SED contours for the first longitudinal cross-section were determined for each deformation stage.
- The valleys and peaks of the SED contours \(\left(\frac{dW}{dV}\right)_{\text{min}}\) and \(\left(\frac{dW}{dV}\right)_{\text{max}}\) respectively, were located. \(\left(\frac{dW}{dV}\right) = \int_0^{\varepsilon_y} \sigma_{ij} d\varepsilon_{ij}\) is the SED function, \(W\) denoted the total stored energy while \(V\) denoted the volume element).
- The location of \(\left(\frac{dW}{dV}\right)_{\text{max}}\) and \(\left(\frac{dW}{dV}\right)_{\text{max}}\) were found; they corresponded respectively to fracture and yield initiation.
- All nodes with \(\left(\frac{dW}{dV}\right)_{\text{max}}\) were determined. As the tool advanced by an increment, the SED also accumulated incrementally. When the accumulated SED reached the critical value, \(\left(\frac{dW}{dV}\right)_{c}\), the node was considered to be in a fracture state that was about to be separated into two nodes.

The crack was found to occur close to the location where the workpiece was in contact with the top of the tool. The crack on discontinuous chip then grows along the nodes until it reaches a free surface. The model presented lacked the capability of dividing elements and thus the crack and chip formation were created along a predefined plane.
A 3D FE model for the turning of INCONEL 718 using ABAQUS/Explicit was presented in [50]. Chip formation was achieved through a shear failure criterion where the equivalent plastic strain was taken as the failure measure. This was done by defining a series of short link elements in the vicinity of the area where the chip was expected to detach from the workpiece. Figure 3-21 presents an isometric view of initial tool-workpiece mesh configuration, while the predicted mesh deformation at a cutting speed of 50 m/min is shown in Figure 3-22.
ABAQUS/Explicit allowed automatic deletion of the links when the damage criterion was met.

The presented approach was unable to predict chip morphology due to the lack of a suitable subroutine to properly define the onset and propagation of shear localization and fracture along the shear plane.

A 3D numerical model of metal cutting with damage effects was developed and presented in [51]. An Arbitrary Lagrangian Eulerian (ALE) formulation was used and the Johnson-Cook damage law [52] was employed for the removal of elements from the computation. The use of fracture criterion presented in the paper avoided the problem of a predefined fracture line. This allowed the modelling of complex tool trajectories and kept free chip formation (Figure 3-23).
The Coulomb friction law was assumed to model the tool-chip and tool-workpiece contact zones. The results for cutting forces agreed with those from experimental tests and 2D models.

3.5.5 Cutting tool modelling

In most of the papers dealing with FE modelling of machining the cutting tool is considered to be rigid, and the prime focus had been on the study of distributions of stresses and strains, strain rates and temperature [18,20,21,38,53,54]. In this case no stresses/strains in the tool could be predicted, its wear could not be studied but computational complexity of a problem is considerably reduced.

The study of the thermomechanical behaviour greatly necessitated the use of the tool with heat transfer capabilities. Models of such tools were developed in [3,22,26,43,44,55,56].

An analytical method that enabled crater and flank wear of tungsten carbide tool to be predicted for a wide range of tool shapes and cutting conditions was presented in [57]. An energy method was developed to predict chip formation and cutting forces in turning with a single-point tool from the orthogonal data. Using the predicted results, stress and temperature on the wear faces was calculated. Computer simulations of the wear development were then carried out by using the characteristic equations and the predicted stresses and temperatures upon the wear faces. The predicted wear progress and tool life were in good agreement with experimental results. The experimental results compared were obtained under laboratory conditions, and thus for practical applications of the system proposed, statistical estimation of the predicted results was required in order to enhance its practical validity.

Estimation of the tool wear in orthogonal cutting using FEA was performed in [58]. The wear rates in a cutting tool were calculated using a four phase simulation
cycle, which was repeated for consecutive data points resulting from the previous cycle. Phase 1 consisted of a coupled mechanical and thermal analysis of orthogonal cutting, followed by phase 2 where calculations of heat flux and pure heat transfer analysis of the tool was performed. During phase 3, calculation of nodal wear rates and worn tool geometry were performed. Phase 4 consisted of tool geometry updating and keyword file preparation. The quasi-steady-state field solution for cutting variables was determined in phases 1 and 3. Local tool wear rates and the worn tool geometry were then calculated in phase 3. In phase 4, the rake face and flank face geometries were subsequently updated based upon results of phase 3. The evolution of tool wear on the rake face and tool tip normally follows a concave shape in the cross-sectional profile. Therefore, tool rake adjustments by moving individual surface nodes in the normal direction was used to account for varying wear depth along the rake face. The results obtained underestimated the wear rate, when compared with the experimental results. The disadvantage of the proposed technique was that the tool wear was introduced manually after every stage and the model was unable to update the geometry automatically based upon the cutting conditions.

A model of a cutting tool in FEA just before its fracture was presented in [59]. The work relied totally on experimentally obtained results, which were implemented as boundary conditions on the separately modelled cutting tool. Both static and dynamic analyses were conducted for a DNMG 150608 insert and the tool holder. After the static finite element analysis, the modal and harmonic response analyses were carried on and the dynamic behaviours of the cutting tool structure were investigated. Von Mises equivalent stress criteria was used for fracture analysis of the cutting tool. Several aspects of the metal cutting process predicted by the finite element model agreed well with experimental results. The harmonic response analysis was done by including the cutting forces that cause the tool to fracture. Experimental data from cutting force output were used to validate the model. Although, both static and harmonic forcing analysis were performed, transient analysis of the cutting process considering inertial terms, damping and transient cutting forces were not simulated.
3.5.6 Ultrasonically assisted turning simulations

The models presented above are all related to simulation of conventional turning. But ultrasonically assisted turning modelling requires the introduction of some extra boundary conditions to reflect the ultrasonic vibration of the cutting tool. This section present the details of FE modelling approaches adopted to simulate UAT.

The first 2D model for ultrasonically-assisted turning was developed and presented in [60]. The FE model developed, which utilized MARC/MENTAT general FE code, provided a transient analysis for an elasto-plastic material and accounted for the frictionless contact interaction between a cutter and workpiece as well as material separation in front of the cutting edge. In the model the workpiece moved with a constant velocity, which corresponded to the cutting speed $V$ and equalled 300 mm/s. Kinematic boundary conditions providing this type of movement were applied to the left, right and bottom side of the work piece (Figure 3-24), while its top surface was free:

$$V_x|_{AH} = V, V_x|_{FG} = V, V_x|_{HG} = V,$$
$$V_y|_{AH} = 0, V_y|_{FG} = 0, V_y|_{HG} = 0.$$  \hfill (3-35)

$$V_x|_{AH} = V, V_x|_{FG} = V, V_x|_{HG} = V,$$
$$V_y|_{AH} = 0, V_y|_{FG} = 0, V_y|_{HG} = 0.$$  \hfill (3-36)

The cutting tool was rigid and immovable (for simulations of the conventional turning process) or vibrated harmonically in the direction of cutting velocity in simulations of ultrasonic turning:

$$u_x = -a \cos \omega t, u_y = 0,$$
$$u_x = -a \cos \omega t, u_y = 0,$$  \hfill (3-37)

where $\omega = 2\pi f$, the frequency $f = 20$ kHz and amplitude $a = 13 \mu m$.

The velocity of the cutting tip was described by the relation $v_x = v_t \sin \omega t$, where $v_t = 2\pi f \approx 1600$ mm/sec. Thus $v_t > V$, providing a condition for separation of the cutter from the chip within each cycle of ultrasonic vibration. A detailed analysis of cutting for a single cycle of ultrasonic vibration was carried out for isothermal conditions. Figure 3-25 shows characteristic stages of a single cycle of vibration, in which modelling takes 20 time increments ($2.5 \times 10^{-6}$ s each).
Figure 3-24 A scheme of the relative movement of workpiece and cutting tool in orthogonal ultrasonically assisted turning [60]

Figure 3-25 Distribution of equivalent (von Mises) stresses for ultrasonic turning simulations at different moments of a single cycle of vibration: (a) cutter approaching the chip; (b) cutter in contact with chip and (c) cutter moving away from the chip [60]
Differences between conventional and ultrasonic turning in stress distributions in the process zone and contact conditions at the tool/chip interface were investigated. In conventional turning the cutting tools stayed in permanent contact with the chip throughout the entire cutting process. In contrast, in ultrasonic turning the cutter remained in contact with the chip only about 40% of the time (according to FE simulations). Different contact conditions between the cutter and chip in ultrasonic and conventional cutting influenced the evolution of thermal processes and the friction type, causing difference in chip formation observed experimentally in high-speed filming experiments.

Later in [3,6], the first 2D thermomechanically coupled FE model of UAT was presented which accounted for temperature-dependant material properties, strain-rate sensitivity, heat generation due to plastic deformation and friction at the cutter/workpiece interface. For comparison purpose a model with the same properties was developed for CT as well and the effect of friction on the radius of curvature and thickness of the chip were compared for both cases. Thermal boundary conditions included convective heat transfer from the workpiece, chip and tool free surfaces to the environment.

\[ -K \frac{\partial T}{\partial n} = h(T - T_\infty), \]  

where \( K \) is the conductivity. The thermal flux passing from the chip to the cutter at the contact length \( L_c \) was described as follows:

\[ q = H(T_{\text{chip}} - T_{\text{tool}}), \]

where \( H \) is the contact heat transfer coefficient, \( T_{\text{chip}} \) and \( T_{\text{tool}} \) are chip and tool surface temperatures, respectively. Initial thermal conditions used in simulations were as follows:

CT: \( T_{\text{work}}|_{t=0} = T_\infty; T_{\text{tool}}|_{t=0} = T_\infty; \)  

UAT: \( T_{\text{work}}|_{t=0} = T_\infty; T_{\text{tool}}|_{t=0} = T_{\text{ultr}}; \)

where \( T_{\text{work}} \) and \( T_{\text{tool}} \) are workpiece and cutting tool temperatures and \( T_{\text{ultr}} \) is initial temperature of the tool in UAT, as found in infrared thermography experiments. It was shown that an increase in contact heat conduction between the chip and cutting tool lead to a significant growth in cutting tip temperature and decrease in temperatures in the chip and workpiece. The temperature in the cutting region were
some 15% higher than those in CT, which was in a good agreement with the infrared thermography data. Material separation in front of the cutting edge and automatic remeshing of distorted elements were implemented in the computational scheme. Figure 3-26 shows the temperature distribution in the tool-workpiece interaction zone in UAT simulation.

\[ h = 0.05 \text{ W/m}^2\text{K}, \quad H = 500,000 \text{ W/m}^2\text{K}, \quad \mu = 0.5, \quad t = 2.9\text{ ms} \] [3]

Frictionless analysis resulted in a very small rise in the tool temperature, justifying the significant effect of friction on resultant temperatures in the tool. Lower values of average stresses in simulations of UAT naturally resulted in smaller forces acting on the cutting tool. Ultimately, FE simulations showed that superimposition of ultrasonic vibration led to the fourfold reduction in the cutting forces for various friction conditions as compared to those in CT. FE simulations indicated smaller forces acting on the shear plane in UAT than those in CT, thus reflecting the reduced possibility of large slips along the shear plane for UAT. This correlated well with experimental results showing reduced level of chip segmentation in UAT compared to that in CT.

Chips formed during the simulations of cutting were compared for cases with and without friction. A larger (by 20%) radius of curvature of the chip was found in CT as compared to UAT in simulations of both cases. It was noted that the chip thickness ratio attained higher value and that the radius of curvature of the chip was approx.
60% smaller in the frictionless case than that in the analysis with friction for both CT and UAT [6].

Finally, Table 3-2 summarizes the major stages of development in FE models for turning.

<table>
<thead>
<tr>
<th>Table 3-2 Major stages in developing FE models for turning</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Stage</strong></td>
</tr>
<tr>
<td>General application of FEM to turning</td>
</tr>
<tr>
<td>CT: 2D model with predefined line of separation</td>
</tr>
<tr>
<td>CT: 2D model with remeshing and rezoning</td>
</tr>
<tr>
<td>CT: 3D model with predefined line</td>
</tr>
<tr>
<td>CT: 3D model with remeshing and rezoning</td>
</tr>
<tr>
<td>UAT: 2D</td>
</tr>
<tr>
<td>UAT: 3D</td>
</tr>
</tbody>
</table>

Note:Italicized reference numbers italicized refers to the papers by the research group at Loughborough University, UK.
This chapter describes the basics of ultrasonically assisted turning technology and its development in recent years.

4.1 Overview of ultrasonic technology

Research in recent years has demonstrated great advantages of working difficult-to-work materials with the use of ultrasonic oscillations to reduce the force and power consumption [1], to increase productivity of equipment, to work high-strength materials, to improve product quality, etc.

Ultrasonic vibrations are elastic waves at high frequency, which could propagate in a gaseous, liquid or solid medium. These waves are directional, and energy can be focused on to a comparatively small area of a working tool [104]. Ultrasonic vibration energy can be applied to machining of materials in two different ways explained below.

The indirect method is generally understood to mean the treatment of workpiece by imbonded abrasive grains, which receive their energy from a source of ultrasonic frequency vibration [105]. A simple application of this method is the precision ultrasonic machining with abrasive. A schematic diagram of the process is shown in Figure 4-1.

![Figure 4-1 Schematic diagram of ultrasonic erosion process [105]](image)
The tool vibrates at an ultrasonic frequency 16 kHz – 30 kHz with a small amplitude of 0.01 mm – 0.06 mm [106]. A slurry of abrasive is delivered to the working zone, i.e. to the zone between the vibrating tool and the workpiece. The cutting is performed by abrasion, which is driven by the vibrating tool. The abrasive is usually boron carbide and the liquid (water).

The direct method, on the other hand, involves the removal of chips by a vibrating the cutting tool without the use of abrasive particles. This technique has so far been applied to nearly all principal machining operations such as turning, drilling, milling and grinding [1]. The experimental setup of ultrasonically assisted turning resembles the general turning operation except for the fact that the cutting tool in not static but is vibrated ultrasonically due the presence of an attached ultrasonic system. Application of ultrasonic vibration to turning results in better surface finish, great reduction in cutting forces and reduction in noise. The following sections will present the details of ultrasonic assisted turning and the apparatus required for it.

4.2 Ultrasonically assisted turning

Ultrasonically assisted turning is a metal removal technique, in which the cutting edge of a tool vibrates at a regular frequency within an ultrasonic range. There are three independent principle directions, in which ultrasonic vibration can be imposed in a turning operation. These include the feed direction, cutting direction and the radial direction (Figure 4-2).

Ultrasonically assisted turning has characteristics of intermittent cutting, which involve repeated cutting and pausing. The critical cutting speed in ultrasonically assisted turning is

$$V_t = a \omega = 2nf,$$  \hspace{1cm} (4-1)

where \(f\) is the frequency, \(\omega\) is the angular velocity and \(a\) is the amplitude of vibration.

In the case of VAT, to perform turning, the following condition should be met

$$V < V_t,$$ \hspace{1cm} (4-2)

where \(V\) is the cutting speed and is equal to \(nD\), with \(D\) being the diameter of the workpiece, and \(n\) being the rotations per minute.
The above equation shows that for \( f = 20 \text{ kHz} \) and \( a = 20 \mu\text{m} \), the cutting tip velocity does not exceed 100 m/min.

It was shown in [107] that application of ultrasonic vibration along the feed direction enables the cutting parameters used in manufacturing industry for most materials to be reached independently of the workpiece diameter. For example, \( n \) is limited not to exceed approximately 20,000 rev/min when \( s = 1 \text{ mm/rev} \) and condition \( 0.2V_t = sn \) is required. Therefore tool vibration in the feed direction seems to be more suitable for industrial ultrasonical turning requiring high levels of productivity.

4.3 Ultrasonically turning apparatus

The principal components of an ultrasonic turning system are shown in Figure 4-3 and comprise of high frequency generator; transducer; concentrator; tool holder; and tool.

Many varieties of high frequency generators are available for excitation of, and power supply to, ultrasonic transducers. The principal requirements that apply to generators intended for ultrasonic machining are that the frequency should be stable and smoothly controlled over a certain interval and output power should be
controllable over a certain range. The generator's output voltage curve should not contain higher harmonics because if these are present in addition to the fundamental ones they cause additional electric power loss in both the generator and the vibrator. Additionally, the generator should be small, cheap, reliable and easy to operate.

A transducer used in UAT converts electrical energy into mechanical motion. There are two main types of transducers in this category: piezoelectric and magnetostrictive.

A piezoelectric transducer is used for production of high-frequency ultrasonic oscillations and is based on the piezoelectric effect, which was discovered by the Curie brothers in 1880. They discovered that in some crystals compression or tension produces free electrical charges on their faces. This phenomenon was called the direct piezoelectric effect. Shortly after this it was discovered that the piezoelectric effect is reversible, i.e., stresses and deformations occur in crystals under the influence of an electric field. These properties are strongest in crystals of quartz, Rochelle salt, and tourmaline and barium titnate. A typical piezoelectric transducer (Figure 4-4) consists of a stack of ceramic disks, sandwiched between a high density base and a lower density block of material, which provides the radiating face of the transducer [108]. The action of the transducer is based on the fact that, from momentum conservation, the velocity and amplitude of vibration are high in a lower density material. Such a transducer is capable of converting electrical energy into vibrational energy at 96 percent efficiency and, consequently, does not require cooling.
In 1847, Joule discovered that if a ferromagnetic material, a rod or tube, is placed in a magnetic field it changes dimensions, becoming larger or smaller. This phenomenon is called magnetostriction, or the Joule effect. There was also found a reverse effect, in which a change in the dimensions of a ferromagnetic body under the action of external forces causes a change in the magnetic properties of the body, and an electromotive force is excited in a coil wound on the ferromagnetic rod (Figure 4-5) [108]. The elongation or shortening caused by magnetostriction is small. In view of comparatively low acoustic power, the use of magnetostrictive transducer for industrial purposes, where high-power ultrasonic oscillations are necessary, is quite limited.

