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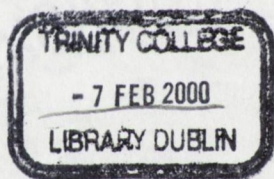
An Experimental and Numerical Investigation into the Machining of Inconel 718 using Coated Cemented Carbide Cutting Tools.

Toman MacGinley

Submitted in fulfilment of the requirements for the award of the degree of Doctor of
Philosophy to the University of Dublin, Trinity College, 30th September, 1999.

Supervisor: **Professor John Monaghan**

The work presented in this thesis was conducted at the Department of Mechanical and
Manufacturing Engineering, University of Dublin, Trinity College, Ireland.



Thesis
5324.

Declaration

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Abstract

The nickel-base superalloy Inconel 718, a tough material capable of maintaining its strength at high temperatures and resisting corrosion in various environments, can be classified as a “difficult to machine” material. Yet despite advances in near net shape casting, forging and powder metallurgy, machining still plays a major role in the manufacture of aircraft gas turbine engines. Traditional machining techniques are still used, since the metal removal rates are higher than those of non-traditional techniques.

The objective of this study is to analyse the effect of the machining process on a nickel base superalloy, Inconel 718, using coated carbide cutting tools, and provide details of the stress distributions within the tool and possible explanations for coating failures at various key points on the coated tool. The research combined an experimental and numerical test program with the intention that the orthogonal machining process could be modelled using finite element analysis (FEA). A secondary objective was to analyse the effect of the decrease in the cutting forces for coated tools, as cutting speeds increase.

FE and experimental analyses were directed towards the finish machining of Inconel 718 using various grades of coated and uncoated cemented carbide inserts. The primary emphasis of the experimental phase was aimed towards maximising the tool life of the inserts with different combinations of cutting speeds, depths of cut, feed rates, lubricants and chip breaking geometries. The influence of the rake face, nose angle and flank face were examined using FEA. Uncoated, single-coated, double-coated and multi-coated cemented carbides were simulated to see the effect each combination had on various key machining parameters. Good correlation was obtained between the numerical and experimental results.

A multi-coated carbide cutting tool was shown to be the most suitable tool for both the experimental and FE tests, due to its superior resistance to abrasion and flank wear, as well as having good resistance to the high temperatures and stresses that are caused by the machining of Inconel 718.

Contents

<u>Declaration</u>	I
<u>Acknowledgements</u>	II
<u>Abstract</u>	III
<u>Contents</u>	IV
<u>Nomenclature</u>	IX

Part I.

<u>1.0 Introduction</u>	2
<u>1.1 Machining, Tools and Superalloys</u>	2
<u>1.2 Objectives of the Thesis</u>	5

Part II.

<u>2.0 Literature Review on Workpiece and Tools</u>	7
<u>2.1 Inconel Alloy 718</u>	7
<u>2.1.1 Superalloys and Machining</u>	7
<u>2.1.2 Distortion and Stress Relieving</u>	9
<u>2.1.3 Microstructure</u>	10
<u>2.1.4 Classification of Nickel Superalloys</u>	12
<u>2.1.5 Shear Localised Chip Formation</u>	12
<u>2.1.6 Built-up-edge (BUE) and its influence on machinability</u>	15
<u>2.2 Introduction to Tools and Coatings</u>	16
<u>2.2.1 Two Processes Predominate</u>	17
<u>2.2.2 The Role of TiC, TiN, TiCN and Al₂O₃ Coatings</u>	21
<u>2.2.3 Increasing Popularity of the CVD-coating process</u>	24
<u>2.3 Tool Wear and Tool Life</u>	27
<u>2.3.1 Tool Failure Criteria</u>	29
<u>2.4 Contact Stresses</u>	34
<u>2.4.1 Stress Distributions in Thin Layer Coatings</u>	34
<u>2.4.2 Normal Stress Distributions in Thin Layer Coatings</u>	36

2.4.3 Normal and Tangential Stress Distributions in Thin Layer Coatings	38
3.0 Literature Review on Machining	40
3.1 Conventional Machining of Inconel 718	40
3.1.1 Turning Inconel 718	40
3.2 Finite Element Modelling of Contact Stress Analysis	43
3.3 Finite Element Modelling of Machining	46
Possible Variations.....	46
 Part III.	
4.0 Experimental Procedures	56
4.1 Material Characterisation	56
4.2 Mechanical Testing	56
4.2.1 Uniaxial Tension / Compression Tests	57
4.2.2 Strain rate sensitivity tests.....	58
4.3 Cemented Carbide Cutting Tools	59
4.3.1 Machinability Tests.....	60
4.3.2 Cutting Force Measurement.....	62
4.3.3 Surface Roughness Measurement	63
4.3.4 Tool Wear.....	64
5.0 Finite Element Method	66
5.1 Modelling the multi-coated tools (ANSYS).....	68
5.1.1 Yield Criteria.....	68
5.1.2 Flow Rule	69
5.1.3 Hardening Rule	71
5.2 Modelling the orthogonal metal cutting process (FORGE2)	71
5.2.1 Method of Analysis	71
5.2.2 Finite Element Analysis of Orthogonal Machining	72
5.2.3 Solution Technique of Finite Element Analysis (FORGE2).....	74
5.2.4 Contact Modelling.....	75
5.2.5 Modelling Tool Wear.....	78
5.3 Finite Element Coating Models (ANSYS)	78
5.4 Finite Element Metal Cutting Models (FORGE2)	81

5.4.1 Cutting Tool Geometry variation	83
5.4.2 Rigid Tools.....	84
5.4.3 Uncoated Deformable Tools	84
5.4.3 Single-Coated Deformable Tools.....	85
5.4.3 Multi-Coated Deformable Tools	85

Part IV.

6.0 Experimental Results and Discussions	88
6.1 Tool Wear	88
6.2 Cutting Forces	93
6.3 Surface Finish	96
6.4 Chip-Breaking Carbide Tools.....	98
6.4.1 Influence of Chip-Breakers	98
6.4.2 Influence of Feed Rate	99
6.4.3 Influence of Lubricant.....	100
6.5 Summary of Experimental Results.....	101
7.0 Analytical Results and Discussions	103
7.1 Stress Distributions in Uncoated Substrates.....	103
7.2 Stress Distributions in Single-Coated Substrates	103
7.2.1 Effect of thickness ratio on maximum tangential stress for single-coated substrates.....	105
7.2.2 Variation of tangential stress, σ_x , through single-coated substrates	107
7.2.3 Variation of σ_x through thin single-coated substrates ($[t/a]=0.2$).....	107
7.2.4 Variation of σ_x through thick single-coated substrates ($[t/a]=0.8$)	110
7.2.5 Variation of σ_x along top of single-coated tool.....	112
7.2.6 Variation of σ_x along coating/substrate interface.....	114
7.2.7 Effect of thickness ratio on maximum shear stress, $\tau_{xy(max)}$, for single-coated substrates	115
7.2.8 Variation of τ_{xy} along single-coated substrate interfaces	116
7.2.9 Summary of Stresses within Single-Coated Tools.....	118
7.3 Stress Distributions in Double-Coated Substrates.....	119

7.3.1 Effect of thickness ratio on maximum tangential stress, $\sigma_{x(\max)}$, for double-coated substrates	119
7.3.2 Variations of σ_x through double-coated substrates ($[t/a]=0.2$)	122
7.3.3 Variations of σ_x through double-coated substrates ($[t/a]=0.8$)	124
7.3.4 Variation of σ_x along the interfaces of double-coated substrates	127
7.3.5 Effect of thickness ratio on maximum shear stress, $\tau_{xy(\max)}$, for double-coated substrates	129
7.3.6 Variation of τ_{xy} along double-coated substrate interfaces	130
7.3.7 Summary of Stresses within Double-Coated Tools	132
7.4 Stress Distributions in Multi-Coated Substrates	133
7.4.1 Effect of thickness ratio on maximum tangential stress, $\sigma_{x(\max)}$, for multi-coated substrates	133
7.4.2 Variations of σ_x through multi-coated substrates ($[t/a]=0.2$)	134
7.4.3 Variations of σ_x through multi-coated substrates ($[t/a]=0.8$)	135
7.4.4 Variation of σ_x along interfaces of multi-coated substrates	136
7.4.5 Effect of thickness ratio on maximum shear stress, $\tau_{xy(\max)}$, for multi-coated substrates	138
7.4.6 Variation of τ_{xy} along multi-coated substrate interfaces	139
7.4.7 Summary of Stresses within Multi-Coated Tools	140
8.0 FE Modelling of Orthogonal Metal Cutting	141
8.1 FE Model Verification with Experimental Data	141
8.2 FORGE2 Meshing difficulties during Orthogonal Machining	143
8.3 Variation in Cutting Tool Angle	145
8.3.1 Rigid Tools without chipbreaker	145
8.3.2 Rigid Tools with -QM type chipbreaker	148
8.3.3 Deformable Tools without chipbreaker	150
8.4 Variation of machining parameters on Metal Cutting Simulations	152
8.4.1 Effect of Depth of Cut increasing during Metal Cutting	152
8.4.2 Effect of Cutting Speed increasing during Metal Cutting	153
8.4.3 Effect of Coolant during Metal Cutting	154
8.5 Temperature Distribution in workpiece over length of cut	155
8.6 Effect of single-coated cutting tools on machining parameters	157

8.6.1 5µm single-coated WC substrates.....	157
8.6.2 Maximum stress locations for single-coated tools	165
8.6.3 10µm single-coated WC substrates.....	167
8.7 Effect of double-coated cutting tools on machining parameters	171
8.7.1 Double-coated WC substrates	171
8.8 Effect of Cutting Speed on Coated Deformable Tools.....	177
8.8.1. Single-coated TiN substrate	177
8.10 Effect of Chipbreakers on Coated Deformable Tools	179

Part V.

9.0 Conclusions.....	181
9.1 Experimental Conclusions.....	182
9.2 FEA of Hertzian Pressures Conclusions.....	183
9.3 FEA of Machining Conclusions	184
9.3 Recommendations for Future Research.....	185

References.....	186
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Appendix 1.....	193
Appendix 2.....	195
Appendix 3.....	197
Appendix 4.....	199

Nomenclature

Symbol	Unit	Definition
ϕ	°	Shear angle
γ	°	Rake angle
β	°	Clearance angle
μ	----	Friction coefficient
ε	----	Strain
$\dot{\varepsilon}$	----	Strain Rate
$\bar{\varepsilon}$	----	Effective Strain
$\dot{\bar{\varepsilon}}$	----	Effective Strain Rate
δ	mm	Displacement
ρ	kg/m ³	Density
η	----	Deformation efficiency
Ω	----	Continuum volume
Φ	----	Variational Functional
τ_l	N/mm ²	Shear stress in plane of deformation
ν_c	----	Poisson's ratio of coating
σ_e	N/mm ²	Effective stress
ϕ_f	----	Heat flux into chip material
σ_{ij}	N/mm ²	Deviatoric Stress
ϕ_l	----	Heat flux into tool material
σ_n	N/mm ²	Normal stress
σ_o	N/mm ²	Hydrostatic Stress
ν_s	----	Poisson's ratio of substrate
ΔV	mm/s	Change in Velocity
σ_x	N/mm ²	Tangential stress
τ_{xy}	N/mm ²	Shear stress on the Y plane in the X direction
σ_y	N/mm ²	Yield stress
[J]	----	Jacobian matrix
[t/a]	----	Thickness ratio

[x/a]	----	Distance along interface ratio
2D	----	Two dimensional
3D	----	Three Dimensional
a	mm	Contact half-width
Al ₂ O ₃	----	Alumina
b	kg/s ^{2.5} K	Effusivity
b ₁	kg/s ^{2.5} K	Tool material effusivity
b ₂	kg/s ^{2.5} K	Workpiece material effusivity
BUE	----	Build Up Edge
C	----	Strength Coefficient
c	J/kgK	Specific heat capacity
CTE	----	Coefficient of thermal expansion
CVD	----	Chemical vapour deposition
D	----	Critical Separation Distance
DOC	mm	Depth of Cut
e	----	Base Natural Log
E*	GPa	Effective Youngs modulus
E _{bottomcoating}	GPa	Youngs modulus for bottom coating
E _c	GPa	Youngs modulus of coating
EDM	----	Electro-Discharge machining
El%	----	Percentage elongation
E _{middlecoating}	GPa	Youngs modulus for middle coating
E _s	GPa	Youngs modulus for substrate
E _{substrate}	GPa	Youngs modulus for substrate
E _{topcoating}	GPa	Youngs modulus for top coating
F	N	Force Applied
f	Mm/rev	Feed Rate
F _a	N	Axial Cutting Force
F _c	N	Resultant Cutting Force
FE	----	Finite element
FEA	----	Finite Element Analysis
FEM	----	Finite element method
F _r	N	Radial Cutting Force

F_t	N	Tangential Cutting Force
G	GPa	Modulus of rigidity
h	W/mK	Convection coefficient
HV	HV	Vickers hardness
J_2	----	Yield Criterion
k	W/mK	Thermal conductivity value
K	----	Strength Coefficient Constant
k_1	N/mm	Stiffness
K_1	MPa	Consistency
KB	mm	Crater width
KM	mm	Crater centre distance
KT	mm	Crater depth
m	----	Sensitivity to hardening index
M_R	----	Machinability rating
MRR	----	Metal Removal Rate
n	----	Strain Hardening Index Constant
P	N/mm^2	Pressure
P_n	N/mm^2	Normal contact pressure
P_o	N/mm^2	Maximum applied pressure
PVD	----	Physical vapour deposition
Q	----	Activation Energy for self-diffusion
Q	mm^3/sec	Metal removal rate
r	mm	Radius of cylindrical/spherical indenter
R	----	Universal gas constant
r_ϵ	mm	Nose radius
R_a	μm	Arithmetic mean roughness value
R_t	μm	Roughness height
t	μm	Coating thickness
T	$^{\circ}C$	Temperature
t_1	mm	Undeformed chip thickness
t_2	mm	Deformed chip thickness
TiC	----	Titanium Carbide
TiCN	----	Titanium Carbon Nitride

TiN	----	Titanium Nitride
T _o	°C	Exterior area temperature
U	----	Wear Function
UTS	MPa	Ultimate tensile strength
V ₆₀	----	Specific cutting speed
VB _B	mm	Average flank wear width
VB _{Bmax}	mm	Maximum flank wear width
VB _N	mm	Maximum notch wear
V _c	mm/s	Cutting Velocity
V _t	mm/s	Tangential Sliding Velocity
V _x	mm/s	Velocity in x-direction
V _y	mm/s	Velocity in y-direction
W	----	Internal heat generated by plastic deformation
WC	----	Tungsten Carbide
x	mm	Distance from centre of contact
y	mm	Depth below contact surface
YS	MPa	Yield stress

PART I

Introduction

1.0 Introduction

1.1 Machining, Tools and Superalloys

Machining has become indispensable to the modern man. It is used directly or indirectly in the production of almost all the goods and services being created worldwide. It is the basis of everything manufactured - paper, drugs, clothes, computers, cars, spacecraft etc. Wherever metal is used in any man-made object, one can be sure that it will have reached its final stage through processing with machine tools. In the USA, the yearly total cost associated with metal removal, including the cost of tooling, labour and capital costs of the machinery, has been estimated at approximately 10% of GNP [1]. However last year (1998), the total machine tool consumption figure in the USA was \$5.31 billion alone [2]. Hence any small improvement in aspects of the machining process can be economically significant.

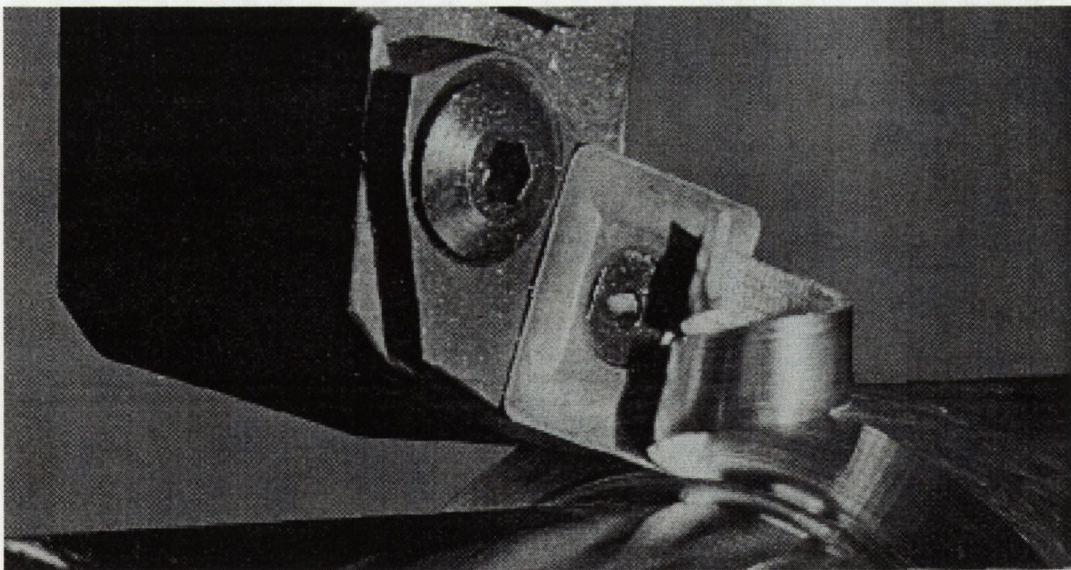


Figure 1.1a. Single Point Machining

The term "machining" is often taken to mean "turning with a single point tool" as this is perhaps the most fundamental metal cutting operation. A picture of a turning operation is shown in Figure 1.1a. It is only since the turn of the century that the chip formation process and the mechanisms of tool wear have been partially understood [3]. To

date no general theory of metal cutting exists. Indeed, the main role of theory in metal cutting has often been not so much to predict what will happen, but to understand what is observed in order to reduce the number of trials required to reach the desired objective [1]. The lack of a predictive theory is due to the complexity of the machining process and the large number of input variables associated with it. Great importance is placed on the workpiece as materials can vary greatly in the ease or difficulty with which they can be machined. Some of the most difficult materials to machine are found in the aerospace industry where the high strength, high creep resistant alloys are needed for use in gas turbine engines and other components. These alloys are commonly termed "Superalloys".

Superalloy development has paralleled the evolution of aircraft jet engines ever since the late 1930s, although superalloys are also employed in other areas, including the chemical and nuclear industries. Great advances have been made in superalloy performance through both alloy composition and process development. The development of vacuum metallurgy and remelting technologies permit high alloy compositions, and by controlling the impurity levels, allow corresponding increases in operating temperatures and stresses. Some modern superalloys are capable of operating at almost 90% of their absolute melting points [4]. These improvements in performance, together with designs which utilise the materials at the very limit of their capability, increase the problems in machining them.

The numerous series of modern superalloys are broadly classed into three families; nickel-base superalloys, cobalt-base superalloys and iron-base superalloys. Nickel-base superalloys are produced under the tradenames such as "Inconel", "Nimonic", "Monel" and "Nilo". Inconel 718 is currently one of the most commonly used nickel-base superalloys in production and Herb Eiselstein is often credited with its development in the 1950s [5]. This alloy now accounts for up to 45% of wrought nickel-base alloy production and 25% of cast nickel-base products.

The primary uses of these nickel alloys are in aircraft gas turbines, steam turbine power plants, reciprocating engines, medical applications, space vehicles, heat-treating equipment, nuclear power stations, the chemical and petrochemical industries, pollution control equipment and coal gasification and liquefaction systems [6]. In aeroengines, it is

used for such components as blades/buckets, discs, shafts, casings and fasteners as shown in Figure 1.1b.

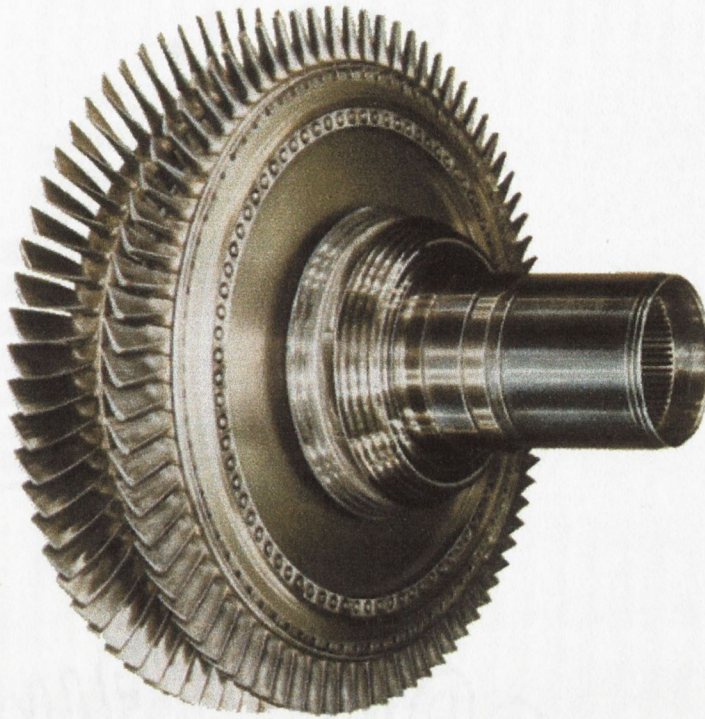


Figure 1.1b. Turbine from Aircraft Aeroengine.

As already stated, Inconel 718 and its family of nickel-base superalloys are some of the most difficult materials to machine. It has been suggested that to machine a component made of Inconel 718 would be 34 times more expensive than machining the same component made of 7075-T6 Aluminium alloy, not including the cost of work materials, cutting tools or heat treatment [7]. Optimum cutting speeds for superalloy machining are quite low - about 10% of those used for common steels. Yet, despite advances in near net shape casting, forging and powder metallurgy, machining still plays a major role in the manufacture of aircraft gas turbine engines. Of approximately 25,000kg of workpiece metal involved in making a typical engine, only about 3,600kg of it remains in the final product after machining. It has been shown that in some cases, machining can account for up to 90% of the cost of turbine blades [5]. Traditional machining techniques are still used however, since the metal removal rates are higher than those of non-traditional techniques.

High tool wear in finish machining of superalloy components can necessitate multiple tool changes which can result in surface quality defects. Industry therefore needed

a tool that was able to successfully machine these superalloys. The most important tool development in the 1960s was the introduction of coated carbide tools which occurred around 1969. The initial process was the deposition of a titanium carbide (TiC) thin film onto a steel cutting grade carbide tool [1]. The thickness of this film was only 0.005mm. The effect of this coating was to enable low-alloy steels to be machined at higher values of temperatures (i.e. at higher speeds and feeds) without excessive cratering.

Although other carbides and nitrides have been used as coatings to a limited extent, there are four which are in widespread commercial use. These are titanium carbide (TiC), titanium nitride (TiN), titanium carbonitride (TiCN) and alumina (Al_2O_3). In addition to chemical vapour deposition (CVD) of carbides, nitrides or oxides of titanium and alumina, other methods of altering the chemistry of tool surfaces are being explored in laboratories throughout the world.

1.2 Objectives of the Thesis

The present research combines an experimental and analytical test program with the intention of studying the effect of coatings on carbide tools whilst machining Inconel 718. The initial experimental work was required to obtain thermo-mechanical data needed for the finite element modelling (FEM) and also to provide results to which the FE results could be compared.

It is the author's intention to analyse the effect of the machining process on coated carbide cutting tools and provide details of the stress distributions within the tool and possible explanations for coating failures at various key points on the coated tool. The effect of different tool geometries along with machining parameters will be analysed to determine the effect, if any, they have on the coated insert. A secondary objective was to analyse the effect of the decrease in the cutting forces for coated tools as cutting speeds increase.

PART II

Literature Review

2.0 Literature Review on Workpiece and Tools

2.1 Inconel Alloy 718

Inconel nickel-chromium alloy 718 is a high strength, corrosion-resistant material for use at temperatures which range between cryogenic (-252°C) and 926°C. This precipitation hardenable alloy can be readily fabricated - even into complex parts. Its welding characteristics, especially its excellent oxidation resistance, results in a resistance to postweld cracking [8]. The ease with which Inconel 718 can be fabricated, combined with good tensile, fatigue, creep, and rupture strength, has resulted in its use in a wide range of applications. Examples of these are for gas turbine components, liquid rockets, cryogenic tankage, pump bodies and parts, in aerospace, nuclear engineering, and oil and gas well tooling [9].

2.1.1 Superalloys and Machining

Nickel-base superalloys, are tough materials capable of maintaining their strength at high temperatures and resisting corrosion in various environments, they can be machined by techniques employed for iron-base alloys. Certain requirements are imposed largely by the high yield and shear strengths of the alloys, their tendency to work-harden, and their "gumminess" in some conditions. Table 2.1.1 below compares some properties of Inconel 718 with medium-carbon steel (SAE 1045) and ductile cast iron. The tensile strength, hardness, and ductility properties of these materials vary widely.

	YS (MPa)	UTS (MPa)	El. (%)	HV
Inconel 718	1210	1450	20	402
Medium-Carbon Steel	450	730	24	221
Ductile Cast Iron	380	550	6	252

Table 2.1.1. Differences in Material Properties

Inconel 718 has an austenitic matrix and, like the austenitic stainless steels, work-hardens rapidly. The high pressures developed between the tool and workpiece during cutting produce a stressed layer of deformed metal on the surface of the work. The deformation causes a hardening effect which retards further machining. The stresses in this deformed layer not only affect the mechanical properties of the workpiece but can also cause distortions of parts which have small cross sections [10]. One method of reducing work hardening during machining is to stress relieve the material prior to machining by cold working. Cold-drawn material, after stress relieving is always preferable for machining, particularly when a smooth finish is desired. Hot-rolled material is the next in preference whilst solution treated material is least desirable for most applications [11]. However, nickel-chromium and nickel-chromium-iron alloys (Inconel 718) are less abrasive than the common grades of austenitic stainless steels because they have a lower carbon content and therefore contains fewer carbides.

The best surface finish is produced on Inconel alloys by machining them in the age hardened condition. Because the high strength and hardness of aged material prevent heavy cuts, rough machining is done before age hardening. Solution annealing usually improves machinability of age-hardenable alloys by dissolving hard phases. A second method of minimising work hardening is by employing careful machining practices. Sharp tools with positive rake angles, which cut the material instead of pushing it, are required. Feed rates and depths of cut must be sufficient to prevent burnishing or glazing. Tools should not be allowed to rub against the work, either because of improper clearance, or by being allowed to dwell in the cut.

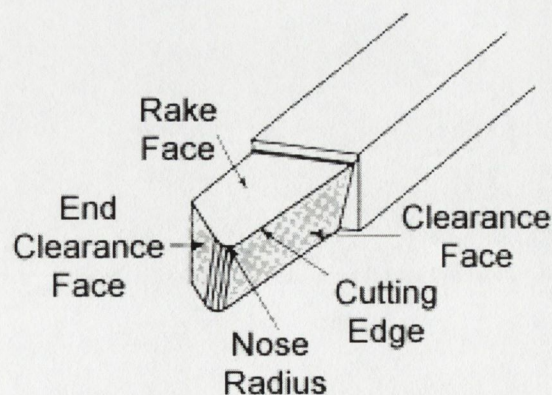


Figure 2.1.1a – Angles and Faces on Cutting Tool [10]

If there is too small an angle between the end of the tool and the work (clearance angle), it will cause the tool to rub against the workpiece once some flank wear occurs. This will cause surface work hardening. On the other hand, too large an angle will weaken the support of the cutting edge. The same principles apply to the end cutting edge angle. This angle supports the nose of the cutting tool by resisting the tool feed forces. The side cutting edge angle is second in importance only to the rake angle. The thickness of the chip is controlled to some extent, by the size of this side cutting edge angle because it also affects distribution of the load on the cutting edge. It also, in conjunction with the rake angle, provides directional control to the chips. The nose radius, which joins the end and side-cutting edges, provides strength to the tool nose and helps dissipate the heat generated by the cut.

The rounded scalloped effect produced by a tool with a nose radius (Figure 2.1.1b) gives a better surface finish, shallower scratches, and a stronger workpiece with fewer tendencies to crack at sharp corners than the notched effect produced by a sharp tool. However, oversized nose radii can interfere with the cutting action, causing tool vibrations which tend to work-harden the machined surface and reduce tool life.

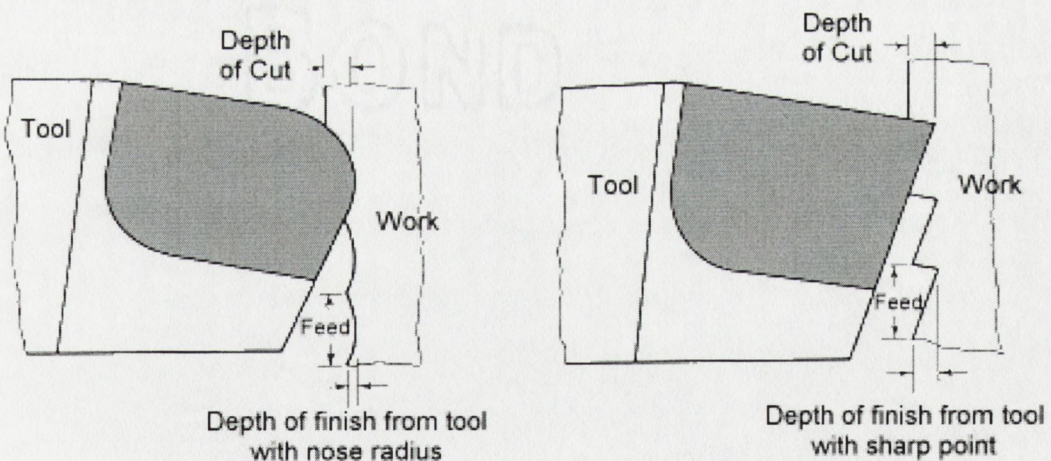


Figure 2.1.1b - Effect on work by tool with nose different radius.

2.1.2 Distortion and Stress Relieving

Even under the best of machining conditions, stress conditions arise which may cause subsequent distortion of the work. For maximum dimensional stability, it is best to

rough out the material to be slightly oversized, stress relieve it, and then finish it to size. Stress relieving has little effect on dimensions but may affect mechanical properties.

2.1.3 Microstructure

Grain size has little direct effect on the machinability of Inconel 718, although it may have some indirect effect, since grain size does reflect thermal processing and a change in the organisation of its structure [8]. Some difference in surface finishing can be detected but this can be minimised by correct cutting procedures. In general, microstructure affects machinability in two ways:

- The presence of graphite or sulphide phases will greatly enhance machinability
- Hard phases such as carbides, nitrides, carbonitrides, oxides and silicates are abrasive and tend to cause rapid tool wear.

Table 2.1.3a classifies the superalloy with respect to element composition.

Element	C	Mn	Si	S	P	Cr	Ni	Mo
% Composition	.036	.12	.11	.001	.007	18.4	52.14	2.97

Element	Co	Cu	Al	Fe	B	Nb	Ta	Ti
% Composition	.31	.05	.57	19.12	.0039	5.09	.01	1.01

Table 2.1.3a. Aged Inconel 718 (AMS 5663)

Inconel 718 is a precipitation-strengthened (Ni_3Nb or γ'') nickel-iron base superalloy that contains about 5 wt% Nb. The FCC austenite matrix, γ , is a solid solution of Fe, Cr and Mo in nickel. Solid solution strengthening is not, however, considered as a major factor for strengthening at elevated temperatures. In view of the low levels of carbon present in this alloy (0.036 wt% C), the presence of carbides is expected to provide very little strengthening. The small amount of carbon present is used as a deoxidant and also forms carbides (typically with Ti, Mo and Nb) and help form small amounts of grain boundary carbides (typically with Cr). The various precipitates which form in Inconel 718 under heat treating conditions are [5]:

- A predominant, ordered, body-centred tetragonal Ni_3Nb metastable precipitate or γ'' phase which is coherent with and uniformly distributed in the γ matrix. About 20 vol% of this phase is present in the alloy under fully solution-treated and aged conditions.
- A small amount of extremely fine, ordered, metastable, face-centred cubic Ni_3 (Al, Ti) precipitate of γ' phase. The γ' precipitate is spherical in shape, rich in Al and Ti, and coherent with the γ matrix.
- An orthorhombic Ni_3Nb phase or δ phase which decorates the grain boundaries. The δ phase is used to pin the grain boundaries and inhibit grain growth during solution treatment.

One of the hardest and most abrasive particles is titanium carbide (TiC), which is present in most of the age-hardenable alloys. Another phase which affects Inconel 718 is niobium carbide (NbC). These carbides are usually present in as-rolled or mill-annealed products. Solution annealing at high temperatures (1100°C and above) is required to dissolve them. Age hardening tends to precipitate greater amounts of niobium and titanium carbides along with chromium carbides. The obstinate nature of these phases, together with their strengthening effect, makes age-hardened material less machinable, although the chip action is improved.

Solution Treatment.	Aging.
• 926°C for 1 hour • Air/water quench to 595°C in 3min • Air cool to room temperature	• 718°C for 8 hours • Furnace cool to 621°C at a rate of 38°C per hour • 621°C for 8 hours • Air cool to room temperature

Table 2.1.3b. Heat Treatment Cycles used for Inconel 718

Since thermomechanical properties appear to play an important role in shear localisation, some basic physical properties are needed. Figures 2.1.3a and 2.1.3b give the variation of different physical parameters with temperature for Inconel 718. This information is relevant for the analytical modelling of shear localised chip formation when

machining materials such as Inconel 718. Table 2.1.3b gives the heat treatment cycles used to age harden the bars used in experimental tests.