**Figure 4-3** A schematic diagram of ultrasonic turning system (after [108])
Since at the resonant frequency the oscillation amplitude of a transducer does not exceed 10 μm, it is practically impossible to obtain high intensities with ordinary plane transducers. To amplify the oscillation of a transducer, i.e., to concentrate ultrasonic energy in a small volume, rods of varying cross section, so called concentrators or velocity transformers, are used. The oscillatory energy is uniformly distributed over the length of the rod and therefore at the narrow end of the rod the oscillatory velocity and displacement amplitude are greater than at its wide end.
Vibration amplitude may be increased by as much as 600% with a suitably shaped concentrator. A similar increase in the amplitude at the tool face can occur in the tool holder, and final amplitudes at the tool face are usually in the range of 13 μm to 100 μm [108].

4.4 Ultrasonic cutting techniques

Many attempts have been made to employ ultrasonic power for cutting hard and brittle materials. This section reviews the development and application of this technology.

Babikov in his book [109] summarized some of the research results up to time of its publication about the application of ultrasonic technology to machining of hard and brittle materials. It was shown that ultrasonic processing not only resulted in a finished surface of high quality but it provided ease in cutting hard materials.

Later Frederick [110] presented the results on friction reduction in metal cutting. It was reported that the beneficial effects, which can be obtained by ultrasonic vibration during machining, included: lower residual stresses in hard materials, better surface finish due to reduction in tearing in ductile materials and better surface finish in those cases, where ordinarily a built-up edge would be formed on the cutting tool.

Markov [106] presented different uses of ultrasonic turning, its implementation, advantages and disadvantages. In the section on application of ultrasonic vibration in turning intractable materials, it was mentioned that high intensity vibrations shortened the tool life, but on the other hand, low intensity vibrations lengthened it. Also, ultrasonic vibration affects appearance of the machined surface. In the absence of vibrations, the surface of heat resistant alloy workpiece was lustrous; with vibration, especially at high intensity, it became matt.

In [111] a magnetostrictive transducer was used to vibrate a carbide-tipped cutting tool while turning (Figure 4-6).
It was shown that the tool can be vibrated in either the horizontal direction (parallel to the workpiece axis) or in the vertical direction (tangential to the workpiece surface). It was found that cutting forces were reduced with the use of a transducer particularly at low cutting speeds. It was suggested that such reduction may be due to a reduction in mechanical strength of the workpiece material in the presence of ultrasonic vibration. The importance of the presence of vibration were shown by studying the surface finish, and it was suggested that built-up edge effects that were observed at low cutting speeds were no longer evident with vibration present.

Further advantages of UAT were presented in [112]. Experiments were conducted on different materials, e.g., carbon steel C1010 and C1045 and aluminium (2024-T6). The results obtained showed an increase in material removal rates, reduction of the tool load, improvement of surface finish, reduction of subsurface deformation, elimination of the tool built-up edge and extension of the tool life.

Kumabe summarised in [1] some of the results of the research and also presented the outcomes of his own experiments on the application of ultrasonic vibrations for various conventional material removal processes. All his experiments were carried out with magnetostrictive vibrators, specially designed for the generation of ultrasonic vibration. Kumabe also suggested that vibration of the cutting tool in the direction of cutting velocity (tangential to the workpiece) might give better results than the application of vibration in the radial and feed directions (parallel to the workpiece axis). The observed advantages of ultrasonic machining included: a rise in machining
accuracy due to the increase in the system’s dynamic rigidity, a reduction in cutting
temperature, abolition of the built-up edge, elimination of burrs, a smooth formation
of chips, an improvement in lubrication and cooling properties of lubricants and an
increase in the tool life and in wear and corrosion resistance of machined surfaces.

Kumabe in [113] performed a study into the theory and techniques of turning
precise circular components. A form of chucking type precision cylindrical machining
was developed by combining an insensitive vibration cutting mechanism using a main
spindle system, which featured an air bearing with superposition superfinishing. The
workpiece was chucked on the main spindle and machined using a continuously and
systematically pulsating cutting force. The work was then finished by a newly
developed superposition superfinishing device which featured equivalent grades of
ultrasonic vibration stone. The key points of the techniques were a torsional vibration
mode tool for producing accurate, high amplitude vibration of the cutting point and a
contrivance for making accurate movements of the superposition superfinishing
device (Figure 4-7). Machine roundness 0.1 μm-0.2 μm and surface roughness \( R_{\text{max}} \)
0.03 μm-0.09 μm were obtained with plain carbon steels, stainless steel and hardened
steels.

Ultrasonic assisted cutting was applied to the machining of glass-fibre reinforced
plastic [114]. Chip formation, cutting forces and surface quality were evaluated. It
was concluded that by applying ultrasonic machining, average cutting forces and
roughness of machined surface are dramatically reduced. A reduction of approx. 80%
in the depth of damage layer was recorded for fibres oriented at 90°, when compared
to conventional cutting.

Ultraprecision ductile cutting of glass was studied in [115] by applying ultrasonic
vibration to a single-crystal diamond tool in the cutting direction. Grooving
experiments were carried out in order to clarify the effect of tool vibration on ductile
machining. The depth of cut was continuously increased as the spindle was rotated
and the workpiece was cut. It was sensed that the formation of groove changed from
the ductile mode to the brittle mode as the depth of cut exceed certain critical value.
The conclusion was that the critical depth of cut grooving was increased to about
seven times of that of conventional cutting, as the maximum speed of tool vibration
was increased to about 10 times of the cutting speed. The profile of the tool edge was
much better transferred to the cut groove by ultrasonic vibration cutting as compared
to a conventional technique. The surface finish of soda-lime glass was also improved. The work presented extended the practical application of ultra-precision cutting, which had been limited to cutting of light metals, plastics etc. and lead to improvement of the quality and reduction of the cost of various precision parts made of brittle materials.

Figure 4-7 Vibration cutting device: (1) torsional vibration tool; (2) torsional vibration mode; (3) torsional transducer (150 W, 20 kHz); (4) torsional vibration mode horn; (5) ultrasonic generator; (6) fan [113]

Optimum conditions for cutting of hard-to-cut materials were presented in [116]. The optimal vibrational amplitude was found for all frictional pairs that were studied. Tangential vibrations were found to be more efficient than radial vibrations. Analysis of the dependence of $\mu$ on the normal load $P$ in the case of static friction and friction with ultrasound showed that high-frequency vibration changed not only its magnitude but also its dependence on $P$.

Improvements in vibrational performance were shown in [117] by obtaining a full vibration picture for the multi-component tuned cutting system and its components.
This was achieved by studying excitation conditions and design parameters, which lead to enhanced control of vibration transmission to the cutting edge at the tuned frequency. Complementary experimental techniques such as Laser Doppler Vibrometry (LDV) and Electronic Speckle Pattern Interferometry (ESPI) were used to obtain model parameters of the system. It was concluded that performance improvements can be achieved both by improving the quality and tuning control of the driving signal and by identifying modal characteristics of the system that can be modified by component re-design.

The characteristics of a surface, machined by ultrasonic vibration cutting (20 kHz) in the ductile mode for cutting of optical plastic, were analyzed in [118] by carrying out wavelength spectrum analysis and comparing results with those of conventional cutting. From the cutting experiments at extremely low cutting velocities with a single crystal diamond tool and from SEM study of the chip produced it was concluded crests on the cut surface were eliminated and surface roughness of the machined surface was improved. The research showed that even ultrasonic vibration cutting did not perfectly removed brittle fracture on the machined surface, but made the magnitude of fracture much smaller.

An active error compensation scheme incorporated into the ultrasonic cutting technique was proposed in [119] for precise machining, and a piezoelectric actuator integrated into a cutting tool was developed. The ultrasonic vibration actuator was developed for machining at a fixed frequency of 20 kHz to generate the vibration with amplitude of 10 µm. Cutting experiments showed that the integrated cutting tool was effective in terms of improved roundness and roughness of the machined surface. Further improvement, to the proposed integrated cutting tool incorporating the active error compensation, were possible by developing a closed-loop control system for tackling the nonlinear dynamics present in the tool system.

The mechanism of chip generation and characteristics of the surface in ductile mode, machined by ultrasonic vibration cutting, were investigated in [120]. It was confirmed that chips generated by the ductile-mode cutting were obtained at 1/40 of the critical cutting velocity of the ultrasonic-vibration cutting system. Also the chip formed at this velocity was completely continuous.

The theory of cutting with superimposition of ultrasonic vibration in terms of nonlinear dynamics was presented in [121]. The accumulated experimental results
were explained theoretically within the framework of rheological models. It was confirmed that under the influence of high-frequency vibration, the transformation of elasto-plasticity into visco-plasticity and fluidization of dry friction occurred. The dynamic characteristics of transformed machining process were obtained. The nonlinear amplitude response of the vibrating tool in the process of cutting was obtained. Also a method to stabilize resonant ultrasonic excitation was described.

A development of a practical ultrasonic vibration cutting tool system was presented in [122]. An attempt was made to prevent chipping of the edge of a cutting tool that occurred when difficult-to-cut materials were cut by means of an ultrasonic vibration cutting method. It was concluded that cutting with stable finishing can be carried out without tool chipping if hardened steel is cut using an inclined vibrating direction at a level of about 30°. Also, precision finishing cutting can be carried out for a workpiece with high hardness and interrupted cutting shape, in which finishing cutting is difficult by conventional cutting, with an increase in the tool life of about 36 times that of conventional cutting.

Diamond cutting of glass with the aid of ultrasonic vibration was studied in [123] to analyse the brittle-ductile transition mechanism. The ultrasonically vibrating system was attached to the cross slide of the two-axis CNC ultra precision diamond turning machine and the tool tip was vibrated at a frequency 40 kHz. The authors manufactured an ultrasonic vibration cutting tool with high rigidity so that the vibrating direction of the cutting edge could not change (Figure 4-8).

Figure 4-8 Diamond turning machine equipped with an ultrasonic vibration tool [123]
A critical cut depth in the ultrasonic-vibration cutting process was found to be related to the ratio of the cutting speed of a workpiece to the maximum vibration cutting speed of a tool. In conventional turning, the chip always stayed on the rake face of the tool which was an area of high temperature and high pressure. It was very difficult for coolant to reach the cutting zone, and coolant only functioned around the tool tip indirectly. In ultrasonic vibration cutting, with the frequent separation between the workpiece and tool, the tool-chip contact zone was opened periodically. The ultrasonic excitation of the tool promoted aerodynamic lubrication that decreased the friction between tool and workpiece. Also, in ultrasonic cutting, instead of static friction between the tool and workpiece, dynamic friction was generated and thus the friction between the tool and work-material was reduced. Hence it was suggested that in ultrasonic vibration cutting, the forces were reduced due to the effect of dynamic friction and aerodynamic lubrication. The reduction in cutting forces was believed to be one of the main reasons for the increase in the critical cut depth.

The influence of cutting conditions on the surface microstructure of ultra-thin wall parts in ultrasonic-vibration cutting was studied in [124]. Results were drawn by comparing ultrasonic cutting with common cutting using a cemented carbide tool and a polycrystalline diamond (PCD) tool. The test results showed that surface characterization was influenced clearly by rigidity of the acoustic system and the machine tool. Surface roughness in ultrasonic cutting was better than that in conventional cutting. The surface profiles of the two surfaces machine using common cutting and ultrasonic cutting are presented in Figure 4-9.

![Figure 4-9 Profiles of the vibration cutting aluminium alloy [124]](image-url)
Kerosene was suggested as a better coolant to improve surface finish in UAT. In UAT of an aluminium alloy, the PCD tool performed better than the cemented carbide tool.

Ductile machining of brittle materials, even at the high critical depth-of-cut, had been realised by applying ultrasonic vibration to a diamond tool tip [125]. A surface roughness $R_a$ of 100 nm at a 2 μm depth-of-cut was achieved. Experiments were conducted with the workpiece of fused silica of diameter 15 mm, and a crystal diamond tool with rake angle 0°, clearance angle 11°, tool nose radius of 0.8mm and a feed rate of 5 μmrev⁻¹. Vibration with a frequency of 40 kHz and an amplitude of 3 μm was applied in the cutting direction. From the results of those experiments it was concluded that average cutting forces in ultrasonic vibration cutting were smaller than those in conventional cutting. Decreasing the cutting speed of the workpiece and/or increasing the vibration frequency resulted in better surface quality. The same results were obtained in [126], when vibration-assisted precision machining of steel was performed with a PCD tool. Short contact times and the improvement in cooling and lubrication in ultrasonic vibration were considered responsible for a reduced level of average forces and temperature.

The effect of the tool nose radius in ultrasonic-vibration cutting of hard materials was studied in [127]. Stainless steel SUS304 and nickel-based alloy INCONEL 600 were used in turning experiments. Five different tool nose radii, $r_n = 0.02, 0.1, 0.2, 0.5, 1$ mm were investigated. The experimental results showed that vibration cutting enables a larger nose radius to be used than in conventional cutting. The vibration cutting experiment using the suitable nose radius of 0.2 mm showed that tool fracture was prevented and the machining accuracy was improved in comparison with an initial nose radius of 0.02 mm (Figure 4-10).
Turning of some modern aviation materials was conducted with ultrasonic vibration applied in the feed direction using an autoresonant control system [107]. Two modern high grade nickel-based alloys, C263 and INCONEL-718, were used in the experiments. Results from experiments showed that the surface roughness was improved by 25-40% for ultrasound vibration compared to conventional cutting. Roundness improvements achieved were up to 40%. The application of feed-direction vibrations for ultrasonic cutting seemed less limiting than vibrations in the cutting direction. The scatter in values of relative surface roughness ($R_a$) obtained by ultrasonic cutting was much less compared to conventional cutting.
The development of an autoresonant system (Figure 4-11) with supervisory computer control, which was successfully used for the control of the piezoelectric transducer during ultrasonically assisted turning, was described in [2]. This permitted an intensive supply of ultrasonic energy into the machining zone under conditions of variable cutting loads. The effect was achieved through the generation and maintenance of the nonlinear resonant mode of vibration and by active matching of the oscillating system with dynamic loads imposed by the cutting process.

![Figure 4-11 Schematic diagram of autoresonant ultrasonic cutting system [2](image)](image)

Experiments with various configurations of the autoresonant control system showed that optimal performance, in terms of energy consumption and reliability of self-excitation, was achieved for the system. The system was developed as a combined analogue-digital system, where the control signal was processed by analogue devices, and the parameters of the devices were controlled digitally by a computer. The software developed provided features for control and monitoring of the autoresonant system operation, which covered the essential needs of research and applications. The developed autoresonant control system was successfully applied for ultrasonic turning under the regimes of different direction of application of ultrasonic vibration, different vibration amplitudes, different designs of tool holder and cutting tips, different modes of cutting (feed rate, cutting speed, depth of cut) and different
modern materials. Application of the system showed significant improvements in surface quality compared to conventional methods.

The effect of tool geometry on regenerative instability in ultrasonic vibration cutting was presented in [128]. An attempt was made to determine cutting parameters based on regenerative chatter prediction in order to facilitate the machining objectives of high accuracy, high efficiency and low cost in ultrasonic vibration cutting. The simulation and experimental results show that parameters of the workpiece material had a direct influence on the occurrence of regenerative chatter in ultrasonic vibration cutting. It is also found from the predictive results that regenerative chatter under a general range ultrasonic cutting can be suppressed by a change of tool geometry.

Literature survey has shown that various practical attempts have been made to study different aspects of VAT including force measurement, high speed filming and thermal analysis. The workpiece surface that has been machined using VAT was studied using techniques like SEM and light microscopy and some attempts have also been made to study the hardness of the cut surface using nanoindentation. However, little information exists about the experimental analysis of the effect of cutting speed on cutting forces. The following chapter will present the details of experiments performed on the in-house VAT setup. The new studies performed include the high speed filming and cutting force analysis studies.
5 EXPERIMENTAL WORK

5.1 Introduction

Cutting is a fast process, and it is difficult to understand the cutting mechanism without experimental studies. Various experimental methods adopted by researchers to study different aspects of cutting are discussed in Chapter 2; this chapter presents the experimental techniques used in the current research to study cutting-related effects that can help to develop and validate the simulation models for both CT and UAT. Two main features of the cutting process – its kinematics and respective cutting forces – should be studied in order to have a basis for comparison with results from numerical simulations.

A prototype of the UAT system which is capable of performing both CT and UAT, was designed at Loughborough University, UK and a program of experimental tests has been implemented confirming advantages of UAT in comparison to CT. Experiments that were conducted in previous years cover most of the areas, explored in this thesis using the simulations, including high-speed filming, microstructure analysis, thermal analysis, surface profile analysis, cutting force measurement and the effect of directions of vibration [84,100,107].

This chapter does not review results obtained at Loughborough University before this study; it presents only the results and discussion of some recent experiments conducted on the UAT setup to further explore the technology. These include the high speed filming and experimental measurements of cutting forces.

Table 5-1 presents the comparison of the UAT facility at Loughborough University with UAT elsewhere.