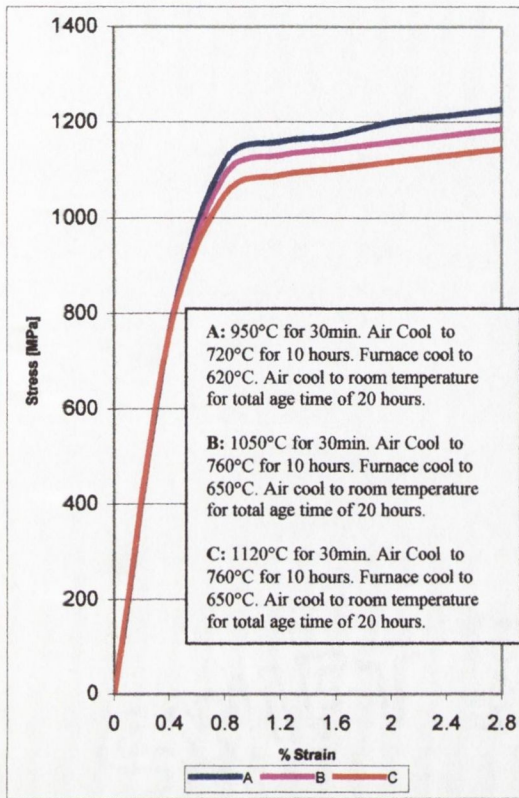


Figure 2.1.3a. Stress-strain curves at three different heat treatments for Inconel 718 [12]

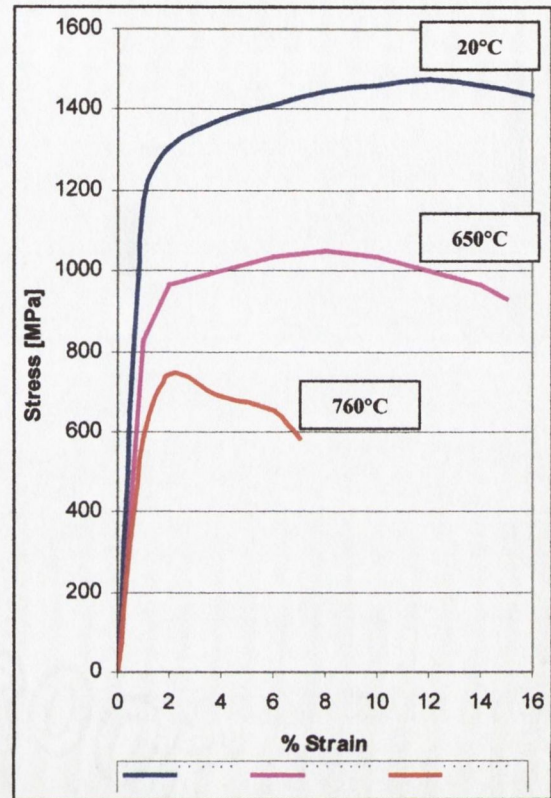


Figure 2.1.3b. Stress-strain curves at three different temperatures for Inconel 718 Solution treated and aged [12]

2.1.4 Classification of Nickel Superalloys

The Inconel family of alloys is characterised by high strength and hardness, particularly when aged. Table 2.1.3a classifies the superalloy with respect to element composition. Material which has been solution annealed and quenched, or rapidly air cooled is in the softest condition and does not machine easily. Thus the best way to machine this family of superalloys is to machine slightly oversized in the unaged condition, age-harden, and then finish to size. Because the age hardening will relieve the residual stresses as a consequence of the initial machining, allowance must be made for possible warpage. Aged material has good dimensional accuracy.

2.1.5 Shear Localised Chip Formation

Figure 2.1.5a illustrates an idealised model of orthogonal machining showing each of the different deformation zones. Zone 1 is the Primary shear zone where shear takes place across the narrow plane AB at an angle ϕ to the workpiece. As the materials flows from zone 1 to zone 2, the material from the shear zone slides up the rake face of the tool.

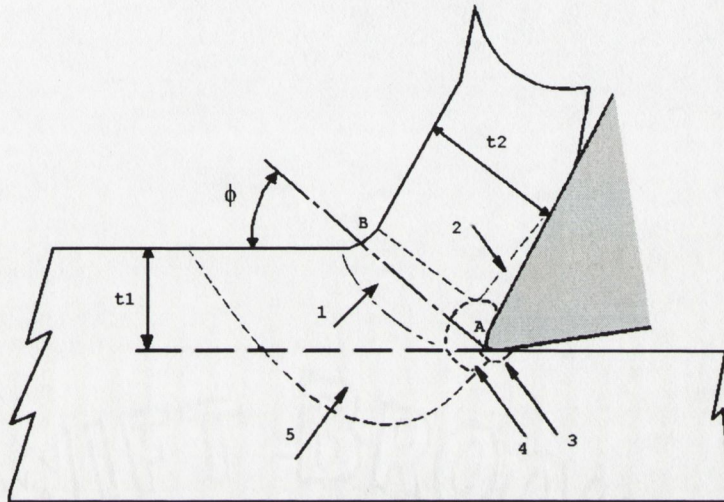


Figure 2.1.5a. Stress Zones in Orthogonal Machining [13]

This causes further energy to be expended in friction by contact as the chip slides relative to the tool. Sticking friction occurs towards the bottom of the rake face and sliding friction at the top. As the tool is not perfectly sharp - it has a small corner radius, rubbing occurs and this generates frictional heating which further affects the stresses in this region, as can be seen happening in zone 3. The round nose of the tool also causes frictional heating of the workpiece materials in zone 4. Strain hardening occurs in zone 5 which causes plastic deformation to spread below the depth of the undeformed chip.

However, this idealised model changes for the case of Inconel 718 and Figure 2.1.5b illustrates a schematic of the shear localised chip formation process, and shows the various surfaces that are involved - described by *Komanduri et al* [14]. This is a two stage process, the first comprises of plastic instability and strain localisation in a narrow band (along line 5) in the primary shear zone, whilst the second involves a gradual build-up of the segment with little deformation. This is caused by the upsetting of the inclined work material by the advancing tool. During this second stage, intense shear takes place in

another band (lines 3 & 4), between the newly formed segment and the one undergoing upsetting.

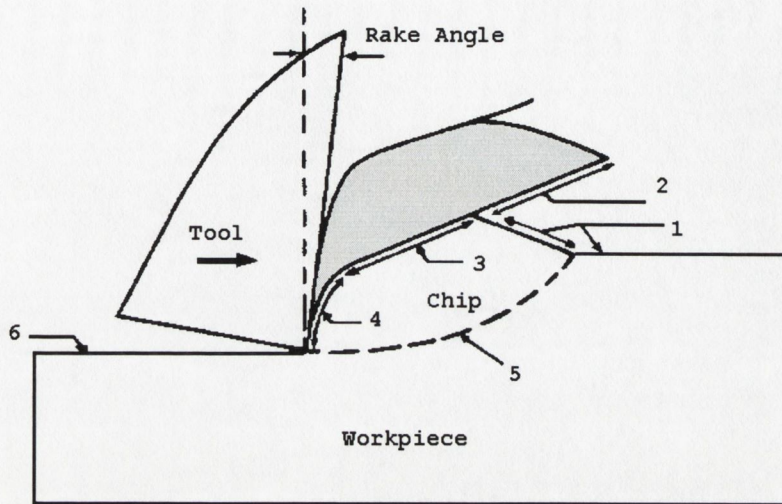


Figure 2.1.5b. Shear localised chip formation [14]

1	Undeformed Surfaces	2	Part of the catastrophically shear failed surface separated from the following segment due to intense shear
3	Intense shear band formed due to catastrophic shear during the upsetting stage of the segment being formed	4	Intense sheared surface of a segment in contact with the tool and subsequent slide on the tool face
5	Intense localised deformation in the primary shear zone	6	Machined surface

With an increase in cutting speed, the extent of the contact between segments decreases until a speed is reached where total shear takes place between the segments. Although this process appears similar to a discontinuous chip formation process, it has consequences which affects the tool life, and this intense shear can be seen above in *Komanduri's* shear localised chip formation diagram.

In discontinuous chip formation, strain localisation in the primary zone ultimately leads to fracture. During the next upsetting cycle, the fractured surface of the segment in contact with the tool face becomes exposed to the atmosphere. This will not be a hot surface because it is a fractured surface as opposed to a highly sheared hot surface on a shear localised chip. In contrast, in a shear localised chip formation, such as with Inconel

718, extensive shear takes place across a narrow band between the segment just formed and the segment undergoing upsetting. Consequently, the shear localised surfaces are extremely hot, freshly generated, and are prevented from interacting with the atmosphere before coming in contact with the tool rake face (line 4). This explains why tool wear and high temperatures are such factors in machining Inconel 718.

2.1.6 Built-up-edge (BUE) and its influence on machinability

A built-up-edge is often formed at the tip of the tool when machining Inconel 718 and is one of the more important factors responsible for surface roughness. The size and shape of the built-up-edge affects the metal cutting process to a large extent. The ploughing action in cutting is caused by the nose radius of the tool or the built-up-edge increasing the nose radius. Thus, this newly modified rake angle of the tool results in a larger negative rake angle being present at the nose radius due to the built-up-edge (Figure 2.1.6).

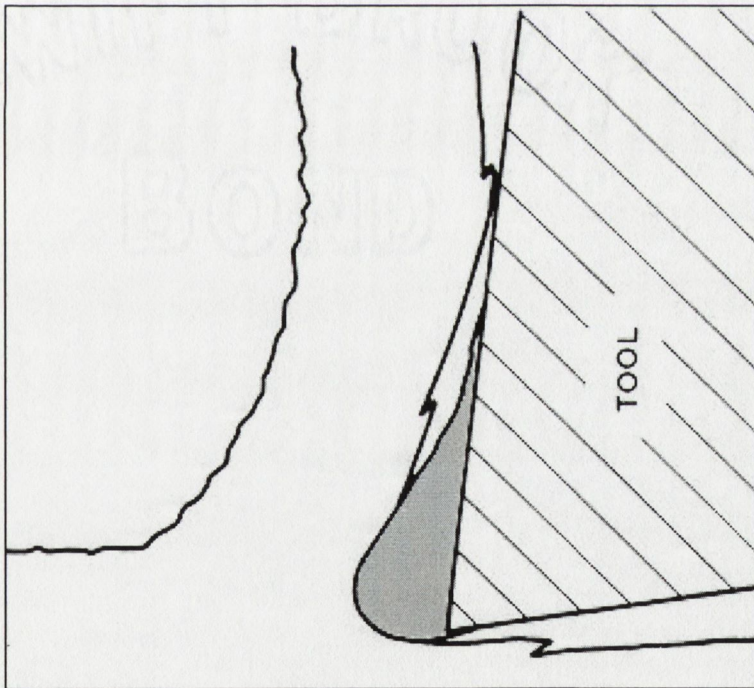


Figure 2.1.6. Effect of BUE showing increase in rake

A built-up-edge occurs when the titanium or aluminium in the workpiece material steals carbon or nitrogen from the cutting insert. This creates a flux that increases the chances for the material to cold weld and adhere to the insert [15]. An alloy with a high tensile strength also contributes to this problem by making it difficult to shear. The

increased pressure this puts on the insert as it pushes its way through the workpiece encourages the alloy's tendency to cold weld. The standard way to eliminate the effects of a BUE is to increase the cutting speed. But with high temperature superalloys, this creates higher temperatures, which in turn causes the workpiece to react even more with the cutting tool.

In single point machining, the work material below the built-up-edge is merely squeezed to the side and pushed over the machined surface. The overflowed metal (side flow), as well as the ridge of the machined surface is distorted by the chip flow. The amount of metal squeezed by the side depends on the size of the built-up-edge. Furthermore, the work material squeezed below the built-up-edge is strain-hardened and this causes the formation of groove wear on the secondary cutting edge. Due to this groove wear - the amount of side flow is further increased. In addition to these indirect effects, the stable build up of material which continues until fracture of the built-up-edge, leaves a saw tooth form on the machined surface along the direction of the cut. The roughness height, R_t , which is the peak to valley height, is due predominantly to this saw tooth form [16]. Thus, the built-up-edge is directly responsible for the surface roughness in the circumferential, and indirectly in the axial direction of the cut.

2.2 Introduction to Tools and Coatings

In metal cutting, a hard tool is forced to pass through a softer workpiece, and this interaction produces a chip. Since the first coating process was introduced, the industry has recognised that applying a further hard surface to a cutting tool will enhance the tool's performance and extend its useful life. A wide variety of coatings are now available, and each has its own unique properties. Titanium nitride (TiN) has strong oxidation resistance and a low coefficient of friction. Aluminium oxide (Al_2O_3) is recognized for its heat-resistant properties, and titanium carbide (TiC) and titanium carbonitride (TiCN) have high hardness and wear resistance. Increased gains in performance and tool life have been achieved with every advancement in equipment and technology and have made it possible to develop new coatings or new coating processes.

The manufacture of the substrate involves a lengthy and complex sequence of operations, in which almost every factor is critical to success [17]. The substrate consists of fine tungsten carbide particles, a hard and abrasive resistant constituent, which is bonded with tough, comparatively soft, metallic cobalt. The tungsten carbide is frequently supplemented or substituted by the carbides of titanium, tantalum, niobium, chromium, vanadium or molybdenum, independently or in combination with each other. The main stages of the manufacturing process are as follows:

- Production of tungsten metal powder
- Preparation of tungsten carbide
- Preparation of alloyed and other carbides
- Add mixture of cobalt to produce grade powders
- Addition of pressing lubricant
- Pressing
- Sintering
- Final sintering

Tungsten Carbide-Cobalt (WC-Co) tool material can only withstand small deformation before fracturing, even at elevated temperatures. Therefore the compressive deformation of the cutting edge of a carbide tool limits the feed rate, cutting speed and the strength of the material being cut. Where it is desirable to resist greater deformations, carbide grades with lower cobalt content should be used [18]. A constant problem in carbide manufacture has been to combine the abrasion/crater resistance of high-titanium grades with the toughness of compositions containing higher cobalt and little or no TiC. The answer has been a compromise with intermediate amounts of TiC, or WC/TiC/Ta(Nb)C grades with a good toughness and a reasonably high resistance to cratering.

2.2.1 Two Processes Predominate

Coatings are without doubt the single most important development in the hardmetal industry in recent years. Much of the original work seems to have come from the Swiss Watch Research Institute where vapour deposited TiC was first intended as a wear resistant

coating on steel watch cases and component [17]. It was subsequently developed by a number of progressive carbide manufacturers as the answer to the steel-cutting problems.

Pure titanium carbide coatings are usually produced by reactive deposition from the gaseous phase whereby, for example, titanium tetrachloride (TiCl_4) vapour is converted to extremely fine-grained titanium carbide crystals. These are "grown" from a substrate of conventional sintered carbide, as a coating only a few micrometres thick. The early equipment used for chemical vapour deposition (CVD) was not unduly elaborate, but with development it has become increasingly complicated. Figure 2.2.1a shows diagrammatically a typical apparatus needed for the chemical vapour deposition of refractory layers, such as TiC , on hardmetals.

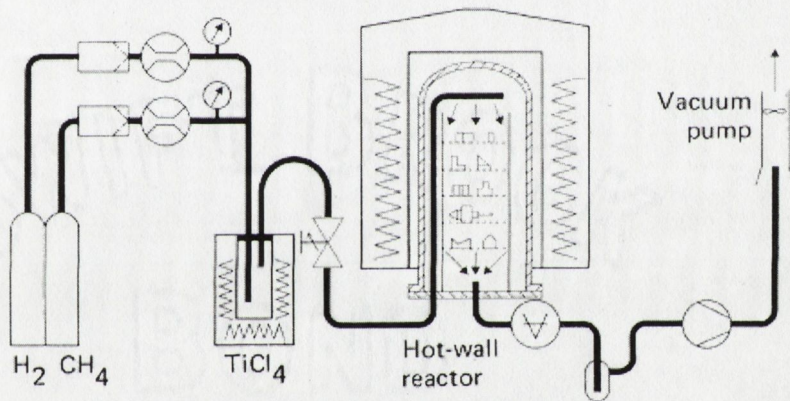
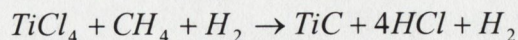


Figure 2.2.1a. The CVD Process.

Titanium chloride (TiCl_4) is evaporated and carried, mixed with gaseous hydrogen and methane (CH_4) into an externally heated hot-wall reactor containing the tools to be coated. At a reactor temperature of around 1000°C the gas mixture decomposes:



which leaves the TiC deposited as a fine-grain layer on the exposed tip surfaces. An early problem with TiC coatings was surface decarburisation of the base hardmetal, giving rise to a thin brittle "eta-phase" layer at the interface between the coating and substrate. This greatly diminished the shear strength, or flaking resistance of the coating substrate interface. Progressive manufacturers have largely overcome this defect. On the basis of

investigations of the effectiveness of TiC coatings, and because a large number of requirements for optimum tool life are not fulfilled by one homogeneous coating, multilayer coatings were developed.

In 1973 a multilayer coating was developed which gave a noticeable increase in tool life [19]. This consisted of a TiC layer adjacent to the substrate with a number of TiCN layers and with a TiN layer on top. For carbonitride coatings, dry nitrogen may be fed as a third gaseous component. By varying gas ratios and other parameters, a series of distinct layers can be laid down. The above CVD coated insert in Figure 2.2.1b has a top coat of thin, gold-coloured TiN on top of a middle layer of TiC and a base layer of coarse Al_2O_3 .

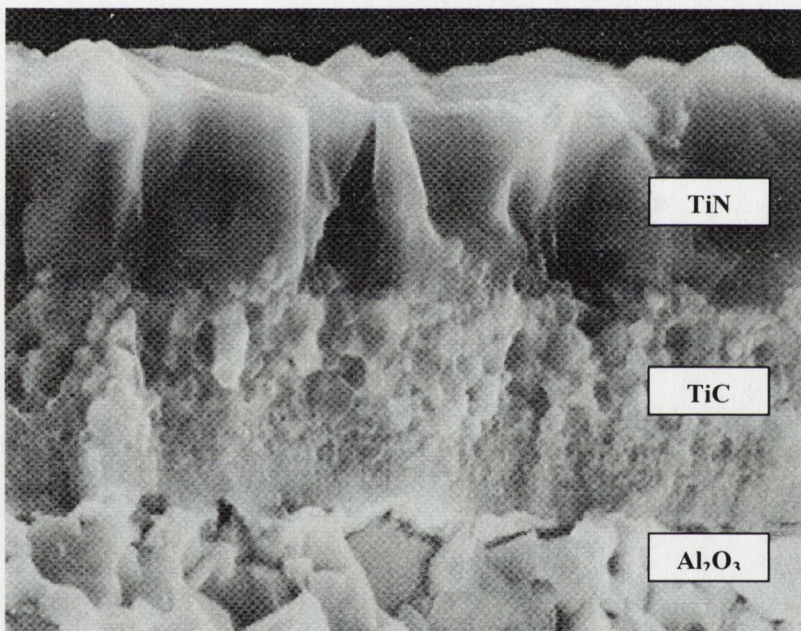


Figure 2.2.1b. Multi Layer Coating. [19]

Further research and development has produced a host of alternative coatings, each with their own deposition chemistry. Titanium nitride, TiN, and titanium carbonitride, TiCN, distinctively gold in colour were followed by alumina, Al_2O_3 , titanium oxide, TiO_2 , and then various combinations and permutations of these with or without the original TiC.

Further coating methods include physical vapour deposition (PVD) - in which the vaporised compound is deposited without chemical reaction - for example sputtering, electrophoresis, electroplating and ion transfer. PVD is the most widely recognised coating

process. Simply put, PVD is a line-of-sight process. This means that the hard coating material is transferred from a source to the tool, travelling in a straight line. PVD coatings are usually applied as a single layer of titanium nitride (TiN), titanium carbonitride (TiCN), or titanium aluminium nitride (TiAlN). PVD coatings have two distinct advantages over CVD coatings. First, the coatings' 3µm to 5µm thickness works very well on sharp-edge tools, where a thicker coating might have trouble adhering and cause a dulling effect. Second, PVD is performed at relatively low temperatures; therefore, it can be used on HSS and tool steels without adversely altering the steel's underlying properties.

During the CVD process, the coating is virtually grown on the surface of the tool. However, coatings applied with the process have three distinct advantages over PVD coatings. First, the CVD process will generally result in a better bond with the substrate, and this better adhesion can result in increased performance. Secondly, the CVD process allows for much thicker coatings, which means more protection for the cutting edge and a potentially longer tool life. Thirdly, tool coaters can apply a variety of multilayer coatings with the CVD process. By combining several different coating layers, the coater can take advantage of each layer's unique strength. The end result is a coating which will make it possible for users to achieve a greater increase in production than they could achieve with any one layer by itself.

The first CVD coatings were single layers of titanium carbide (TiC) about 5µm thick. Today's CVD coatings range in thickness from 3µm to more than 14µm. CVD coatings are typically applied in multiple layers, combining TiC, TiCN, TiN, and aluminium oxide (Al₂O₃) in a variety of ways to create the finished product. A recent development as shown in Figure 2.2.1c is the deposition of a dozen or more very thin layers in order to minimise interfacial stresses and enhance the performance of the coated hardmetal. This new class of CVD-coated insert carries as many as thirteen distinct surface layers, some so thin as to be almost invisible under high SEM magnification. The first five layers form a transition from TiC, through TiCN, to Nitride. The next eight alternate between Al₂O₃ to more TiN layers.

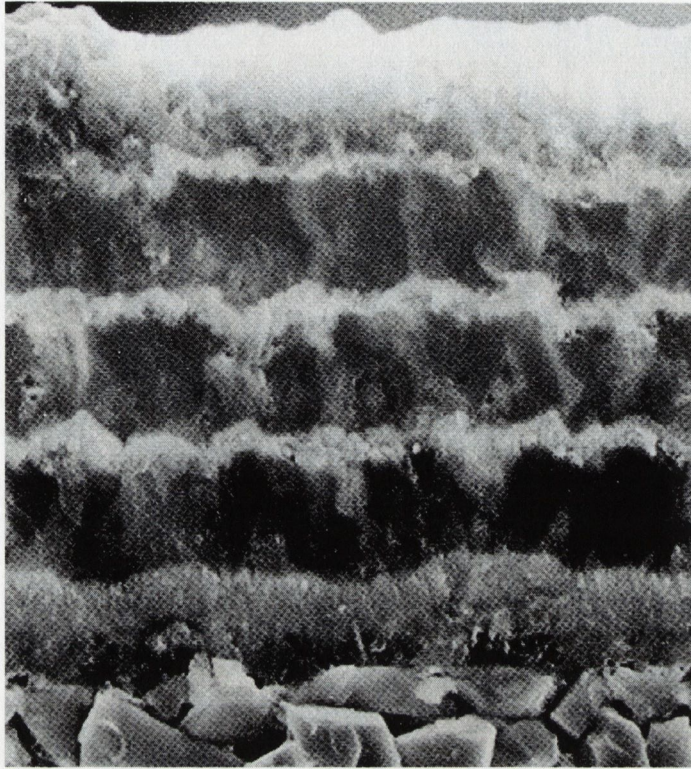


Figure 2.2.1c. Multiple CVD Coated Insert [19]

Historically, tool coaters and workshops have chosen to use the PVD process to coat sharp edges on such tools as indexable inserts, form tools, drills, and endmills because the thin-film coating produced by PVD worked best in these applications. But advancements in the CVD coating process have made an increasing number of choices available. With more precise automated control of the CVD process, coaters can apply a much thinner coating with both consistency and repeatability. This has made it practical for several companies to offer a thin-film CVD coating for sharp-edge tools. Thin-film CVD coatings have proven to be effective on a variety of carbide cutting tools, including standard inserts, top-notch inserts, form tools, threading-and-grooving tools, and many other styles.

2.2.2 The Role of TiC, TiN, TiCN and Al₂O₃ Coatings

A thin layer of titanium carbide can be deposited over a tungsten carbide substrate using the CVD process, with no cobalt binder; the layer metallurgically bonds to the substrate. The cutting forces are reduced due to very low friction. It becomes cost effective as it allows increased feed rates and hence increases metal removal. TiC coatings are good

for light and semi-finished cuts on materials that tend to alloy or weld to the cobalt binder in carbides. They have limited applications in interrupted cutting because of the sensitivity of the coating to cracking. A comparison of various materials hardness is shown in Figure 2.2.2a, whilst a list of the material properties of the various coatings is shown in Table 2.2.2.

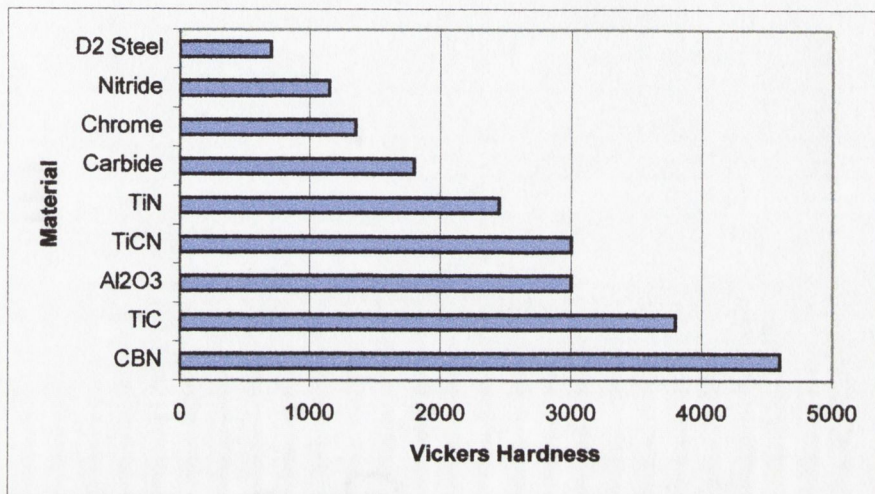


Figure 2.2.2a. Material Hardness Comparison.

	Hardness (HV)	Melting Temp (K)	Recrystallisation Temp (K)	Thermal Conductivity W/(m-K)		Thermal Expansion coefficient (x10 ⁻⁶ /K)	Elastic Modulus (GPa)
				373K	1273K		
WC	2400	2900	1373	84~126		5~6	490~588
TiC	3200	3400	1573	33	41	7.4	451
TiN	1950	3220	1473	21	26	9.4	255
Al ₂ O ₃	3000	2320	1123	28	6	8.4	373~520

Table 2.2.2. Material Properties of Substrate and Coatings.

Other coatings can be deposited also. In the case of titanium nitride, a layer with an extremely fine grain size is chemically bonded to a tough carbide base, without a binder. It has a higher resistance to crater wear than a TiC coating and a much higher tool life because of its lower coefficient of friction. TiN coatings are normally used in general metal removal applications and in finish-machining of steels and alloys.

The drawback of a TiN coating is that, although it is less brittle than a TiC coating, it does not have much adhesive strength and does not bind strongly to the substrate. This is overcome by double-coating or multi-coating the substrate. In this multi-layer coating, a

layer of TiC with strong adhesion is deposited on the carbide substrate. This layer is followed by an interpenetrating carbonitride layer and finally by a TiN coating as seen in Figure 2.2.2b. The resultant coating is strong, less brittle and more stable. It combines the good flank wear resistance property of TiC with the crater resistance of TiN which usually results in an increased cutting performance when compared to ordinary TiN coated tools.

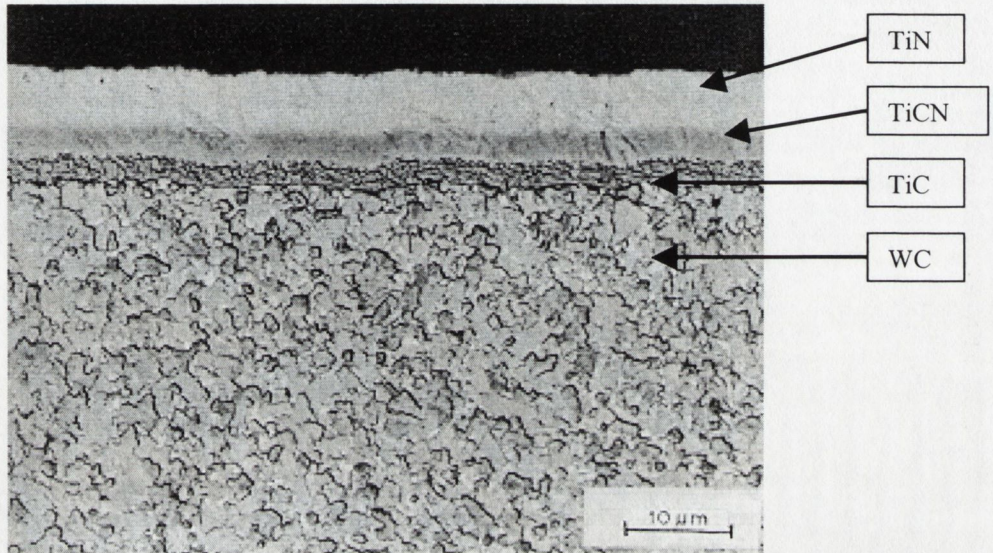


Figure 2.2.2b. WC-TiC-TiCN-TiN Multi-Coat.

Al_2O_3 is another popularly used coating. A controlled thickness of Al_2O_3 can be vapour deposited on carbide substrates. The resulting tool can be used effectively in the higher speed ranges where TiN and TiC coated tools fail due to oxidation. Wear resistance of this material is high because of good heat dissipation. An Al_2O_3 coated tool can generally operate at about twice the speed used for TiC or TiN coated tools. A dual coating of TiC and Al_2O_3 (generally depositing the TiC onto the carbide and having the Al_2O_3 as the outermost layer) combines wear resistance with toughness. These inserts are suitable for use at higher speeds where TiC and TiN are ineffective due to the tendency to oxidise. Al_2O_3 has high thermal stability and an extremely high rate of heat dissipation. However, the coating tends to fracture when subjected to high impact loading. Also, the coating is largely ineffective at lower cutting speeds. Many CVD coatings benefit from a layer of Al_2O_3 . Typically, it is included as one of the layers in multilayer coatings. Al_2O_3 coatings possess high hot hardness and are chemically inert, however this material fractures easily.

Al_2O_3 cannot be applied using PVD because it is not possible to create oxides such as Al_2O_3 economically using PVD equipment. However, a growing number of manufacturers are producing PVD-coated tools with TiAlN . This coating provides excellent results for applications which generate high temperatures at the cutting edge. When enough heat is generated, the aluminium in the TiAlN reacts with the oxygen in the air to create a microthin layer of Al_2O_3 at the cutting surface. As this layer is created and worn away during the cutting process, it provides heat protection for the cutting tool and potentially longer life. An Al_2O_3 layer that is created during a deposition process rather than during the cutting process performs better for several reasons. One reason is that the Al_2O_3 created during the CVD-coating process is much thicker than the microthin layer that the PVD-coated tool produces whilst cutting. Also, an Al_2O_3 layer that exists in the coating before the tool begins cutting can provide heat protection with the first cut and can benefit the tool even when temperatures do not rise sufficiently to create an Al_2O_3 layer as in the PVD case.

In the past, it was thought that CVD did not adhere to a sharp edge, however, it is now thought to be untrue due to advancements made to the coating processes. As mentioned earlier, one of the advantages of the CVD-coating process is the ability to create coatings that are much thicker than PVD-coatings. However, thick-film coatings are more easily broken away from sharp edges than thin-film coatings. This has led many researchers to question the ability of any CVD coating to adhere to a sharp edge. Thin-film CVD coatings, however, adhere extremely well to sharp-edge tools, which proves that it is the coating's thickness, rather than the CVD process, which impairs its ability to adhere to a sharp edge.

2.2.3 Increasing Popularity of the CVD-coating process

A recent survey [20] on coatings compared responses from both small and large manufacturing companies in the United States who both used a variety of tool materials. It showed that in large companies, where the benefits of coatings may be more widely recognised, only 15% of the tools are left uncoated, compared to 42% in smaller companies. Figure 2.2.3a and 2.2.3b presents the combined breakdown of all the manufacturers surveyed.

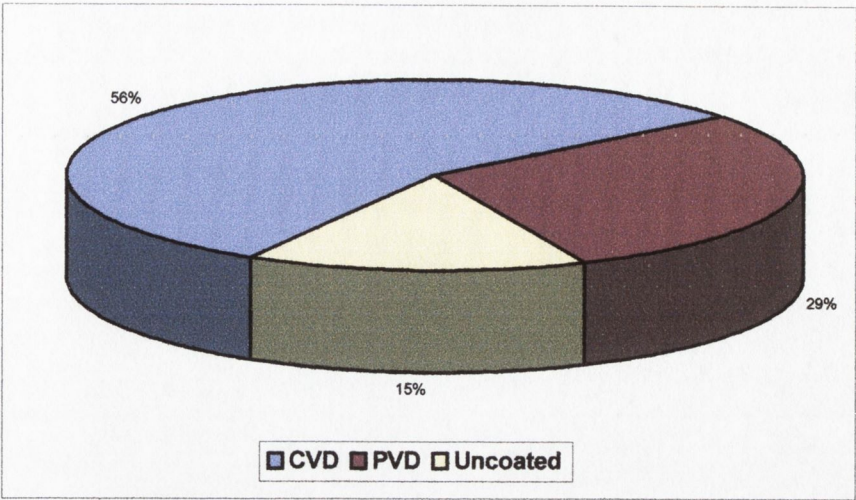


Figure 2.2.3a. Breakdown of Coating Processes amongst Larger Companies

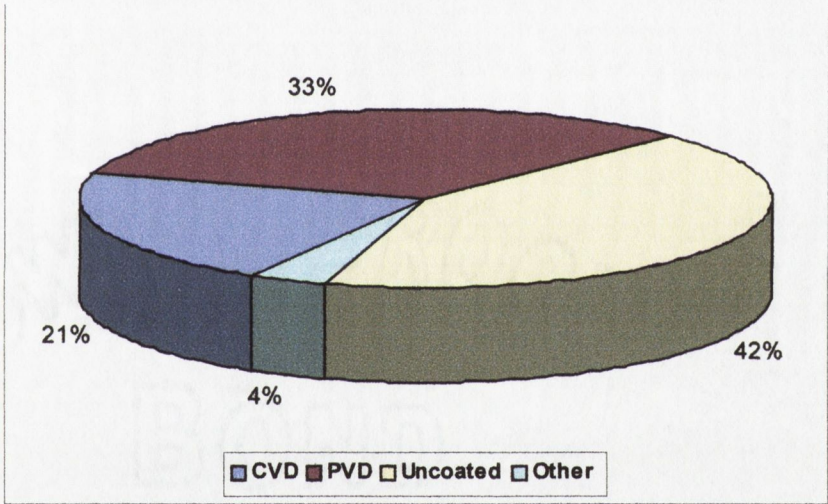


Figure 2.2.3b. Breakdown of Coating Processes amongst Smaller Companies

According to the survey, the use of the CVD process is growing. This is especially true among the larger manufacturers, where the CVD process is used more than 56% of the time. The number of smaller manufacturers turning to CVD coatings is increasing, as more of them recognise that CVD can be used for most applications, and are seeking the increased performance associated with thicker coatings and superior adhesion.

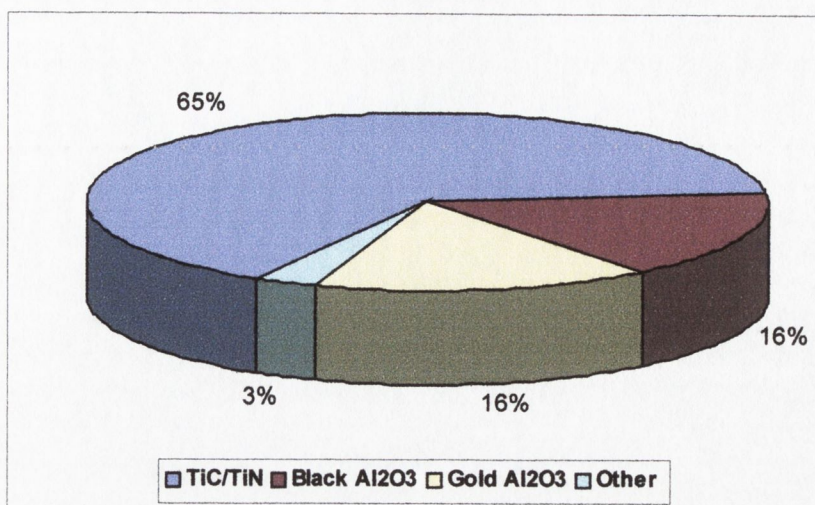


Figure 2.2.3c. Most Popular CVD Coatings amongst Larger Companies

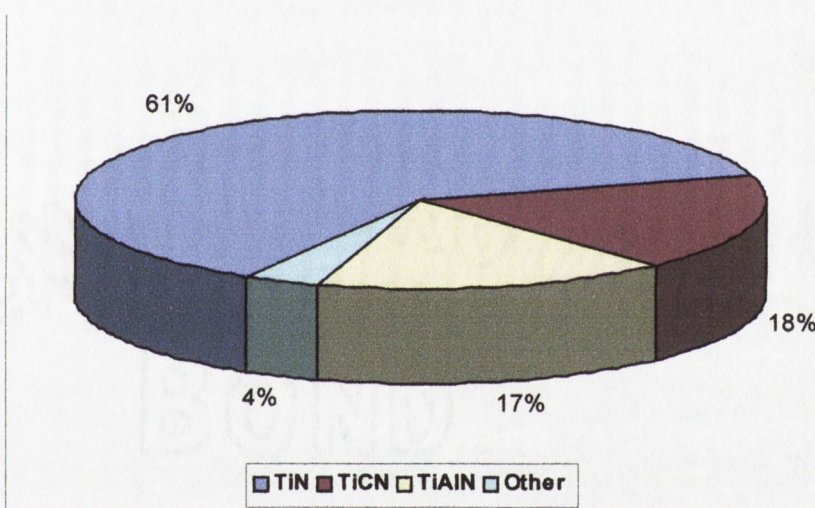


Figure 2.2.3d. Most Popular PVD Coatings amongst Larger Companies

The survey also found that, amongst smaller manufacturers, a multilayer TiC/TiN coating was the most popular CVD product applied, whereas approximately 70% of the CVD coatings applied by these companies are TiC/TiN. By comparison, the larger companies have reduced their use of TiC/TiN coatings and in their place, are using coatings which include a layer of Al₂O₃. Price and availability account for the difference between the large and small companies. The extra cost associated with Al₂O₃ has kept many of the smaller companies from switching to these coatings. Also, the scarcity of coating services that can consistently provide a quality Al₂O₃ coating has discouraged its use.

Some companies try to simplify their operations by selecting one coating process for all of their coating needs, or by continuing to use the coating that has worked well in the past. In this case, however, the old saying, “If it isn’t broken, don’t fix it,” doesn’t apply. By not seeking a coating or coating process that can offer higher performance, these companies may be forfeiting business to their more efficient and productive competitors. It may serve smaller companies well to follow the lead of their larger counterparts. The large companies assess what is available and adopt a solution that which will improve performance and maintain a high level of customer satisfaction. In their quest for constant improvement, the larger companies are continually evolving their coatings and coating processes.

2.3 Tool Wear and Tool Life

Wear is the unwanted change in the dimension and quality of tools and components. Together with failure, deformation and corrosion, wear limits the useful life of the object on which such phenomena occur. Wear occurs wherever the relative movement of contacting surfaces produces friction and/or wherever corrosive media act on unprotected surfaces [19].

A new tool has sharp cutting edges and smooth flanks, but when it is used it gets subjected to cutting forces that are concentrated over a relatively small contact area on the rake and flank faces. Furthermore, the chip slides over the rake face and the machined surface rubs past its flank. Each time the tool enters or exits from the cut, it is subjected to mechanical, as well as thermal shock. Under such adverse conditions, these hard tool materials and coatings gradually wear out and even fracture, necessitating a tool change. Precious machining time is lost in the process.

Hard metal softens at high cutting temperatures and hence its shear yield stress is less than that at room temperature. Though the metal sliding over the tool has a lower yield stress, it may become so work hardened so as to be able to exert frictional stress sufficient to cause yielding, by shearing the hard tool material. The higher the temperature at the interface between the tool and metal, the greater the effect.

This softer metal sliding over the surface of the harder tool may contain concentrations of hard particles. In these conditions the hard particles act as small cutting edges, like that of a grinding wheel in contact with the surface of a hard metal, which in due course are worn away through abrasion [18]. In addition, the particles of the hard tool which intermittently come unstuck (or get torn out) from its surface, are dragged along or rolled over. These particles plough grooves into the surface of the hard metal workpiece.

When two surfaces slide in contact with each other under pressure, asperities on one surface interlock with those of the other. Due to frictional stress, compressive stress is produced on one side of each interlocking asperity and tensile stress on the other side as shown in Figure 2.3, only reducing when they have moved over, or through each other [18]. As new asperities are formed, this cycle is repeated. Thus the material of the hard metal near the surface undergoes cyclic stress. This phenomenon causes surface cracks, which ultimately combine with each other and lead to the crumbling of the tool.

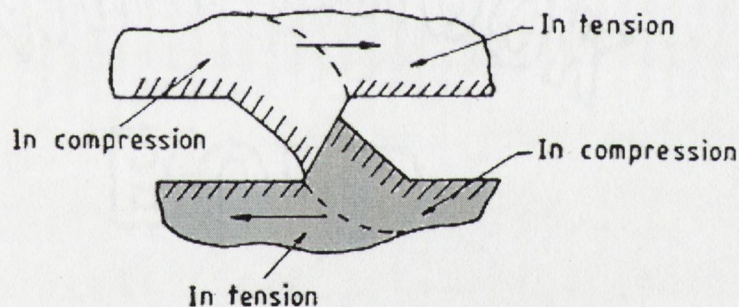


Figure 2.3. Stress distribution around interlocking asperities

Hard coatings usually have a low coefficient of friction when in contact with various workpiece materials, and they also greatly increase wear resistance. They may also reduce cutting forces and temperatures at the tool edge and thereby indirectly affect the deformation and fracture behaviour of the tool. Unlike the PVD process, chemical vapour deposition (CVD) reduces the fracture strength, the transverse rupture resistance (TRS) and the chipping resistance of the carbide tool material due to interfacial η -phase formation or the presence of grown-in cracks due to tensile residual stresses in the coating arising from the coating process [21].

There are several components to crater wear, including abrasive wear, dissolution wear, and diffusion wear. Abrasive wear occurs as the chip rubs on the insert. Dissolution wear is the chemical tendency of the tool material to dissolve in the workpiece. Dissolution wear must precede diffusion wear, which is the actual rate of atomic transfer between the tool and the workpiece. The degree to which the coatings resist these components of crater wear can be related to the differences in their mechanical, chemical, and thermal properties as a function of temperature.

2.3.1 Tool Failure Criteria

The time for which a cutting tool can be usefully employed before replacement is called the tool life. It is not economical to use the tool beyond its useful life as increased bluntness of the cutting edge causes an increase in the cutting forces, and as a result tool temperatures also increase. Consequently both the dimensional accuracy and the surface finish of the machined piece suffer.

Cutting tool life has a very important economic consideration in metal cutting and considerable research has been undertaken in investigating the factors that limit the lifespan of a cutting tool. In 1906, Taylor showed that a relationship existed between tool life and cutting speed which is presented in Equation 2.0:

$$VT^n = C_v \quad \text{Eqn. 2.0}$$

where V is the cutting speed, n an exponent depending on cutting conditions/cutting tool, T the tool life in minutes and C_v a constant dependent on the cutting speed for a tool life of one minute.

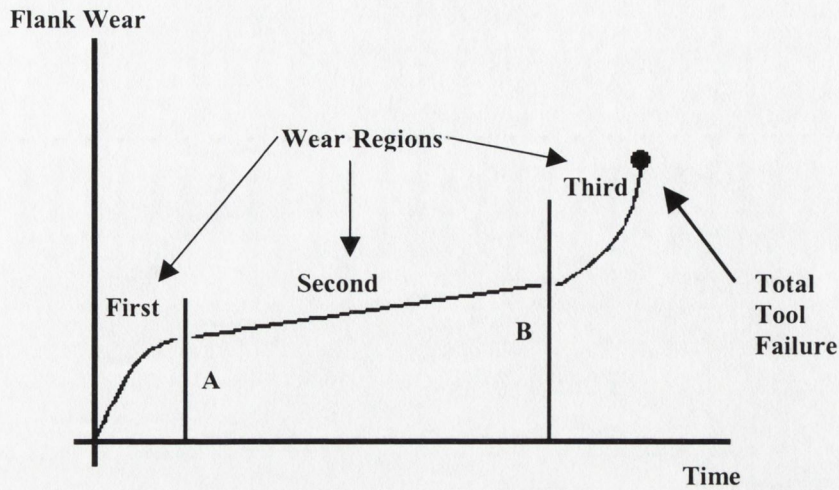


Figure 2.3.1a. Tool Life over Time [18]

Classic wear propagation in turning has been investigated for as long as metal cutting research has existed and still continues today. From Figure 2.3.1a, it has been shown [22] that the curves which describe flank wear versus the cutting time, generally vary from convex to concave shapes during the tool's lifetime. Three different regions exist, which dissect the typical tool wear curve over its lifetime.

1. The first region usually lasts from one to two minutes of cutting time and the wear behaves in the form of an exponential curve. The flank wear initially increases rapidly and then gradually reduces to maintain a constant rate. This point of the wear curve represents the end of the first region.
2. The second region covers the total period of effective cutting time in which the wear propagation is linearly related to the cutting time. This part of the curve usually provides the most important information about the performance of the tested tool.
3. The third and last period of cutting time behaves differently when different groups of tool-workpiece materials are machined. In some cases the wear curve once again gradually increases its rate in an increasing exponential manner until total tool failure stops the machining process. However, in other cases, which are typical for coated tools, the wear grows sharply at an extremely high rate. This wear propagation ends quickly with total failure of the cutting edge.

The progress of crater wear is also of a similar nature. Continued use of the worn tool would result in catastrophic failure or total loss of the tool and damage to the machined component. If the tool is changed before the occurrence of catastrophic failure then the damage to the workpiece is minimised. Tool failure criteria have been devised to specify the maximum wear of the tool that can be tolerated before the tool has to be changed. Direct tool life criteria depend on the direct measurement of tool wear or direct visual examination of the cutting edge. Figure 2.3.1b is a diagram of ISO recommended measurements for tool wear [23].

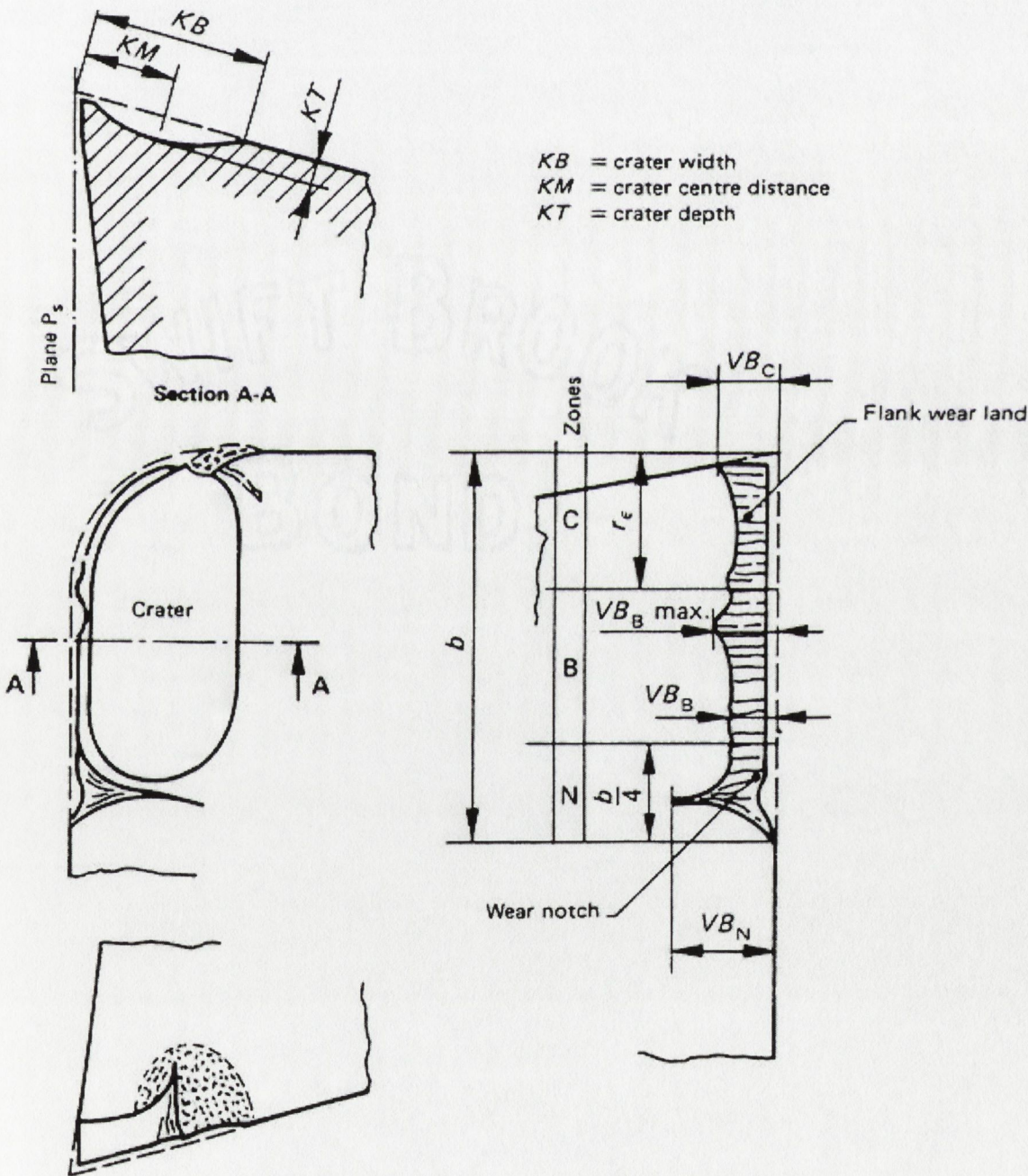


Figure 2.3.1b. ISO recommended measurements of tool wear [23]

The wear land on the flank face is not of uniform width. It is larger at, or near the two ends of the active portion of the cutting edge. At the nose end the chip flow is complicated and the wear conditions severe. At the rear portion of the flank wear land, a groove or notch is formed as a result of accelerated wear caused by abrasion, and metal transfer enhanced by chemical interaction with the surrounding atmosphere. The width of the wear land, VB_B , is usually a maximum at the end of the flank land. According to ISO recommendations [23], the following wear land criteria are appropriate for sintered carbide tools are shown in Table 2.3.1a below.

Average Width of the Flank Wear Land, VB_B .	0.3mm
Maximum Width of the Flank Wear Land, VB_{Bmax}	0.6mm

Table 2.3.1a. Wear Land Criteria for Carbide Tools

The two parameters of interest in crater wear are the maximum crater depth, KT , and the distance, KM , between the cutting edge and the location where the crater depth is a maximum as shown in Figure 2.3.1b. It is obvious that as the crater increases in depth, the tool is weakened. ISO recommends [23] that the following crater wear criteria, as shown in Table 2.3.1b, are appropriate for sintered carbide tools (where f is the feed in millimetres per revolution).

Maximum Depth of the Crater, KT [mm]	$0.06 + 0.3f$
--	---------------

Table 2.3.1b. Wear Land Criteria for Carbide Tools

The term machinability refers to the ease with which a given work material can be machined under a given set of cutting conditions. It is of considerable economic importance for the manufacturing engineer to know in advance about the machinability of a work material, so that it can be processed in an efficient manner. The tool life criterion is one of the most important criterion to be considered from the point of view of machining economics. In general, if a work material produces more rapid tool wear, the tool has to be changed more often and machining would be more expensive on account of the increased cost of tools and the production time lost during tool changes. A convenient machinability criterion based on tool life is called the "specific cutting speed" which is determined as

being the cutting speed corresponding to a predetermined tool life. This equation, known as the machinability rating, M_R , of a test material is presented in Equation 2.1.

$$M_R = \frac{V_{60}^t}{V_{60}^s} \times 100\% \quad \text{Eqn. 2.1}$$

where V_{60}^t is the specific cutting speed for a tool life of 60min for a test material and V_{60}^s is the corresponding specific speed for a standardised material (SAE 1212 with a machinability rating of 100%). Inconel 718 has a machinability rating of 30%.

2.4 Contact Stresses

2.4.1 Stress Distributions in Thin Layer Coatings

The use of hard ceramic coatings on a tougher substrate has long been considered in research. Coatings are widely used to extend the working life of a cutting tool subjected to high loads during machining. *Arnell* [24] was one of the first researchers to investigate the problems with the use of coatings. He found that the failure of a tool is often more dependant on fracture or detachment of the coating than from excessive wear of the tool [25]. Detachment is caused by stresses which arise at the interface between the coating and the substrate, while fracture is generally caused by stresses which arise within the coatings and on the surface of the tool.

The underlying mechanics associated with interfacial fracture between two elastic solids was identified by *Evans et al* [26] who showed that coating detachment is a mixed mode fracture process. Therefore, a detailed knowledge of the direct and shear stresses at the coating surface is required, and at the interface between the coating and substrate. These stresses may originate in three ways. Firstly as residual stresses due to the thermal mismatch between the coating and the substrate during cooling from the deposition process. Secondly due to stresses arising from the deposition process itself, and the last as a result of an externally applied load. Only the last of these stress inducing conditions is

addressed in this work and as a result the insert is assumed to be initially stress free – hence ignoring the influence of deposition and thermal effects.

There are two basic approaches to the modelling of contact stresses. The first approach is to model a plane surface loaded by a rigid indenter so that the surface displacements can be calculated and hence the resulting stress and strain fields can be determined. The second approach makes no assumptions regarding the shape or properties of the indenter and the plane surface is subjected to a known Hertzian type pressure distribution [25].

Indentation tests have been widely used to measure the elastic and fracture behaviour of solids and to examine the adhesion between coatings and substrates. The indentation process simulates the flow of the chip onto the rake face of the tool. Indentation type problems have been studied using a contact mechanics approach, however, when a solid thin film is deposited over a substrate, the problem becomes much more complex. The exact response of the coating/substrate system subjected to indentation loading can be categorised according to the coating material [27]. There are two specific cases that are generally studied:

- The coating material is not as stiff as the substrate
- The coating material is stiffer than the substrate

In the first case, the substrate has usually been approximated as a rigid body by most researchers. However the substrate is not rigid in reality, and its elastic properties cannot be disregarded when a soft coating is applied to it. With the increased use of hard coatings on softer substrates, the second case is of more interest to this research project. A proper evaluation of the mechanical behaviour of the coating and substrates is necessary and so a full knowledge of the effects their elastic properties is required. In addition to this, the thickness of the coating and the size of the "loading contact radius" [27] are also required, as shown in Figure 2.4.1a.

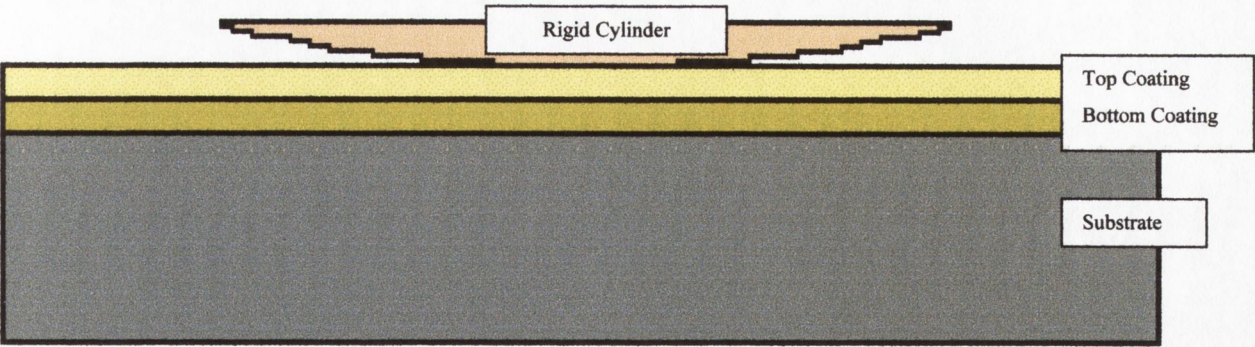


Figure 2.4.1a. Schematic of a coating/substrate system under indentation

The work here is subjected to a simplified Hertzian normal pressure distribution to establish the influence of substrate and coating stiffness on the resulting stress distributions. It has been shown by *Johnson* [28], that modelling the chip flow over a tool is similar to the effect of a cylindrical indenter applying a load to a flat surface. This type of idealised loading, is broadly similar to that occurring during metal cutting as the machined chip impinges on the rake face of the workpiece as shown in Figure 2.4.1b.

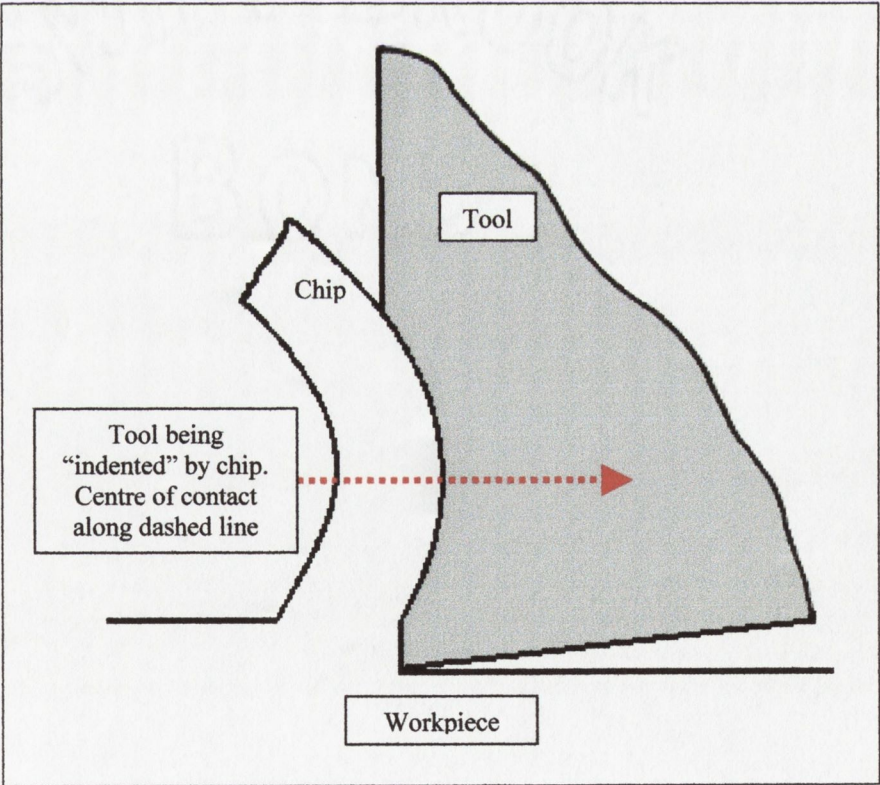


Figure 2.4.1b. Chip flow over rake face.

2.4.2 Normal Stress Distributions in Thin Layer Coatings

The theory associated with this type of contact was developed initially by *Hertz* and later modified by *Johnson* [28]. With this type of contact problem, the contact pressure is considered to act over a strip of width $2a$. The resulting normal contact pressure distributions given by the following expression was taken from *Johnson* [28]:

$$P_n(x) = \frac{2P}{\pi a^2} (a^2 - x^2) \quad \text{Eqn. 2.2}$$

where P_n is the normal force per unit axial length of the cylindrical indenter, and x is the distance of a point from the centre line of the contact zone and a the half-width of the contact surface.

The half-width a is related to the applied load P , the radius of the cylinder R and the elastic properties of the materials in contact through the expression:

$$P = \frac{\pi a^2 E^*}{4R} \quad \text{Eqn. 2.3}$$

To enable the width of the area of contact to be calculated, an effective Young's modulus, E^* , needs to be found, as there was more than one material being used on a cutting tool. This effective Young's modulus was calculated for each different combination of coating/carbide grade of substrate according to the equation:

$$E^* = \left(\frac{1 - \nu_s^2}{E_s} + \frac{1 - \nu_c^2}{E_c} \right)^{-1} \quad \text{Eqn. 2.4}$$

where E^* is the effective young's modulus, ν_s Poisson's ratio for the substrate, ν_c Poisson's ratio for the coating, E_s Young's modulus of the substrate and E_c Young's modulus of the coating. Therefore the maximum pressure P_o , which occurs at the centre of the contact zone, is given by the expression:

$$P_o = \frac{2P}{\pi a} = \left(\frac{PE^*}{\pi R} \right)^{\frac{1}{2}} \quad \text{Eqn. 2.5}$$

Along the vertical axis below the centre of contact area the stress distributions for σ_x , σ_y and τ_l are given by *Johnson* as:

$$\sigma_x = \frac{-P_o}{a} \left[\frac{a^2 + 2y^2}{\sqrt{a^2 + y^2}} - 2y \right] \quad \sigma_y = \frac{-P_o a}{\sqrt{a^2 + y^2}} \quad \tau_l = \frac{\sigma_x - \sigma_y}{2} \quad \text{Eqn. 2.6}$$

where y is the depth from the surface.

2.4.3 Normal and Tangential Stress Distributions in Thin Layer Coatings

The above equations (2.2-2.6) deal with the contact stresses resulting from a normal contact pressure distribution. With the addition of sliding contact, these equations are further modified. For a cylindrical indenter sliding perpendicular to its axis on a plane, the width of the contact strip, $2a$, and the normal pressure are given by:

$$a^2 = \frac{4PR}{\pi E^*} \quad \text{Eqn. 2.7}$$

$$p(x) = \frac{P_o (\sqrt{a^2 - x^2})}{a} \quad \text{Eqn. 2.8}$$

where the maximum contact pressure occurs on the centre line:

$$P_o = \frac{2P}{\pi a} \quad \text{Eqn. 2.9}$$

The sliding process gives rise to a tangential traction of the form:

$$q(x) = \frac{2\mu P (a^2 - x^2)}{\pi a^2} \quad \text{Eqn. 2.10}$$

According to Amonton's law of friction [28], the relationship between the normal and sliding traction during the sliding process at an elementary area of the interface is given by the expression:

$$q(x) = \mu p(x) \quad \text{Eqn. 2.11}$$

where μ is a constant coefficient of kinetic friction whose value is determined by the material and physical conditions at the interface of the contact area. The direct and shear stresses at the surface contact due to both the normal and the tangential loads are given by:

$$\begin{aligned} \sigma_x &= -P_o \left\{ \left[1 - \left(\frac{x}{a} \right)^2 \right]^{\frac{1}{2}} + \frac{2\mu x}{a} \right\} & \sigma_y &= -P_o \left[1 - \left(\frac{x}{a} \right)^2 \right]^{\frac{1}{2}} \\ \tau_{xy} &= \mu P_o \left[1 - \left(\frac{x}{a} \right)^2 \right]^{\frac{1}{2}} \end{aligned} \quad \text{Eqn. 2.12}$$

and the principal shear stress in the plane of deformation is given by:

$$\tau_1 = \left[(\sigma_x - \sigma_y)^2 + 4\tau_{xy}^2 \right]^{\frac{1}{2}} = \mu P_o \quad \text{Eqn. 2.13}$$

The above equations are used in the ANSYS finite element section when the Hertzian pressure distribution is applied on the surface of a coated substrate.

3.0 Literature Review on Machining

3.1 Conventional Machining of Inconel 718

There is a consensus in the published literature that Inconel 718 has proved extremely difficult to machine due to the presence of the abrasive carbide particles. Conventional machining, such as turning and milling, appear to be the only cutting processes which provide a balance between realistic metal removal rates and the production of an acceptable surface finish. The subsequent sections describe the current and past research into the machinability of Inconel 718 using conventional machining techniques.

3.1.1 Turning Inconel 718

There are a lot of variables influencing the machinability of Inconel 718 with single point turning. These include feed rate, cutting speed, depth of cut, cutting tool material, cutting fluid, tool rake, clearance and approach angles, type of workpiece material etc. The following paragraphs provide details of some of the research performed to-date on single point turning of Inconel 718.

Komanduri et al [14] studied the chip formation that occurred whilst machining Inconel 718 at various cutting speeds up to a maximum speed of 213.5 m/min. They concluded that up to cutting speeds of 30.5 m/min, the chips formed are essentially continuous and ribbon-like, although deformation in the chip is inhomogeneous. At cutting speeds of approximately 60 m/min, shear-localised chips form. These chips show gross inhomogeneous deformation with shear localisation between the segments, and very little deformation within any individual segment. With a further increase in speed, the extent of contact between segments decreases rapidly until the individual segments become completely detached and discontinuous chip formation takes place. The speed at which this takes place was shown to depend on the metallurgical condition of the workpiece and its hardness. These shear-localised chips formed with Inconel 718 were similar to the chips

generated when machining titanium alloys and hardened AISI 4340 steels at higher speeds. The strengthening of Inconel 718 by γ'' precipitates, which remains up to high temperatures, limits deformation within the individual segments – thus favouring shear localised chip formation.

Choudhury et al [6] conducted a series of machining experiments on Inconel 718 using coated (Sandvik GC3015) and uncoated (Sandvik H13A) carbides. The effect of cutting variables (speed, feed and depth of cut) on the cutting forces and tool life was studied. It was found that at small depths of cut, there was no advantage in using a coated carbide tool. However at higher depths of cut, the thermal conductivity of the Al_2O_3 outermost coating on the GC3015 tool decreases with the rise in cutting temperature. Hence, the Al_2O_3 coating maintains a low temperature in the tool and protects the mechanical properties of the tool at high cutting speeds and depths of cuts. They also found that the use of coatings was justified, only if the depths of cut exceeded 1mm. The effect of cutting speed on tool life was reported to be more pronounced than the effect of either feed rate and depth of cut for coated tools. They recommended cutting speeds of 20-25m/min, with feed rates of 0.15-0.20mm/rev and a depth of cut greater than 1mm.