The standard experimental setup for UAT at Loughborough University is shown in Figure 5-1, while Figure 5-2 presents a schematic diagram of the whole setup.
Table 5-1 Parameters of UAT systems

<table>
<thead>
<tr>
<th></th>
<th>Various UAT systems</th>
<th>UAT system at Loughborough University</th>
</tr>
</thead>
<tbody>
<tr>
<td>Autoresonant control</td>
<td>No</td>
<td>Yes</td>
</tr>
<tr>
<td>Frequency</td>
<td>10 - 40 kHz</td>
<td>10 - 35 kHz</td>
</tr>
<tr>
<td>Amplitude</td>
<td>3 - 50 μm</td>
<td>10 - 20 μm</td>
</tr>
<tr>
<td>Feed rate</td>
<td>0.015 - 0.4 mm/rev</td>
<td>0.03 - 0.2 mm/rev</td>
</tr>
<tr>
<td>Cutting speed</td>
<td>1 - 120 m/min</td>
<td>5 - 30 m/min</td>
</tr>
</tbody>
</table>

Figure 5-1 General experimental setup

Figure 5-2 Schematic diagram of the experimental setup
A Harrison 300 lathe was used for the turning tests, with special arrangements made for specific experimental needs such as mounting of a special tool holder replacing the standard tool post, for holding the ultrasonic transducer and mounting a force dynamometer. The material being used in all these experiments was INCONEL 718, and in some cases different grades of mild steel were also used for preliminary tests.

5.2 Properties of INCONEL 718

INCONEL 718 is a precipitation hardenable nickel-chromium alloy containing significant amounts of iron, niobium, and molybdenum along with lesser amounts of aluminium and titanium. It combines corrosion resistance and high strength with outstanding weldability, including resistance to postweld cracking [129].

Table 5-2 shows the chemical composition of INCONEL 718 (provided by supplier, Nigel Smith Alloys Ltd., East Leake, UK) used in the experiments, while Table 5-3 presents its properties. INCONEL 718 used in the experiments had been solution annealed at 1026°C for 1.5 hrs followed by rapid cooling in water, and additionally precipitation hardened at 780°C for 8 hours followed by air cooling.

<table>
<thead>
<tr>
<th>Parameter (units)</th>
<th>Magnitude</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tensile strength (MPa)</td>
<td>1223</td>
</tr>
<tr>
<td>Elongation at break (%)</td>
<td>25</td>
</tr>
<tr>
<td>Melting Point (°C)</td>
<td>Min 1260, Max 1336</td>
</tr>
<tr>
<td>Hardness (HRC)</td>
<td>37-40</td>
</tr>
</tbody>
</table>

Alloy 718 is hardened by precipitation of secondary phases into the metal matrix. Precipitation of these nickel-(aluminium, titanium, niobium) phases is induced by heat.
treatment in the temperature range between 593°C to 815°C. For this metallurgical reaction to properly take place, the aging constituents (aluminium, titanium, niobium) must be in solution (dissolved in the matrix); if they are precipitated as some other phase or are combined in some other form, they will not precipitate correctly and the full strength of the alloy will not be realized [129]. To perform this function, the material must first be solution heat treated (solution annealed is a synonymous term).

Two heat treatments are generally utilized for INCONEL 718:

a) Solution annealing at 927-1010°C followed by rapid cooling, usually in water, plus precipitation hardening at 718°C for 8 hours, furnace cooling to 621°C, holding at 621°C for a total aging time of 18 hours, followed by air cooling.

b) Solution annealing at 1038-1066°C followed by rapid cooling, usually in water, plus precipitation hardening at 760°C for 10 hours, furnace cooling to 650°C, holding at 650°C for a total aging time of 20 hours, followed by air cooling [129].

A typical stress-strain plot of INCONEL 718 is presented in Figure 5-3 [130].

![Figure 5-3 Typical tensile stress-strain curve for INCONEL 718 at room temperature](image)

The type of heat treatment affects the deformation behaviour of INCONEL 718 and changes dramatically its stress-strain relation. In [131] large differences in static properties were found between the annealed and aged materials. Annealed INCONEL
INCONEL 718 possesses lower strength and hardness, as well as greater elongation before failure than the aged material (see Figure 5-4).

![Figure 5-4 Effect of heat treatment on tensile behaviour of INCONEL 718 sheet specimens at room temperature](image)

More recently, illustrative flow curves for INCONEL 718 at different temperatures and strain rates were obtained by Thomas et al. in [132] and are shown in Figure 5-5 for 900°C as an example. One can notice that they are similar to those corresponding to other metals, such as steels but, generally, demonstrate higher strength levels. If a medium carbon non-alloyed steel can reach 90 MPa at 1000°C when deformed at 0.1 s⁻¹, INCONEL 718 attained, under the same conditions, almost three times higher stress values (260 MPa). Another feature is the lack of clear evidence of dynamic recrystallization, which is usually identified by a large drop in the stress values after attaining a peak stress. In INCONEL 718 this fact seemed to occur only at 1080, 1050 and 1000 °C at lower strain rates.
Various material models are used in literature to account for the strain hardening and superplasticity characteristics of INCONEL 718.

One of the first equations used to describe the material behaviour as a function of the strain rate was the Malvern formulation [133]:

$$\sigma = f(\varepsilon) + \ln(1 + b\dot{\varepsilon}),$$  \hspace{1cm} (5-1)

where \( f(\varepsilon) \) denotes the stress obtained from the conventional quasi-static material curve and \( b \) is a tuned empirical coefficient.

A superplastic behaviour is conventionally defined as a deformation regime in the temperature-strain rate-strain (T, \( \dot{\varepsilon} \), \( \varepsilon \)) space, where plastic instabilities are delayed or suppressed by a high value of the strain rate sensitivity of the flow stress \( \sigma \). The strain-rate sensitivity is usually characterised by the parameter

$$m = \frac{\dot{\varepsilon}}{\sigma} \left( \frac{\partial \sigma}{\partial \dot{\varepsilon}} \right) \approx \frac{\dot{\varepsilon} \Delta \sigma}{\sigma \Delta \dot{\varepsilon}},$$  \hspace{1cm} (5-2)

obtained from intermittent nominally instantaneous changes between two strain rates during tensile tests performed at constant temperatures [134]. The application of this
material model can be found in [135]. That work was based on obtaining the strain-rate sensitivity parameter \( m \) from the material's response to the tensile test, in which a sinusoidal perturbation is imposed on a nominally constant strain rate \( \dot{\varepsilon} \).

Zhang et al. in [136] used a constitutive model to represent the relationship between the stress, strain rate, strain and deformation temperature as a hyperbolic sine relation as

\[
\dot{\varepsilon} = \exp\left(-\frac{Q}{RT}\right) \frac{\sinh(\alpha \sigma)}{B},
\]

(5-3)

where \( \dot{\varepsilon} \) is the strain rate, \( Q \) is the activation energy, \( R \) is the gas constant, \( T \) is the absolute temperature, \( \alpha \) is a constant, \( B \) is the material parameter and \( \sigma \) is the true stress. The hyperbolic sine relation has many advantages when compared to common forms used to represent superplastic materials, the obvious one being its ability to represent the temperature dependency.

Many researchers developed formulations to simulate the material behaviour under dynamic loads and high strain rate. One of these formulations is the Cowper-Symonds relations, which correlates the yield stress \( \sigma_Y \) to the yield stress obtained during dynamic loading with a high strain rate [137]:

\[
\frac{\sigma_d}{\sigma_Y} = 1 + \left(\frac{\dot{\varepsilon}}{D}\right)^p,
\]

(5-4)

where \( \sigma_d \) is the yield stress under dynamic load, \( D \) and \( p \) are the empirical coefficients to be determined by experimental tests. Sciuva in [137] used this formulation because of its simplicity as a part of the material model in order to establish the correlation between the experimental and numerical results obtained with the explicit finite element code MSC/Dytran.

The Ramberg-Osgood equation that was suggested to describe a non-linear relationship between stresses and strains in materials near their yield points is capable of describing the non-linear hardening behaviour of INCONEL 718 [138]. This equation is useful for metals that harden with plastic deformation, showing a smooth elastic-plastic transition. In its original form, it states that
\[
\epsilon = \frac{\sigma}{E} + K \left( \frac{\sigma}{E} \right)^n, \tag{5-5}
\]

where \( \epsilon \) is strain, \( \sigma \) is stress, \( E \) is the Young's modulus and \( K \) and \( n \) are material constants. The first term on the right side, \( \frac{\sigma}{E} \), is equal to the elastic part of the strain, while the second term, \( K \left( \frac{\sigma}{E} \right)^n \), accounts for the plastic part, with parameters \( K \) and \( n \) describing the hardening behaviour of the material. Introducing the yield strength of the material, \( \sigma_0 \), and defining a new parameter, \( \alpha \), related to \( K \) as

\[
\alpha = K \left( \frac{\sigma_0}{E} \right)^{n-1}, \tag{5-6}
\]

it is convenient to rewrite the last term of equation (5-5) as follows

\[
K \left( \frac{\sigma}{E} \right)^n = \alpha \frac{\sigma_0}{E} \left( \frac{\sigma}{\sigma_0} \right)^n. \tag{5-7}
\]

Replacing the first expression, the Romberg-Osgood equation can be written as

\[
\epsilon = \frac{\sigma}{E} + \alpha \frac{\sigma_0}{E} \left( \frac{\sigma}{\sigma_0} \right)^n. \tag{5-8}
\]

According to this formulation the hardening behaviour of the material depends on the material constants \( \alpha \) and \( n \).

The mechanical behaviour of aged INCONEL 718 at high strains, strain rates and elevated temperatures can be adequately described by the Johnson-Cook material model, accounting for the strain-rate sensitivity:

\[
\sigma_y = \left(A + B \epsilon_p^* \right) \left(1 + C \ln \left( \frac{\dot{\epsilon}}{\dot{\epsilon}_0} \right) \right) \left[1 - \left( \frac{T}{T_m} \right)^{n} \right], \tag{5-9}
\]

where \( A, B, C \) and \( n \) are the material constants, and

\[
T^* = \frac{(T - T_{room})}{(T_{melt} - T_{room})}. \tag{5-10}
\]

\( \epsilon_p \) and \( \dot{\epsilon} \) are plastic strain and strain rate, \( T_{room} \) and \( T_{melt} \) are the room and melting temperatures, respectively.

This form is employed in numerical simulations in this thesis.
5.3 High-speed filming

In case of UAT, where high-frequency vibrations are generated in the cutting tool, it is important to use some means, capable of studying the process occurring at a speed greater or equal to the workpiece speed. High-speed (HS) filming is one solution to the stated problem. HS filming provides a direct observation of the tool-workpiece interaction zone. The cameras used for HS filming are capable of recording small intervals of time (in ms) and thus provide an opportunity to study the chip formation, development of different shear zones and the material’s response to different vibrations (produced as a result of vibration of different parts attached to the lathe in the case of CT and, additionally, due to deliberate introduction of excitation in case of UAT).

These cameras have their limitations. Recording at high frame rates requires the provision of very intense light, properly focused at the area of interest. The use of a laser is recommended for such purposes and therefore an additional setup for laser arrangement and strict precautionary steps are required. Recording at frame rates 100,000 s\(^{-1}\) to 150,000 s\(^{-1}\) results in a large amount of data, so only small instances are recorded, which sometimes does not satisfy the required criteria. The opaque nature of the workpiece, cutting tool and the resulting chip causes light reflection, and, therefore, considerably worsens the quality of the images. Also these cameras can only record data related to the surfaces exposed, and are unable to study more important areas between the workpiece and cutting tool in the interaction zone.

5.3.1 Experimental setup

The experimental setup for high-speed filming is shown in Figure 5-6. High-speed filming was conducted using the Phantom® V 7.0 camera, which was capable of recording up to a maximum of 150,000 frames/sec (fps) for a 32 pixel × 32 pixel area.

HS camera was mounted using a special fixture on the in-house UAT prototype. The camera was connected to a fast workstation (P-IV, 2.8 GHz processor, 2024 MB RAM) via the 10/100 Ethernet port, capable of fast data transfer from the camera to workstation. The HS camera was fully controllable from the computer and also had a trigger switch for manual control.
In the process of filming, images were first recorded in the built-in memory of the camera from which only the desired images were then transferred to the computer for further study. Filming was conducted for both CT and UAT at different resolutions and frame rates. High intensity lighting was used to illuminate the filming area. Although a laser was recommended to be used at high frame rates, the special requirements due to health and safety regulations demanded isolation of the whole setup in a special enclosure, which was not possible with the current setup. Therefore, a laser was replaced by high-intensity lights.

5.3.2 Experimental procedure

HS filming was conducted for both CT and UAT of steel, for different cutting speeds, depth of cuts, and frame rates (25,000 fps - 68,000 fps). A round workpiece was held in the chuck and was trued once to remove any irregularity from the surface.
and to ensure a uniform diameter of 30 mm and was then rotated at different cutting speeds for the final experiment.

For a detailed study of chip formation, the camera was fixed at two locations. Firstly, the camera was fixed over the headstock (Figure 5-7), and filming was conducted for both CT and UAT. The location of the camera was then shifted to the cross slide (Figure 5-7).

![Figure 5-7 Lathe](image)

The cross-side location was advantageous as compared to the headstock location, because the camera was also moving along with the cutting tool, and therefore was always maintaining a fixed zoom (setting a proper zoom is a very time consuming process in case of HS cameras when working with larger magnifications) which resulted in time saving.

5.3.3 Results and discussion

The difference in chip formation in case of conventional turning and ultrasonically assisted turning can be seen in Figure 5-8.

The chip in CT was intermittent as compared to the smooth and straight chip of UAT. An idea of surface finish can be taken from the chip continuity. A continuous material removal from a surface will result in smooth surface finish, and experimental results showed better surface finish in case of UAT as compared to CT.
The high frame rate of the camera enabled a detailed view of the ultrasonic vibration in the cutting tool, which was difficult to observe in our previous studies. In case of UAT the chip oscillation was made visible by HS filming: it is obvious that the chip disconnected from the tool for some time, and these observations were helpful in understanding the drop of averaged force with transition to UAT.

Other magnified images of both CT and UAT are presented in Figure 5-9. This kind of detailed vision of the process zone is especially beneficial for 3D FE modelling.
A more close-to-real FE model can be developed on the basis of HS filming results by studying carefully the geometry of forming chip, the contact length and radius of curvature. These results also provided an insight into understanding of kinematics of the turning process. The chip formation results obtained from HS filming justified the FE simulations conducted in 2D [6].

5.4 Cutting force measurement

A significant reduction in the cutting force in ultrasonically-assisted machining processes was reported by many researchers [1,107,139,140]. Cutting forces in this study were investigated to be used for validation of the simulation models since they integrally incorporate the effects of all the factors (e.g., friction, temperature, cutting parameters etc.) of the process. Force measurement experiments were carried out for both CT and VAT.

5.4.1 Experimental setup

Force measurement was performed using a Kistler three component dynamometer (type 9257 A). The dynamometer was bolted to the cross-slide of the Harrison lathe, and the ultrasonic transducer was mounted over the dynamometer. The dynamometer used had a sensitivity of ± 1% and was capable of measuring three components (x, y and z) of forces. In the dynamometer, the output voltage of the built-in charge amplifier is proportional to the applied force. The data acquisition frequency of the used dynamometer was 200 Hz that was considerably lower than the ultrasonic frequency of the excitation used in the VAT - from 10 kHz to 35 kHz. Hence, the force measured with the dynamometer was averaged over a large number of cycles of ultrasonic vibration. So, the magnitude of the averaged force is used here as a basis for comparison in all the studies.

The Picoscope® 2202 data logger was used to transfer data from the dynamometer to computer where the data was analysed and recorded. The data obtained was providing information on the charge voltage with time. Therefore the dynamometer was calibrated using a spring balance in a quasistatic mode prior to force measurements in turning to find the respective relation between the magnitude of forces and voltage. Two components of forces - the main cutting force (tangential to
the surface of the workpiece and perpendicular to its axis) and the component of cutting force acting in feed direction - were recorded.

5.4.2 Experimental Procedure

The following cutting conditions were used in the experiments: depth of cut, \(d = 0.2\) mm, feed rate, \(s = 0.1\) mm/rev, cutting speed, \(V_c = 177.9\) mm/s, 376.8 mm/s and 544.2 mm/s. Lubricant was applied in all cases. The INCONEL 718 rod of 40mm diameter was held in the chuck of lathe. Initially the whole length was turned using CT with a very low rotational speed \((n = 40\) rev/min, i.e. cutting speed \(V_c = 83.6\) mm/s, \(s = 0.03\) mm/rev, \(d = 0.3\) mm) to improve alignment of the workpiece with respect to the tool insert. Experiment was started for CT \((d = 0.2\) mm, \(s = 0.1\) mm/rev, \(V_c = 177.9\) mm/s) and was continued till the tool head traversed 10 mm with the force data being recorded. The operation was then stopped and tool insert was checked for signs of wear. The duration of each cut was short and no tool tip wear was observed. The force values observed during each cut were constant, apart from normal vibration induced oscillations. Therefore, a new tool tip was only replaced after experiments were conducted at the 3 different speeds to give a following set of data. Experiment was then continued for UAT \((d = 0.2\) mm, \(s = 0.1\) mm/rev, \(V_c = 177.9\) mm/s, \(f \approx 20\) kHz, \(a \approx 15\) \(\mu m\)) for a length of cut of 10 mm and force data was recorded. The operation was stopped and the cutting speed was adjusted to 376.8 mm/s and the process was started again \((d = 0.2\) mm, \(s = 0.1\) mm/rev, \(f \approx 20\) kHz, \(a \approx 15\) \(\mu m\)) with force data being recorded. After the tool insert had traversed 10 mm, the operation was stopped again. Cutting speed was changed from 376.8 mm/s to 544.2 mm/s and the final stage of the experiment was started \((d = 0.2\) mm, \(s = 0.1\) mm/rev, \(f \approx 20\) kHz, \(a \approx 15\) \(\mu m\)). A turning was continued till the tool insert traversed 10 mm and the respective force data was recorded. This procedure was repeated once again to record a second set of data for the same cutting variables.