Leverenz et al [15] found that the built-up-edge and notching effects of Inconel 718 frustrates attempts to machine this superalloy. What was needed therefore was a tool material that could cut this material without the formation of either BUE or notching. Again, for lower cutting speeds they recommend coated carbides and ceramics only at very high cutting speeds (220-300m/min). The coated tools were not as cost-effective at higher cutting speeds because they do not last as long due to the high cutting temperatures. Carbides begin to deform at 800°C while ceramics retain their hardness up to 1200°C. Again the preferred choice of a coating for the tool is Al_2O_3 due to its high hot hardness and the fact that it is chemically inert.

Wright et al [29] carried out machining experiments to determine the effects of machining on pure nickel and nickel alloys. They found that the combination of the high primary shear zone strength and high secondary shear stress close to the cutting edge meant that the cutting temperature was very high when machining the nickel alloys. The

high strength of these nickel alloys also causes high tangential stresses which act normal to the cutting edge.

Kramer et al [30] studied the various tool wear mechanisms occurring whilst machining Inconel 718 using coated carbide cutting tools. The cutting speed for the experimental tests was set so as to minimise flank wear while maintaining a crater wear rate of approximately $5\mu\text{m}/\text{min}$. It was found that the wear rate initially increased with speed until a point was reached where wear began to decrease with increasing speed. At higher cutting speeds ($135\text{m}/\text{min}$), plastic deformation of the substrate took place, and this resulted in cracking and spalling in the coatings. This was attributed to the low fracture resistance of the coatings that which it impossible for them to conform to the deformation of the tungsten carbide substrate without cracking.

Quigley [31] investigated the machinability of both precipitation hardened and solution treated Inconel 718 using coated and uncoated carbide cutting tools. It was found that at lower cutting speeds ($20\text{m}/\text{min}$), the coatings on the tools did not prevent flank wear and it was only when the cutting speed was increased to $40\text{m}/\text{min}$ or $60\text{m}/\text{min}$ when these coating became beneficial by lowering wear, and hence forces. The effect of chip-breaking geometries was also studied combined with the effects of lubricants and feed rates. Only at a higher feed rate ($0.34\text{mm}/\text{rev}$) did discontinuous chips form whilst lubricants had little effect on the outcome of the process.

Sadat [32] investigated the effect of cutting speeds and chip-tool contact length on the surface finish generated in machining Inconel 718 under both dry (unlubricated) and lubricated conditions. At low cutting speeds ($6\text{m}/\text{min}$), the use of lubricants leads to a small decrease in the axial and tangential cutting forces, however at higher cutting speeds ($18\text{--}60\text{m}/\text{min}$) the effect is negligible. Again at low cutting speeds, the effect of lubricants resulted in a small decrease in surface damage produced. However, at high cutting speeds the effect of lubricants was negligible on the quality of the surface finish. At all higher cutting speeds, the quality of the surface finish was radically improved [33].

Hanasaki et al [34] used four different coated tools turning a high nickel-base superalloy. It was discovered that the flank groove wear, which occurred when turning

with a cemented carbide was prevented, and the major flank wear decreased. When turning with the coated tools, corner wear was the largest and this type of wear determined the life of the tool. In the case of this corner wear, the Al_2O_3 coated tool sustained the smallest wear width because of its low thermal conductivity at high temperatures. Comparing different grades of WC, the K10 carbide sustained the smallest wear land width due to its higher toughness. Tool life is dependent on the wear arising from the increases in temperature at high cutting speeds, and from chipping due to adhesion at low cutting speeds.

3.2 Finite Element Modelling of Contact Stress Analysis

The finite element method can be used to investigate the contact stresses due to elastic indentation of coating-substrate systems consisting of a high modulus surface coating on a relatively low modulus substrate. The two types of conditions that are commonly simulated are the pressure distributions associated with a spherical indenter and also a cylindrical indenter which are identical to distributions arising during Hertzian indentation of a homogeneous elastic substrate. For each distribution, the stresses have been investigated in terms of:

- The ratio of Young's moduli of the coating to that of the substrate
- The ratio of coating thickness to contact half-width (a)
- The frictional contact conditions
- The normal loading
- The normal load in combination with tangential loadings

There are two basic approaches to the modelling of contact stresses. The first approach assumes a plane surface being loaded by a rigid indenter so that the surface displacement can be prescribed and the resulting stress and strain fields can be computed. In the second approach, no assumptions are made about the shape or properties of the indenter and the plane surface is simply subjected to a known pressure distribution. Both approaches are artificial but the second approach is used, as it is very easily extended to include frictional tractions.

Djabella et al [25] lead the field in this area of contact stress research. In some initial research, when a single-coated substrate was loaded under a normal load, the results showed that the stresses likely to cause coating failure, i.e. in-plane tensile and compressive stresses and interfacial shear stresses, depend on the coating thickness and the modulus ratio in quite a complicated fashion. For some coating thicknesses, the maximum in-plane stress is compressive while for other coating thicknesses it is tensile; in both cases the magnitude of the stress increases with the coating modulus. There are three contributions to the magnitude of the stress in any of the coatings:

- The compressive stress due to normal load
- The in-plane stress arising from the modulus mismatch
- The bending stress due to deformation of the coating

Where the coating has a higher modulus than the substrate, the modulus mismatch will cause an additional compressive stress in the coating, whereas, bending will produce a tensile stress. In both cases the stresses increase with the coating modulus.

In some further research *Djabella et al* [35] and others [36,37] not only looked at the contact stresses within an elastic coating/substrate under a normal load, but combined a tangential traction to simulate the stresses due to frictional sliding at the coated surface. What was concluded was that at a fixed value of substrate stiffness, the maximum tensile stress, $\sigma_{\max}$, at the surface increases very significantly with an increasing coefficient of friction and increasing the coating to substrate modulus ratio. The shear stress also increased very significantly with increases in the friction coefficient, but as the thickness of the coating increases, this becomes less apparent. This analysis was among the first to show that both tensile and compressive stresses, which may lead respectively to tensile fracture and buckling, and the shear stress, which can be responsible for delamination at the interface, vary in a rather complicated fashion as a function of the coating thickness ratio. It was shown that a relatively thin coating is preferred to avoid brittle fracture at the surface of the coating whereas a relatively thick layer reduces the risk of delamination at the interface.

In a similar analysis by *Komvopolous* [38], it was found that the ratio of the coating thickness to the contact half-width has a significant effect on the stress-strain field produced in the compressed layered solid. A stiffer coating increases the resistance to deformation and may result in virtually insignificant stresses in the substrate when it is sufficiently thick. However, in the case of a thin coating, the stresses may be higher in the substrate than for a completely uncoated substrate. Both the maximum tensile stress and the VonMises effective stress decrease rapidly as the relative coating thickness, $[t/a]$, increases; where t is the coating thickness and a the contact half-width. The initiation of microcracking in the coating, coating debonding and plastic deformation in the layered solid depends on the magnitude of the contact half-width. Theoretically these mechanisms should disappear if the coating thickness is larger than the contact half-width. However this is not the case due to a maximum coating thickness limitation associated with residual stresses and microstructural defects introduced by the coating process.

In further research, *Djabella et al* [39] looked at contact stresses in double-layer systems under a normal load. The double-layer consisted of two layers of equal thicknesses but having different moduli. At the double-layer system interfaces, the stress beneath the edge of the loaded area, which is tensile at the centre of the contact zone, decreases with increasing double-layer thicknesses, and becomes compressive for very thick double layers. When these stresses were compared to those from previous research [25], it was shown that for any chosen thickness and modulus ratio, the tensile stress at the coating-coating interface decreased nearly twofold relative to that calculated at the same depth for an equivalent mono-layer. This important result showed the advantages of using a suitable double-layer system instead of a thicker mono-layer. It was shown that thin double-layer systems had the disadvantage of developing high tensile and shear stresses at the surface and interfaces respectively. However, for thicker systems, high tensile stresses are found between the coating-coating interface and lower shear stresses are found between the coating-substrate interface.

Again, it was necessary to combine the results from the above research [39] with a tangential traction to simulate the stresses due to frictional sliding at the double-layer surface [40,41]. As previously discussed, the double-layer coatings consisted of two layers of equal thicknesses but different moduli. At the surface of the double-coated system, the maximum shear stress increases substantially with the friction coefficient and decreases

with total coating thickness. However thick double-coatings, with a decreasing modulus from the outermost coating to the substrate, are more resistant to both surface tensile failure and interface delamination than thinner ones. This is also the case with higher friction conditions.

Some research has been performed on multi-layered coatings. *Djabella et al* [42,43] showed that for a given total thickness and outer layer substrate modulus ratio, the tensile stresses at the base coating-substrate interface is progressively reduced as the number of intermediate coatings is increased. For the protection of the substrate from shear and delamination, thick coating systems are preferred. A combination of the effects of the various coating parameters considered from previous analyses [25,35,39,40,42,43] show that simultaneous minimisation of all the stresses which may lead to coating failure is not possible. A balance has to be achieved between the number of coatings and their respective thicknesses.

3.3 Finite Element Modelling of Machining

The finite element analysis of the machining process is very complex and requires assumptions to be made with regard to the setting up of the model. To date, the majority of the finite element work has concentrated on orthogonal machining (2-D). However some research has been carried out on oblique machining (3-D) where a rotating bar has been modelled as a linearly traversing block of metal. To date, there has been no attempt to model the full 3-D process with the rotating billet. Table 3.3a below indicates the possible variables and factors that can arise in any simulation.

	Possible Variations
Machining Process	Orthogonal (2-D), Oblique (3-D)
FEM Frame	Eulerian, Lagrangian, Arbitrary Lagrangian Eulerian (ALE)
Sub-Surface/Surface Damage	Damage Criterion, No Damage Criterion
Analysis Type	Un-Coupled Thermo-Mechanical, Fully Coupled
Meshing Procedure	Adaptive Remeshing, No Remeshing
Material Recovery	Elastic Recovery, No Recovery
Chip Type	Continuous, Segmental
Tool Type	Rigid, Deformable, Coated Deformable
Tool Wear	Tool Wear, No Tool Wear
Friction Model	Combined Coulomb-Tresca, Visco-Plastic
Tool Chatter	Coupled Modal Analysis, No Modal Analysis.

Table 3.3a Possible Variations between metal cutting finite element models.

The ideal simulation, using various combinations of Table 3.3a, would be a 3-D model using an Arbitrary Lagrangian Eulerian (ALE) FE formulation with adaptive remeshing and would be a fully coupled thermo-mechanical analysis. The thermal part of this analysis type should be able to account for material property changes due to temperature, whilst the mechanical analysis should be able to incorporate large stress and strain effects into the material behaviour model. This is discussed further in Section 5.2. The simulation should also be able to account for stresses and strains within the cutting tool itself, and be able to quantify the amount of abrasive wear through some proven model (Section 5.2.5). In order to model segmental chip formation, it is necessary to have a damage criterion so as to allow node splitting. This damage criterion could also be used to enable the modelling of tool failure due to edge chipping or catastrophic tool failure. A combined Coulomb-Tresca friction law should be used for large strain problems (Section 5.2.4). During the machining process, chatter often occurs, and these vibrations also have to be taken into account. To date there has been no published literature related to the modelling of a machining process that addresses all these variations.

Reference No. (See References)		44	45	46	47	48	49	50	51	52	53	54	13	55	56	57	58
Machining Process	2D	-	-	-	-	-	√	√	√	√	√	√	√	√	√	√	√
	3D	√	√	√	√	√	-	-	-	-	-	-	-	-	-	-	-
Finite Element Frame	Lagrangian	√	-	-	√	√	√	√	-	√	-	√	√	√	√	√	√
	Eulerian	-	-	√	-	√	√	-	√	-	√	-	-	-	-	√	√
Solution Type	Coupled Thermo-Mechanical	-	-	-	-	√	-	-	-	√	√	√	-	-	√	√	√
Machining Conditions	Transient	√	-	-	-	√	√	-	-	-	√	√	-	-	-	-	-
	Steady State	-	√	√	√	-	√	-	-	√	-	-	√	√	√	√	√
Assumed Data in Model	Chip Geometry	-	√	√	-	√	√	-	√	-	-	-	-	-	√	√	
	Flow Lines	-	-	√	√	-	√	-	√	√	√	-	√	-	√	-	-
Material Model	Rigid-plastic	-	-	√	√	-	-	-	-	-	-	-	-	-	-	-	-
	Elastic-plastic	√	-	-	-	-	√	√	-	-	-	-	√	-	-	-	-
	Visco-plastic	-	-	-	-	√	√	-	√	√	-	-	-	-	√	-	-
	Elasto-viscoplastic	-	-	-	-	-	-	-	-	-	√	√	-	√	-	√	√
	Temperature Effects	-	-	-	-	√	-	-	-	√	√	√	-	√	√	√	√
Cutting Variables Simulated	Depth of Cut	√	√	√	-	-	-	√	√	√	-	√	√	√	-	√	-
	Cutting Speed	√	-	√	-	-	√	-	√	√	-	√	√	√	√	√	-
	Feed Rate	-	√	-	-	-	-	√	-	-	-	-	-	-	√	-	-
	Rake Angle	√	√	-	√	-	√	√	√	√	-	√	√	√	√	√	-
	Friction	-	-	√	-	-	√	-	-	-	-	√	√	√	-	√	√
Simulation Results	Stress/Strain	-	-	√	-	√	√	√	√	√	√	√	√	√	√	√	√
	Chip Geometry	√	-	-	√	√	√	-	-	-	√	√	√	√	-	√	-
	Velocity	-	-	-	-	-	√	-	-	-	√	√	-	-	√	-	-
	Tool Forces	-	√	√	√	-	√	√	√	√	-	√	√	-	√	√	√
	Tool Wear	-	-	-	-	-	-	-	-	-	-	√	-	-	-	√	-
	Shear Angle	-	-	-	-	-	√	√	√	√	-	√	√	-	-	-	-
	Residual Stress	-	-	-	-	-	-	-	-	-	√	√	-	√	-	√	-
	Plastic Strain	-	-	-	-	√	√	√	√	√	√	√	√	√	√	√	√
Comparison with Experimental Results		√	√	√	√	√	√	-	√	√	√	√	√	√	√	√	√
Special Features/Comments (See Table 3.3d)		A	-	-	B	C	-	-	D	E	F	G	H	I	-	-	-

Table 3.3b. Summary of literature in Metal Cutting Simulations using Finite Element Methods (Continued in Table 3.3c)

Reference No. (See References)		59	60	61	62	63	64	65	66	67	68	69	70	71	72	73	74
Machining Process	2D	√	√	√	√	√	√	√	√	√	√	√	√	√	√	√	√
	3D	-	-	-	-	-	-	-	-	-	-	-	-	-	-	-	-
Finite Element Frame	Lagrangian	√	√	√	√	-	√	√	√	√	√	√	√	√	√	√	√
	Eulerian	-	√	-	-	√	-	-	-	-	-	-	-	-	-	-	-
Solution Type	Coupled Thermo-Mechanical	-	√	-	-	-	-	-	-	√	-	√	√	√	√	√	√
Machining Conditions	Transient	-	-	√	√	-	-	√	√	-	-	√	√	-	-	-	-
	Steady State	√	-	-	-	√	√	-	-	√	√	-	-	√	√	√	√
Assumed Data in Model	Chip Geometry	-	-	√	-	√	√	-	√	-	-	-	-	-	-	-	-
	Flow Lines	-	-	-	-	√	√	√	-	-	-	-	-	√	√	√	√
Material Model	Rigid-plastic	-	-	√	√	√	-	-	√	-	-	-	-	-	-	-	-
	Elastic-plastic	√	-	-	-	√	√	√	-	-	√	√	-	-	-	-	-
	Visco-plastic	-	-	-	-	-	-	-	-	-	-	-	√	√	√	√	√
	Elasto-viscoplastic	-	√	-	-	-	-	-	-	√	-	-	-	-	-	-	-
	Temperature Effects	√	√	-	-	-	√	-	-	√	-	√	√	√	√	√	√
Cutting Variables Simulated	Depth of Cut	√	√	-	-	-	-	√	-	-	-	√	-	√	√	-	√
	Cutting Speed	√	√	-	-	-	-	√	-	-	√	√	√	-	-	√	√
	Feed Rate	-	-	-	-	-	-	-	-	-	-	-	-	-	-	-	-
	Rake Angle	√	-	√	√	√	-	-	√	-	√	√	-	√	√	√	√
	Friction	√	-	-	-	√	√	-	-	-	√	-	-	√	√	√	√
Simulation Results	Stress/Strain	√	-	√	√	√	√	-	√	√	√	√	-	√	√	√	√
	Chip Geometry	-	√	-	-	-	√	√	-	√	√	√	√	-	-	-	-
	Velocity	√	-	-	-	-	-	-	-	-	-	-	-	√	√	√	√
	Tool Forces	√	-	-	-	-	√	-	-	√	-	√	-	√	√	√	√
	Tool Wear	-	-	-	-	-	-	-	-	-	-	-	-	√	-	√	√
	Shear Angle	-	√	-	√	-	-	-	-	-	√	-	-	-	-	-	-
	Residual Stress	√	-	-	-	-	-	-	-	√	-	-	-	-	-	-	-
	Plastic Strain	-	√	-	-	√	-	√	-	√	√	√	-	√	√	√	√
Comparison with Experimental Results		√	-	-	√	√	-	√	-	-	-	√	-	√	√	√	√
Special Features/Comments (See Table 3.3d)		J	-	K	-	L	M	N	-	-	O	-	P	-	Q	-	-

Table 3.3c. Summary of literature in Metal Cutting Simulations using Finite Element Methods (Continued from Table 3.3b)

A	Simulations limited to non-ferrous materials.
B	Produced a chip formation using 3-D analysis that compared favourably to experimental chips under the same conditions.
C	First metal cutting simulation with a fully coupled Arbitrary Lagrangian Eulerian (ALE) frame. Results compared favourably with experimental data.
D	Initial work used a combined Lagrangian Eulerian simulation
E	Finite element model based on a novel flow criterion developed by Zienkiewicz [75].
F	Finite element model studied effects of residual stresses in workpiece due to machining. Simulation results compared favourably with XRD measurements made experimentally.
G	First metal cutting simulation to use adaptive remeshing, quadratic contact and no initial assumptions about predicted chip geometry and stress-strain distributions.
H	Finite element model created using a predefined separation line. The critical separation parameter, D , was used to calculate the distance between the cutting tool nose and link element.
I	Finite element model created using a predefined separation line. The critical separation parameter, D , was fine tuned by experimental results of residual stress in the workpiece.
J	2-D simulations were initially carried out to predict 3-D results.
K	Finite element model created using a predefined separation line. The critical separation was based on the material reaching its effective strain.
L	Modifying the cutting tool geometry simulated the effect of a BUE and crater wear.
M	Needed 2-D data to predict 3-D results. Did not use the finite element method.
N	First shear localised chip formation under finite element modelling. A user subroutine was developed for the workpiece material.
O	Finite element model simulated segmental chip formation based on the strain state within the chip material.
P	Finite element model simulated segmental chip formation using a user subroutine to account for material-material contact.
Q	<i>First known metal cutting simulation to model orthogonal machining using coated carbide cutting tools. An uncoated, single-coated and double-coated tool were compared.</i>

Table 3.3d. Summary of special features and comments related to Table 3.3b and 3.3c

The objective of this section is to review the use of the finite element method in metal cutting simulations. A summary of all papers reviewed is given in Table 3.3b and 3.3c. The majority of FE models used to simulate metal cutting have concentrated on 2-D plane strain analysis. Most of the 3-D simulations that have been performed have modelled orthogonal machining [44,45,46,47,48]. The results obtained from these 3-D simulations do not appear to contain any more relevant information that can be obtained from an equivalent 2-D simulation. In fact, most 2-D simulations [49,50,51,52,53,54,55,56,57,58,59,60] predict more results, with greater accuracy, than those obtained from a 3-D simulation. The computational time for a 2-D simulation is generally much less than that for a similar 3-D model. Some 3-D simulations considered the oblique machining process [47,48] which can be useful in estimating the effect of the cutting tool approach angle and the extent of notch and groove wear.

The choice of the finite element “frame” has also varied from author to author depending on the package that is used. Both Eulerian and Lagrangian FE formulation has advantages and disadvantages. The modelling of metal cutting is a large strain and displacement process. The simulation of the chip forming involves severe mesh distortion which makes the convergence of the solution an important issue. There are two ways to avoid excessive mesh distortion, the first is to introduce some type of mesh/node separation criterion [13,55]. However, with the chip curl, careful selection of the element shape is still needed to avoid distortion. The second technique is to incorporate a remeshing algorithm [54] into the simulation, which enables an arbitrary surface of separation and chip curl to be predicted. However, these remeshing algorithms are computationally expensive. The most efficient FE formulation for any simulation involving large strain and displacement is that of a combined Eulerian-Lagrangian formulation called the Arbitrary Lagrangian Eulerian (ALE) formulation. The use of this ALE formulation enables a continuous update of the free and contact surfaces of the model, without needing continuous iterative computations as required in a purely Eulerian formulation. The ALE approach avoids the need for continuous remeshing by a grid movement control algorithm [61] and also enables successful modelling of the tool-chip contact without requiring a separation criterion in the region of the chip-root and the tool cutting edge.

Since a metal cutting process involves high plastic strains and high temperatures, it is important that a coupled thermo-mechanical analysis should be used within the FE simulations. The FORGE2 package uses this type of technique which calculates the heat generation due to viscoplastic deformation and uses this generated temperature field to update the mechanical properties at each load/displacement increment. This is explained in detail in Section 5.2. Much of the earlier research involving metal cutting neglected taking into account temperature effects and treated the problem as a pure mechanical formulation [44,45,46, 49,50,62,63]. Given the fact that most material properties vary considerably with temperature, thermal effects have to be taken into account. Machining also involves very high strain rates, hence there is a need to incorporate material strain rate effects into the material model in any FE simulation. Again, the earlier research work assumed that the workpiece material was elastic-rigid plastic and so these material models neglected to account for any strain hardening or strain rate effects [62,46,63,57]. Since a material's shear flow stress varies considerably with the cutting speed [76], the accuracy of these earlier models could be questioned.

As discussed previously, the mechanism of chip formation is dependant on many variables such as the cutting speed, depth of cut, shear plane angle, cutting temperature, cutting edge preparation etc., Finite element modelling of metal cutting should enable an arbitrary surface of separation and allow unconstrained chip curl. Many finite element models adopt a specific material separation line on which chip formation occurs [64,59,46,51,63,52,53,47,13,65,56,66].

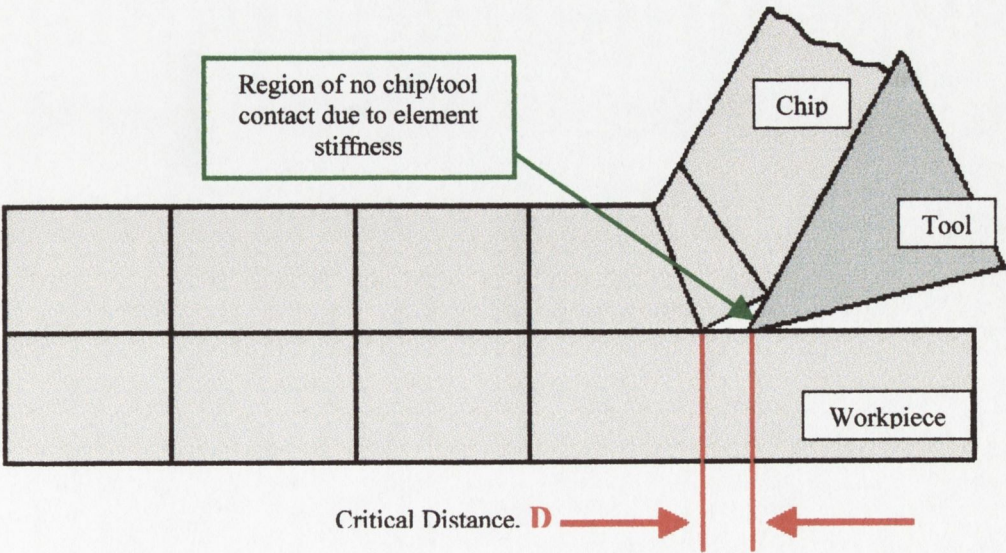


Figure 3.3. Conditional link elements for chip formation [13]

Two different techniques are available to enable chip separation to be modelled. The first technique, developed by *Zhang et al* [13], allows the nodes closest to the tool cutting edge to split once a critical distance, D , has been reached. Before the nodes along the separation line split, they are assumed to have identical nodal displacements to simulate a continuous body of material. The second technique, developed by *Zemitis* [77], is similar to the first except an element kill technique is used rather than node splitting. The element “death” involves reducing an element stiffness by a factor of $1e^{-9}$ at a specified time or separation distance, D , thus eliminating material fracture.

The predicted results, using either technique, have been found to correspond well to experimental data. However in some cases [78], it is reported that the reason for good correlation is due to “fine tuning”. That is, the selection of the critical separation parameter, D , is obtained by varying its size, until good correlation is obtained with experimental stresses, temperatures and forces.

Shih [55,67] measured the residual stress in AISI 1020 carbon steel using an x-ray diffraction technique and fine tuned the critical separation parameter, D , to obtain the best possible correlation between finite element and experimental results. Based on his conclusions, it appears that a simulation of this type is only accurate for the particular workpiece material to which the critical separation distance has been tuned. This limits the application of his model to other materials and other machining variables, i.e. cutting speed, depth of cut, rake angle etc.

Other problems which are associated with the nodal or element separation models is that because of individual element stiffnesses the true curvature of the chip formation can not be reproduced as accurately as that of a simulation with adaptive remeshing. A consequence of inaccurate chip curvature is that the tool-chip contact length is underestimated and thus the amount of heat generated due to frictional sliding between the chip and tool will also be underestimated. This will also lead to an underestimation in the tool forces and stresses. Only a few finite element simulations of metal cutting processes have compared the chip shapes predicted by FE to those obtained from experimental tests [65,68,47,62]. However in all cases, the predicted FEM chip form compared well with those experimentally obtained using in-situ SEM photography.

Most of the research into chip formation during orthogonal metal cutting concentrates on continuous chip formation, however some work into segmental chip formation has taken place [65,69,70]. The finite element simulations of *Marusich et al* [70] and *Ceretti et al* [69] were able to model segmental chip formation and shear localisation leading to the formation of shear bands and saw tooth chip forms. In both cases, a special user implemented algorithm was used to account for the material-material contact. Node splitting took place to allow the segmental chips to form.

The volume of published material of coatings on elastic tools is limited. *MacGinley et al* first published the effect that uncoated, single and double-coated tools had on machining Inconel 718 [73]. Due to the protection afforded by the coatings, the stresses in the tool decreased while the stresses remained the same in the workpiece. The maximum effective stress in all cases was found in the nose radius of the tool. *Brazil* [57] modelled a single diamond coated elastic tool to determine the effect the base substrate material has on the stress distribution. Tungsten Carbide (WC) and Silicon Nitride (SiN) were used as the substrates. It was shown that the WC substrate induced a higher stress discontinuity across the interface between the diamond coating and substrate and this led the author to conclude that tool failure would occur for a tool with a WC substrate before one with a SiN substrate.

Some previous research has also accounted for tool wear [54,57,71,72,73,74] but none yet on catastrophic tool failure. The FORGE2 code seems to be the only package capable of simulating tool wear by being able to calculate the magnitude of the abrasive tool wear on the rake face. It takes into account the normal stress and tangential sliding velocity (Section 5.2.5). Additionally, there has been little published work taking into account tool chatter with coupled modal analysis and dynamic responses of the tool [49,60]. This phenomenon affects the integrity of the workpiece surface with a variation in surface roughness.

PART III
Experimental Procedures & Finite
Element Method

4.0 Experimental Procedures

The experimental test program was carried out primarily to obtain input material properties for finite element modelling (FEM) of the machining process and to obtain experimental machining data for comparison with the results predicted by FEM. The initial mechanical testing of Inconel 718 was done to establish the response of the materials to large strain deformation, similar to that associated with a turning operation. The experimental test program was divided into two main streams "Mechanical Testing" and "Machinability Tests".

4.1 Material Characterisation

The work material consisted of billets of fully heat treated (solution treated and age hardened) Inconel 718 alloy, a half metre in length and 100mm in diameter. It was supplied as precipitation hardened (aged) material of AMS 5663 specification. The composition of the workpiece is detailed in Table 4.1.

Element	C	Mn	Si	S	P	Cr	Ni	Mo
% Composition	.036	.12	.11	.001	.007	18.4	52.14	2.97

Element	Co	Cu	Al	Fe	B	Nb	Ta	Ti
% Composition	.31	.05	.57	19.12	.0039	5.09	.01	1.01

Table 4.1. Workpiece Composition – Precipitation Hardened Inconel 718

4.2 Mechanical Testing

The primary objective of the mechanical testing was to obtain accurate input data for the finite element modelling (FEM). Mechanical tests were carried out including:

- Uniaxial Tension / Compression Tests
- Strain rate sensitivity tests

4.2.1 Uniaxial Tension / Compression Tests

A full elasto-plastic material stress-strain curve is required for any non-linear FE model, since it defines the strain state and stress hardening of the material for any given displacement. When modelling the strain hardening behaviour of a material, the yielding behaviour can be different in tension than in compression. Essentially, a tensile test causing slight yielding permanently deforms (causes slip in) unfavourably oriented crystals in preference to other crystals in a specimen. Consequently, upon unloading, some of the permanently deformed crystals are in compression. After re-loading with tension, the onset of yield is raised because the deformed crystals do not reach their new slip stress until the load is higher than that reached on the previous load cycle. If the material is compressed after tensile loading, the deformed crystals reach their compression slip stress before the rest of the crystals, with the result that compression yielding starts sooner than in a fresh unstrained specimen. Thus the tensile test cycle raises the elastic limit in tension, but lowers the elastic limit in compression. This is referred to as the Bauschinger effect [79]. With the addition of a hardening law in the FE simulations, this phenomenon can be dealt with. An isotropic hardening law assumes that the yield in tension is equivalent to the magnitude of the yield in compression.

Compression test samples were machined from a bar of Inconel 718. Cylinders 8mm in diameter and 15mm in height were parted from the main billet. They were subsequently finish machined to their required test height (12mm) as shown in Figure 4.2.1a. This aspect ratio of 1:1.5 was maintained for all the samples.

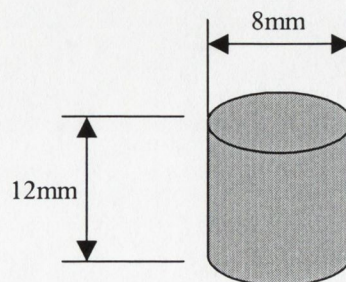


Figure 4.2.1a. Compression samples dimensions.

The compression tests were carried out on an Instron 8501 testing machine which was controlled with the aid of Instron Series 9 software. The specimens were compressed between two carbide plates to ensure that no die deformation occurred during testing. A thin layer of PTFE tape was attached between the test cylinders and both the top and bottom plates in an effort to eliminate the effects of friction and help promote homogeneous deformation. Figure 4.2.1b shows test specimens before, during and after compression took place. The compression tests were carried out to two different levels of strain (100% and 50%). Each test was repeated multiple times to ensure accuracy in the results. The average of each test was used to produce the compressive stress-strain curve for the material.

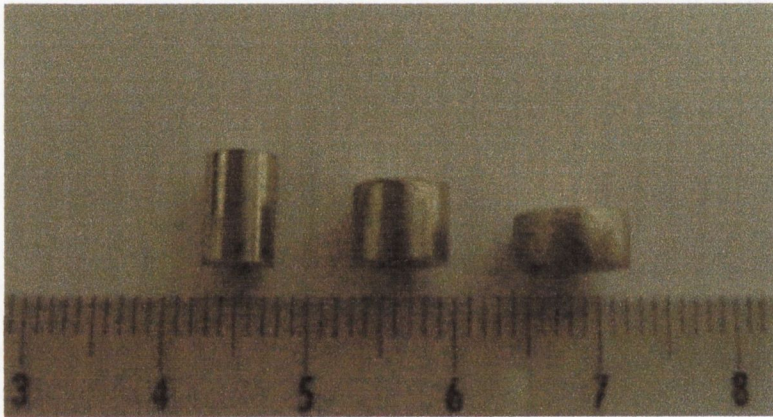


Figure 4.2.1b. Compression samples in both tested and untested conditions.

Due to limited workpiece material, tensile tests were not performed on site, however the author was given access to confidential results from numerous tests, for the same material at various temperatures and strain rates [80]. The tensile blanks were produced from a combination of Electro Discharge Machining (EDM) and finish machining operations to minimise the effect of thermal damage in the samples. Initially, the tensile tests were performed at a constant strain rate of 0.1mm/sec. The data from both the compression and tensile tests was used to plot the materials non-linear plastic behaviour that was needed for the finite element analysis. Using Ludwicks power law as shown below in Equation 4.1 [81], we can find n and K , which are constants used to model the non-linear behaviour of a material.

$$\sigma = K\varepsilon^n$$

Eqn. 4.1

Both these constants can be found through linear regression of the log stress-strain curve. The slope of the line, n , represents the strain hardening index, while the intercept of the y-axis line represents the strength coefficient constant, K .

4.2.2 Strain rate sensitivity tests

When a material is being turned, it can be subjected to extreme conditions of temperature and strain. Thermo-mechanical loading can occur rapidly with a corresponding increase in cutting speed. Due to the fact that the stress-strain behaviour of any material is dependant on the loading, or strain rate, it is necessary to establish the response of a material to rapid loading conditions in order to effectively model a machining process using FEA.

Tensile and compression tests were performed at four different strain rates. The geometrical designs of both test specimens were identical to those mentioned above for deformation rates of 0.1mm/sec. Each specimen was subjected to further deformation rates of 1mm/sec, 10mm/sec and 100mm/sec under compression and tension. The power law shown in Equation 4.2, was fitted to the test data to obtain the sensitivity to hardening index, m . This can be found by the linear regression of the stress-strain log curve.

$$\sigma = C \dot{\epsilon}^m \quad \text{Eqn. 4.2}$$

The slope of the line is m while the y-axis intercept is the constant C , the strength coefficient.

4.3 Cemented Carbide Cutting Tools

In industry, coated carbides are still the first choice for machining Inconel 718. Various reasons for this have already been outlined in Section 2.2 but it is generally because of the resistance of the coatings to thermal and mechanical loads. Carbide tools are available in numerous grades and geometries, and the commercial cost has decreased to the point where they are the most economical tool to use. The various carbide cutting tools

used in this study are outlined in Table 4.3, included with the type of chipbreakers used, if any.

Basic Insert (SNMA)	Chipbreaker (SNMG)	Substrate & Coatings
Sandvik H13A	-MF, -QM	WC
Sandvik S1P	-15	WC
Sandvik GC1015	-QM	WC-TiN
Sandvik GC3015	-MF, -QM	WC-TiC-Al ₂ O ₃
Sandvik GC215	-QM	WC-TiC-TiN
Sandvik GC415	-QM	WC-TiC-Al ₂ O ₃ -TiN
Krupp Widia THM		WC
Krupp Widia HK15		WC-TiN

Table 4.3. Carbide Inserts used in Experimental Tests

Following ISO recommendations [23], tool geometry was standardised on two types of square inserts, SNMA or SNMG 120408 ISO grade tools as seen in Figure 4.3a. The difference between SNMA and SNMG is the addition of a chipbreaker. All tools had a nose radius of 0.8mm. The main reason for the selection of this tooling was to provide eight cutting edges when the insert was clamped into the shank. This toolholder was a Sandvik PSSNR 2020K12 (Figure 4.3b) which provided a plan approach angle of 45°, a normal rake angle of -6° and an inclination angle of -6°. Negative top rake was selected to give the tool more strength during machining. Chipbreaking geometries are sometimes used due to the long continuous chips formed at various cutting speed and feed combinations when machining Inconel 718.

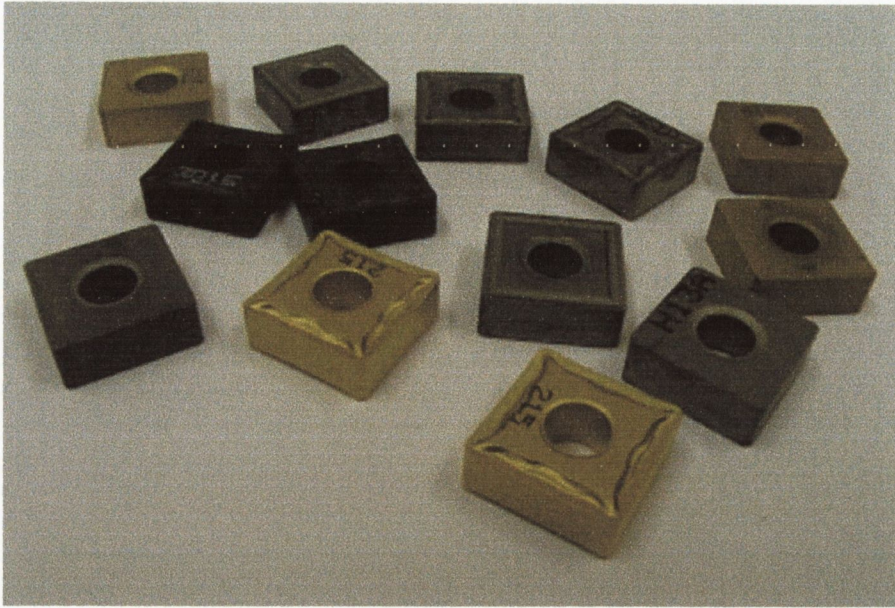


Figure 4.3a SNMA & SNMG 120408 Inserts

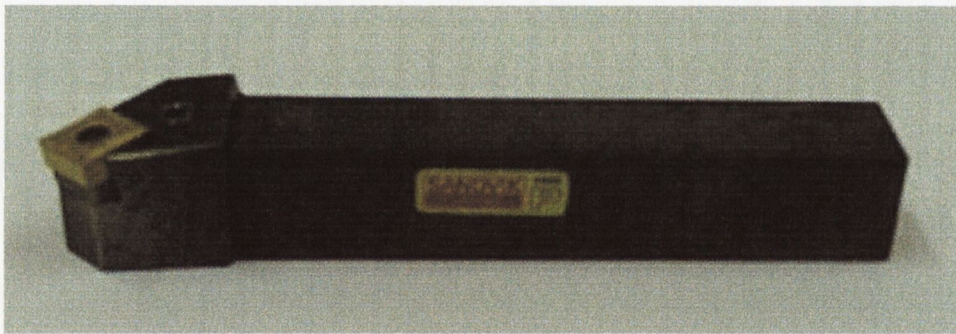


Figure 4.3b PSSNR 2020K12 Tool Holder

4.3.1 Machinability Tests

Finish machining tests were performed rather than rough turning because of the excessive tool wear experienced and the high cost of the Inconel 718 workpiece material. Machining large volumes of Inconel 718 should be avoided in order to minimise component cost. Machinability tests were performed in order to study the performance of each carbide under the following headings:

- Chip Control
- Tool Life
- Cutting Edge Strength
- Surface Roughness

Chip control is one of the most important factors as Inconel 718 tends to have long, continuous chips at most cutting speeds and feeds. These chips will wrap themselves around the machined component and destroy the surface finish.

Cutting edge strength of a tool is very important, as low edge strength will result in chipping and hence premature failure. With this failure, surface roughness increases whilst the cutting forces also increase. Monitoring the cutting forces gives the first indication of a problem with the cutting edge and the necessity of replacing the tool.

Tool life is important from a manufacturing point of view because it lowers the time a machine is idle whilst tool changes are made. Flank and crater wear were measured throughout this test program to give an indication of when a tool starts to fail catastrophically.

The surface finish achieved on Inconel 718 is generally good provided a sharp edge tool is used under higher cutting speeds. As the speeds decrease, a rubbing action between the tool and chip occurs which increases the cutting forces due to the rounding of the nose radius of the tool. Surface finish measurements were taken throughout the machining operation.

A selective set of tests had to be carried out in order to characterise the performance of each cutting tool. All cutting tests were carried out with two different depths of cuts, 0.8mm and 1.6mm. Experiments were carried out on an URSUS 225 centre lathe (Figure 4.3.1a). The spindle speed was set to vary between 20m/min, 40m/min and 60m/min. Coolant was sometimes used in tests however in most cases machining took place dry. The coolant used in these cases was Metacut 900 in 10% concentration with water.

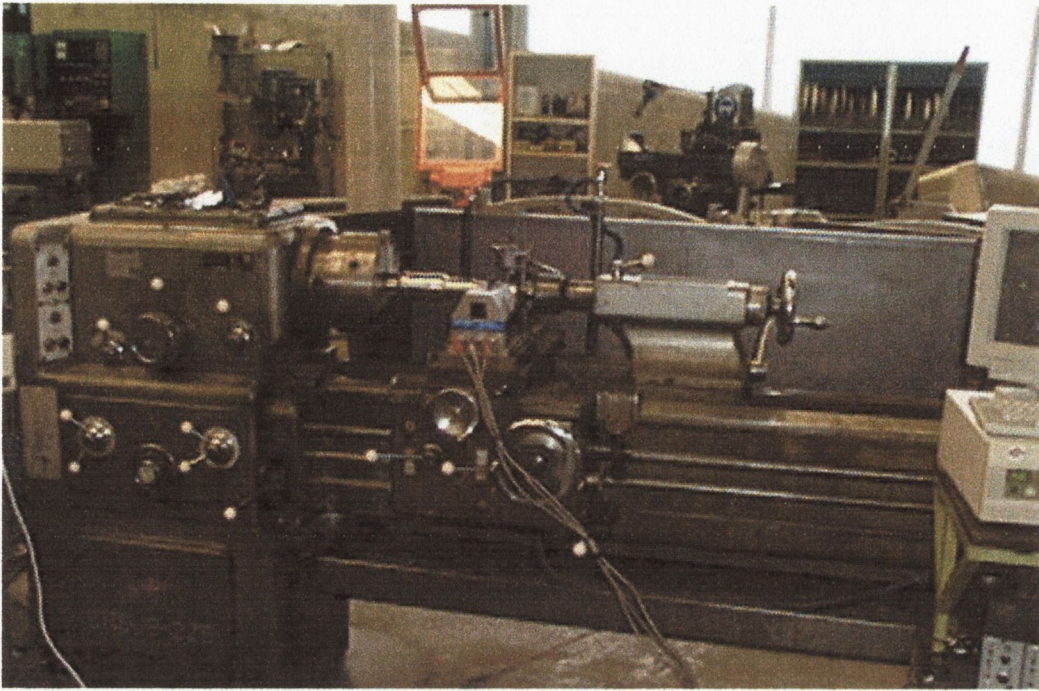


Figure 4.3.1a. URSUS 225 Centre Lathe

The schedules of the machining tests were to machine each bar of Inconel 718 at the predefined cutting speeds. During every test the cutting forces were recorded along with a typical sample of the swarf for that test. Every two minutes, tool wear measurements were taken using the Mitutoyo toolmakers microscope. Surface finish measurements were also taken along the machined surface. This was continued till catastrophic tool failure occurred or until the cumulative time of the total cut surpassed twelve minutes. Each of these tests was repeated twice for each of the tools used, unless a noticeable difference was observed between the results. If this happened the test was rerun.

The cutting edge of each tool was examined carefully under a Mitutoyo toolmakers microscope to detect damage or chipping. The opposite edge of the insert was used for each subsequent cutting test. For those tests where catastrophic tool failure occurred (usually uncoated carbides), the billet was re-machined down to a constant diameter. This procedure ensured that the depth of cut was constant for the next machining test.

An effect of a machining process is the generation of heat by plastic deformation within the workpiece and by chip/tool frictional contact. This heat energy is detrimental as the life of all tools will decrease with a corresponding increase in temperature. At elevated temperatures tool wear mechanisms such as diffusion start to occur. Coupled with this, the

hardness of the tool decreases rendering the tool less resistant to abrasion. Most of the heat generated during metal cutting passes away in the chip and the rest is conducted into the tool or remains within the workpiece. Increasing the cutting speed increases the cutting temperature.

4.3.2 Cutting Force Measurement

Sudden changes in the magnitude of the cutting forces are predominantly the most important indication of a problem with the machining process. An increase in tool forces during a cutting operation generally indicates a breakdown of the cutting edge. As tool wear increases so do the cutting forces. Therefore, it is important to record each component of the cutting force, and this was achieved by mounting the tool holder in a Kistler 9263A piezoelectric tool-force dynamometer. This was connected to three Kistler 5006 charge amplifiers that were connected to an Amplicon PC26AT Analog/Digital input board as shown in Figure 4.3.2a. A windows based data acquisition package, DASH300, was used to obtain readings of the axial, radial and tangential components of the cutting forces from the PC26AT A/D converter. A graphical representation of these forces is shown below in Figure 4.3.2b.

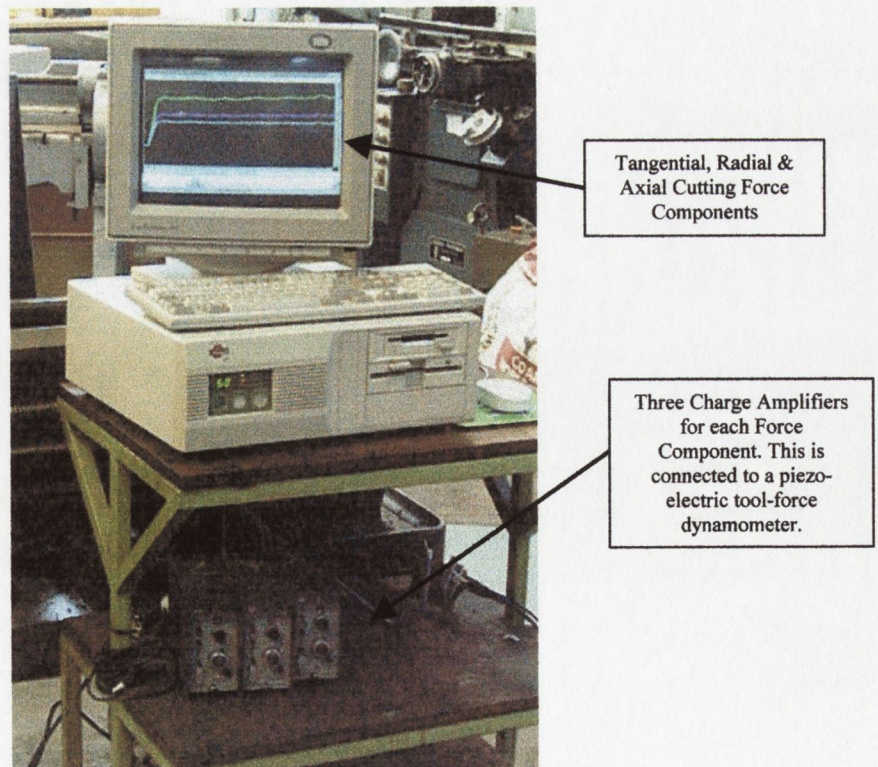


Figure 4.3.2a. The Dynamometer with A/D Package

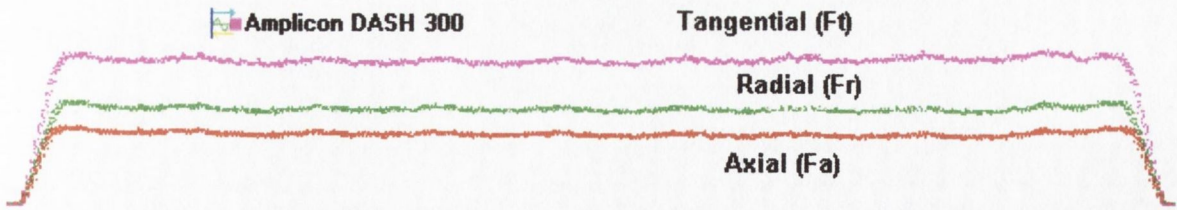


Figure 4.3.2b. Cutting forces obtained using DASH300

These forces were saved in MSDOS text format and imported into Microsoft Excel for analysis. The initial and final ten seconds of each test was ignored so that only the steady state machining operation was examined.

4.3.3 Surface Roughness Measurement

The surface finish of each machined surface was measured when the bar was in-situ after each machining pass. A portable Hommel T1000 stylus surface roughness profiler was used. The surface roughness values were measured at three locations along each axis of the bar (0° , 90° , 180° and 270°). It was noticed that as measurements were taken along the length of the bar, the surface roughness values increased which indicated that tool wear was progressing. Both R_a , the centre line average, and R_{mD} , the maximum peak to valley height, were measured.

4.3.4 Tool Wear

When machining a component, it is of utmost importance to be able to predict when a tool has exceeded the ISO tool wear standards [23]. If machining continues beyond this level of wear, the component could be machined past tolerances due to uneven cutting edges, and warped due to increased heat affected zones caused by increased cutting forces. Also, if a tool wears prematurely or breaks catastrophically, it interrupts production and causes damage to the component. Tool life tests were performed in order to analyse tool wear patterns with differing machining conditions. A Mitutoyo toolmakers microscope (Figure 4.3.4a.) was used to measure flank wear on each insert. When a build up edge was detected on the tool it was removed by dissolving it in a 10% sodium hydroxide solution (NaOH) before tool wear measurements were taken. Tool wear can occur in a number of

places as shown from the ISO standards in Figure 2.3.1b. A number of readings were taken and averaged.

Crater wear was measured using a Vickers light microscope (Figure 4.3.4b). The unworn rake face was focused and centred on the graduated height adjustment dial. The microscope was then focused on the centre of the crater region and the change in the reading on the height adjustment dial was the depth of the crater wear on the tool's rake face. Again, a number of readings were taken and averaged.

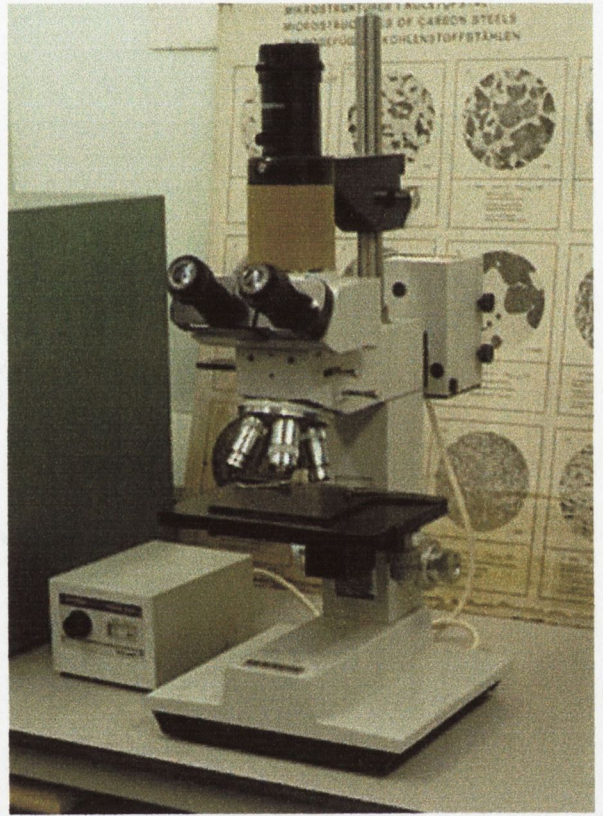
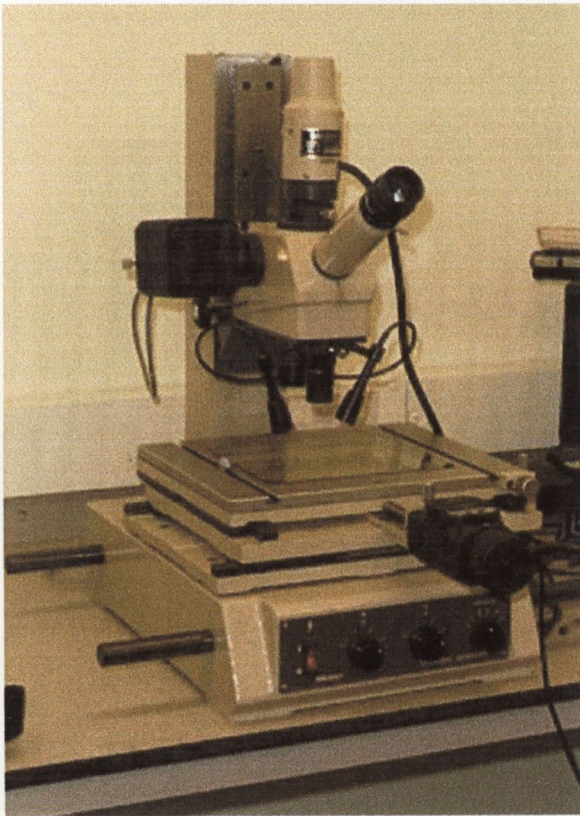


Figure 4.3.4a. Mitutoyo Tool Makers Microscope **Figure 4.3.4b. Vickers Light Microscope**

5.0 Finite Element Method

The finite element method was developed towards the end of the 1940s and early 1950's and was first used in the structural design of aircraft. It gained prominence in the 1960's in parallel with developments in the processing capabilities of computers which made it possible of solving thousands of simultaneous equations in seconds.

The finite element method is a computer based numerical technique used to solve complex problems by assuming that the area under investigation can be subdivided into smaller more manageable units, or elements, of finite size. When a solution is obtained for one of these sections, the solution procedure is applied to the remaining sections in turn. The final overall solution is based on a summation or assembly of all the partial solutions to produce the overall model geometry. After producing the initial design, the performance of the model must be checked for its adequacy. A suitable computational model must be chosen to provide some insight into its behaviour. If the solution is inadequate then the model must be modified. Only when a satisfactory model has been obtained, can the performance of the overall design be assessed.

For structural or solid mechanics problems, the process uses the relationship between the force exerted upon a simple spring and the displacement that is caused when this occurs.

$$F = k_1 \delta \qquad \text{Eqn. 5.1}$$

where F is the force exerted, k_1 the stiffness of the spring and δ its displacement. The stiffness of the spring corresponds to the slope of the force versus displacement curve. Knowing this value of stiffness and the force applied, the displacement may be obtained. If a single node in a model is considered, a force on that node deforms the element, this displacement value can then be used to calculate strain, and hence stress, using the constitutive equations. However each of the nodal forces may produce a displacement not only to the node on which it is applied, but also to each of the other nodes of the element.

The mathematical model representing the force-displacement relationships requires certain information before it can be solved. These include:

- Co-ordinates of each nodes
- Material properties of all materials
- Loading conditions & magnitudes.
- Boundary conditions – how model is restrained
- Type of analysis to be performed – static, dynamic, thermal

A large number of equilibrium conditions are then formulated and these have to be satisfied by solving thousands of equations (depending on size of model) with the aid of finite element software. The resulting nodal displacements are then used to obtain the stress and strain distributions within the body. As the amount of data required to have the model simulate an actual component is large – the overall computation time is great. Therefore a relationship between CPU time and accuracy is formed.

Prior to using any FE models for an engineering application, it is most important that the modelling techniques and boundary conditions used are defined as accurately as possible. The most assured method of verifying the FE results is by direct comparison with experimental results. Once the predictions from the FE results have been shown to correlate with the experimental data, the FE technique can be used to its full potential. FE models can be produced which can optimise a wide range of machining variables which would be difficult to do so experimentally. These would include coating sizes, thickness & materials in this area of research. Also other variables as cutting speed, rake angles, tool edge preparation are tested.

These numerical simulations were initially divided into two categories which were:

5.1 Modelling the multi-coated tools (ANSYS 5.4)

5.2 Modelling the orthogonal metal cutting process (FORGE 2.7.4)

Each of the two analyses above used different modelling techniques and the second one was verified by comparison to experimental data.

5.1 Modelling the multi-coated tools (ANSYS)

The FE simulations, which were performed on the coated and uncoated cutting tools, were modelled as either elastic or elasto-plastic. In the ANSYS FE code the elasto-plastic behaviour of a material is modelled using the Von Mises yield criterion, the Prandtl-Reuss flow rule and a multi-linear isotropic hardening rule.

5.1.1 Yield Criteria

The yield criterion is a measure of the stress state that may be readily compared to the yield stress of a material obtained from uniaxial tensile or compression tests. Depending on the yield criterion used (Von Mises or Tresca) this value may be referred to as the Von Mises or Tresca effective stress. For isotropic materials, plastic yielding can be expressed as being dependent on the magnitude of the principle stresses. Therefore, the yield stress can be expressed as:

$$f(\sigma_1, \sigma_2, \sigma_3) = \text{Const} \quad \text{Eqn. 5.2}$$

The yield stress has been shown to be strongly dependent on the deviatoric stress tensor as this causes the material to yield plastically and to a lesser degree on the hydrostatic or mean stress [82]. A deviatoric stress (σ_{ij}') can be obtained by subtracting the hydrostatic stress (σ_o) from each of the normal stress components. Since the principle deviatoric stress components are not zero, the yield criterion can be written as [83]:

$$f(J_2, J_3) = \text{Const} \quad \text{Eqn. 5.3}$$

where:

$$J_2 = -(\sigma_1' \sigma_2' + \sigma_2' \sigma_3' + \sigma_3' \sigma_1') \quad \text{Eqn. 5.4}$$

and:

$$J_3 = \sigma_1' \sigma_2' \sigma_3' \quad \text{Eqn. 5.5}$$

This formulation is generally referred to as J_2 flow theory. The Von Mises criterion states that yielding will occur when the J_2 component reaches a critical value, but assuming that the J_3 is not critical. Modifying Equation 5.4 will give:

$$J_2 = \frac{1}{2}(\sigma_1'^2 + \sigma_2'^2 + \sigma_3'^2) = \frac{1}{2}\sigma_{ij}'\sigma_{ij}' = \kappa^2 \quad \text{Eqn. 5.6}$$

or:

$$J_2 = (\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 = 6\kappa^2 \quad \text{Eqn. 5.7}$$

or:

$$J_2 = \sqrt{(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + 6(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2)} = 6\kappa^2 \quad \text{Eqn. 5.8}$$

where κ is a parameter dependent on the material properties and is obtained from a uniaxial tension or compression test. The interpretation of κ for a Von Mises yield criterion used in the ANSYS FE code gives:

$$\kappa = \frac{1}{\sqrt{3}}\sigma_y \quad \text{Eqn. 5.9}$$

Therefore yielding occurs when the stress in the object reaches the yield stress of the material obtained from a tensile test. Substituting Equation 5.9 into Equation 5.8 gives:

$$\sigma_y = \sqrt{(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + 6(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2)} \quad \text{Eqn. 5.10}$$

5.1.2 Flow Rule

The material flow rule used with the Von Mises yield criterion is the Prandtl-Reuss flow equation. This law describes the direction of the individual plastic strain components when yielding occurs. The general form of the equation is [83]:

$$\dot{\epsilon}_{ij}^P = \frac{\delta g}{\delta \sigma_{ij}} h \dot{f} \equiv \frac{\delta f}{\delta \sigma_{ij}} \dot{\lambda} \quad (\text{Assuming } f = g) \quad \text{Eqn. 5.11}$$

$$d\dot{\epsilon}_{ij}^P = \frac{\delta g}{\delta \sigma_{ij}} h \delta f \equiv \frac{\delta f}{\delta \sigma_{ij}} \delta \lambda \quad \text{Eqn. 5.12}$$

where h and g are scalar functions of the invariant of the deviatoric stress. f is the yield function and $\delta \sigma_{ij}$ is called the plastic multiplier which determines the amount of plastic straining. $\delta \lambda$ or $\dot{\lambda}$ are positive proportionality constants, being equal to $h \dot{f}$ or $h \delta f$ respectively. This shows that the flow equation is a function of the yield stress and can be expressed in rate form as:

$$\dot{\epsilon}_{ij}^P = \sigma'_{ij} \dot{\lambda} \quad \text{Eqn. 5.13}$$

Since

$$\sigma'_{ij} = \frac{\delta f}{\delta \sigma_{ij}} \quad \text{Eqn. 5.14}$$

Equation 5.13 can be rewritten and expressed in terms of the deviatoric stress components and the plastic strain to give:

$$\frac{\dot{\epsilon}_x^P}{\sigma'_x} = \frac{\dot{\epsilon}_y^P}{\sigma'_y} = \frac{\dot{\epsilon}_z^P}{\sigma'_z} = \frac{\dot{\gamma}_{xy}^P}{2\tau'_{xy}} = \frac{\dot{\gamma}_{yz}^P}{2\tau'_{yz}} = \frac{\dot{\gamma}_{zx}^P}{2\tau'_{zx}} \quad \text{Eqn. 5.15}$$

So combining the elastic strain rate components and the plastic strain rate components according to $\dot{\epsilon}_{ij} = \dot{\epsilon}_{ij}^E + \dot{\epsilon}_{ij}^P$, gives the Prandtl-Reuss equation for elasto-plastic materials as:

$$\dot{\epsilon}_{ij} = \sigma'_{ij} \dot{\lambda} + \frac{1}{2G} \sigma'_{ij} + \delta_{ij} \left(\frac{1-2\nu}{E} \right) \dot{\sigma}_m \quad \text{Eqn. 5.16}$$

5.1.3 Hardening Rule

The hardening rule describes the changing of the yield surface with progressive plastic straining, so that the conditions of stress and strain for subsequent yielding can be established. The hardening rule in ANSYS was the multi-linear isotropic hardening rule. With isotropic hardening, the rule assumes that the yield stress expands uniformly in all directions. The multi-linear isotropic hardening rule implies that, upon load reversal, the material will not re-yield unless the maximum applied stress exceeds the previously induced stress from the last step and the stress-strain curve continues to follow the last slope used [83].

5.2 Modelling the orthogonal metal cutting process (FORGE2)

The effective modelling of an orthogonal metal cutting process requires a fully coupled thermo-mechanical simulation to account for the large temperatures and strains that occur during machining. A fully adaptive non-linear re-meshing algorithm is required to predict the formation of the machined surface and other workpiece factors such as chip curl, thickness and tool/chip contact.

5.2.1 Method of Analysis

The workpiece material was modelled as a single phase non-linear elasto-viscoplastic substance. The FORGE2 FEA code, developed at CEMEF [84], was used for all simulations. The code splits the deformation increment into sufficiently small increments and, at each time-step, solves the material flow and heat transfer problems independently of each other. The results of the material flow analysis are used to update the workpiece geometry and the internal heat generation, caused by plastic deformation or friction, enable fresh computation of the material properties in accordance with the altered temperature fields.

It is commonly assumed that the elastic component of the total strain in the workpiece can be neglected, as the plastic strain component is the more dominant during machining. However, in the following FEA simulations, the residual stresses in the

machined workpiece are required and so the elastic component has to be included in the total strain. The elastic strain was calculated using the linear elasticity law presented in Equation 5.17:

$$\Delta \varepsilon = \frac{I + \nu}{E} \sigma + \frac{3\nu}{E} P \quad \text{Eqn. 5.17}$$

where $\Delta \varepsilon$ is the elastic strain tensor, I is a unit tensor, ν is Poisson's ratio, σ is the stress tensor, E is Young's modulus and P is the hydrostatic pressure given by:

$$P = -\left(\frac{\sigma_1 + \sigma_2 + \sigma_3}{3} \right) \quad \text{Eqn. 5.18}$$

5.2.2 Finite Element Analysis of Orthogonal Machining

The constitutive equation used by the FORGE2 FEA code for metal cutting simulations is based on the NORTON-HOFF behaviour law as given below [84]:

$$\{\sigma'\} = 2K_1(T, \varepsilon) (\sqrt{3}\dot{\varepsilon})^{m-1} \{\dot{\varepsilon}\} \quad \text{Eqn. 5.19}$$

This constitutive equation relates the deviatoric stress tensor $\{\sigma'\}$, to the effective plastic strain rate tensor $\{\dot{\varepsilon}\}$, through the consistency K_1 , and the sensitivity to strain rate, m . The consistency, K_1 , can have various forms which describe the thermo-mechanical conditions of the workpiece. However, for these particular metal cutting simulations, the consistency equation takes into account a strain hardening power law combined with Arrhenius law [85] for any temperature, T , to describe the material flow:

$$K_1(T, \varepsilon) = K_o (\varepsilon + \varepsilon_o)^n e^{\frac{\beta}{T}} \quad \text{Eqn. 5.20}$$

This law requires the definition of various constants. K_o , is a function of the uniaxial yield strength of the material and requires an analysis of the stress-strain curve of the material to determine its magnitude. To determine K_o , it is necessary to consider a

common power law used to represent a materials stress-strain response. Using Ludwicks power law [76]:

$$\sigma_o = Y\varepsilon^n \quad \text{Eqn. 5.21}$$

where Y is a stress constant and n is the sensitivity to strain hardening. Both are determined from a log-log plot of the stress-strain curve. The slope of this curve is n and the y-axis intercept is Y . A more reliable version of this law for representing the stress-strain response of a material over a larger range of strain is that of *Swift* [86]:

$$\sigma_o = C(\varepsilon + \varepsilon_o)^n \quad \text{Eqn. 5.22}$$

where the constants n and C are determined from a uniaxial stress-strain curve of the material. The constant ε_o indicates the amount of pre-strain in the material so a value of $\varepsilon_o=0$ would indicate an annealed workpiece. However, with FORGE2, the materials flow stress is a function of strain, strain rate and temperature, and is represented by:

$$\sigma_o = \sqrt{3}^{m+1} K_o (\varepsilon + \varepsilon_o)^n e^{\frac{\beta}{T}} \quad \text{Eqn. 5.23}$$

where e is the base of the natural log. Equating Equations 5.22 and 5.23, an expression for K_o can be obtained:

$$K_o = \frac{C}{\sqrt{3}^{m+1} e^{\frac{\beta}{T}}} \quad \text{Eqn. 5.24}$$

where m is the sensitivity to strain hardening index, β is the ratio of (Q/R) where Q is the activation energy for self-diffusion and R is the universal gas constant. β can also be found experimentally by means of tensile tests performed at different temperatures and different strain rates. Linear regression can then be used to obtain the slope (β) from a graph of $\ln(\sigma_o)$ V's ($1/T$).