5.4.3 Results and discussion

The measured cutting forces in turning of INCONEL 718 are presented in Figure 5-10. There were many factors affecting the cutting forces such as the depth of cut,
tool wear, tool tip orientation and the amount of applied lubrication. The experimental setup available did not provide accurate control over depth of cut, tool tip orientation and the amount of lubricant supply; therefore, lack of accuracy of these parameters was a source of error in measurements. Figure 5-10 shows the comparison of forces for CT and UAT. The measured forces in case of UAT were approx. 60% lower than those in case of CT.

![Comparison of forces for CT and UAT](image)

Figure 5-10 Comparison of forces for CT and UAT \( (d = 0.2 \text{ mm}, s = 0.1 \text{ mm/rev}, V_c = 177.9 \text{ mm/s}) \)

Influence of the cutting speed on cutting forces was also analysed. As the cutting speed was increased from 177.9 mm/s to 376.8 mm/s, an increase of approx. 40% in cutting forces was recorded. A further increase of approx. 15% was recorded when the cutting speed was increased from 376.8 mm/s to 544.2 mm/s (Figure 5-11).
An increase in cutting forces for UAT with the increasing cutting speed can be explained by the fact that at lower speeds the separation time between the tool insert and the workpiece, which is the primary reason for the force drop, was more as compared to the cases where the speed was high. This is a principal difference between the standard cutting process and the ultrasonically-assisted one: in CT the cutting force effectively remains the same for a wide range of the normal (i.e. not high) cutting speed [141,142].

5.5 Concluding remarks

Experimental studies were performed to examine the effects of ultrasonically assisted turning on the cutting tool in comparison with conventional turning. High-speed filming helped in developing a respective 3D simulation model, which will be presented in following chapters. Observation of results from high-speed filming provided necessary information about the orientation of the cutting tool insert with regard to the workpiece. These results also helped to understand kinematics of the
separation process and provided the data on the geometry of the forming chip, contact length and radius of curvature.

Results of the experimental force analysis showed a drop in the tangential forces in the cutting tool in case of VAT when compared to CT. Experiments conducted to show the influence of the cutting speed on the forces in the cutting tool showed an increase in the cutting forces as the cutting speed was increased.
6 GENERAL FEATURES AND DEVELOPMENT OF THE FE MODEL

6.1 General features

Finite element modelling is by far the most vital tool to simulate the process zone and tool-workpiece interaction in turning. This chapter presents the development of three-dimensional thermomechanically coupled FE models for both conventional turning (CT) and ultrasonically assisted turning (UAT). The models incorporate a description of the advanced material behaviour, taking into consideration non-linear hardening and strain-rate sensitivity effects of the material during cutting. They also take into account various heat-transfer, friction and cooling conditions at the chip-tool interface.

Two types of a vibration motion are considered in the suggested models: in the tangential direction, with the cutting tool vibrating tangentially to the surface of a workpiece, and in the feed direction, with vibration being applied parallel to the axis of rotation of the workpiece (Figure 6-1).

![Figure 6-1 Principle directions of motion in UAT](image)

Figure 6-1 Principle directions of motion in UAT [87]
The Johnson-Cook material model [52] accounting for the strain-rate sensitivity is employed in simulations of the aged INCONEL 718 that adequately describes the mechanical behaviour of INCONEL 718 at high strains, strain rates and elevated temperatures (Figure 6-2):

\[
\sigma_y = (A + B \varepsilon_p^n) \left(1 + C \ln \left( \frac{\dot{\varepsilon}_p}{\dot{\varepsilon}_0} \right) \right) \left(1 - T^* \right),
\]

(6-1)

where \( A = 1241 \), \( B = 622 \), \( C = 0.0134 \), \( n = 0.6522 \), \( T^* = \frac{T - T_{\text{room}}}{T_{\text{melt}} - T_{\text{room}}} \), \( \varepsilon_p \) and \( \dot{\varepsilon}_p \) are the plastic strain and strain rate, \( T_{\text{room}} \) and \( T_{\text{melt}} \) are the room and melting temperatures, respectively. Term \( T^* \) is assumed to be negligible since within the temperature range, modelled in our FE simulations and justified by infrared thermography experiments [6], thermal softening of INCONEL 718 is insignificant (less then 5%). This model, utilised by various researchers (see, e.g. [143,144]), has been modified by them to prevent unrealistically high stress values at high strains, so that maximum stress values are limited to the ultimate tensile strength of INCONEL 718 at corresponding strain rates (reaching 105 s\(^{-1}\) in FE simulations).

Figure 6-2 Effect of strain rate on plastic behaviour of INCONEL 718
The updated Lagrangian formulation of FEA is used to model the turning process. It allows the element to change its shape with material flow, and the calculation embeds a computational mesh in the material domain and solves the problem for the position of the mesh at discrete points in time. The updated Lagrangian formulation includes all kinematic nonlinear effects due to large displacements, large rotations and large strains.

The forces in cutting can be represented with the Merchant’s force circle (Figure 6-3). The resultant force \( R \) can be resolved into components acting along different axes. Firstly, it can be obtained as a combination of the main cutting force \( F_x \) and feed force \( F_y \). Secondly, it is can be projected onto the rake face and normal to the rake face directions, becoming a combination of friction force \( F_{fr} \) and normal force \( F_N \) on the rake face of the tool. In our experiments and FE calculations, the main cutting force \( F_x \) is analysed.

![Merchant's force circle](image)

**Figure 6-3 Merchant’s force circle**

The presence/absence of the lubricant is simulated within the finite-element framework by changing friction conditions at the tool–chip interface. As generally observed experimentally, adding lubricants causes the chip to become thinner and
more curled. The extent of influence of lubricants on the cutting process also depends on the cutting speed, feed rate and rake angle.

In CT, the cutting tool is in permanent contact with the chip, and it is generally agreed that no lubricant can penetrate the contact area where normal stresses at the chip/tool interface are high [16]. However, lubricants can infiltrate along the non-contact channels due to surface roughness of the rake face of the tool. The length of these channels generally varies from half to full chip thickness. When the lubricant reacts with the chip in the region of these channels, the resistance to chip flow is reduced, and that increases the shear plane angle. Consequently, the chip becomes thinner and unpeels from the tool surface. Hence, the lubricant does not have to penetrate the whole contact distance at the rake face to reduce the contact area; its influence at the edge of the contact length is enough.

The nature of lubrication processes in UAT has not been studied yet. Nevertheless, the intermittent character of the contact at the rake face of the tool in this case should allow gaseous or liquid lubricants to penetrate deep inside the contact area. It is believed that this should further increase the shear angle in cutting, and decrease the chip thickness.

In many papers the frictional contact at the tool–chip interface in conventional turning is taken into consideration, and various friction models are employed for this purpose. They include the Coulomb friction model, with friction stress being proportional to normal pressure at the interface ($\tau = \mu \sigma_n$) [143,145,146]; the shear friction model ($\tau = \mu k$, where $k$ is shear yield strength) [83]; the modified shear friction model (described below) [37], and the stress-based polynomial model [72].

The classical Coulomb model is unable to adequately reflect friction processes due to high contact stress generation at the tool–chip interface leading to significant friction forces. Hence, the shear friction model [12] was chosen for simulations; here the friction force depends on the fraction of the equivalent stress of the material and not the normal force as in the Coulomb model. Thus, friction stress is introduced in the following form:
\[
\sigma_{te} \leq -\mu \frac{\bar{\sigma}}{\sqrt{3}} \frac{2}{\pi} \text{sgn}(v_r) \arctan \left( \frac{v_r}{v_{cr}} \right),
\]

where \( \bar{\sigma} \) is the equivalent stress, \( v_r \) is a relative sliding velocity, \( v_{cr} \) is a critical sliding velocity below which sticking occurs, \( \mu \) is a friction coefficient.

The workpiece is modelled using the MSC.MARC element type 7 (Figure 6-4), which is an eight-node, isoparametric, arbitrary hexahedral. As this element uses trilinear interpolation functions, the strain is constant throughout the element and thus results in poor representation of shear behaviour [12].

![Figure 6-4 Element 7 [12]](image)

The element 7 was used to model the workpiece and was not used in the simulations due to its poor representation of shear behaviour and lack of support for remeshing. The simple shape of element 7, when compared to element 157, makes it suitable for modelling the simple shape of workpiece. Element 7 is automatically converted by the remeshing to the Tetrahedral element type 157 of MSC.MARC [12]. The tetrahedral element type 157 is a Herrmann type element, which typically uses pressure as well as displacement in the FEM analysis. These elements with mixed unknowns (or degrees of freedom, DOFs) allow the element to model incompressible materials undergoing large shear deformation. The standard displacement-based tetrahedral element cannot perform well in this situation because the element locks and hence lacks flexibility. Element 157 had 5 nodes, with 4 corner nodes and one
interior node. There are 1 pressure DOF and 3 displacement DOFs in each corner node while only 3 displacement DOFs in the interior node (Figure 6-5). This element is introduced for incompressible or nearly incompressible three dimensional applications. The stiffness of this element is formed using four Gaussian integration points. This element can be used for incompressible elasticity via total Lagrangian formulations or for rubber elasticity and elasto-plasticity via updated-Lagrangian formulations.

Figure 6-5 Element 157 [12]

The cutting tool is modelled using the MSC.MARC element type 43 (Figure 6-6) [12]. Element type 43 is an eight-node, isoparametric, arbitrary quadrilateral used for three-dimensional heat transfer applications and is recommended for modelling coupled thermomechanical analysis [12].
6.2 Model description

This section presents the detailed description of the development of the 3D FE model to simulate the turning process with vibration of the cutter in a tangential direction. Figure 6-7 shows relative movements of the workpiece and cutting tool.

Figure 6-6 Element 43 [12]

Figure 6-7 Scheme of relative movements of the workpiece and cutting tool in 3D simulations of UAT
The dimensions of the developed 3D model for a simulated part of the workpiece are 2.5 mm in length by 0.5 mm in height by 0.4 mm in depth. The uncut chip thickness $t_1$ varied between 0.1 mm to 0.2 mm. The workpiece was modelled using simple eight-node hexahedral element type 7, and the total number of elements was 64 (Figure 6-8(a)), which was automatically converted to tetrahedral element type 157 and the number increased to approx. 1000 due to remeshing performed immediately in the first iteration (Figure 6-8(b)). The material properties used for the workpiece were those of INCONEL 718.

For all the experiments, performed in this thesis, a tool insert with a positive rake angle was used and hence the cutting tool used in simulations also had a positive rake angle $\alpha = 7.5^\circ$ and a clearance angle $\gamma = 5^\circ$. A positive rake angle is recommended over negative rake angle because it reduces the main cutting forces [147]. The tool tip used for the experiments is typically used in industry for machining of INCONEL 718. The material properties defined for the cutting tool were those of tungsten-carbide. The cutting tool was modelled using eight-node hexagonal elements, and the number of elements kept constant at 512 (Figure 6-8).

In the model the workpiece moved with a constant velocity, which corresponded to the cutting speed $V_c$ and equalled 80 rev/min, 160 rev/min and 240 rev/min (167.6 mm/s, 335.2 mm/s and 502.9 mm/s). As the workpiece had to undergo remeshing to account for the high deformation in the tool-chip interaction zone, it was not possible to apply boundary conditions directly to the workpiece. Kinematic boundary conditions were thus applied to the left, right and bottom side of the workpiece using three rigid surfaces. These surfaces were then ‘glued’ to the workpiece by defining contact between the workpiece and the rigid surfaces.

Recent developments in the software now allow the user to apply boundary conditions directly to the remeshing parts, but the results are not accurate, as sometimes the values of the boundary condition are changed due to calculation errors. The surfaces created for the purpose of applying boundary condition are shown in Figure 6-9 and have the following conditions applied...
Figure 6-8 Initial model formulation (a) and actual model (b) after the beginning of the simulation

\[ V_x|_{ABCD} = V_C, \; V_x|_{ABFE} = V_C, \; V_x|_{EFGH} = V_C, \]

\[ V_x|_{ABCD} = 0, \; V_x|_{ABFE} = 0, \; V_x|_{EFGH} = 0, \]  \hspace{1cm} (6-3)

\[ V_z|_{ABCD} = 0, \; V_z|_{ABFE} = 0, \; V_z|_{EFGH} = 0. \]  \hspace{1cm} (6-4)
The cutting tool for CT was static but in UAT it was vibrating harmonically about its equilibrium position with a frequency $f = 20$ kHz and amplitude $a = 15 \mu m$ along the $x$-axis. The velocity of the cutting-tip vibration was described by the relation $\dot{u}_x = v_t \sin \omega t$, where $v_t = 2\pi af$. In UAT $v_t > V_c$, providing a condition for separation of the cutter from the chip within each cycle of ultrasonic vibration.

Thermal processes in turning comprises:

- heat generation in the workpiece material due to its plastic deformation and creation of new surfaces,
- frictional heating at the tool-chip interface,
- contact heat conduction between the chip and tool,
- convective heat transfer from free surfaces of the workpiece, chip and tool to the environment. The convective heat transfer from a free surface of the workpiece, chip and tool to the environment is given by

$$-k \frac{\partial T}{\partial n} = h(T - T_\infty),$$

(6-6)

where $n$ is the outward normal to the surface, $h$ is a convective heat transfer coefficient, $T_\infty$ is the ambient temperature. The thermal flux, passing from the chip to the cutter at the contact length, is described as follows:
\[ q = H(T_{\text{chip}} - T_{\text{tool}}), \]  \hspace{1cm} (6-7)

where \( H \) is a contact heat transfer coefficient, \( T_{\text{chip}} \) and \( T_{\text{tool}} \) are chip and tool surface temperatures, respectively. The influence of heat transfer in the overall temperature distribution in the turning process has been studied using simulation. The specific values of heat transfer coefficient used can be found in Section 7.4.

Initial thermal conditions used in simulations are as follows:

\[ \text{CT: } T_{\text{work}}|_{t=0} = T_{\text{m}}; T_{\text{tool}}|_{t=0} = T_{\text{m}}; \]  \hspace{1cm} (6-8)

\[ \text{UAT: } T_{\text{work}}|_{t=0} = T_{\text{m}}; T_{\text{tool}}|_{t=0} = T_{\text{UAT}}; \]  \hspace{1cm} (6-9)

where \( T_{\text{work}} \) and \( T_{\text{tool}} \) are workpiece and cutting tool temperatures, \( T_{\text{UAT}} \) is the initial temperature of the tool in UAT, as found in infrared thermography experiments. The contact heat transfer in UAT between the chip and tool occurs only during the time of their contact, in contrast to the permanent contact heat transfer in CT.

During the simulation, elements in the process zone can become highly distorted, and hence are no longer appropriate for calculations. Automatic re-meshing/re-zoning was used in the workpiece and chip to replace those distorted elements with elements of a better shape. Activation of the remeshing subroutine required exceeding either of a predefined value of penetration of the cutter’s element into elements of the workpiece or a maximum strain value. Figure 6-10 shows discretisation of the formed chip as a result of the successful implementation of the re-meshing/re-zoning algorithm. Material separation in front of the cutting tool is determined by the character of the plastic flow of the material under the action of the moving cutting tool.
In previous simulations performed for metal cutting, an initial cut was modelled in the workpiece as the starting point for separation, propagation of which resulted in formation of a chip. As in the real turning there is no initial cut required, the current 3D model do have an initial cut (Figure 6-8) which makes possible the study of chip formation from its start to a fully formed chip. This type of modelling also provides the benefit of using the same model of the workpiece to simulate different types of cutting techniques as well as different shapes of cutting tools, which is important in order to perform optimization of the turning process.

The following section explains the development of specific FE models formulated to understand different turning methods and will present the improvement in the modelling technique from simpler models to models more close-to-reality.

6.2.1 Orthogonal turning model

In the model of orthogonal turning the tool edge was kept normal to both cutting and feed directions (Figure 6-10). Orthogonal turning is rather simple to model as the scheme can easily be developed by extruding the 2D model of turning in the third dimension. This model is widely used in the current study to examine different effects of turning since modelling this type of turning does not require defining a very dense
mesh: Even rather coarse meshes were capable of predicting values, which proved helpful in understanding different phenomena in the turning process. Since in orthogonal turning the initial contact between the tool and the workpiece is a line contact, which is then transformed into a surface contact, calculation of stresses do not require defining a very fine mesh at the point of contact as compared to other cases where the initial contact is usually a point.

6.2.2 Oblique turning model

In the oblique type of turning, the tool edge is at some angle to the cutting and feed directions (Figure 6-11). In the developed FE model for oblique turning the tool face was tilted to one side. A high-density mesh is required to simulate accurately this type of turning, because the first contact between the cutting tool and workpiece is a point contact, which is then transformed into line and finally becomes a surface contact.