5.2.3 Solution Technique of Finite Element Analysis (FORGE2)

A solution to the equations given in Section 5.2.2 can be obtained by minimising a variational equation that describes the internal energy of the system. This variational equation is obtained using the principle of virtual work (internal energy $\equiv$ external energy) and is expressed below:

$$\delta\Phi = \int_{\Omega} \frac{K}{m+1} \sqrt{3} \dot{\epsilon}^{m+1} d\Omega + \frac{1}{2} \rho_p \int_{\Omega} K \dot{\epsilon}_v^2 d\Omega - \int_{S_f} T u dS \quad \text{Eqn. 5.25}$$

where Ω and S_f are the volume and surface of the workpiece respectively at time t . T represents the surface traction applied to the surface S_f and $\dot{\epsilon}_v$ is the volumetric strain rate. The first term in the equation represents the potential energy of the system and contains both the plastic and elastic contribution of the deforming body. The second term represents the strain energy of the system where K is a large positive constant to penalise volume change. The final term represents the external work done on the system, in this case both the normal and frictional components over the surface of the body are incorporated into the model. Equation 5.25 is converted into a non-linear algebraic form by utilising the FEM discretisation procedure [83]. FORGE2 uses the Rayleigh-Ritz technique to minimise the energy of the system with respect to the nodal variables (nodal velocities).

$$\delta\Phi[u_{i0}] = 0 \equiv \frac{\delta\Phi}{\delta u_{ia}} = 0 \quad \text{Eqn. 5.26}$$

The nodal velocities are represented by u_{ia} where i represents the component of velocities (x- or y-direction) and a represents the node number. Quadratic shape functions were used to approximate the velocities at the element level and the elemental equations were assembled to approximate the variational equation over the domain. Therefore the final form of the quadratic equation is:

$$K(T, \dot{\epsilon})\{u\} = \{F\} \quad \text{Eqn. 5.27}$$

where K represents the stiffness matrix and is dependant on the strain, strain rate and temperature of the system, $\{u\}$ is a vector representing the elemental velocity and $\{F\}$ is a vector of the nodal forces obtained from the surface integral in Equation 5.25. The above equation is solved by iteration for the nodal velocities. Derivatives of the velocities give the strain rates, and Equation 5.19 is used to obtain the stresses at the nodes.

5.2.4 Contact Modelling

The metal cutting process induces complex contact conditions simultaneously between several deforming bodies. Figure 5.2.4a depicts a typical mesh of the workpiece, cutting tool and rigid dies. The most essential initial boundary conditions are also shown. The tool has zero velocity in both the x- and y-directions (rigidly clamped) and the workpiece has zero velocity in the y-direction and the cutting velocity, V_c , in the x-direction.

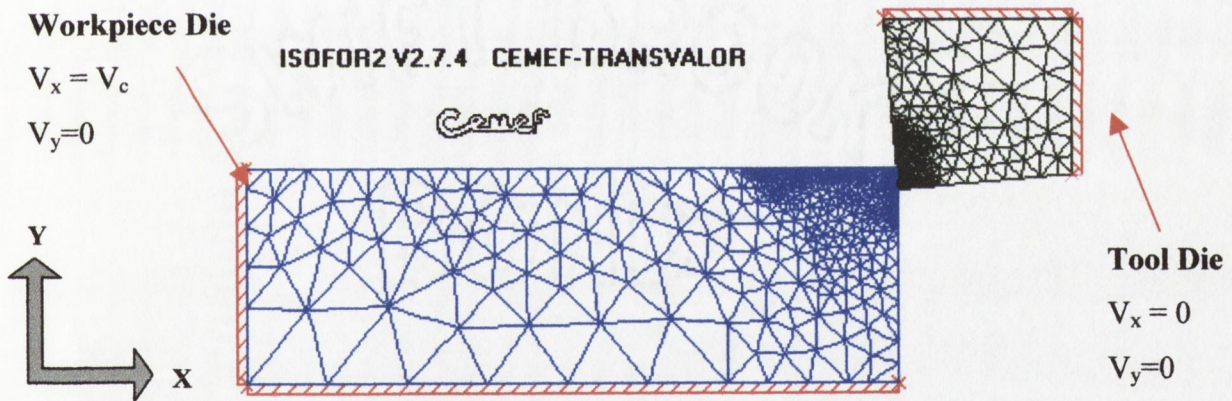


Figure 5.2.4a Boundary Conditions used for typical machining simulation

Contact between the cutting tool and the workpiece material was characterised by a combined Coulomb-Tresca friction law:

$$\mu = \frac{F}{N} \quad \text{-Coulomb} \quad \text{Eqn. 5.28}$$

$$m = \frac{\tau_i}{k} \quad \text{-Tresca} \quad \text{Eqn. 5.29}$$

where μ is the Coulomb friction factor, F is the tangential force and N is the normal force acting on the sliding body. m is referred to as a Tresca friction factor and is used when the average frictional shear stress, τ_i , exceeds $0.5\sigma_f$ (σ_f being the Tresca effective stress). So for high values of frictional shear stress ($\tau_i > 0.577\sigma_f$) a Von Mises friction law is used instead.

The temperature change in the system was a result of the competition between the internal heat conduction and internal heat dissipation. Figure 5.2.4b depicts the various forms of heat transfers taking place within the workpiece and tool during machining.

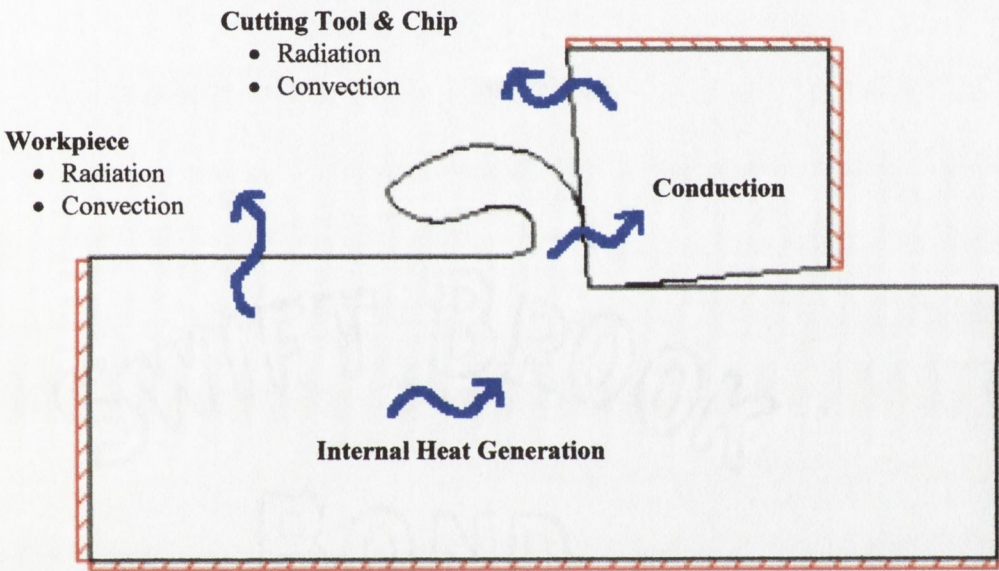


Figure 5.2.4b Thermal processes occurring within the workpiece/tool whilst machining

These heat transfers are represented by the following heat transfer equation:

$$\rho c \frac{\delta T}{\delta t} = \text{div}(k \text{grad} T) + \dot{W} \quad \text{Eqn. 5.30}$$

The left-hand side of the equation represents the temperature change, whilst the right-hand side represents internal conduction with $\dot{W}$ being the internal heat generated by viscoplastic deformation. This internal heat generated was calculated using a Norton-Hoff law [87]:

$$\dot{W} = \eta * \sigma_{ij} * \dot{\varepsilon}_{ij} = \eta * K_1 \sqrt{3} \dot{\varepsilon}^{m+1} \quad \text{Eqn. 5.31}$$

where η is the deformation efficiency, K_1 is the consistency, $\dot{\varepsilon}$ is the strain rate and m being the sensitivity to strain rate. Three other sources of heat transfer are also represented. These include conduction, convection and radiation. A standard quadratic power law was used to calculate the radiation effect:

$$\Phi_r = \sigma * \varepsilon * (T^4 - T_o^4) \quad \text{Eqn. 5.32}$$

where ε is the materials emissivity, σ is Boltzman's constant, T_o is the exterior area temperature and T is the workpiece temperature. Both the conduction and convection can be represented as follows:

$$\Phi_c = h(T - T_o) \quad \text{Eqn. 5.33}$$

where h is the global conduction and convection transfer coefficient, T_o is the exterior area temperature and T is the workpiece temperature.

Frictional heat flux at the tool/workpiece interface is shared according to their respective effusivities. The heat generated by frictional sliding is represented by the expression [88]:

$$\phi_{fr} = \frac{b_1}{b_1 + b_2} * \alpha * K_1 * \Delta V^{m+1} \quad \text{Eqn. 5.34}$$

where b_1 is the tool material effusivity, b_2 is the workpiece material effusivity, α being the heat transfer coefficient between the tool and the material, K_1 is the consistency, ΔV is the difference in velocity between the tool and the workpiece and m is the sensitivity due to strain rate. The effusivity of a material can be calculated as follows:

$$b = \sqrt{k * \rho * c} \quad \text{Eqn. 5.35}$$

where k is the conductivity, ρ is the density and c is the specific heat capacity of the material. With the above equations, the thermo-mechanical coupling is assured by the updating of the material properties according to the calculated temperatures at each time increment.

5.2.5 Modelling Tool Wear

A calculation for tool abrasion wear is possible in FORGE2 with the use of a wear function, U . A user subroutine allows wear to be evaluated for a set of fixed points on the tool and quantifies the depth of wear by abrasion using the following formula:

$$U = \int_0^t \sigma_n V_t \delta t \quad \text{Eqn. 5.36}$$

where σ_n is the normal stress and V_t is the tangential sliding velocity as shown in Figure 5.2.5.

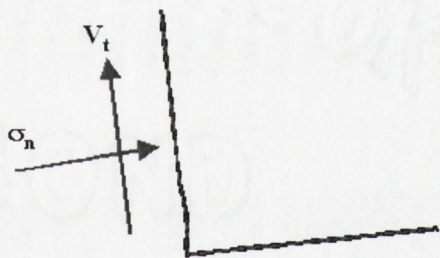


Figure 5.2.5. Calculation of Tool Wear Function, U .

5.3 Finite Element Coating Models (ANSYS)

It has been established that the failure of a tool is often more dependant on the break-up and detachment of the coating rather than from excessive wear [24]. Failure of the coating can arise from either adhesive or cohesive wear. Adhesive failure generally occurs by detachment at the interface between the coating layers or between the coating and the substrate, while cohesive failure is generally a result of internal stresses within the coating or at its surface. Coating detachment is a mixed mode fracture process, so to model tool wear it is necessary to know both the direct and shear stresses at the coating surface

and within the coating interfaces. To simulate the conditions frequently found during metal cutting, a Hertzian type normal pressure distribution is applied on the outer surface of the coating. The work here shows the effect this pressure has on uncoated, single-coated, double-coated and multi-coated substrates.

The interaction between coating thickness, pressure distribution, distance from centre line of load and other factors is complex, with each interrelated. However for clarification purposes the next sections will be broken down as follows (as indicated on Figure 5.3a):

- Tangential Stresses
 - Tangential Stress through coating thickness (Line1)
 - Tangential along coating surface (Line 2) and coating/substrate interface (Line3)
- Shear Stresses
 - Shear Stresses along coating surface (Line 2) and coating/substrate interface (Line3)

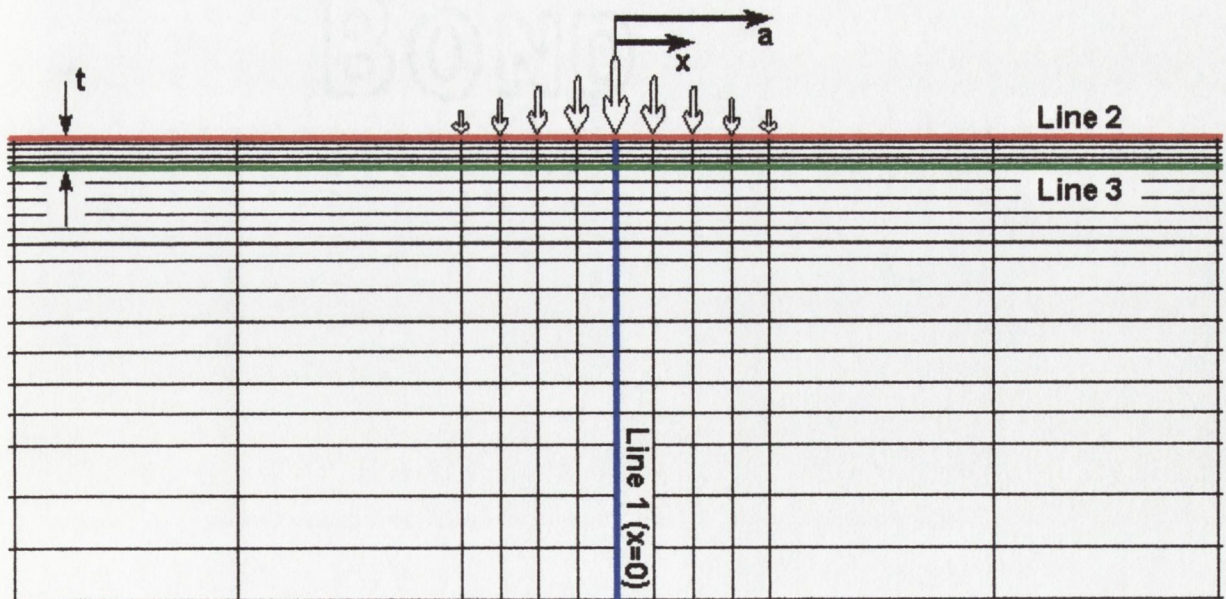


Figure 5.3a. Coating Contact Regions

Each of the above sections take into account three variables:

- t : Thickness of Coating
- a : Contact half-width
- x : Distance from centre of load line

which are shown on Figure 5.3a. Line 1 measurements occur when x is equal to zero and line 2 measurements are taken over the outermost coating surface for x increasing from zero, past a to the edge of the system. The thicknesses of the coatings also have to be investigated. The coating thicknesses are varied with values of $[t/a]$ being 0.1 to unity. i.e. for a contact half-width of 5, we choose a thickness of 3 for a $[t/a]$ ratio of 0.6. At each coating ratio the stress distributions are investigated to determine the optimal coating thickness.

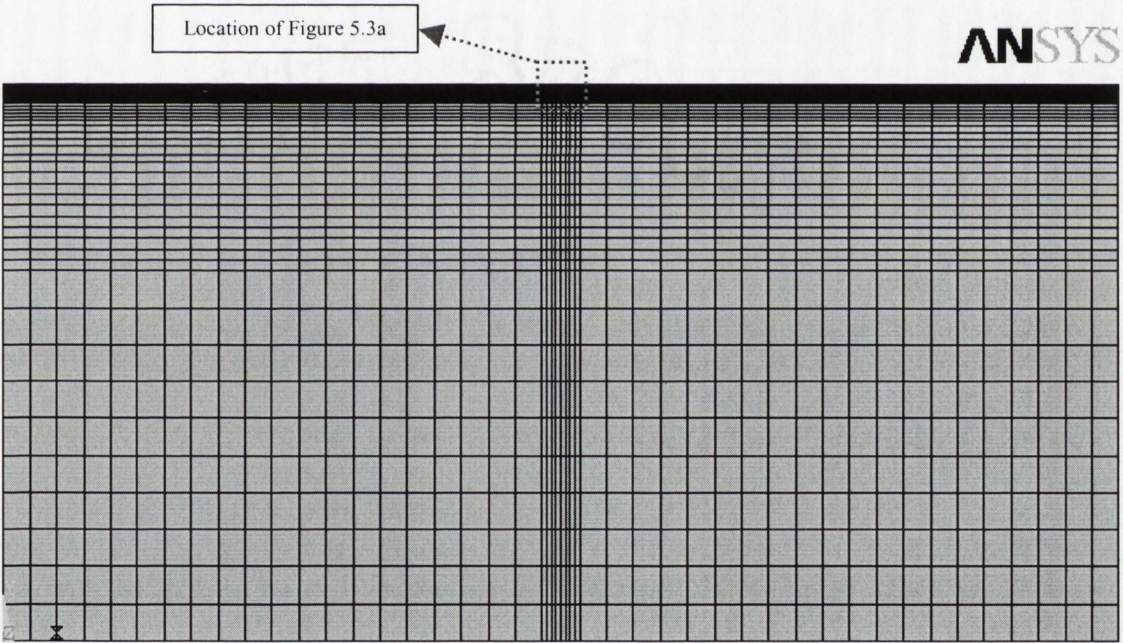


Figure 5.3b. Single-coated ANSYS model

Figure 5.3a is a small section of the overall model which is shown in Figure 5.3b. An important feature of the finite element model is that the mesh is 30 times deeper and wider than the contact half-width, a . These distances were necessary in order to accurately reproduce Hertzian stresses in the elastic model [25]. It can be observed that the mesh refinement at the top of the model is quite large as this is the area where the Hertzian

distribution is applied over the coatings. The zoom-box on Figure 5.3a shows the location of Figure 5.3a.

When analysing multi-layered coatings, the combined thicknesses of each individual coating make up the overall value of t as shown in Figure 5.3c and 5.3d. Therefore comparing the single and double-coated models:

$$t_{\text{coating1}} = t_{\text{coating1}} + t_{\text{coating2}}$$

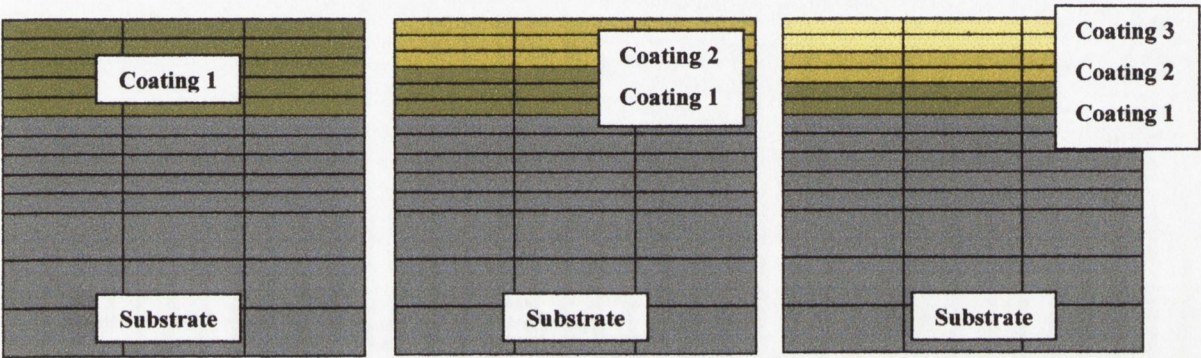


Figure 5.3c. FE meshes of single-, double- and multi-coating systems for $[t/a]=0.2$.

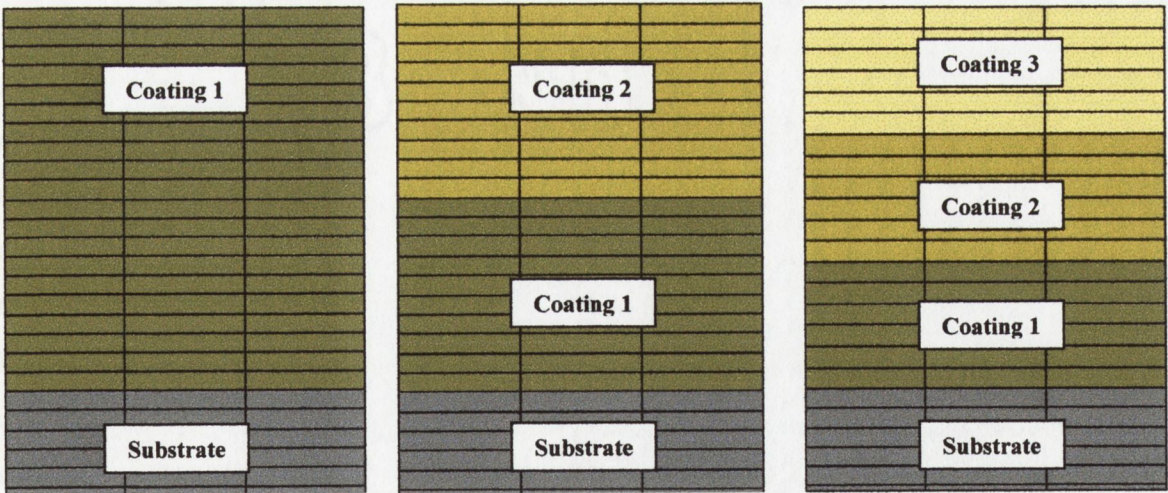


Figure 5.3d. FE meshes of single-, double- and multi-coating systems for $[t/a]=0.8$.

Each ANSYS simulation took into account a combination of variables from Table 5.3.

	Possible Variations
Substrate Material	Tungsten Carbide (WC), Tool Steel (D2)
Single-Coatings	TiN, TiC, Al ₂ O ₃
Double-Coatings	TiN-TiC, TiN-Al ₂ O ₃ , TiC-TiN, TiC-Al ₂ O ₃ , Al ₂ O ₃ -TiC, Al ₂ O ₃ -TiN
Multi-Coatings	TiN-TiC-TiN, TiN-Al ₂ O ₃ -TiN, TiC-TiN-Al ₂ O ₃ , etc.
Coating Thickness ratio, $[t/a]$	0.1 to 1
Contact half-width, a	5, 10
Applied Pressure, P [N/mm ²]	100, 1000

Table 5.3. Variable examined in the coating simulations.

5.4 Finite Element Metal Cutting Models (FORGE2)

Prior to any detailed FEA modelling of the effects of machining parameters, the FE technique for metal cutting was first validated. This was done by comparing the cutting forces obtained from a series of orthogonal machining tests to those predicted by FORGE2 for a similar cutting process. A depth of cut of 0.8mm and a cutting speed of 20m/min was used. The orthogonal cutting tests were performed on an URSUS 225 centre lathe and the cutting force components were measured using a Kistler piezo-electric tool force dynamometer. Two Sandvik SNMA 120408 tungsten carbide cutting inserts were used. One was uncoated (H13A) and the other had a 5µm TiN coating (GC1015). The tool holder was model PSSNR 2020K12 with a plan approach angle of 45°, rake angle of 6° and a clearance angle of 6°. The FORGE2 metal cutting simulation was set up using the same cutting tool angles, depth of cut and cutting speed. The cutting forces were then compared for both the experimental and FE simulation and a favourable correlation was obtained.

The metal cutting models were divided into four groups as given below:

- Rigid Cutting Tools
- Uncoated Cutting Tools
- Single-Coated Cutting Tools
- Multi-Coated Cutting Tools

and each simulation took into account a combination of variables from the Table 5.4.

Cutting Variables	Possible Variations
Type of Insert	Rigid, Uncoated, Single-Coated, Multi-Coated
Workpiece Material	Precipitation Hardened Inconel 718
Coating Material	TiN, TiC, TiCN, Al ₂ O ₃
Rake Angle	-6° Rake, +6° Rake
Clearance Angle	-6° Clearance
Cutting Speeds	20m/min, 40m/min, 60m/min
Depth of Cut	0.8mm, 1.2mm, 1.6mm
Feed Rate	0.24 mm/rev
Manufacturers	Sandvik, Widia
Chip breaking Geometries	None, -QM, -MF, -23, -15
Lubrication	Dry, Wet

Table 5.4. Variables examined in the metal cutting analysis.

In all models the workpiece was modelled as single-phase non-linear elasto-viscoplastic material. The workpiece was modelled using 3-noded triangular elements while the deformable cutting tools were modelled using 4-noded isoparametric quadrilateral elements and the initial mesh is shown below in Figure 5.4. The mesh density was increased around the tool-workpiece contact area because this is the region where greatest amount of deformation and strain takes place. The geometries of the cutting tools used in the experimental tests and the FE models are shown in Appendix 4 whilst Table 5.4 lists the matrix of the variables analysed. The material properties of the cutting tools under investigation are given in Table 2.2.2. The dies were modelled as rigid bodies and therefore no material properties were required for these in the FE simulations. The lower

workpiece die A moved at a constant cutting velocity (V_c) in the x-direction but was fixed in the y-direction. The upper tool die, B, was fixed in both the x- and y-directions. This was in order to simulate the workpiece flowing over the cutting tool.

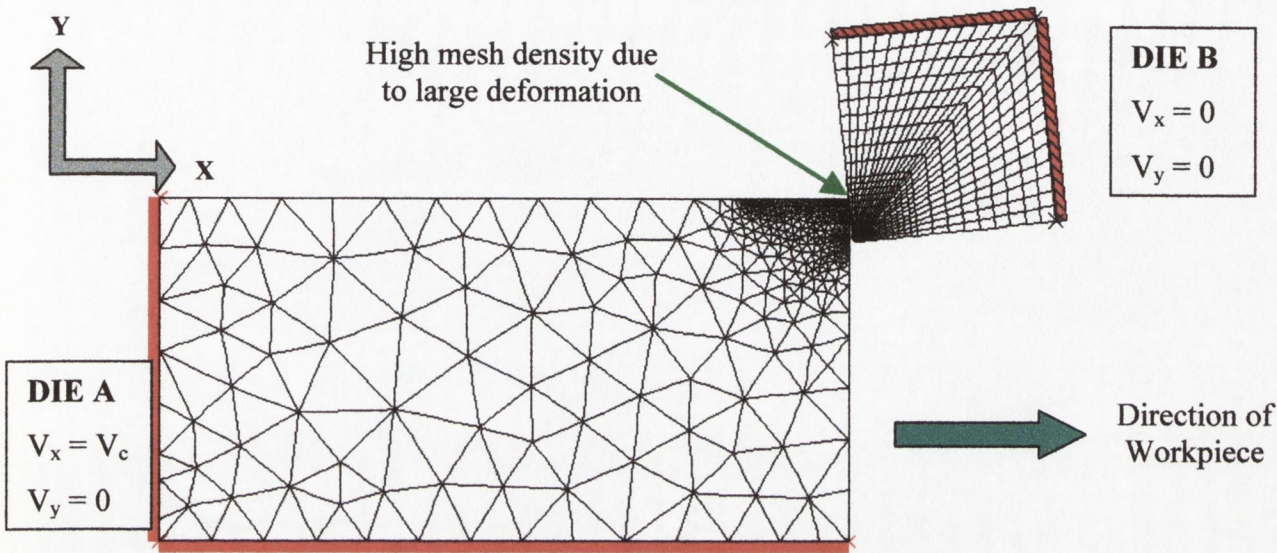


Figure 5.4 Model of Metal Cutting Process (FORGE2)

5.4.1 Cutting Tool Geometry variation

The cutting tool geometry can be varied to increase the machinability of a material. Soft ductile materials are usually machined with a positive rake tool and harder more abrasive materials, such as Inconel 718, are generally machined using a negative rake tool. Changing the geometry of a cutting tool from positive to negative is usually done to improve the edge strength of the tool. Other variations in geometry include chip-breakers and tool edge preparation. Variation in any of the cutting tool parameters will affect the mechanics of the metal cutting process. So in order to optimise cutting tool geometry, FE is a very suitable method of taking all these variables into account.

5.4.2 Rigid Tools

The effects of chip breaking geometries were studied on both positive and negative rake tools to determine what relief a chip breaker gave a cutting tool. Wear measurements were taken and compared to experimental results. The cutting speed was increased from 20m/min to 40m/min to determine the effect on the wear patterns and tool forces. The

depth of cut was increased from 0.8mm to 1.6mm to determine the effect in the wear patterns and tool forces.

5.4.3 Uncoated Deformable Tools

In this analysis the elements were evenly distributed along the insert as shown in Figures 5.4.3a. This involved drawing the insert in AutoCAD r14, exporting it in IGES format to FORGE2 and creating outline files from it. Each outline had to be manually meshed using a mapped meshing technique for every element, which was a very difficult, and time intensive process.

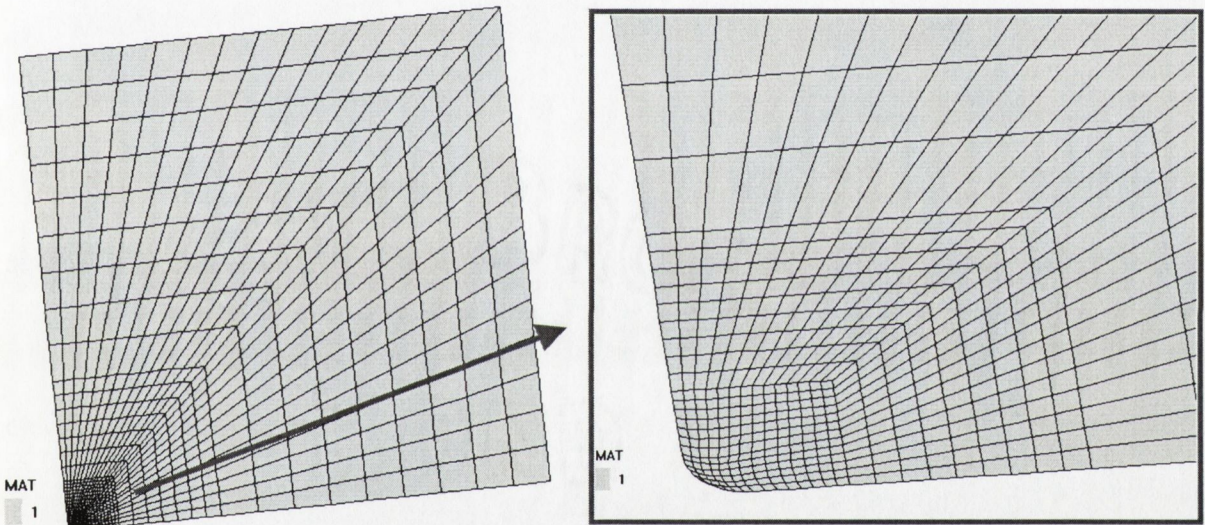


Figure 5.4.3a Uncoated Tool and detailed view of Nose Radius.

The results from this type of model provided more detail than a rigid simulation because it was allowed to deform elastically. The base substrate material (Mat. 1) is assumed to be Tungsten Carbide (WC) and which has similar material properties to the uncoated tools used in the experimental test program. Again various machining and tool parameters were varied including cutting speed, depth of cut and chip breaking geometries.

5.4.3 Single-Coated Deformable Tools

The FE model for these tools was a variation on the above model. The addition of a single-coating of TiN, TiCN, TiC or Al₂O₃ (Figure 5.4.3b) has been shown experimentally

to dramatically increase the tool life of the Tungsten Carbide insert. Various depths of coatings were simulated, to correspond to the types and thicknesses of coatings modelled previously using ANSYS. Again the depth of cut and cutting speed were varied.

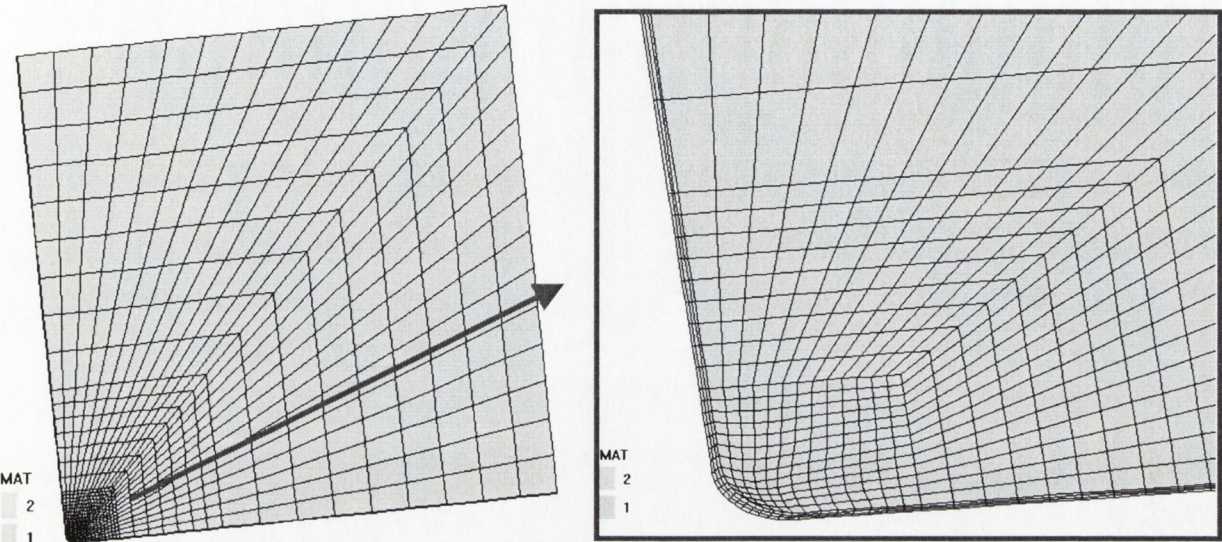


Figure 5.4.3b. Single-coated Tool and detailed view of Nose Radius.