![Model for simulation of oblique turning](image)

Figure 6-11 Model for simulation of oblique turning

An accurate estimation of stresses around the area of a point contact requires definition of a relatively dense mesh. Due to the necessity to use the high-density
mesh in this type of model, the computational limitations did not allow an accurate analysis at the current stage. Simulations for these types of models are possible with introduction of a new FE code DEFORMTM in the current research, as will be presented in sections that follow.

6.2.3 Incremental turning model
Incremental turning modelling is performed to study the effect of left over stresses from turning on the machinibility of the following layer. In this type of turning model successive layers of the workpiece material are removed from its surface in the form of chips. The simulation starts with the removal of the first layer after which the cutting tool is moved back to the initial position and is shifted by an increment in the negative y-direction (Figure 6-7), after which the second cut is started (Figure 6-12).

The simulation was divided into multiple load-steps to analyze both the cutting increments separately. For the first stage of turning the model used was the same as that of orthogonal turning. After the completion of the first increment, the tool was traversed back to the starting position using a special algorithm that controlled the motion of the workpiece. The feed rate was then increased and the second increment of turning was started. The result of two successive incremental steps is shown in Figure 6-12. At this stage it is not possible to fully separate the first chip from the workpiece, thus the cutting tool has to come back after turning the first chip partially and restart the second step. It is hoped that with the development of computational schemes and suitable processing power, a finer mesh will allow simulation of complete removal of the several incremental chips and will result in successful study based on this type of simulation.
6.2.4 Simulation of real geometry

The modelling explained so far in the current work includes several suppositions, which prevent the direct comparison of results with those of the experiments. Therefore an effort was made to model the real geometry of the machining process. For the purpose of simulating the real turning process the actual orientation of the tool insert was taken from high speed filming of the turning process. In order to reduce the computational time, only a part of the cutting tool and workpiece, that were interacting, were modelled (Figure 6-13). The tool insert shown here has a tool nose radius of 0.79 mm and a 0 mm tool edge radius. A complex interaction between the
workpiece and cutting tool requires defining a very dense mesh for the workpiece and thus the results recorded for this kind of simulation are limited to only a few steps.

**Figure 6-13 Introduction of real geometry**

### 6.3 Simulation of UAT using FE code DEFORM™

Although the FE code MARC.MENTAT proved to be helpful to perform basic simulations of models for orthogonal cutting, it is not capable of solving problems with real geometry and those of oblique cutting. The main reason for this is that MARC.MENTAT demands a high volume of available memory (approx. 4 GB of RAM) and higher processing power, which at the time of study were not available (a 32-bit architecture workstation cannot address memory more than 2 GB RAM [12]). Hence, the focus was therefore put towards the selection of a more advanced specialized FE code, which can solve this type of problem. DEFORM™ 3D is a special-purpose FE software for large-deformation cases and is capable of modelling complex three-dimensional material flow patterns. With its fully automatic mesh generator (AMG) with a local control of the element size and easy-to-use templates for quicker modelling and support for user-defined subroutines for material modelling and fracture criteria, it was an obvious choice [148]. DEFORM™ in the previous few
years had won the attention of many researchers working in the area of simulation of machining operations.

Although applying DEFORM™ to the simulations of UAT did not prove to be a straightforward procedure, a considerable effort was put into overcoming the limitations of the software during the model development phase. The following section will explain the development of the FE model in DEFORM™ and also the ways in which software deficiencies to modelling of UAT were overcome.

6.3.1 Model description

The material properties in the model developed with DEFORM™ are the same as those used in MARC.MENTAT models. The advantage offered by DEFORM™ is the modelling of a full tool rather than modelling only the tool-tip as was the case previously. Simulations with DEFORM™ did not use the RAM of the computer directly but rather were relying on the processing power, thus providing considerable computational benefits in comparison with MARC.MENTAT. This resulted in shorter simulation times: successful completion of one simulation in MARC.MENTAT required about 7 hours calculation time on a Pentium IV, 2.8 GHz workstation with 2048 MB RAM, whereas a fully simulated model in DEFORM™ required only 2 hours for successful completion on the same workstation.

Modelling of real geometry improves the chance to predict forces, stresses and temperature close to experimental values in this type of simulation. Figure 6-14 (a) shows the actual workpiece and tool interaction, highlighting the area that was actually considered in the FE simulation. Figure 6-14 (b) presents the actual FE model. In order to improve the computation time, tool holder was not modelled in simulations.
Figure 6-14 Basic simulation model: (a) actual workpiece and cutting tool, (b) analysis domain

Figure 6-15 shows the developed FE model. With this software not only cases with a straight workpiece (Figure 6-15(a)) can be simulated but simulation can also be conducted for cases with curved workpiece (Figure 6-15(b)) and even for an entire circular workpiece.
As in the case of MARC.MENTAT-based simulations, this approach also employed an updated Lagrangian solver, and the model was fully thermomechanically coupled. The length of the modelled workpiece was 2 mm. Both the depth of cut and feed rate were maintained at 0.2 mm for preliminary studies and then varied over a range of values for the purpose of comparison. The cutting speed was 335.2 mm/sec (160 rev/min). The ambient temperature was 20°C while the cutting tool has the initial temperature of 70°C (the temperature of the tool insert prior to the engagement with the workpiece was recorded using an infrared camera. The application of ultrasonic energy increased the tool tip temperature from ambient to 70°C [6]) and a nose radius of 0.79 mm. The heat transfer coefficient was 50 N/sec/mm/C. The cutting tool was discretized into 4-noded tetrahedral elements with approximately 12000 nodes and a higher mesh density at the tool tip that was in contact with the workpiece. The workpiece was modelled as elasto-plastic. It was also meshed using 4-noded tetrahedral elements with a minimum element length 0.05 mm. The software was automatically controlling the mesh size in different zones and kept a higher mesh density near the tool tip.
density in the chip formation zone and around it, thus improving the simulation time by focusing more on the tool-workpiece interaction zone, which was the main area of study. The number of simulation steps was kept at 960 with a total time of cutting, analyzed in a simulation, at 0.006 sec. Figure 6-16 shows the meshed cutting tool and workpiece.

**Figure 6-16** Meshed models of cutting tool (a) and workpiece (b)

In DEFORM™ different cases were simulated for UAT aiming at the comparison of the effects of cutting speed, friction condition, feed rate, direction of vibration, vibration amplitude and vibration frequency. Models of CT were also simulated for comparison purposes. All the basic simulation variables such as material properties and a friction model were the same for the cases of CT and UAT, with the only difference being the absence of vibration parameters in case of CT.

Implementing the boundary condition for vibration was not an easy task in DEFORM™. In MARC.MENTAT, the boundary conditions are applied separately on the workpiece and the cutting tool, i.e. vibration is applied to the cutting tool and a constant velocity to the workpiece. The resulting relative motion of both the boundary conditions is the desired working condition. Initially, the same procedure was followed in DEFORM™ but due to remeshing, the software was not transforming the
defined boundary conditions to the new mesh and thus the motion of a workpiece was not consistent. Therefore the two boundary conditions were planned to be imposed on the cutting tool. The new requirement was to have a cutting tool which not only vibrated due to the superimposed ultrasonic vibration but also moved constantly in one direction, thus resulting in a cutting tool which was vibrating as well as moving with a constant velocity.

The following equation is used to calculate the final boundary conditions for the cutting tool having both vibration and constant velocity

\[ d(t) = \left( \frac{L \tau}{t} + a \sin(2\pi f \tau) \right), \quad (6-10) \]

where \( d(t) \) is the position of the cutting tool along the X-axis (Figure 6-14); \( a \) and \( f \) are the amplitude and frequency of vibration respectively, \( L \) is the total length of cut and is calculated by simply calculating the length of cut for a specific cutting speed in a specific time, \( \tau \) is the instantaneous time and \( t \) is the total simulation time.

Figure 6-17 shows the displacement vs time plot for the final tool motion along the X-axis (Figure 6-14).

This type of equation for the cutting speed gave the cutting tool the required kinematic boundary condition, i.e. velocity 160 rev/min and modelled vibration characteristics: amplitude 0.03 µm and frequency 20 kHz.

![Figure 6-17 Displacement of cutting tool in DEFORM™](image-url)
DEFORM™ incorporated for conduction, convection and radiation in modelling. Conduction (transfer of heat through solid material or from one material to another by direct contact) was described using Fourier's law. The rate of heat flow $Q_{\text{cond}}$ is expressed as

$$Q_{\text{cond}} = \frac{kA(T_{\text{hot}} - T_{\text{cold}})}{L},$$

(6-11)

where $A$ is the cross-sectional area and $L$ is the distance through which heat is conducted. The amount of heat transferred by conduction is directly proportional to the temperature gradient and the thermal conductivity of the material. Higher thermal conductivity results in more heat flow.

Convection (transfer of heat through a liquid or gas by the actual movement of the fluid) was described by Newton's law of cooling which states that the rate of heat flow, $Q_{\text{conv}}$, can be expressed by:

$$Q_{\text{conv}} = hA(T_{\text{surface}} - T_{\text{fluid}}),$$

(6-12)

where $h$ is the convection heat transfer coefficient and $A$ is the object's exposed area. This heat is directly proportional to the convection heat transfer coefficient and the temperature gradient between the part and the fluid.

Radiation (transfer of heat by the emission of electromagnetic waves which carry energy away from the emitting object) was described by Stefan-Boltzmann equation which states that the rate of heat flow, $Q_{\text{rad}}$, between a body at temperature $T_{\text{surf}}$ and an environment at temperature $T_{\text{env}}$ can be expressed by:

$$Q_{\text{rad}} = \sigma e A F_{1-2} (T_{\text{surf}}^4 - T_{\text{env}}^4),$$

(6-13)

where $\sigma$ is the Stefan-Boltzmann constant, $e$ is the emissivity of the body's surface, $A$ is the body's surface area and $F_{1-2}$ is the view factor. The heat flow due to radiation is proportional to the fourth power of the temperature gradient between the surface of the part and environment.
7 RESULTS AND DISCUSSION OF THE FINITE ELEMENT MODEL

7.1 Introduction

The development of different types of FE models used in the current work is presented in the previous chapter. This chapter presents the results obtained by numerical simulations based on those models using the FE software MARC.MENTAT.

The processes of CT and UAT were simulated for a cutting time of 3 ms (i.e., 60 cycles of vibration in case of UAT) that corresponded to formation of the chip from the first cut to the fully shaped chip. This interval was subdivided into 2400 time increments each with a duration of $1.25 \times 10^6$ s. Successful completion of one simulation required about 7 hours calculation time on a Pentium IV, 2.8 GHz workstation with 2048 MB RAM. Both the UAT and CT simulations were divided into the same number of increments so that the study of different effects associated with short increments could be possible. Although a cutting time of 3 ms is a relatively short duration and is not enough to reach the steady state temperature for turning, but due to limitations in computational power it was not possible to run cases of longer duration.

In order to get a good understanding of effects due to different processing parameters of turning - forces, stresses, temperatures - and the effect of different variables on these process parameters, a series of different simulations were conducted. Investigations into the experimental evaluation of friction in ultrasonic turning process are limited. A review of literature [36,38,51,145,149,150] had shown the use of coefficient of friction value ranging from 0 to 0.5 in numerical simulations of turning process. Hence, two cases of friction condition were selected for simulations, i.e. 0 representing a well lubricated case and 0.5 for cases without lubrication. Three different cutting speeds 80 rev/min, 160 rev/min and 240 rev/min (167.6 mm/s, 335.2 mm/s and 502.9 mm/s respectively) were selected for simulations, resembling to those used in our experiments of force measurement.
Table 7-1 shows the details of variables for turning conducted for a workpiece of 40 mm diameter. These include the friction, feed rate, amplitude and frequency of ultrasonic vibrations.

**Table 7-1 Variables used in simulations of turning process**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Magnitudes, used in FEA</th>
</tr>
</thead>
<tbody>
<tr>
<td>Friction coefficient</td>
<td>0; 0.5</td>
</tr>
<tr>
<td>Feed rate (mm/s)</td>
<td>0.1; 0.2</td>
</tr>
<tr>
<td>Amplitude (µm)</td>
<td>7.5; 15; 30</td>
</tr>
<tr>
<td>Frequency (kHz)</td>
<td>10; 15; 20</td>
</tr>
</tbody>
</table>

In order to have a clear understanding of the features of a turning process under conditions of superimposed ultrasonic vibrations, a set of case studies into the effect of various parameters was planned. Table 7-2 shows the final matrix of cases for FE simulations that utilised various combinations of the above variables. The last row in this table represents a case of CT and thus does not include parameters of vibration. The names of these cases represent the type of the turning process, the cutting speed, friction condition and the feed rate, e.g. UT_167_FP0_P2 represents the ultrasonic turning process with a cutting speed of 167.62 mm/s, a friction coefficient of 0 and a feed rate of 0.2 mm.

A separate matrix of cases was developed in order to study the effect of the varying vibration amplitude (Table 7-3) while keeping its frequency constant. For these simulations all the other variables were kept constant and only the amplitude was varied. The case with the cutting speed 335.2 mm/s was selected merely because of the computational benefits: this case proved to offer an ease in simulations and it also did not required additional mesh size refinement (even a minimum mesh size of 0.075 mm was sufficient to simulate formation of a chip).
Table 7-2 Matrix of analysed cases for CT and UAT

<table>
<thead>
<tr>
<th>Case Name</th>
<th>Cutting Speed (mm/s)</th>
<th>Friction</th>
<th>Feed Rate (mm)</th>
<th>Amplitude (μm)</th>
<th>Frequency (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>UT_167_FP0_P1</td>
<td>167.6</td>
<td>0</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_167_FP0_P2</td>
<td>167.6</td>
<td>0</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_167_FP5_P1</td>
<td>167.6</td>
<td>0.5</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_167_FP5_P2</td>
<td>167.6</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP0_P1</td>
<td>335.2</td>
<td>0</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP0_P2</td>
<td>335.2</td>
<td>0</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP5_P1</td>
<td>335.2</td>
<td>0.5</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP5_P2</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_502_FP0_P1</td>
<td>502.9</td>
<td>0</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_502_FP0_P2</td>
<td>502.9</td>
<td>0</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>UT_502_FP5_P1</td>
<td>502.9</td>
<td>0.5</td>
<td>0.1</td>
<td>30</td>
<td>20</td>
</tr>
<tr>
<td>CT_335_FP5_P2</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>NA</td>
<td>NA</td>
</tr>
</tbody>
</table>

Table 7-3 Matrix of cases to study the effect of vibration amplitude

<table>
<thead>
<tr>
<th>Case Name</th>
<th>Cutting Speed (mm/s)</th>
<th>Friction</th>
<th>Feed Rate (mm)</th>
<th>Amplitude (μm)</th>
<th>Frequency (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>UT_335_FP5_P2_7P5</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>7.5</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP5_P2_15</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>15</td>
<td>20</td>
</tr>
<tr>
<td>UT_335_FP5_P2_30</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
</tbody>
</table>

Another matrix of cases was developed to study the effect of the vibration frequency (Table 7-4) while keeping the amplitude constant.

Table 7-4 Matrix of cases for simulations to study the effect of frequency

<table>
<thead>
<tr>
<th>Case Name</th>
<th>Cutting Speed (mm/s)</th>
<th>Friction</th>
<th>Feed Rate (mm)</th>
<th>Amplitude (μm)</th>
<th>Frequency (kHz)</th>
</tr>
</thead>
<tbody>
<tr>
<td>UT_335_FP5_P2_10</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>10</td>
</tr>
<tr>
<td>UT_335_FP5_P2_15</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>15</td>
</tr>
<tr>
<td>UT_335_FP5_P2_20</td>
<td>335.2</td>
<td>0.5</td>
<td>0.2</td>
<td>30</td>
<td>20</td>
</tr>
</tbody>
</table>

The following sections present the results obtained by means of FE simulations with MARC.MENTAT of these cases.
7.2 Comparison of forces in CT and UAT

In simulations, forces were calculated in the cutting tool only so that comparison can be made with experimentally determined values of forces that were calculated by mounting the force dynamometer on the tool holder. Also, with MARC.MENTAT all the forces and moments in a rigid body are resolved to a single point, which is normally the centroid of the body [12]. It is therefore easy to monitor the force versus displacement plot for the rigid body, which in the current case was the cutting tool.

Simulations were conducted using models of UAT and CT to compare the forces acting on the cutting tool during turning. The results of simulations showed a significant difference in forces acting on the cutting tool for UAT and CT. Since the cutting tool was in permanent contact with the workpiece during CT simulations, a practically non-changing force was present in the cutting tool. In UAT in a single cycle of vibration from the moment of the first contact with the chip, the forces increased with penetration and attain levels somewhat higher than the average force in CT when the tool reached the maximum penetration depth (Figure 7-1). The force magnitude then declined at the unloading stage until it vanished when the cutter separated from the chip. The forces stayed close to zero until the cutter came into contact with the chip again in the next cycle of ultrasonic vibration. Low-level fluctuations of the cutting force at the withdrawal and approach stages of the cycles are explained by re-meshing the contact between the cutter and freshly formed workpiece surface, as well as by numerical errors inherent to FE simulations. This level of fluctuation can be reduced further by using a finer mesh size in the tool-workpiece interaction zone and also by increasing the number of increments and introducing more re-meshing between increments. However, a smaller mesh size was not possible at this stage due to larger memory requests, which were already kept at the maximum (2048 MB RAM) possible limit (a 32 bit architecture workstation cannot address memory more than 2048 MB RAM) [12]. As the force measured with the dynamometer were averaged over a large number of cycles of ultrasonic vibration in our experiments, so, for the purpose of comparison, the magnitude of the averaged force was used in case of UAT as a basis for comparison in all the studies
Figure 7-1 Comparison of calculated forces in cutting tool for CT and UAT simulations ($\mu = 0.5$, $t_i = 0.1$ mm, $d = 0.4$ mm, $V_c = 335.2$ mm/s)

Simulations show a consistent force value of 140 N in the cutting tool for CT. In the case of UAT, the maximum value of this force was somewhat higher than in CT but for quantitative analysis averaging of the two values for one full cycle of vibration (Figure 7-1) was performed using the following relation:

$$\langle F_c \rangle = \int_{t_s}^{t_e} F_c dt,$$

(7-1)

where $f$ is the frequency, $t_s$ and $t_e$ are the start and end times of the vibration cycle.