5.4.3 Multi-Coated Deformable Tools

With multi-coated tools, it was possible to simulate various combinations of different coatings along with different coating thicknesses. A double-coated tool is shown in Figure 5.4.3c which has a TiN outermost coating, along with a TiC inner coating. Various multi-coated combinations were also simulated. Again the depth of cut and cutting speed were varied.

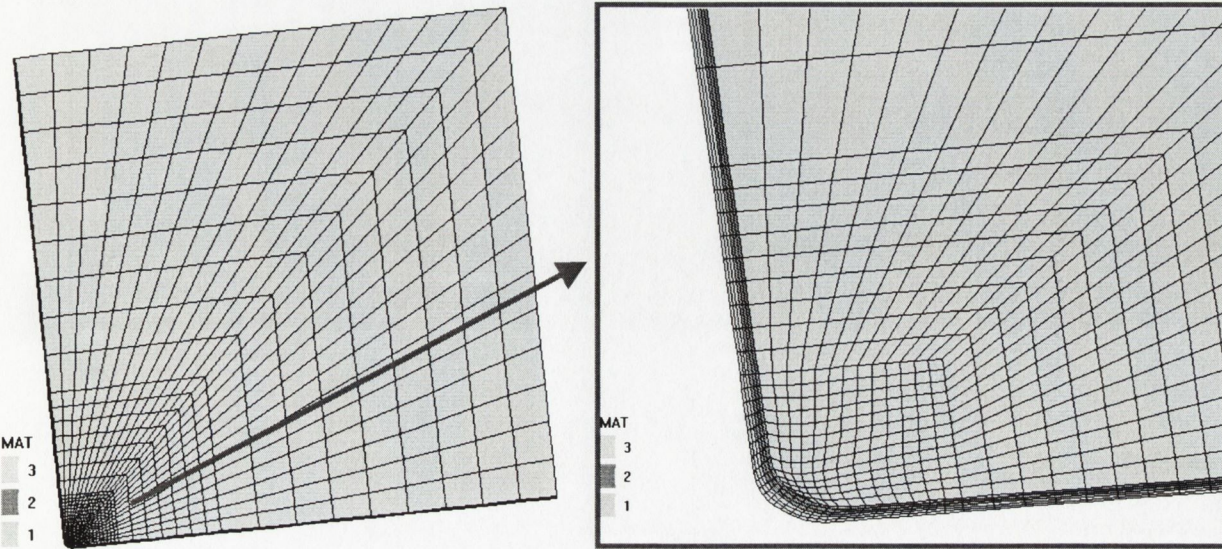


Figure 5.4.3c. Double-coated Tool and detailed view of Nose Radius.

PART IV

Results and Discussions

6.0 Experimental Results and Discussions

This chapter presents the results from the tests undertaken to evaluate the performance of coated tungsten carbide inserts when machining precipitation hardened Inconel 718. Tool wear characteristics and features, cutting forces and surface finish are considered. The experimental work was broken down into two categories – tests with SNMA inserts (without chipbreakers) and SNMG inserts (with chipbreakers). Most of the tests were performed with the SNMA inserts due to the fact that these were the worst case scenario tools without the benefit of being able to break the continuous chip.

6.1 Tool Wear

Figures 6.1a and 6.1b show the progression of uniform flank wear (VB_B) as cutting time increases when cutting Inconel 718 at both 20m/min and 40m/min with SNMA type inserts at a fixed feed rate of 0.24mm/rev.

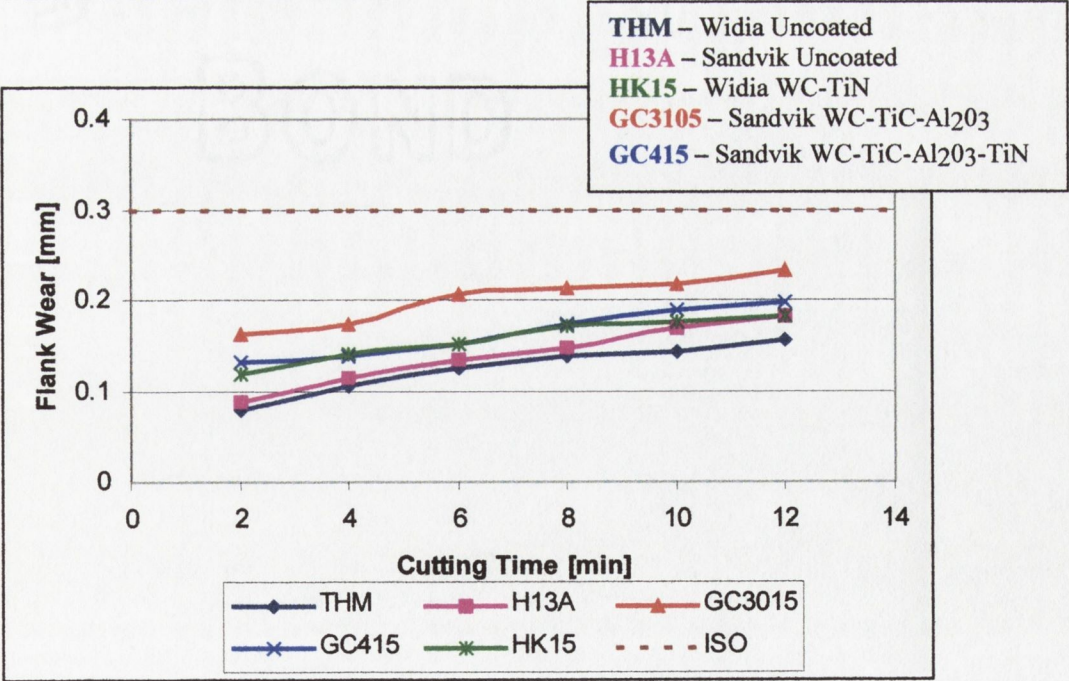


Figure 6.1a. Flank Wear Vs Cutting Time for Inconel 718 at 20m/min.

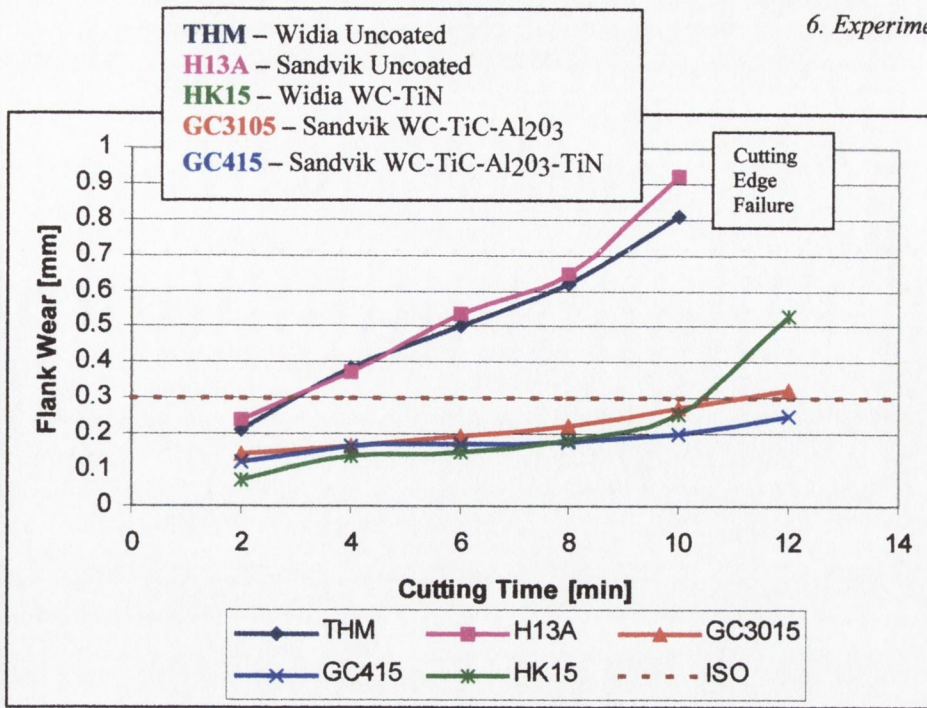


Figure 6.1b. Flank Wear Vs Cutting Time for Inconel 718 at 40m/min.

At 20m/min, the wear progressions are similar for all tools used, with the wear values for the uncoated tools slightly lower than the wear values for the coated tools. All tools are within the permitted ISO limits of 0.3mm for flank wear. This can be contrasted with the flank wear on the tools when the cutting speed increases to 40m/min. After approximately 3 minutes, the uncoated tools fail the ISO standard for acceptable flank wear whilst the coated tools are still within limits. It appears that at higher cutting speeds, and hence temperatures, the coatings protect the WC-Co substrate from effects that would otherwise accelerate tool wear. After 10 minutes of machining, further measurement of flank wear on the uncoated tools was impossible due to the catastrophic failure of the cutting edge.

The single-coated HK15 insert (WC-TiN) fails ISO standards after just 10 minutes as indicated on Figure 6.1b. At this time, the protection provided by the coating breaks down and tool wear proceeds at a rate similar to that of an uncoated tool. Figure 6.1c shows this flank wear on this tool after 10 minutes of machining at both 20m/min and 40m/min. The formation of groove wear, concentrated at the nose radius and depth-of-cut line on the flank face is of particular importance when machining nickel superalloys. Deep grooves are often worn in the tool at the positions where the chip edge moves over the tool. This “groove” or “notch” wear is associated with chemical interaction between the tool and

workpiece material [89]. In all the present tests notch wear was evident, mainly on the coated tools.

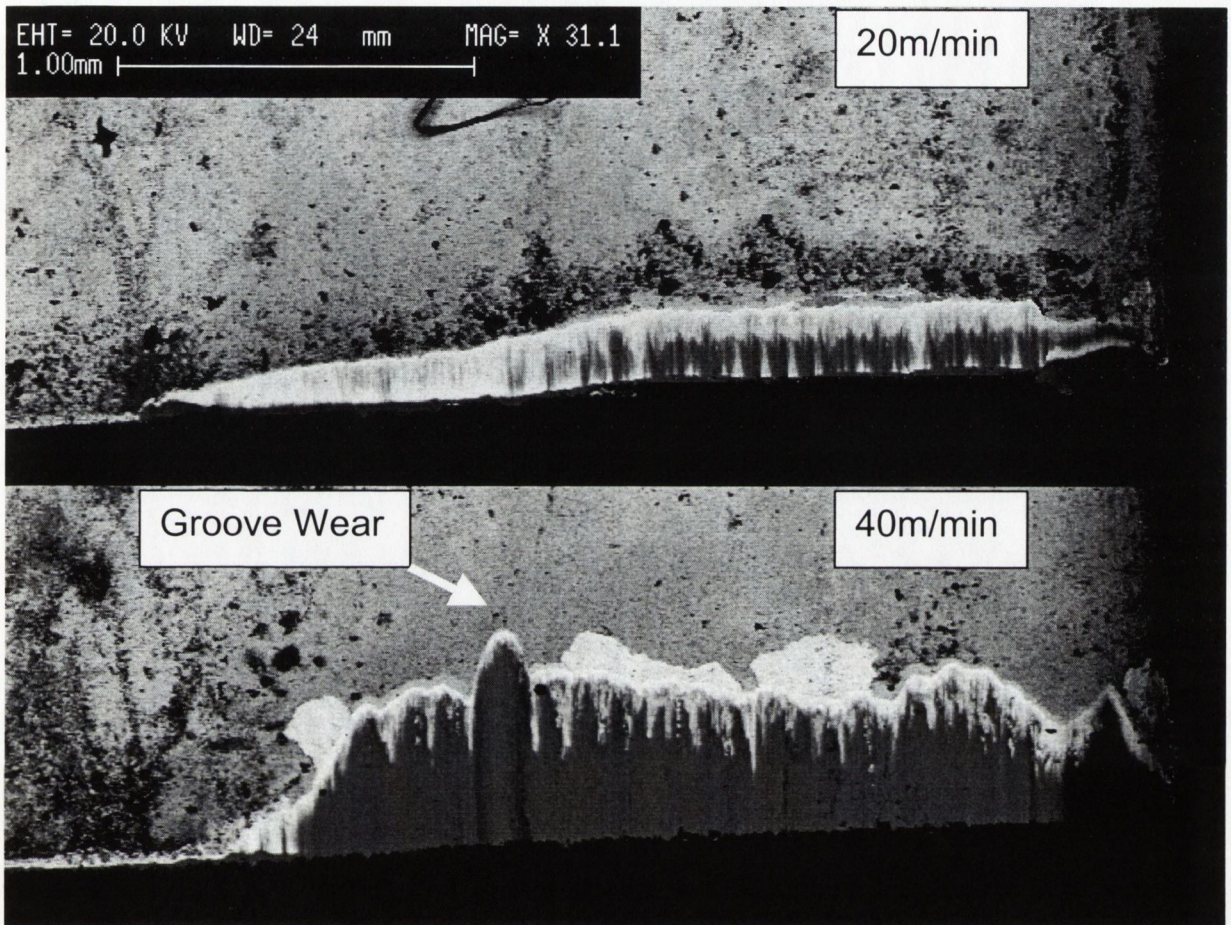


Figure 6.1c. Flank Wear of Widia HK15 tool after 10 minutes at 20m/min and 40m/min.

The behaviour of both the GC3015 and GC415 tools indicates the performance of two different multi-coated coatings. At the 12 minute mark, the double-coated GC3015 tool ($\text{WC-TiC-Al}_2\text{O}_3$) fails ISO limits whilst the multi-coated GC415 tool ($\text{WC-TiC-Al}_2\text{O}_3\text{-TiN}$) still remains within limits. While the magnitude of the tool wear for both were quite similar, the multi-coated tool consistently out-performed all inserts without chipbreakers at both 20m/min and 40m/min. The alumina coat on the Sandvik tools protects the tungsten carbide substrate from the high temperatures at the cutting edge due to its low thermal conductivity. The heat is forced out through the chip and not into the tool. Another important reason is due to the stresses within the coatings and the substrate during the machining operation. It will be shown in later sections that if Young's modulus for the

substrate increases relative to the coatings ($E_{WC} > E_{TiC} > E_{Al_2O_3} > E_{TiN}$), then the stresses within the coating/substrate system decrease.

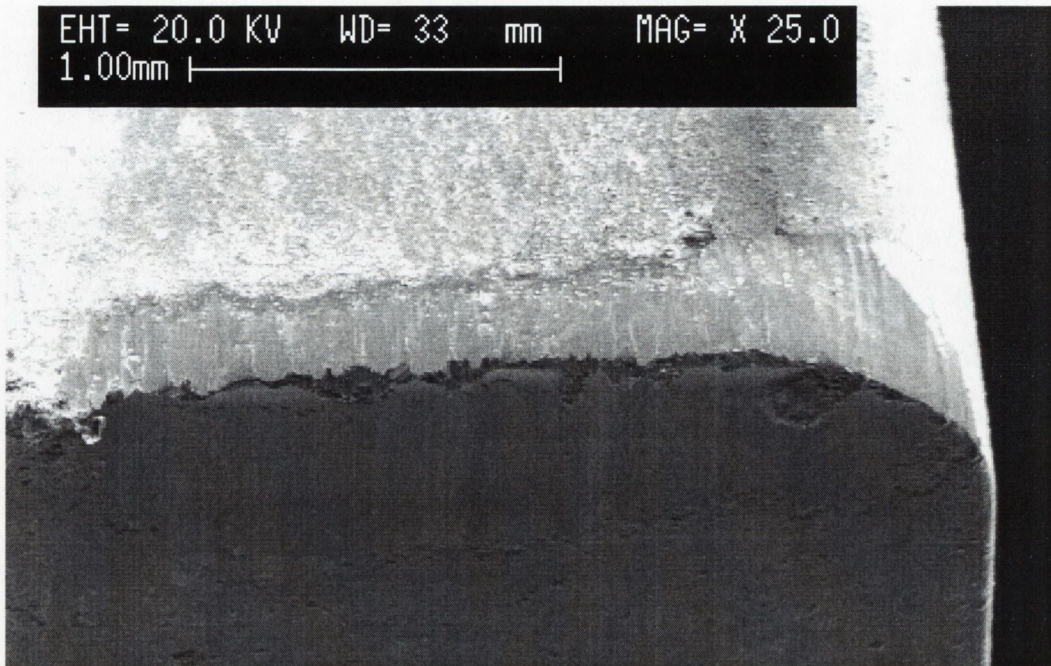


Figure 6.1d. Flank Wear of Sandvik GC415 tool at 40m/min.

Above in Figure 6.1d, the flank wear on a Sandvik GC415 coated carbide is shown after machining Inconel 718 at 40m/min for 12 minutes. This tool performed the best when compared to the other four tools. The reason for this was due to the multi layers of coatings deposited on the tool which afforded it greater protection to the high temperatures and stresses during cutting.

The differences in flank wear on two different tools can be seen in Figure 6.1e when both tools were used to cut Inconel 718 at 20m/min for 12 minutes. Comparing the lower uncoated THM insert with the upper double-coated GC3015 insert ($WC-TiC-Al_2O_3$), it can be seen that the amount of wear is very different with the uncoated tool undergoing less flank wear. At lower cutting speeds these uncoated tools performed better than their coated rivals. Only when the cutting speed is increased to 40m/min, and then with a corresponding increase in temperature, do the coatings protect the tools.

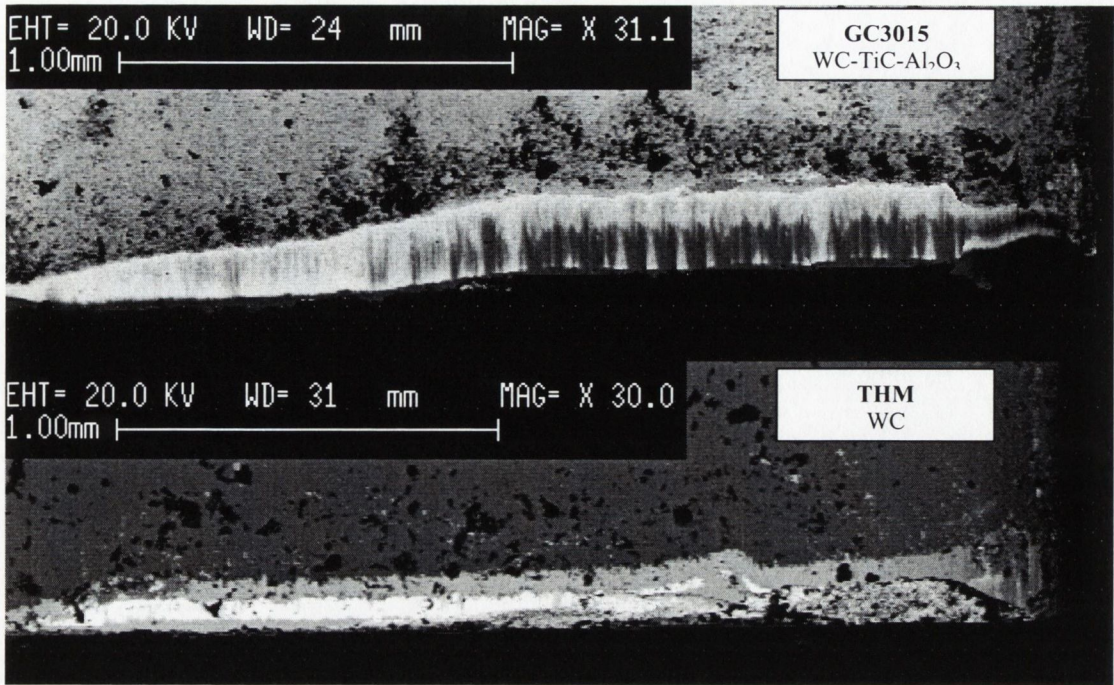


Figure 6.1e. Differences in flank wear at 20m/min.

When machining superalloys, the machined swarf from the workpiece material tends to adhere or weld to the tool due to the high temperatures associated with machining these materials as shown in Figure 6.1f. This has the effect of increasing the surface roughness of the workpiece as these particles rub along the machined face. With an increase in the clearance angle of the tool, this effect can be minimised, however the cutting edge strength of the tool will be weakened.

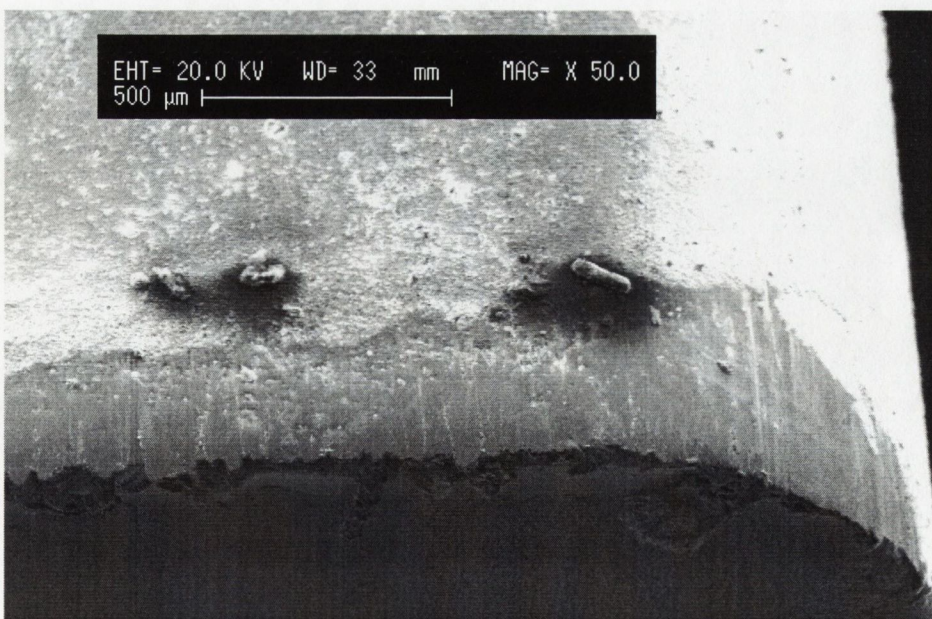


Figure 6.1f. Adhesion of Inconel 718 on tool.

6.2 Cutting Forces

Factors such as tool material, tool coating, cutting speed, feed rate, depth of cut and lubricants can all affect the cutting forces acting on a cutting tool during single point turning. All the results below present the resultant cutting force, F_c , which is made up of the tangential, radial and axial cutting force components (F_t , F_r , F_a) in the following relationship:

$$F_c^2 = F_t^2 + F_r^2 + F_a^2$$

Eqn. 6.1

Figures 6.2a and 6.2b shows the corresponding cutting forces from the tool wear tests carried out from Figure 6.1a and 6.1b respectively. It can be seen that at lower speeds, maintaining the feed rate at 0.24mm/rev, the tool forces increase relative to each other due to the progressive wear on the tools. Compared to the wear results, the ability of being able to rank the tools is not so clearly possible when studying the cutting forces.

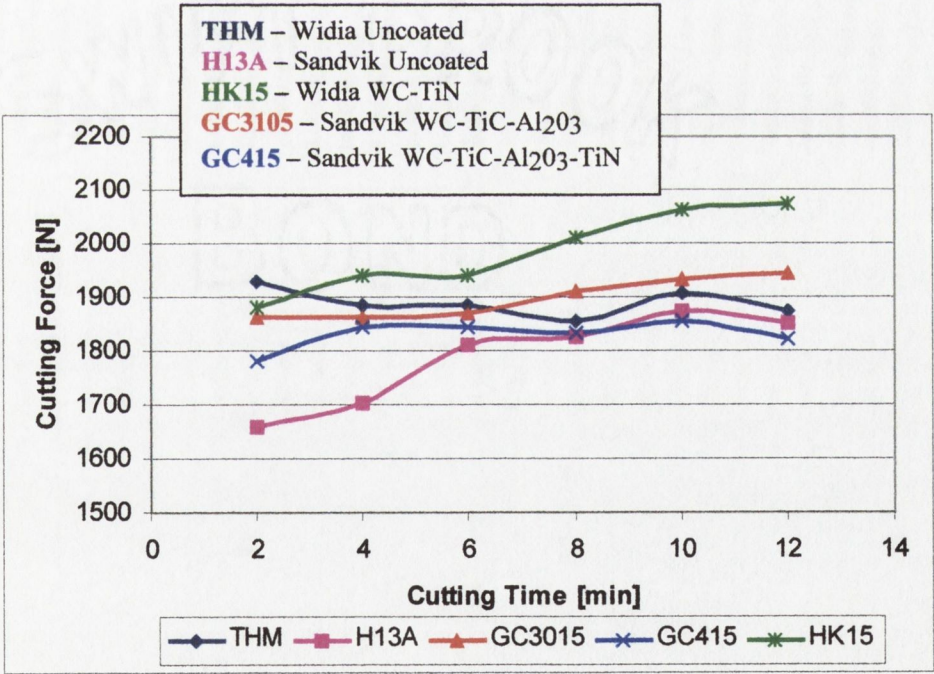


Figure 6.2a. Cutting Force Vs Cutting Time for Inconel 718 at 20m/min.

However as the cutting speed increases to 40m/min, the forces increase rapidly till tool failure occurs as shown in Figure 6.2b. Due to the lack of protection afforded by the uncoated tools, these inserts initially sustain the greatest forces and over time, gradually

break down. Measurements of the overall cutting force showed that increases in tool wear are broadly reflected by increases in cutting force. Consequently, where coated tools had lower wear than uncoated tools, the accompanying cutting forces were also notably lower.

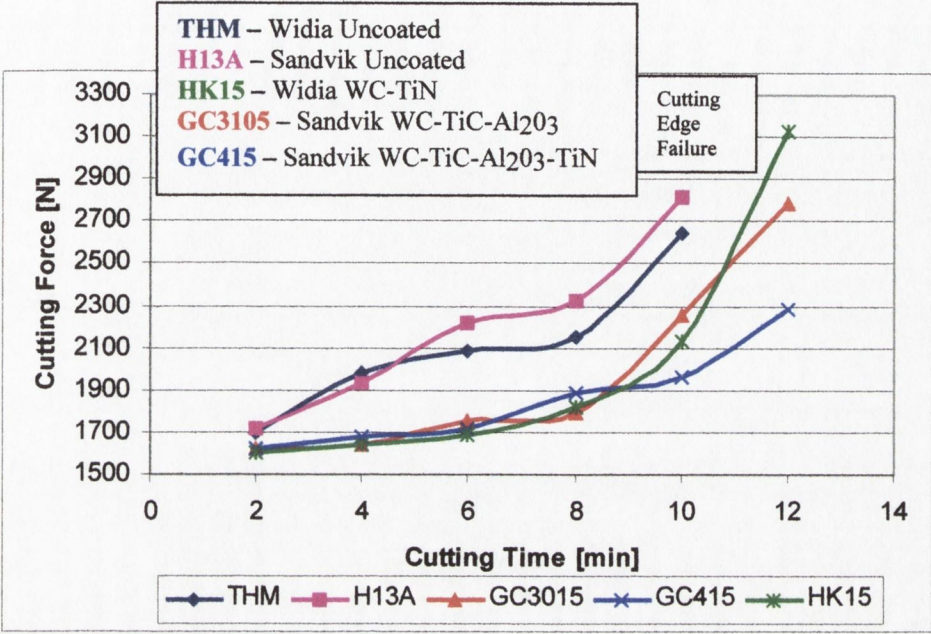


Figure 6.2b. Cutting Force Vs Cutting Time for Inconel 718 at 40m/min.

Under normal cutting conditions with tolerable tool wear, the components of the cutting forces were generally ranked in the order: $F_t > F_r > F_a$. A typical plot obtained from the DASH300 data acquisition software is given in Figure 6.2c. This shows the component forces for the Widia HK15 single-coated tool (WC-TiN) when machining Inconel 718 at 20m/min with a feed rate of 0.24mm/rev and a depth of cut of 1.6mm. A large variation in the cutting forces over the sampled region indicates the occurrence of tool edge wear and thus the cutting forces will vary. A stable cutting force on the other hand is indicative of low tool wear.

When machining “easily machinable” materials such as Aluminium, the axial and radial components are usually much lower than the tangential component, however for Inconel 718 this is not the case. The ratio of $F_t:F_r:F_a$ for the case illustrated in Figure 6.2c is 1:0.65:0.49. The main contribution to this effect is the approach angle of 45°, rake angle of -6° and clearance angle of 6°. Also the high frictional forces along the rake face due to the workpiece tendency to form a BUE on the tool surface, also is expected to influence this result.

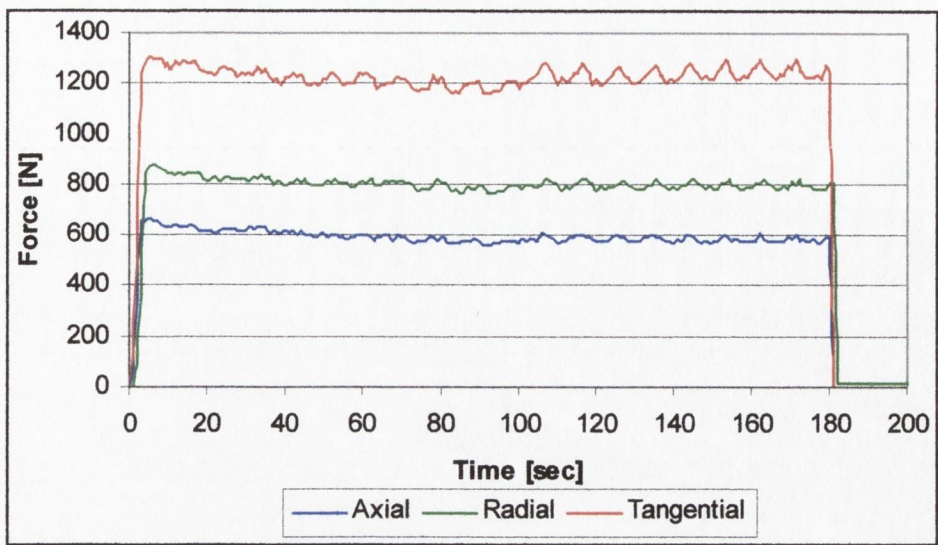


Figure 6.2c. Cutting force components under cutting interval “0 to 2” minutes when machining Inconel 718 at 40m/min with the HK15 tool)

At the point where the flank wear, and consequently the cutting force, on a failing tool increased rapidly, the individual components of force were also found to increase. Both the radial and axial force components rose considerably while the radial component became the major component of the total cutting force as shown in Figure 6.2d.

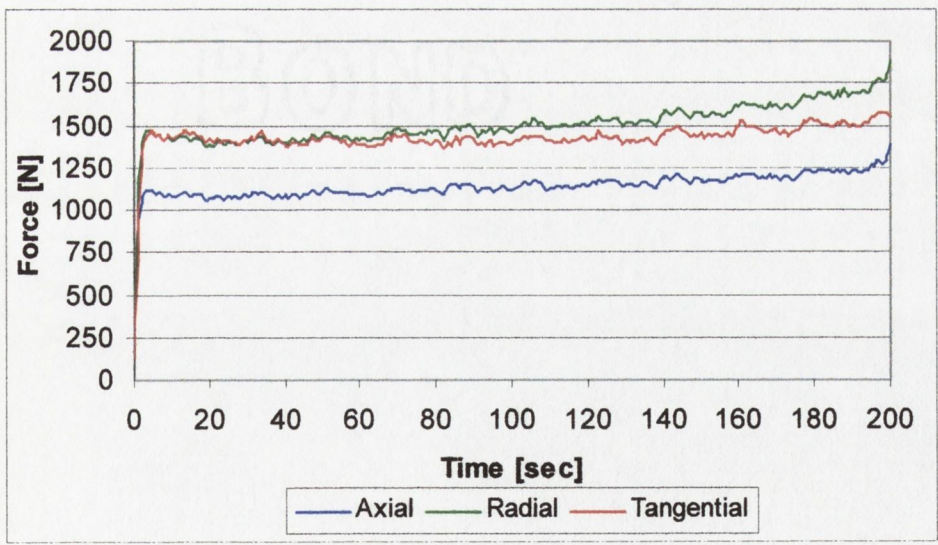


Figure 6.2d. Cutting force components under cutting interval “8 to 10” minutes when machining Inconel 718 at 40m/min with the HK15 tool)

The increase in the “horizontal” forces (F_a and F_r) is due to the increase in flank wear on both the primary and secondary cutting edges, which increases the pressures

between the newly machined surface and the worn tool flanks. This effect was shown earlier in Figure 6.1b. As the single-coating starts to break down, the tool begins to wear like an uncoated tool. This increase in the radial force also causes chatter to occur during the machining operation which results in a further increase in surface roughness of the workpiece surface. Notch wear is usually found on the multi-coated tools and this type of wear causes unsteady chip flow over the rake face which consequently varies the forces on that tool face.

There was an overall drop in the magnitude of the cutting forces with increasing cutting speed from 20m/min to 40m/min for all tools tested. The decrease in both the radial and axial force components is quite significant but with little changing in the tangential component. Normally, a small drop in cutting forces with an increase in cutting speed would be expected, since increasing cutting temperatures lead to thermal softening of the workpiece. Since nickel-base superalloys are specifically designed to retain their strength up to 90% of their melting temperatures, this effect is expected to be less significant for Inconel 718. Another effect of increasing the cutting temperature can be to reduce the coefficient of friction between the tool and workpiece [90]. It appears that this has the biggest effect on cutting forces for the case of Inconel 718 by reducing the frictional forces on the rake face of the tool. Hence, machining this material at this higher speed would not only increase productivity, but would also be the reason for the reduction in the axial and radial forces.