Averaging of the values for UAT shows that the forces for UAT were nearly 38% of the forces for CT. The reduction in forces for UAT compared to CT can be explained by the fact that in case of CT the tool was in constant contact with the workpiece and thus had a uniform value of force, whereas, in case of UAT the tool was not in constant contact with the workpiece but was vibrating with a frequency of 20 kHz. Figure 7-1 shows that forces for UAT only developed when the tool was approaching the workpiece (maximum penetration). Therefore, in case of UAT, the tool was free of forces for most of the time and thus had low average value of force when compared to tool in case of CT. Comparison of this difference to the experimental results (Figure 7-2) showed a fair agreement between the results.
Figure 7-2 Experimental results [section 5.4.3] for comparison of forces for CT and UAT ($d = 0.2\,\text{mm}$, $s = 0.1\,\text{mm/rev}$, $V_c = 177.9\,\text{mm/s}$)

7.3 Effect of friction

In all the FE simulations conducted for the turning process in the current research, the friction conditions defined between the cutting tool and the workpiece greatly influenced the predictions. Accurate definition of friction and contact condition is always needed. The right definition of contact and friction condition at the tool-workpiece interface helps simulate the lubrication conditions. Different lubrication conditions were implemented in the simulations by varying the friction values at the interaction zones between the tool and workpiece. Simulations were conducted to find the effect of friction on the forces in the cutting tool using different models of UAT. A significant difference was observed in forces in the cutting tool when the friction value was increased from $\mu = 0$ (frictionless condition) to $\mu = 0.5$ (Figure 7-3).
Figure 7-3 Comparison of calculated forces in the cutting tool for UAT with friction ($\mu = 0.5$) and without friction ($\mu = 0$) ($t_i = 0.1$ mm, $d = 0.4$ mm, $V_c = 335.2$ mm/s)

The maximum magnitude of cutting forces is reached when the tool is in full contact with the chip, i.e. at the positive peak of amplitude of the vibration cycle, and these forces start dropping to zero levels when the tool disengages with the chip i.e. starts moving away from the workpiece. The maximum magnitude of cutting forces in simulations with friction was 10-15% higher than that in the frictionless simulation.

The difference in friction condition also caused a difference in the chip shape (Figure 7-4). Simulations showed that the radius of curvature of the chip under the frictionless contact condition at the tool–chip interface was approximately 60% smaller than that for the contact with friction for both cutting techniques; that is supported by turning experiments with different lubricants, showing higher values of the radius of curvature for dry turning [100].

The chip thickness in simulations with friction was greater than that in simulations without friction. The chip thickness ratio $r = \frac{t}{t_c}$, that is the ratio of thickness of the uncut chip to that of the deformed chip, equalled 0.6 and 0.7, respectively, for simulations with and without friction.
Figure 7-4 Chip shape and distribution of equivalent plastic strains in the cutting region in simulations of UAT with friction ($\mu = 0.5$) (a) and without it ($\mu = 0$) (b).

Cutting parameters: $t_l = 0.1$ mm, $V_c = 335.2$ mm/s ($t = 3$ ms)

7.4 Temperature distribution in UAT

The model presented in this chapter was a fully thermomechanically coupled FE model that allowed the study of temperature changes both in the workpiece and cutting tool. The calculated temperature distributions in the workpiece for CT and UAT are shown in Figure 7-5, while Figure 7-6 shows the temperature distribution in the cutting tool for both CT and UAT. In case of UAT the data was recorded at the maximum penetration instant of the cycle. Simulations demonstrate a higher temperature in the process zone for UAT as compared to that of CT. The temperatures for UAT reached a value of 650°C to 700°C compared to 400°C to 450°C for CT. According to the infrared thermography results [6], obtained for machining of INCONEL 718, the maximum temperature in case of UAT is 720°C and for CT this temperature is 660°C. The simulation results were obtained for a very short duration of time (3 ms), and due to delay in temperature rise in case of CT the temperature did not reach a saturated state. If the simulations were run for a longer duration, a
saturation state in temperature could have been obtained and difference between CT and UAT temperatures would have matched the results from experiments.

Higher temperatures generated in UAT in the process zone led to greater thermal expansion of the material, and, consequently, to larger thermal strains in the machined layer.

Figure 7-5 Calculated temperature distributions in cutting regions for CT (a) and UAT (b) \((t_i = 0.1 \text{ mm}, \ d = 0.4 \text{ mm}, \ V_c = 335.2 \text{ mm/s}, \ \mu = 0.5, \ t = 3 \text{ ms})\)
Plots of thermal strains are given in Figure 7-8 for both CT and UAT, and results indicate a 30% higher value of thermal strain in the UAT case as compared to CT. The values of thermal strain are higher close to the surface, but they drop rapidly further away (Line 1-2 in Figure 7-7).

Analysis of the simulation results obtained from both CT and UAT models show that thermal conductivity $k$ and specific heat $\rho$ of the workpiece material grow linearly from 12 W/m K and 440 J/kg K to 24 W/m K and 680 J/kg K, respectively, with an increase in temperature from the room temperature $T_{room}$ to 900°C. The coefficient of thermal expansion $\alpha$ of INCONEL 718 increases nearly linearly from $12 \times 10^{-6}$ °C$^{-1}$ to $15 \times 10^{-6}$ °C$^{-1}$, as temperature grows from $T_{\infty}$ to 600°C, and then continues to grow up to $17 \times 10^{-6}$ °C$^{-1}$, as temperature rises to 900°C.

Figure 7-6 Temperature distribution in cutting tool insert for CT (a) and UAT (b) ($\mu = 0.5$, $t_i = 0.1$ mm, $d = 0.4$ mm, $V_c = 335.2$ mm/s, $t = 3$ ms)
Figure 7-7 Calculated thermal strain distributions in cutting regions for CT (a) and UAT (b) \((\mu = 0.5, \ t_i = 0.1 \ mm, \ d = 0.4 \ mm, \ V_c = 335.2 \ mm/s, \ t = 3 \ ms)\)

Figure 7-8 Thermal strain under the machined surface, along the line 1-2 in Figure 7-7
The influence of the change in heat transfer parameters of the workpiece/tool system was studied: the contact heat transfer coefficient $H$ was varied in the range between $10^5$ W/m$^2$ K and $5 \times 10^5$ W/m$^2$K, while the convective heat transfer coefficient $h$ was kept constant at 500 W/m$^2$K. The coefficient $H$ is a very complex parameter that depends on contact pressure, the lubricant type, cutting temperature and surface roughness, and it is a difficult task to measure it experimentally. Here it was assumed that the range $10^5 - 5 \times 10^5$ W/m$^2$ K covered all possible values of $H$ arising in the experiments.

Figure 7-9 shows temperature evolution in the cutting tip for two values of contact heat transfer coefficient $H$. A rise in $H$ from $10^5$ W/m$^2$ K to $5 \times 10^5$ W/m$^2$ K led to an increase in the maximum temperature of the cutting tip in UAT from 105°C to 175°C. This rise could be naturally explained by the increased thermal flux from the workpiece into the tool with growing $H$.

![Figure 7-9 Temperature evolution in cutting tip in simulations of UAT ($h = 500$ W/m$^2$ K)](image)

Heat transfer from the material to the surrounding area was also studied. Temperature data from the nodes along the cutting edge (i.e. along the Z-axis in Figure 7-5) was recorded. This kind of analysis is possible only for a 3D formulation, since in 2D simulations the cutting edge is reduced to a single point. The analysis (Figure 7-10) shows that the maximum temperature was reached somewhere in the
middle of the cutting edge, with slight falls (approx. 11%) towards its ends. This result can be attributed to the convective heat transfer from the surface of the tool into the environment. A similar distribution was observed throughout the simulation time, with the absolute values of the tool temperature growing with time due to the frictional heating and contact heat transfer between the chip and workpiece surfaces.

![Temperature distribution along cutting edge](image)

**Figure 7-10** Temperature distribution along cutting edge (along Z-axis in) for $H = 5 \times 10^5$ W/m$^2$K at $t = 1$ ms

### 7.5 Stress distribution in CT and UAT

Comparative studies were conducted for CT and UAT for different types of stresses to better understand various aspects of the turning process in both types.

Equivalent stresses were chosen to demonstrate the variation of stresses in the workpiece in one complete cycle of vibration. The intermittent character of the chip—cutting tool interaction determined the main differences in stress distributions for CT and UAT. The calculated stress state during CT (Figure 7-9a) was nearly quasistatic with the highest equivalent stresses concentrated in two zones (so-called primary and secondary shear zones), and attaining the magnitude up to 1600 - 1700 MPa. The first of these zones was between the tool tip and back side of the chip, and the second was on the rake (front) face of the cutting tool.
Figure 7-11 Chip shape and distribution of equivalent stresses in CT (a) and UAT at different moments of a single cycle of vibration: tool approaches the chip (b), maximum penetration of the tool (c), and tool moves away from the chip (d). Vibration amplitude $a = 15 \, \mu m$; tool is not shown. ($t_i = 0.1 \, mm$, $d = 0.4 \, mm$, $V_c = 335.2 \, mm/s$)

The stress state in UAT was inherently transient and changed for different staged of the cycle of ultrasonic vibration. The stress distribution in the penetration stage of the cycle (Figure 7-11c) was somewhat analogous to that of CT with the maximum equivalent stress also reaching approximately 1700 MPa. The stresses exceeding 1600 MPa were concentrated in the primary and secondary shear zones. When the unloading stage of the cycle of ultrasonic vibration commenced, the magnitude of the equivalent stress rapidly declined; the zones with high stress levels started to shrink in the direction of the tool tip and towards the chip’s back side. After this quick transitional process that took place at the unloading stage of the cycle, the stress distribution in the process zone remained practically quasistatic during the withdrawal and approach stages of the cycle (Figure 7-9d and Figure 7-9b, respectively). This distribution was characterised by lower levels of the highest stresses concentrated in
the direct vicinity of the tool tip with their magnitude reaching about 1300 MPa. As soon as the tool came into contact with the chip again at the next penetration stage, the maximum stress levels quickly regained the high magnitude of about 1700 MPa, and the cycle repeated. Hence, the maximum stress magnitude was considerably lower and areas occupied by high stresses were significantly smaller at the withdrawal and approach stages of the cycle than those at the penetration stage. Consequently, the mean values of stresses obtained in UAT simulations were significantly lower than those in CT, leading to lower mean cutting forces in the cutting process.

7.6 Comparison of chip shape for CT and UAT

The difference of technique not only resulted in variation of stresses in the workpiece but also showed a visible difference in chip shape. The chip shape varied with changing friction conditions.

![Figure 7-12](image)

**Figure 7-12** Chip shape in simulations of CT (a) and UAT (b) without friction.

Cutting parameters: \( t_1 = 0.1 \, \text{mm} \), \( d = 0.4 \, \text{mm} \), \( V_c = 335.2 \, \text{mm/s} \) \((t = 3 \, \text{ms})\)

From figure 7-10 it can be seen that the chip shape for CT was curlier compared to UAT. If the simulations are allowed to run for longer time intervals, this will result in longer chip length and hence will show a more prominent difference in the curl. In case of MARC.MENTAT the memory demand of the software to run longer
simulations of turning is beyond the available memory limits (more than 4 GB RAM is required but a 32 bit architecture workstation cannot address memory more than 2 GB RAM [12]).

Figure 7-13 Chip shape in simulations of CT (a) and UAT (b) with friction (\( \mu = 0.5 \)). Cutting parameters: \( t_1 = 0.1 \) mm, \( d = 0.4 \) mm, \( V_c = 335.2 \) mm/s (\( t = 3 \) ms)

The same result was true for cases with friction (\( \mu = 0.5 \)), but a smaller difference was observed in this case between chips in CT (a) and UAT (b). This simulation result is also in good agreement with experimental studies showing only slight variations in the chip thickness for both cutting schemes (Figure 5-9).

7.7 Effect of cutting parameters

The following section presents the effects of changing cutting parameters on the overall performance of the turning process. Since the melting point of INCONEL 718 is in the range of 1260-1336°C, homologous temperatures are relatively low to sufficiently affect the turning process, therefore, temperature was not used as a bottom line for comparison between different cutting variables. Cutting forces play a very integral role in the turning process and hence were selected as a parameter for comparison.
7.7.1 **Effect of ultrasonic amplitude**

FE simulations were conducted to study the effect of vibration amplitude on the forces acting on the cutting tool in VAT. An increase in the peak force with an increase in the amplitude was observed (Figure 7-14). However, the average cutting force decreases with an increase in the amplitude. A decrease of approx. 28% in the average force was recorded for an increase in the amplitude from 7.5 μm to 15 μm, and a further approx. 33% decrease was observed when the amplitude was increased from 15 μm to 30 μm.

![Graph showing force vs. time and average force vs. amplitude](image_url)

**Figure 7-14** Effect of ultrasonic amplitude on forces in cutting tool ($t_i = 0.1$ mm, $d = 0.4$ mm, $V_c = 335.2$ mm/s)
An increase in the vibration amplitude also resulted in an increased level of maximum shear stresses developed in the workpiece. Although this increase was insignificant and the maximum difference between shear stresses in CT (i.e. when vibration amplitude was zero) and in UAT when $\alpha = 30 \mu m$ reached only 4%, the clear trend can be seen in Figure 7-15 with shear stresses growing steadily with an increase in the vibration amplitude. This growth can be attributed to the increased deformation rate and subsequent strain-rate hardening of the workpiece material.

![Figure 7-15 Effect of vibration amplitude on maximum shear stress in cutting region](image)

$\tau = 0.1 \text{ mm, } d = 0.4 \text{ mm, } V_c = 335.2 \text{ mm/s}$

A monotonic growth in the maximum level of the equivalent plastic strain ($\bar{\varepsilon}_{max}^p$) was observed when the vibration amplitude was increased from 7.5 $\mu m$ to 30 $\mu m$. A 5% growth in $\bar{\varepsilon}_{max}^p$ was reached for an increase in amplitude from 7.5 $\mu m$ to 15 $\mu m$, followed by a further 5% growth for an amplitude increase from 15 $\mu m$ to 30 $\mu m$. The $\bar{\varepsilon}_{max}^p$ magnitude for $\alpha = 30 \mu m$ was by 7% greater than that in CT (i.e. when $\alpha = 0 \mu m$). The increase in $\bar{\varepsilon}_{max}^p$ with growing amplitude is likely to be related to the increased amplitude of a relative motion between the tool and workpiece and the subsequent increase in deformation levels.
Figure 7-16 Effect of vibration amplitude on maximum equivalent plastic strain in cutting region \((t_i = 0.1 \text{ mm}, d = 0.4 \text{ mm}, V_c = 335.2 \text{ mm/s})\)

7.7.2 Effect of ultrasonic frequency

A separate study was conducted to analyze the effect of ultrasonic frequency on the forces in the cutting tool (Figure 7-17). The results from these simulations show nearly the same peak force in the cutting tool in all three cases.

The average force demonstrated a drop by approx. 24% for frequency increase from 10 kHz to 20 kHz and a further drop by approx. 26% when frequency was increased from 20 kHz to 30 kHz. It is important to mention that as the three cases have different frequencies so they all have unique time duration of their single vibration cycle. For the purpose of comparison of these cases, with each of them having a unique frequency, a common time scale was selected. Average of forces was then calculated over this time scale (0.1 ms) and comparisons were made.
Figure 7-17 Effect of ultrasonic frequency on forces in cutting tool (\(t_1 = 0.1\) mm, 
\(d = 0.4\) mm, \(V_c = 335.2\) mm/s)

7.7.3 Effect of feed rate

The effect of the feed rate on the stresses and forces in the cutting tool was studied for UAT. Two different feed rates - 0.1 mm and 0.2 mm - were used at a constant cutting speed of 80 rev/min. Zones of high stress intensity were observed with a higher level of feed rate, while the maximum value of the stress rate was the same for both cases (Figure 7-18). Figure 7-19 shows the effect of feed rate on the forces in the cutting tool; increasing the feed rate from 0.1 mm to 0.2 mm gave a 45% increase in the force level in the cutting tool.
Figure 7-18 Stresses in the cutting region for various feed rates: 0.1 mm (a) and 0.2 mm (b) \((d = 0.4 \text{ mm}, V_c = 335.2 \text{ mm/s})\)

Figure 7-19 Evolution of forces in cutting tool during a cycle of vibration for feed rates 0.1 mm and 0.2 mm \((d = 0.4 \text{ mm}, V_c = 335.2 \text{ mm/s})\)

7.7.4 Effect of vibration direction

In a UAT system the vibration can be applied either in tangential or in feed direction. Experiments have already been performed showing the significance of
applying vibrations in both directions (tangential or feed) compared to CT [107]. However, so far no attempt had been made to perform a comparison between vibration in the tangential direction and in the feed direction or their comparison with CT using the FE approach. Therefore, cutting forces were compared in simulations of UAT with ultrasonic vibration applied in the tangential and in the feed direction (Figure 7-20).

When ultrasonic vibration was applied in the feed direction, there was no separation between the cutting tool and chip; instead, a separation between the tool and workpiece (along Y-axis in Figure 7-21) took place. This is different to the separation for UAT in the tangential direction, where the tool loses its contact totally with the chip for some time during the vibration cycle (along X-axis in Figure 7-21). For UAT in the feed direction, when the tool was not in contact with the workpiece, the average cutting force was equal to about 120 N. Once the tool came into contact with the workpiece, the force rose to about 200 N and stayed at that level until the tool lost contact with the workpiece. The mean cutting force over the cycle of ultrasonic vibration was 130 N for feed vibration, as compared to 50 N for tangential vibration. Hence, tangential vibration leads to a greater force reduction in UAT, thus it can be more beneficial for the cutting process. Still, the final conclusion on the efficiency of vibration directions cannot be based only on the force comparison, as other factors, e.g. the quality of the machined surface, should be analysed.

![Figure 7-20 Influence of vibration direction on forces in the cutting tool (f = 20 kHz, a = 15 μm, μ = 0.5, t₁ = 0.1 mm, d = 0.4 mm, Vc = 335.2 mm/s)](image)

Figure 7-20 Influence of vibration direction on forces in the cutting tool (\( f = 20 \text{ kHz}, \ a = 15 \text{ μm}, \ μ = 0.5, \ t₁ = 0.1 \text{ mm}, \ d = 0.4 \text{ mm}, \ V_c = 335.2 \text{ mm/s} \))
Figure 7-21 Scheme of relative movement of workpiece and cutting tool in 3D simulations of UAT

7.7.5 Effect of rake angle

Simulations were also performed to study the effect of rake angle on the forces in the cutting tool. Three different cases were formulated with rake angles of 0°, 5° and -5°. Figure 7-22 shows the details of the two types of rake angles.

Figure 7-22 Types of rake angle
Figure 7-23 shows the stress intensity plots of the three types of simulations relating to the effect of rake angle analysis. The stress intensity in the tool-workpiece interaction zone in the material for negative rake angle (Figure 7-22a) was higher compared to the other two cases.

The magnitude of forces on the cutting tool was also higher in case of the negative rake angle (Figure 7-24). Comparison of values averaged over one complete vibration cycle showed an increase of approx. 27% in forces on the cutting tool when rake angle was changed from positive 5 degree to zero. And a further increase of approx. 14% was observed for the change of rake angle from zero to negative 5 degree.

Figure 7-23 Stress intensity plots in case of negative (a), zero (b) and positive (c) rake angle (f = 20 kHz, a = 15 μm, μ = 0.5, t₁ = 0.1 mm, d = 0.4 mm, \( V_C = 335.2 \) mm/s)
7.7.6 Effect of tool edge radius

Simulations were conducted to study the effect of tool nose radius on forces in the cutting tool. Two special cases were formulated for a cutting speed 335.2 mm/sec, coefficient of friction $\mu = 0.5$, vibration amplitude $a = 15 \mu m$ and frequency 20 kHz. One case had a tool edge radius of 0.02 mm, while the other had a tool nose radius as 0 mm. The size of cutting tool was further reduced by half as compared to the previous cases in order to reduce the number of elements resulting from meshing. Figure 7-25 presents the final model and the resulting stress intensity plot for the two cases. From Figure 7-25 it is apparent that there was not much difference in the stress distribution in the tool-workpiece interaction zone.

Figure 7-24 Influence of cutting tool’s rake angle on overall forces in cutting tool
($f = 20 \text{ kHz}$, $a = 15 \mu m$, $\mu = 0.5$, $t_i = 0.1 \text{ mm}$, $d = 0.4 \text{ mm}$, $V_c = 335.2 \text{ mm/s}$)
Figure 7-25 Stress distribution for tool's nose radius $0^\circ$ (a) and $0.02^\circ$ (b) ($f = 20$ kHz, $a = 15$ $\mu$m, $\mu = 0.5$, $t_i = 0.1$ mm, $d = 0.4$ mm, $V_c = 335.2$ mm/s)

The comparison of forces acting on the cutting tool (Figure 7-26) also showed the same behaviour for the tool's edge radius of 0.02 mm being higher by only 5%.

A difference in chip shape was observed, with the chip formed by the tool without the edge radius being slightly more curled. These results are significant as they prove that the use of tools with an edge radius do not results in considerably higher forces. Use of tools with edge radius is recommended, as it increases the area of the initial contact and thus spread the stresses. Tools with an edge radius are also recommended since they reduce the chances of a tool fracturing and also improves surface finish [127].
Figure 7.26 Effect of tool’s nose radius on forces in cutting tool \((f = 20\, \text{kHz},\: a = 15\, \mu\text{m},\: \mu = 0.5,\: t_i = 0.1\, \text{mm},\: d = 0.4\, \text{mm},\: V_c = 335.2\, \text{mm/s})\)

7.8 Concluding remarks

A 3D thermomechanically coupled FE approach was used to model UAT with CT forming a basis for comparative analysis. For the typical combination of vibration parameters \((f = 20\, \text{kHz},\: a = 15\, \mu\text{m})\) the calculated cutting force in UAT was 40% of that in CT, whilst the experimental measurements showed that the force in UAT was 0.25 - 0.6 of that in CT for various feed rates [6]. The comparison of feed rates indicated a 45% increase in the level of cutting forces in simulations of UAT for doubling the feed rate from 0.1 mm to 0.2 mm due to a higher material removal rate in the latter case. Experimental results showed a 60% increase in the cutting force in UAT when the feed rate doubles from 0.05 mm/rev to 0.1 mm/rev [6], hence we can conclude a fair agreement between experimental and numerical results. The comparison of simulations with and without friction, corresponding to dry and lubricated turning conditions, respectively, showed that in the latter case the cutting force was 10-15% lower and in a good agreement with experimental results [6] indicating 30% decrease in the cutting force when the lubricant was applied.
Simulations also showed that temperature along the cutting edge reach higher values in the middle due to the convective heat dissipation. 3D stress distributions were found to be similar for UAT and CT at the maximum penetration depth of the cutting tool. A fivefold increase in the contact heat transfer from the workpiece to the tool led to a 20% increase in the maximum temperature of the tool demonstrating a relatively lower sensitivity of the process to contact heat transfer coefficient. An increase in the vibration amplitude from 7.5 μm to 30 μm in FE simulations led to a 52% decrease in the average cutting force in UAT and could be explained by an increased part of a cycle of ultrasonic vibration without a contact between the tool and chip. An increase in the vibration frequency from 10 kHz to 30 kHz resulted in a 47% drop in the level of average cutting forces, which could be attributed to an increased number of micro-impacts between the tool insert and the workpiece. Hence, an increase in either vibration frequency or amplitude leads to a decrease in cutting forces in the UAT process that is beneficial to increasing the accuracy of the cutting process and improving material removal rates. Calculations also show that vibration in the tangential direction causes the lower cutting force than that obtained with vibration in the feed direction.
8 FURTHER ANALYSIS OF PARAMETERS FOR ULTRASONICALLY ASSISTED MACHINING

8.1 Introduction

The previous chapter presented the results of a more generalized finite element model developed for the purpose of comparison of UAT with CT. Although the 3D model presented in that chapter offered many benefits when compared to 2D but still it had got some limitations: it is incapable to model real-geometry turning cases; the simulations were performable only for short durations of time; only a part of the tool insert was modelled; and simulation of more complicated geometries (curved and fully circular) were not possible. These limitations were a reason for the use of an additional finite element software package DEFORM™.

DEFORM™ is a special purpose turning software that allowed the modelling of relatively more realistic models of turning that improved the chance to predict forces, stresses and temperatures close to experimental values. The models presented in this chapter are developed in DEFORM™, use a model of a real-life tool insert and are capable of removing chips from straight, curved and even fully circular workpieces. Figure 8-1 shows the chip formation and the temperature distribution in the tool-workpiece interaction zone for a UAT model simulated in DEFORM™.

Figure 8-1 Temperature distributions in cutting regions for UAT
8.2 Model description

The main objective of this part of research was to estimate the magnitude of various parameters from the envelop of experiment-relevant values that minimise the cutting forces on UAT. Table 8-1 presents the list of the principle magnitudes for variables selected for the current study.

**Table 8-1 Variables of turning**

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Magnitudes, used in FEA</th>
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<td>Feed rate (mm)</td>
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<td>Amplitude (μm)</td>
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</tr>
<tr>
<td>Frequency (kHz)</td>
<td>10; 20; 30; 40</td>
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The ambient temperature was selected as 20°C while the tool insert had the initial temperature of 70°C (the temperature of the tool insert prior to the engagement with the workpiece measured using infrared camera [6]) and a nose radius of 0.79 mm was selected as the tool insert used in the experiments of force measurement also had the same nose radius. A DNMA432 tool insert was selected from the library of available tools of the software. The heat transfer coefficient was 50 N/s/mm²°C.

Table 8-2 shows the cases formulated to cover the variables defined in Table 8-1 for both UAT and CT. Simulation cases were chosen in a way that only one specific variable was changed within a case study while others were kept constant, so that the effect of changing a single variable can be identified.

Simulations were also performed for selection of better values for the vibration amplitude and frequency while keeping the other variables constant. Details of the selected amplitude, frequency and other variables associated with the simulations of these cases are presented in Table 8-3 and Table 8-4.
### Table 8-2 Matrix of analysed cases for CT and UAT

<table>
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### Table 8-3 Matrix of cases to study the effect of vibration amplitude in UAT

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### Table 8-4 Matrix of cases to study the effect of vibration frequency in UAT

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</tbody>
</table>
8.3 Results and discussion

Simulations for the selection of optimum parameters performed on the basis of the above defined variables resulted in a large number of cases and important results. For the ease of understanding, these results have been studied separately focusing on the impact of a unique parameter.

8.3.1 Comparison of conventional vs ultrasonically assisted turning in tangential and feed direction

The effect of vibration on the overall performance of the turning process was analyzed. Three cases (CT model, UAT model with vibration in tangential direction and UAT model with vibration in feed direction (Figure 6-1)) were formulated and the results were collected for the forces in the cutting tool.

Figure 8-2 presents the forces on the cutting tool in three different modes of turning, i.e. conventional turning, ultrasonically assisted turning with vibrations in tangential and feed direction. The forces for conventional turning have a uniform magnitude, as the tool is always interacting with the workpiece and is thus applying force constantly. For ultrasonically assisted turning, with vibration applied along the feed direction, the forces again have a nearly constant magnitude. Although the cutting tool is vibrating in the feed direction, but when the cutting tool is moving away from the workpiece surface, it is still in contact with the freshly formed chip, and that is the reason for the nearly constant magnitude of the forces.

Forces for ultrasonically assisted turning with vibrations along the tangential direction were initially nearly zero, but once the tool was in contact with the workpiece forces rose sharply and when the tool started moving away from the workpiece, the force dropped back to zero. The principal difference between application of vibration in the tangential and feed direction was that for UAT in the tangential direction, in every vibration cycle, the cutting tool moved against the direction of workpiece motion, while in case of UAT in the feed direction, the cutting tool oscillated along a normal to the direction of workpiece motion and thus never left contact with the workpiece (the tool maintained contact with the chip as it oscillates normal to the surface of the workpiece). Moving away from the cutting tool in case of UAT, with vibrations in the tangential direction, resulted in a drop of forces to zero for some time. The real difference in forces can be observed by taking the average of forces for a single vibration cycle (Figure 8-3). A force drop of 46 % is observed for
transformation of turning mode from CT to UAT with vibration in the feed direction and a further drop of 30% is observed when the turning mode is transformed from UAT with vibration in the feed direction to UAT with vibration in the tangential direction.

![Graph showing forces comparison](image)

**Figure 8-2** Comparison of forces for three different types of turning modes

![Bar chart showing average forces](image)

**Figure 8-3** Average forces for three types of turning
8.3.2 Effect of vibration amplitude

The impact of increasing amplitude on the overall performance of UAT was studied. The respective cases were modelled using combinations of variables from Table 8-3. Figure 8-4 presents the difference in forces resulting from changing the amplitude value from 7.5 μm to 30 μm.

An increase in the amplitude caused an increase in the maximum force in a complete cycle of vibration. From Figure 8-4 the amplitude of 7.5 μm seems to provide better results but examining the average forces for ultrasonic vibration (Figure 8-5) shows that the cutting force for an amplitude of 7.5 μm was in fact higher than for 15 μm and 30 μm. An approx. 16% drop in the value of force resulted when the value of the amplitude was increased from 7.5 μm to 15 μm, while a further increase of amplitude from 15 μm to 30 μm did not cause any significant change in the average force. It indicates that amplitude has an impact up to a certain level, after which a further increase does not affect the forces.

Figure 8-4 Effect of ultrasonic amplitude on cutting forces
Here it is important to mention that this change in forces is for a cutting speed of 335.24 mm/sec. A different optimum value of vibration amplitude can exist for a different cutting speed.

8.3.3 Effect of vibration frequency

The effect of vibration frequency on the overall performance of UAT was studied using the sets of variables from Table 8-4. The peak values of forces (Figure 8-6) for different frequency values remained the same, as the amplitude was constant and so the penetration per cycle into the workpiece was the same. However, examining the average values of forces (Figure 8-7) for the effect of frequency shows that as the frequency increased the average force decreased. There exists an upper limit for the maximum beneficial value of frequency, beyond which the forces start increasing again.
Figure 8-6 Effect of ultrasonic frequency on cutting forces

Figure 8-7 shows that the increasing frequency from 10 kHz to 20 kHz resulted in an average force drop of approx. 35%. A further drop of 38% was observed when the frequency value was increased from 20 kHz to 30 kHz. Although increasing the frequency from 10 kHz to 30 kHz resulted in a drop of average forces this trend had a limit as can be seen from the next increase of frequency from 30 kHz to 40 kHz when the average of force values in fact increased by approx. 10%. This shows that frequency has an upper limit within which its utilization for UAT is beneficial.
8.3.4 Effect of feed rate

The effect of increasing feed rate in turning on the overall forces in the cutting tool was analyzed. Three different feed rates of 0.1 mm, 0.2 mm and 0.3 mm were examined for UAT at a fixed cutting speed of 335.24 mm/s and friction level of 0.5. Figure 8-8 presents the comparison of forces from the three cases. The maximum force values were 116.25 N, 261.16 N and 568.09 N for feed rates of 0.1 mm, 0.2 mm and 0.3 mm, respectively.

Examining the average values of forces for three feed rates over one complete vibration cycle in Figure 8-9 revealed a 127% increase of force when the feed rate was increased from 0.1 mm to 0.2 mm and a further increase of approx. 250% (an approx. 110% increase from force value at feed rate 0.2 mm) when the feed rate was increased from 0.2 mm to 0.3 mm.

Within the envelop of selected feed rates, examination of forces showed that the feed rate of 0.1 mm is a better value for UAT with a cutting speed of 335.24 mm/sec and a friction value 0.5. Higher forces are detrimental for the life of a cutting tool insert especially when machining high-strength alloys.
The effect of the feed rate on cutting forces was also studied by Hua et al. in [151]. The findings suggested that more deformation energy was required when the feed rate was increased. The feed rate influenced the cutting forces more than any other
variable, and the increase in the cutting forces increased by the same factor (110%) for an increase of feed rate from 0.14 to 0.28 mm/rev in hard turning of bearing steel.

8.3.5 Effect of friction
Simulations were conducted to analyze the impact of the coefficient of friction on the overall cutting forces in the cutting tool. Two different coefficients of friction were introduced, i.e. 0 and 0.5. Figure 8-10 presents the forces in the cutting tool for the two cases. At a feed rate of 0.2 mm, a big difference in the forces due to friction was observed. The force increases by 67 % when the coefficient of friction was increased from 0 to 0.5.

![Figure 8-10](image)

**Figure 8-10** Effect of coefficient of friction on cutting forces; feed rate 0.2 mm

\(d = 0.2 \text{ mm}, V_C = 335.2 \text{ mm/s}\)

This difference varied for different feed rates, as can be seen in Figure 8-11, when the feed rate was increased to 0.3 mm. A 33 % increase in forces was observed when coefficient of friction was increased from 0 to 0.5.
8.3.6 **Comparison of forces for different cutting speeds**

Simulations were also performed to study the effect of a changing cutting speed on the forces in the cutting tool. Three different speeds - 167.62 mm/s, 335.24 mm/s and 502.86 mm/s - were considered at a constant feed rate 0.2 mm and a coefficient of friction 0.5. The results obtained are presented in Figure 8-4 with forces in the cutting tool attaining the highest value for a maximum cutting speed.

Figure 8-13 presents the average forces in the three cases for a single vibration cycle and reveals that a 76% increase of forces occurred when the cutting speed increased from 167 mm/s to 335.24 mm/s and an increase of 33% in cutting forces occurred for an increase in the cutting speed from 335.24 mm/s to 504.86 mm/s. Comparison of this results with experimental results (Figure 8-14) showed that forces in case of UAT increased with increase in cutting speed. An increase in cutting forces for UAT with increase of cutting speeds can be explained by the fact that at lower speeds the separation time between the tool insert and the workpiece, which is the primary reason for force drop, was more as compared to the cases where the speed was high.
An increase in the cutting forces with an increase in the cutting speed was also observed by Korkut and Donertas in [152] while machining AISI 1020 and AISI 1040 steels. A tendency for high built-up edge during machining at low and intermediate cutting speeds was considered to be a factor behind the increase of cutting forces with increase in cutting speeds.

![Figure 8-12 Effect of cutting speed on forces](image)

![Figure 8-13 Average forces for a single ultrasonic vibration cycle](image)

Also Mitrofanov in [6] experimentally observed a decrease in forces for CT from 140 N to 130 N for an increase in cutting speeds from 40 to 125 rev/min, whereas an
increase in forces from 60 N to 90 N was observed for UAT. A drop in the CT force was explained by the lower fracture toughness of the workpiece material at higher strain rates resulted from greater cutting speeds, while the increase in the UAT force was caused by the increased time of the contact between the cutting tool and chip, with the cutting speed getting closer to its critical value ($\nu_s = 2nf$).

Figure 8-14 Experimental results [section 5.4.3] for the effect of cutting speed on forces ($d = 0.2$ mm, $s = 0.1$ mm/rev, $f \approx 20$ kHz, $a \approx 15$ $\mu$m) in UAT

8.4 Concluding remarks

Three dimensional thermomechanically-coupled finite element models of both UAT and CT were investigated in order to select a combination of parameters for a better system of UAT. The model presented is a better model describing the cutting processes in UAT with fewer suppositions involved when compared to the previous model presented in chapter 7. The tool insert used resembled the real tool and all the imposed boundary conditions reflected the actual system. The current model was advantageous in many regards if compared with both the 2D models and the simplified 3D models. The 2D model was only capable of studying orthogonal turning still lacking the through-thickness-dimension. The simplified 3D model presented previously in this thesis was capable of simulating both orthogonal and oblique turning, but still the tool insert’s geometry was different from that of the real tool. The 3D model of turning presented in this chapter had thus overcome many if not all the shortcomings of the previous attempts because of its capability to represent a more realistic turning model.
A reduced workpiece size was considered in order to reduce simulations times. Simulations demonstrated considerable differences in cutting forces when turning with CT and then switching to UAT with vibrations in tangential and feed directions. Results proved the reductions in cutting forces by the introduction of UAT. Even for UAT, a comparison of vibrations in different directions was made possible, and analysis recommends the use of tangential vibrations in UAT to minimize the force level.

Simulations were helpful in understanding the impact of changing vibrations parameters, i.e. amplitude and frequency, on the overall forces in the cutting tool. Keeping other cutting parameters like cutting speed, friction and feed rate constant, the effect of changing amplitude and frequency was analyzed. Changing the cutting speed will require determination of a different optimum value for vibration frequency and amplitude. As shown by simulations, increasing amplitude from 7.5 µm to 15 µm resulted in a drop of forces for a cutting speed of 335.24 mm/sec, but a further increase of the amplitude from 15 µm to 30 µm did not effect the forces considerably. The same was true for frequency. This, too, had an upper limit for the optimum performance for a certain speed. At 335.24 mm/s, increasing the frequency from 10 kHz to 20 kHz and then to 30 kHz, reduced forces in the cutting tool. But a further increase in the frequency from 30 kHz to 40 kHz increased the forces by approx. 10%.

The numerical study was also helpful to predict forces in the cutting tool with the increasing feed rate. Three different feed rates 0.1 mm, 0.2 mm and 0.3 mm were considered. Increasing the feed rate from 0.1 mm to 0.2 mm resulted in nearly 127% increase in the cutting force in the cutting tool, whereas a further increase of the feed rate from 0.2 mm to 0.3 mm resulted in 110% increase in cutting tool forces. A feed rate of 0.1 mm is therefore recommended as an optimum value at a cutting speed of 335.24 mm/sec as it resulted in lower forces in the cutting tool.

The effect of friction was also studied in order to estimate the influence of lubrication on UAT. Friction has a different effect on forces for different feed rates. At a feed rate 0.2 mm and a cutting speed 335.24 mm/s, changing friction coefficient from 0 to 0.5 resulted in a 67% increase of forces, while at the same cutting speed but with a feed rate 0.3 mm it only resulted in a 33 % increase. Although simulations predicted an improvement in the cutting forces when lubrication is introduced, it is difficult to introduce the lubricant in experiments in the area of tool-workpiece
interaction zone due to the constant contact. Due to the non-regular nature of contact in the case of UAT, the lubricant can still reach the tool-workpiece interaction zone. Mitrofanov [6] experimentally observed a decrease of forces up to 45% in case of UAT when lubrication was introduced.

Simulations also allowed calculation of the magnitude of cutting forces for different cutting speeds. In the case of UAT cutting forces increase with an increase in the cutting speed from 167 to 504.86 mm/sec. An increase of 76% was observed when cutting speed was increased from 167 to 335.24 mm/s. But when the cutting speed was increased from 335.24 to 504.86 mm/s, the increase was only 33%. The increase of cutting forces with an increased cutting speed was also verified experimentally by Mitrofanov [6]. A non-permanent contact in the case of UAT is the reason for its force improvement as compared to CT but as the cutting speed is increased in case of UAT, the contact time between the tool and the workpiece increases which results in an increase of cutting forces.
9 CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

9.1 Conclusions

The thesis was aimed at developing a coupled thermomechanical 3D Finite Element model of VAT in order to investigate the different stress variables and thermal processes in the tool-chip interaction zone and to suggest optimal cutting parameters for ultrasonically assisted turning processes of advanced engineering materials (in this case – nickel-based alloy INCONEL 718). Various experiments have been carried out to understand the difference between UAT and CT in terms of the chip shape, hardness of the surface layer and surface roughness. A series of simulation studies were performed showing the development of the FE model from a simple 3D case of the orthogonal turning to a more complex and adequate model of oblique and incremental turning.

Experimentation

Experiments were planned as a continuation to those already performed by other researchers on the in-house UAT setup. The studies performed included the high speed filming and experimental force analysis.

Experimental studies with high speed (HS) filming helped in the in-depth analysis of both CT and UAT. Results from HS filming showed the chip formation in a detailed view with a very high frame rate (25,000 frames – 68,000 frames). Results from HS filming were very helpful in development of practical 3D simulations of UAT: observation of results from high speed filming provided necessary information on orientation of the cutting tool insert with regard to the workpiece in simulations. The results helped in observing carefully the geometry of forming chip, contact length and radius of curvature. These results also provided an insight into understanding of kinematics of turning process.

Results of the experimental force analysis showed a drop in the tangential forces in the cutting tool in case of UAT when compared to CT. Experiments conducted to
show the influence of the cutting speed on the forces in the cutting tool showed an increase in the cutting forces as the cutting speed was increased.

**Numerical simulations of UAT**

Three-dimensional thermomechanically coupled finite element models of both UAT and CT have been developed. The developed FE models present many benefits in comparison with previous FE approaches:

- Fewer assumptions, as compared to the previous approaches in case of both 2D and 3D, are involved.
- The model can simulate orthogonal, oblique and incremental turning.
- The model predicts the temperature distribution along the cutting edge along the third – through thickness - dimension. This was not possible with the 2D approach.
- No pre-defined line for chip formation was used.
- Automatic re-meshing and re-zoning was involved.
- Simulation of UAT in both tangential and feed direction was possible.
- The model allowed the bending of chip along the third dimension, which is important to predict the true curl of chip on one side especially in simulations of oblique turning.
- The model had no pre-defined notch; this feature allows a study of chip formation from the first contact between the tool and workpiece. It also made possible use of the same model to analyze different tools, which, otherwise, would have required defining a specific notch for every new tool. The absence of a pre-defined notch makes this approach more demanding from the point of view of optimizations.
- A study of the lateral material flow during the turning process was possible.

Modelling was also performed with the use of the specialized turning simulation software DEFORM™. This model also presented some benefits compared with the models developed previously and the modelling approach based on MSC.MARC used in this thesis. Some of the benefits offered by DEFORM™ are:

- Simulations of a real-geometry turning model are possible.
- Simulations of longer turning times are possible.
• Modelling of straight, curved and even fully circular workpiece can be performed, as the software controls the turning of the tool along the curved periphery of the workpiece.
• Due to the presence of a built-in tool library, a variety of tools with the real form can be used.
• Different variables of turning such as the cutting speed, depth of cut, feed rate etc. can be easily introduced.

The following section will present the conclusions from the FE simulations.

Results of simulations with MARC.MENTAT

A 3D thermomechanically coupled FE approach was used to model UAT with CT being a basis of comparative analysis.

For the same cutting variables (cutting speed, feed rate, depth of cut and friction) and a typical combination of vibration parameters \(f = 20 \text{ kHz}, \ a = 15 \text{ \mu m}\), the calculated cutting force in UAT was 40% of that in CT, which is comparable to the experimental results from [6], where for different feed rates the cutting forces in case of UAT were 40% - 70% less than that of CT.

Within UAT, the magnitude of cutting forces in simulations with friction is some 20% - 25% higher than that in frictionless simulations. The friction had an impact not only on the forces but also a different chip shape was observed when the coefficient of friction increased from 0 to 0.5. Simulations showed that the radius of curvature of the chip under frictionless contact condition at the tool-chip interface was approximately 60% smaller than that for the contact with friction.

Simulations demonstrate a higher temperature in the process zone for UAT as compared to that of CT. Temperatures for UAT in the tool-workpiece interaction zone were 40% higher than those for CT. The higher temperature for UAT leads to a greater thermal expansion of the material that consequently leads to a larger thermal strain in the machined layer. The study of thermal strains indicates they were 30% higher value for UAT compared to CT.

The 3D model allowed the study of heat transfer. The results showed that the maximum temperature was reached somewhere in the middle of the cutting edge, with insignificant falls towards its ends. Convective heat transfer from the surface of the
The tool into the environment was the reason for this spatial temperature gradient along the cutting edge.

Simulations results for CT and UAT also showed an apparent difference in chip shape. The chip for CT was more curled as compared to that of UAT. The difference of chip curl between the two was less in the presence of friction. Limitations in computational power constrained the simulations times at this stage (for MARC.MENTAT). Longer simulations would predict this difference in a more prominent way.

An increase in the peak forces with an increase in the amplitude of vibration was observed. The average cutting forces in the cutting tool - over a single vibration cycle - showed that the overall forces in fact decreased when the amplitude was increased. A 40% decrease in average forces was recorded for an increase in the amplitude from 7.5 \( \mu \text{m} \) to 15 \( \mu \text{m} \), and a further 15% decrease was observed when the amplitude was increased from 15 \( \mu \text{m} \) to 30 \( \mu \text{m} \).

The study of results from simulations for the effect of the changing vibration frequency on the overall forces in the cutting tool show nearly the same peak force for frequencies of 10 kHz, 20 kHz and 30 kHz. But the study of average forces over one complete vibration cycle revealed a drop of 70% in forces for a change in the frequency from 10 kHz to 20 kHz and a further drop of 40% for a frequency increase from 20 kHz to 30 kHz.

Holding other cutting variables constant and changing the feed rate from 0.1 mm to 0.2 mm for UAT brought an increase of 40% in the force level in the cutting tool.

Study of results from simulations with regard to the effect of the vibration direction - changing it from tangential to the feed direction - showed the benefit of the tangential direction as compared to the feed direction in force reduction. On average a force drop of about 60% was observed when the direction of vibration was changed from the feed direction to tangential. The difference in forces for the two directions of vibration was due to the fact that for vibration in the tangential direction the tool is in contact with neither the workpiece nor the chip for a longer period of time, whereas, for vibration in the feed direction, the tool loses its contact with the workpiece, but maintains a contact with the chip.

The effect of the rake angle (being changed from \(-5^\circ\) to \(0^\circ\) and then to \(5^\circ\)) showed a vivid difference in forces in the cutting tool. The stress distribution plot showed greater stress in case of \(-5^\circ\) as compared to the other two cases. A 31% increase in
forces was recorded when the tool's rake face angle changed from 5° to 0°. A further increase of 20% was observed for the change in the rake angle from 0° to -5°. The change in the rake face angle also influenced the chip shape.

No apparent force difference was observed for the introduction of tool edge radius into consideration. Two different tool edges were considered for a UAT case, i.e. 0 and 0.02 mm. The forces in the cutting tool increased by only 12% when the tool edge radius was changed from 0 to 0.02 mm. The tool edge radius did affect the chip shape, with the chip without the edge radius being slightly more curled as compared to when the tool edge radius was 0.02 mm. A tool with a tool edge radius is recommended to be used rather than a tool without tool edge radius, as the area of initial contact in case of tool with a tool edge radius is more than the one without tool edge radius, resulting in a better tool life.

Results of simulations from DEFORM™

After successive attempts with a simplified 3D model in chapter 7, finally, practical 3D thermomechanically coupled models of both CT and UAT were developed with specialized turning simulation software DEFORM™. This model reflects the actual features of a turning setup more closely. With the development of such a model, an attempt had been made to perform simulations in order to select a better combination of parameters for turning process. This section presents the results from simulations performed in DEFORM™.

All the three principal possibilities of cutting tool movement in turning discussed so far in this thesis, i.e. CT, UAT with vibration in the tangential direction and UAT with vibration in the feed direction were modelled with this FE code. The difference in cutting forces was studied on the basis of comparison of the forces in CT and the average forces in the other two cases. A force drop of 46% was observed for transformation of turning mode from CT to UAT with vibration in the feed direction and a further drop of 30% was observed when the turning mode was transformed from UAT with vibration in the feed direction to the vibration in the tangential direction.

Comparing the average forces in the cutting tool for a single vibration cycle to study the effect of amplitude in UAT simulations with vibration in the tangential direction shows that a 15% drop in the value of forces was caused by the increase in the vibration amplitude from 7.5 μm to 15 μm, while the forces remained nearly
constant for a further increase in the amplitude from 15 μm to 30 μm. So, the amplitude had a specific optimum value for given cutting conditions. If the values of cutting parameters such as the cutting speed, depth of cut and feed rate are changed, that will require finding a new optimum value of the amplitude.

The effect of vibration frequency was analyzed, keeping all the other variables of turning constant. A range of frequency values was selected, i.e. 10 kHz, 20 kHz, 30 kHz and 40 kHz. Results demonstrated a drop of 35% in the average cutting forces over time for an increase in the frequency magnitude from 10 kHz to 20 kHz. A further drop of 38% was recorded when the frequency increased from 20 kHz to 30 kHz. When the frequency increased further from 30 kHz to 40 kHz the forces increased by 10% rather then decreasing. It can be concluded that the frequency, too, has an optimum value for a specific set of cutting variables. Changing the cutting speed, depth of cut and feed rate will require calculating a new optimum frequency.

The effect of feed rate in UAT on the overall forces in the cutting tool averaged over a vibration cycle, revealed a 127% increase in the force when the feed rate increased from 0.1 mm to 0.2 mm and a further increase of 110% for an increase in the feed rate from 0.2 mm to 0.3 mm.

The study of the effect of friction - for a frictionless case and the one with friction - at two different feed rates shows that at lower feed rates (0.2 mm) there is a difference of 67% between the cases with coefficients of friction 0 and 0.5. An increase of 33% was recorded at a feed rate of (0.3 mm) when the coefficient of friction was increased from 0 to 0.5. These results are in good agreement with the results from experiments where the reduction in the cutting force with application of lubricant on the surface of workpiece for UAT was from 15% to 45% for different test runs [6].

The comparison of average cutting forces for a single vibration cycle caused by three different cutting speeds - 167 mm/s, 335.24 mm/s and 504.86 mm/s – was studied. An increase of 76% was observed for an increase in the cutting speed from 167 mm/s to 335.24 mm/s and an increase of 33% was recorded for an increase in the cutting speed from 335.24 mm/s to 504.86 mm/s.

Extensive case studies allowed determination of a better combination of parameters for UAT based upon the current study for turning of INCONEL 718 and within the studied envelop of cutting parameters it can be a system with ultrasonic vibration applied in the tangential direction, ultrasonic amplitude 15 μm, frequency 30 kHz, feed rate 0.1 mm, and a constant supply of lubricant. A tool with a positive rake
angle and with some tool nose radius will result in better surface finish, a lower force on the tool insert; such tool will also have a longer life. Although lower cutting speeds have resulted in lower forces over the cutting tool, but to make the system more productive, a cutting speed of 335.25 mm/s is recommended.

9.2 Future work

The need for further work follows from the results of this thesis.

Experimental work:

- Thermal stress analysis (TSA) of the turning process can be performed by designing a special fixture for the camera movement which makes it suitable for a use with turning applications, and also by planning experiments in which it is possible to get a visual access to the tool-workpiece interaction zone. An example of this type of tests is turning of tubular specimens of INCONEL 718 with the lathe.
- X-ray diffractometry of the workpiece specimens resulting after turning with both CT and UAT can be performed to estimate the residual stresses in the machined layer.
- A complete set of experiments can be planned using the suggested combination of parameters from simulations performed in this thesis. This can provide an additional comparison for the results of simulations from this thesis.
- It would be useful to compare Zygo® interferometry results for surfaces machined with UAT both in the tangential as well as the feed direction as well as to study the effect of feed rate, depth of cut, friction and cutting speed on the surface roughness.

Numerical simulations:

- FE modelling with a finer mesh over the whole workpiece can allow the study of the surface roughness.
- An improved re-meshing algorithm in FE software can allow prediction of more consistent forces and the application of boundary conditions directly on the deformable bodies as well.
- With an improvement in computational power, it will be possible to run cutting tool simulation for longer durations and also to perform incremental turning.
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