6.3 Surface Finish

The surface finish was assessed by measuring the centre line average (CLA) roughness value, R_a . The sampling length of 2mm used ensured that the R_a values were taken over four or five peaks or valleys created by the feed of the tool. The R_a value is strongly dependent on the feed rate. This theoretical value of R_a , based on the feed rate and the tool nose radius, is given by the expression:

$$R_{a(\text{theoretical})} = 0.321 \left(\frac{f^2}{r} \right) \quad \text{Eqn. 6.2}$$

So for a feed rate, f , of 0.24mm/rev and with a nose radius r , of 0.8mm, this gave a theoretical value of 2.31 μ m for all the tests.

Figures 6.3a and 6.3b shows the effect of surface roughness over cutting time when machining Inconel 718 at both 20m/min and 40m/min respectively. Surfaces produced when machining Inconel 718 at 20m/min showed a trend of slightly decreasing R_a values with cutting time. They also indicated that the two multi-coated Sandvik inserts tested gave a better machined surface over the whole cutting test. The reason for the decreasing values of R_a is due to the increasing flank wear on the tools producing a burnishing effect on the newly machined surface.

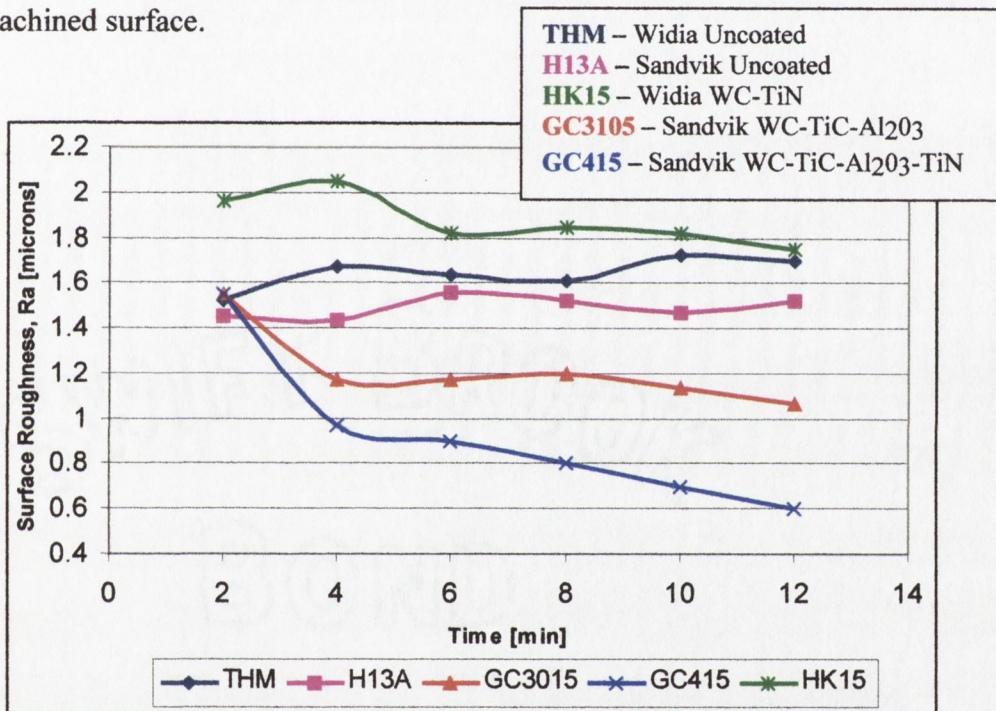


Figure 6.3a. Surface Roughness, R_a , over time when machining Inconel 718 at 20m/min

Cutting Inconel 718 at the higher speed of 40m/min compared to 20m/min resulted in a greater variation in surface roughness and a less well-defined ranking among tools, however, again the Sandvik GC415 performed marginally better. The exceptions were the surfaces produced by the heavily worn uncoated tools. The surfaces produced with both these tools exceeded the theoretical R_a value of 2.31 μ m during the “8-10” minute cutting segment. Theoretically, with an increase in the cutting speed, the R_a value should decrease. This is not the case for Inconel 718 due to the formation of a BUE at higher cutting speeds. The effect of this built-up-edge is to increase the nose radius of the tool and hence blunts

the sharpness of the cutting edge which usually leads to a slight increase in the depth of cut.

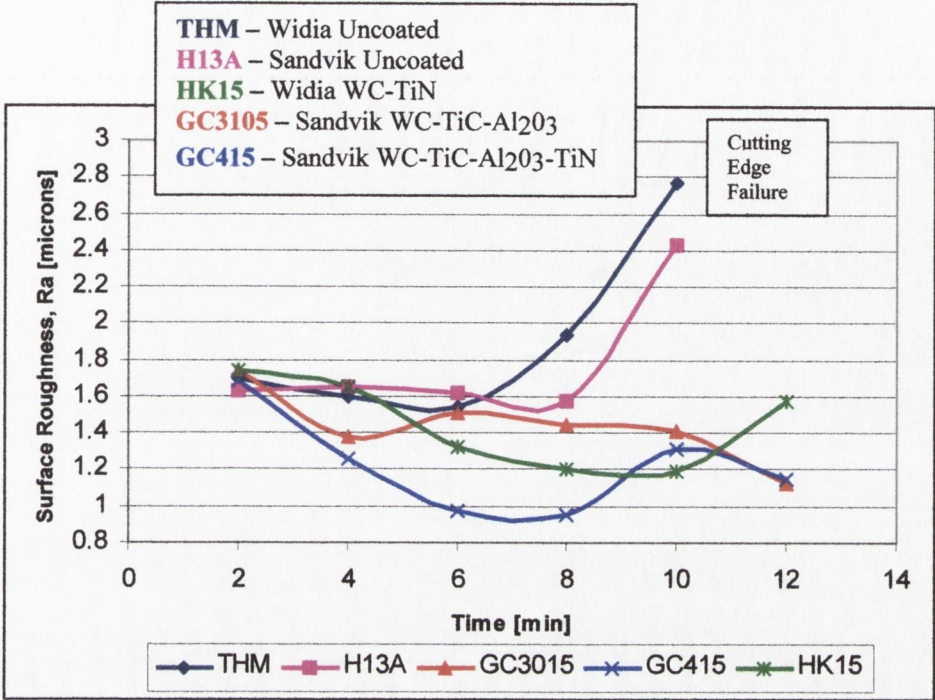


Figure 6.3a. Surface Roughness, R_a , over time when machining Inconel 718 at 40m/min

6.4 Chip-Breaking Carbide Tools

The results of the tests to investigate the effects of various features of pressed-in chip-breaking inserts in machining precipitation hardened Inconel 718 are discussed in this section with regard to the influence of feed rate, lubricant and cutting forces.

6.4.1 Influence of Chip-Breakers

It can be seen from Figure 6.4.1a that the addition of a chip-breaker marginally lowered the tool forces in all experimental tests undertaken. When the values of the overall cutting force for each tool was calculated, an overall trend of F_c proportional to the feed rate, f , became apparent. The Sandvik –QM chipbreaking geometry performed best in all experiments.

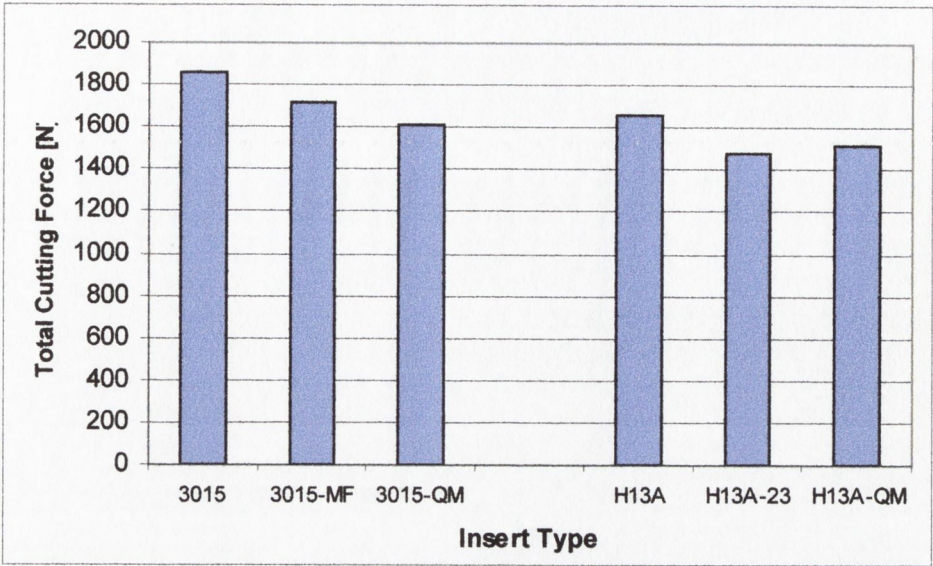


Figure 6.4.1a. Effect of Chip-Breakers on total cutting force under cutting interval “0 to 2” minutes when machining Inconel 718 at 20m/min

6.4.2 Influence of Feed Rate

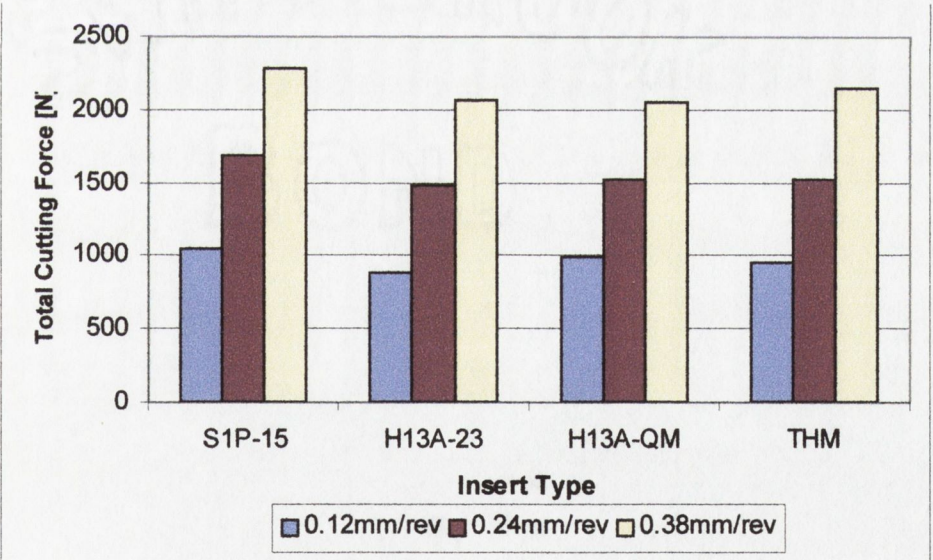


Figure 6.4.2a. Influence of Feed Rate on total cutting force under cutting interval “0 to 2” minutes when machining Inconel 718 at 20m/min

A general observation that may be made when machining Inconel 718, both with and without cutting fluid, is that a feed rate of 0.34mm/rev is needed for successful chip-breaking. This was true for the majority of the tests performed at this higher feed rate. However this higher feed rate causes a corresponding increase in cutting forces which

further decreases the life of the tool. Therefore a decision has to be made about short discontinuous chips and tool-life. Again the Sandvik –QM geometry performed marginally better than all other chipbreakers tested.

6.4.3 Influence of Lubricant

As can be seen in Figure 6.4.3a, the application of a cutting fluid had little, if any, effect on tool forces on cutting speeds of 20m/min. The maximum difference in the total cutting force for all tools tested was 12% which was due to the complex geometry of the –QM chipbreaking geometry. However, disregarding this tool, the maximum difference drops to 5.7% which again points to the –QM insert performing better than all other chipbreakers tested.

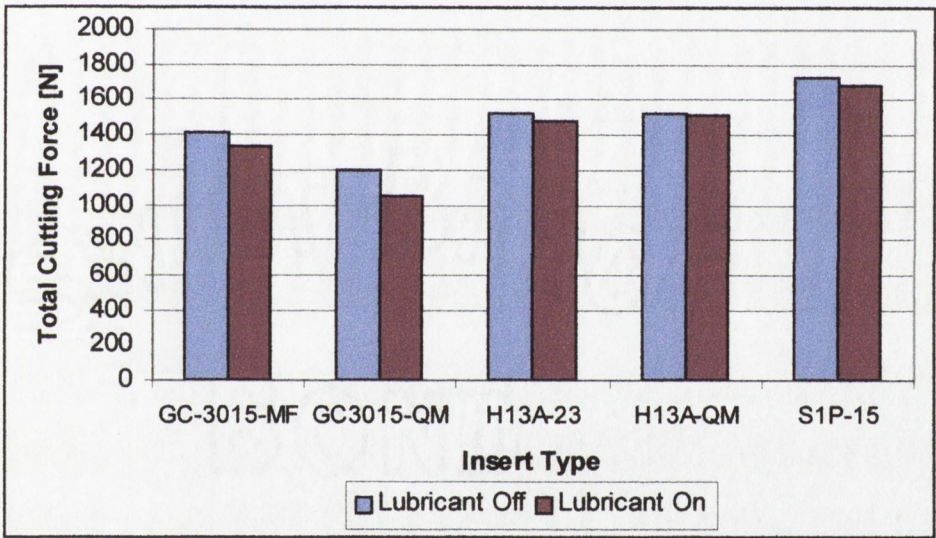


Figure 6.4.3a. Influence of Lubricant on total cutting force under cutting interval “0 to 2” minutes when machining Inconel 718 at 20m/min

It has been discussed earlier in Section 3.1.1 that at low cutting speeds (6m/min), the use of lubricants leads to a small decrease in the axial and tangential cutting forces, however at higher cutting speeds (18-60m/min) the effect is negligible. This is mainly due to the shear-localised chip formation which occurs during the machining of Inconel 718. These sheared chips are intensely hot and prevented from being exposed to the atmosphere before coming in contact with the rake face. Thus the access of the lubricants to the chip/tool contact area is obviously limited. The main functions of the cutting fluid which could be expected under such conditions would be to cool the workpiece, for better accuracy, and to cool the tool, for longer tool life.

6.5 Summary of Experimental Results

- At a cutting speed of 20m/min, all coated and uncoated tools were within ISO limits of 0.3mm flank wear. As the speed was increased to 40m/min, uncoated tools failed after 3 minutes, whilst the coated tools were still within limits after 10 minutes. This indicates at lower speeds, the protective properties of the coatings are not utilised.
- After 11 minutes of machining Inconel 718 at 40m/min, the single-coated Widia HK15 fails the ISO limit of 0.3mm flank wear when the TiN coating fails, and starts to wear similarly to an uncoated tool. Both the multi-coated tools still remain within ISO limits after 12 minutes of machining at 40m/min. Both these tools contained a layer of Al_2O_3 which protected them from the high temperatures associated with machining Inconel 718.
- Measurements of the overall cutting force showed that increases in tool wear are broadly reflected by increases in cutting force. Due to the lack of protection afforded by the uncoated tools, these inserts initially sustain the greatest forces and over time, gradually break down.
- When machining “easily machinable” materials such as Aluminium, the axial and radial components are usually much lower than the tangential component, however for Inconel 718 this is not the case. The ratio of $F_t:F_r:F_a$ for this case is relatively similar. At the point where the flank wear, and consequently the cutting force, on a failing tool increased rapidly, the individual components of force were also found to increase. Both the radial and axial force components rose considerably while the radial component became the major component of the total cutting force.
- There was an overall drop in the magnitude of the cutting forces with increasing cutting speed from 20m/min to 40m/min for all tools tested. The decrease in both the radial and axial force components is quite significant but with little changing in the tangential component.

- The multi-coated tools also left the best surface finish after machining it at both 20 and 40m/min. The reason for the decreasing values of R_a is due to the increasing flank wear on the other tools producing a burnishing effect on the newly machined surface.
- The addition of a chip-breaker marginally lowered the tool forces in all experimental tests undertaken. The Sandvik –QM chipbreaking geometry performed best in all experiments.
- The application of a cutting fluid had little, if any, effect on tool forces on cutting speeds of 20m/min.

7.0 Analytical Results and Discussions

7.1 Stress Distributions in Uncoated Substrates

Two different substrates were used in all the analyses. Tungsten Carbide (WC) was used due to its superior toughness and a tool steel (D2) was used as a less stiff substrate for comparison purposes. Uncoated conditions were modelled so as to have a benchmark against which the coated models could be compared. The tangential stresses in the WC-Co uncoated substrate along the centre line of the contact load can be seen in Figure 7.1. It can be seen that the tangential stress on the surface of the uncoated substrates is approximately 97 N/mm^2 . This value will be used as a benchmark value against which the results for all the coated substrates will be compared to determine the difference that coatings make to the stress distribution. The magnitude of stress decreases with increasing distance below the surface of the substrate.

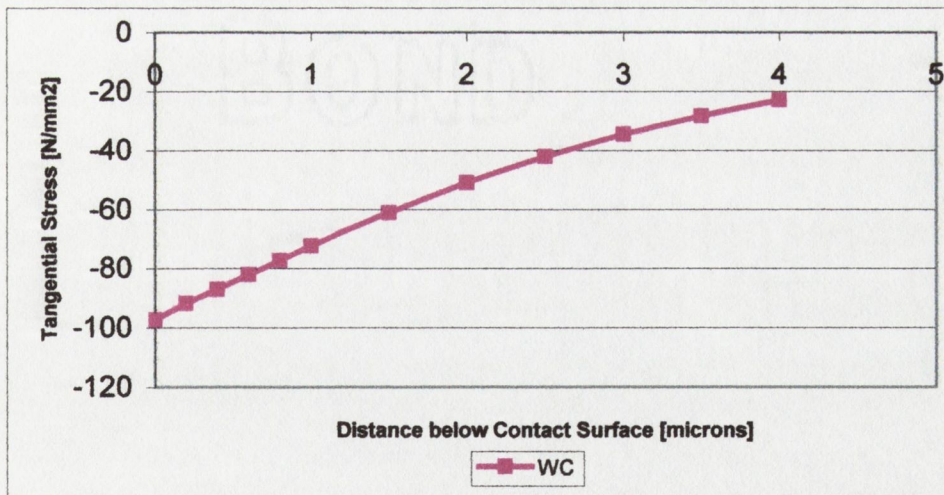


Figure 7.1. Tangential Stress through Uncoated Substrate at $x=0$.

7.2 Stress Distributions in Single-Coated Substrates

The magnitude of the tangential stress, σ_x , at the surface, and over the various interfaces of a coated component has a significant effect on its working life. The maximum

value of this stress always occurs on the contact surface at the centre of the contact region as shown in Figure 7.2 where x is equal to zero (the intersection of Line1 and Line2). Line 3 indicates the interface between the coating and the substrate. Various models were simulated where x increased from 0 to a , for different thickness ratios, $[t/a]$, of 0.1 to 1. For explanation purposes, the models are broken up into two groups – thin and thick coatings. Thin coatings have a $[t/a]$ value of 0.2 whilst for the thick coatings this is deemed to be 0.8. In all models presented below, all the contact half-width, a , was taken to be $5\mu\text{m}$.

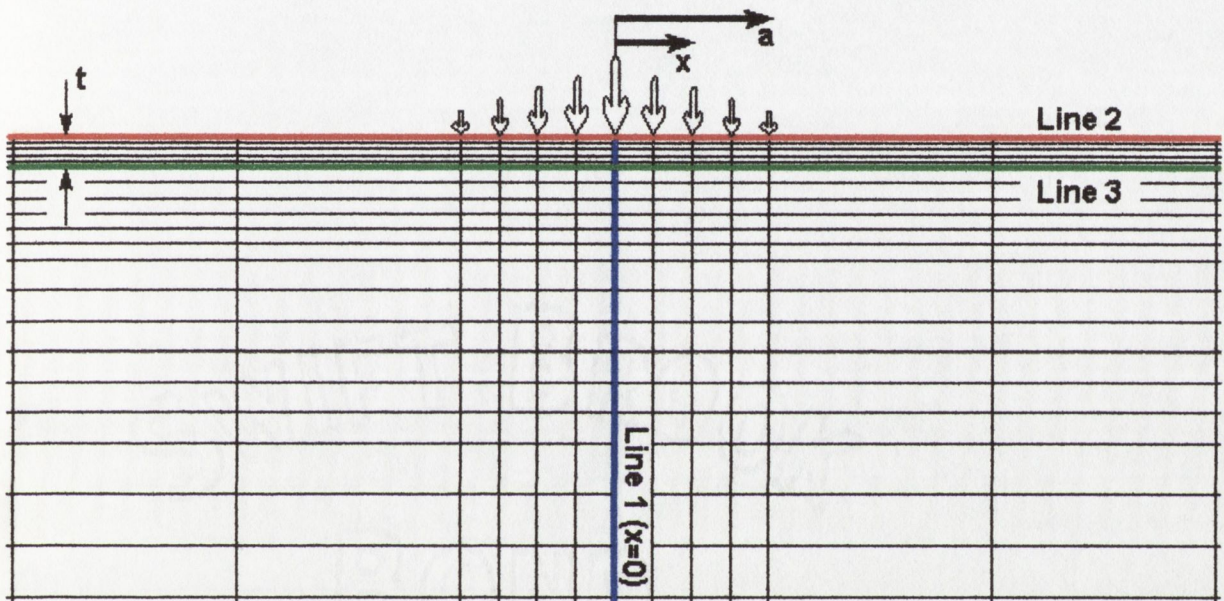


Figure 7.2. Coating Contact Regions

In all the simulations presented below, the results have been broken down into various groups. Initially the single-coated tool is presented, followed with the double- and multi-coated tools. Each tool will have a WC substrate, and some results will compare this with the effects of a softer D2 substrate. The stresses act in a complicated fashion over many interfaces of the tool and its coatings, and so each interface will be dealt with in turn. Initially the tangential stress along the centre of contact/loading line will be investigated (Line 1). Then the tangential stresses along the top of the coating (Line 2) will be compared with the tangential stresses along the interface between the coating and substrate (Line 3). Finally the shear stresses will be dealt with in the same way using the method outlined above. However at each stage the thickness of the coating also has to be taken into account.

For the plane strain models, as shown in Figure 7.2b, equilibrium conditions at the interface dictate that the normal stress, σ_y , and the in-plane shear stress, τ_{xy} , must be the same in each material, while compatibility conditions dictate that the in-plane stresses, σ_x and σ_z , will be different [25]. High compressive in-plane stresses can cause buckling of the coating in the presence of interfacial cracks and local areas of poor adhesion. High tensile in-plane stresses can cause brittle failure of the coating at its surface and interface or coating detachment. In-plane shear stresses contribute to either delamination or mixed mode failures.

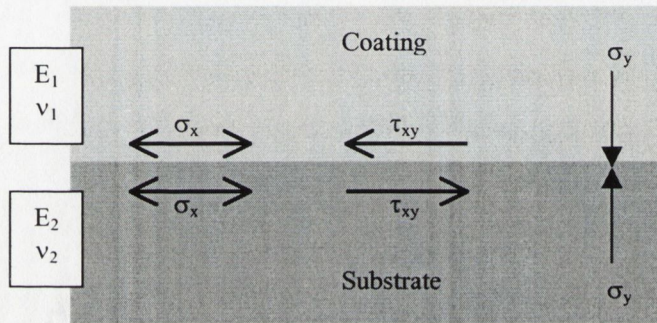


Figure 7.2b. Stresses at an interface between dissimilar materials

Delamination is the separation of the coating from the substrate at the interface. The main cause of delamination is attributed to the mismatch of engineering properties, the two most crucial being Poisson's ratio and the coefficient of mutual interference. Some researchers [91], have analysed the effect of delamination of materials and have predicted the direction and growth rate of delamination along the interface using complex mathematical expressions and numerical methods. However, this behaviour was not pursued in the work undertaken for this thesis, except to analyse the stresses within the coated substrate, and predict the most likely locations for possible delamination.

7.2.1 Effect of thickness ratio on maximum tangential stress for single-coated substrates

Figure 7.2.1a shows the effect of the thickness ratio $[t/a]$ on coatings of increasing thicknesses. All values are taken at the centre of contact line at $x=0$ where σ_x is a maximum. The dashed line represents the stresses within an uncoated WC-Co substrate. As can be seen in Figure 7.2.1a, all WC coated tools have smaller $\sigma_{x(max)}$ values when

compared to the uncoated substrate. The reason for this is due to the fact that the Young's modulus for the WC substrate is greater than that for the coatings (TiC, TiN, Al_2O_3) and so the resulting stress will be lower than within the uncoated substrate when subjected to the same load distribution. However, due to the decreased modulus of the D2 substrate relative to the coatings, the $\sigma_{x(\max)}$ values for all coated D2 substrates are actually larger than that of the uncoated WC substrate. It has been shown [25], that in the case of a single layer coating, the magnitude of σ_x is dependent upon the ratio of the substrate to coating stiffness ($E_{\text{substrate}}/E_{\text{coating}}$). The greater this ratio, the lower will be the tangential stress. This is the reason why a WC-TiN insert has the smallest $\sigma_{x(\max)}$ value of all single-coated tools.

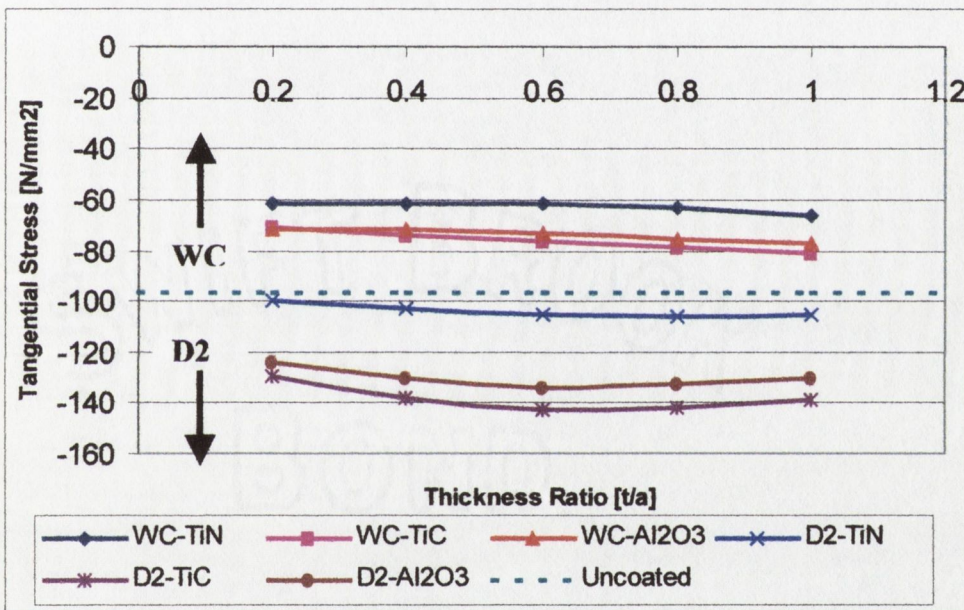


Figure 7.2.1a. Effect of Thickness ratio on $\sigma_{x(\max)}$ for single-coated substrates.

Figure 7.2.1a also shows that the magnitude of $\sigma_{x(\max)}$ is quite independent of the $[t/a]$ ratio for single-coated tools. This is due to the relative stiffness of the two substrate materials compared to the stiffness of the coatings. It will be shown in later sections, that the $E_{\text{substrate}}/E_{\text{coating}}$ ratio is crucial to this independence. For all WC substrates, as this stiffness ratio increases (>1.4), the independence is maintained, however, the effect of having a lower stiffness ratio ($0 < E_{\text{substrate}}/E_{\text{coating}} < 1$) causes $\sigma_{x(\max)}$ to vary with increasing thickness.

For D2 substrates, this ratio is quite the opposite due to its decreased stiffness relative to WC. However, as this stiffness ratio decreases (<0.5), the independence of $\sigma_{x(\max)}$ is maintained for increasing $[t/a]$ ratios.

7.2.2 Variation of tangential stress, σ_x , through single-coated substrates

In the case of all the single-coated systems investigated, the magnitude of σ_x decreases with increasing distance below the contact surface (*along line 1 on Figure 7.2*). On crossing an interface between different coating layers, or, from a coating into a substrate, a stress discontinuity occurs in the magnitude of σ_x . The magnitude of this discontinuity was found to be a function of the stiffness of both the substrate and the coating layers. For explanation purposes, the models are divided into two groups – thin and thick coatings. Thin coatings have a $[t/a]$ value of 0.2 (1 μm thickness) whilst thick coatings are deemed to be 0.8 (4 μm thickness).

7.2.3 Variation of σ_x through thin single-coated substrates ($[t/a]=0.2$)

It can be seen from Figure 7.2.3a that there is initially a lower stress in the TiN single-coated tool when compared to the others, and all tools sustain less stress than the uncoated WC substrate. This is primarily due to the relative differences of the materials moduli. WC has the largest at 640MPa, whilst TiC and Al_2O_3 are quite similar at 450MPa and 390MPa respectively. However TiN has a modulus of 250MPa. It can also be seen from Figure 7.2.3a that for a single-coated tool there is quite a large stress discontinuity at the interface between the coating and the substrate. These stress jump values are presented in Table 7.2.3a. Both the WC-TiC and WC- Al_2O_3 discontinuities are quite similar at -22.45 N/mm^2 and -19.86 N/mm^2 respectively. The discontinuity across the WC-TiN coating is somewhat greater at -28.14 N/mm^2 .

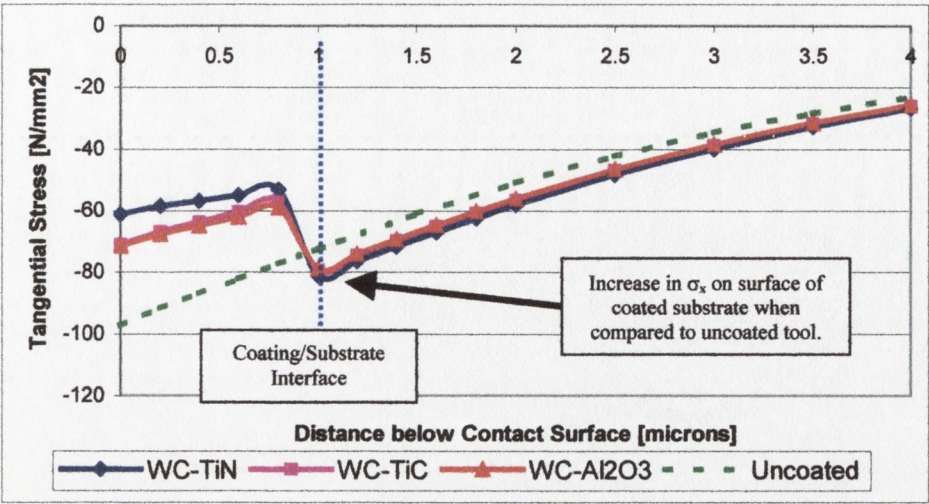


Figure 7.2.3a. Variation of σ_x through single-coated WC substrate ($[t/a]=0.2$)

System (WC-XXX)	First Discontinuity (XXX-WC)
WC-Al ₂ O ₃	-19.86 N/mm ²
WC-TiC	-22.45 N/mm ²
WC- TiN	-28.14 N/mm ²

Table 7.2.3a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.2$)

Table 7.2.3b gives a list of the relative stiffness ratios of the substrates and coatings. For all WC substrates, any stiffness ratio value of 1 or above, will result in generating a compressive tangential stress discontinuity between the coating and substrate. So for the case of the WC substrate, the discontinuities were such as to cause an increase in the compressive tangential stress in the substrate at the interface as shown by the downward movement of all curves relative to the green dashed uncoated curve in Figure 7.2.3a. The greater the value of this stiffness ratio, the greater the magnitude of the tangential stress discontinuity across the coating/substrate interface.

Combination	$E_{\text{substrate}}/E_{\text{coating}}$
WC-Al ₂ O ₃	1.641
WC-TiC	1.422
WC-TiN	2.560
D2-Al ₂ O ₃	0.513
D2-TiC	0.444
D2-TiN	0.800

Table 7.2.3b. Ratio of Moduli for Single-Coated Substrates

For the case of the D2 tool steel substrate as shown in Figure 7.2.3b, initially there are greater stresses in both the Al₂O₃ and TiC coatings due to the relative stiffness of these materials when compared to the less stiff TiN. However, the magnitudes of the stress discontinuities between the coating and the substrate are both lower than those of the WC substrate and are also positive. The magnitudes of the discontinuities are presented in Table 7.2.3c. Again the discontinuities for the TiC and Al₂O₃ coatings are similar at 18.70 N/mm² and 18.14 N/mm² while the discontinuity for the TiN is only 2.75 N/mm². This is primarily due to the decreased stiffness of the D2 substrate, and for any stiffness ratio value of less than 1 (Table 7.2.3b), a positive tangential stress discontinuity would have been generated in the D2 substrate. As opposed to the WC substrate, the discontinuities were such as to cause a decrease in the compressive tangential stress in the substrate at the interface as shown by the upward movement of all curves from the green dashed uncoated curve in Figure 7.2.3c. The lower the value of this stiffness ratio, the greater the magnitude of the tangential stress discontinuity.

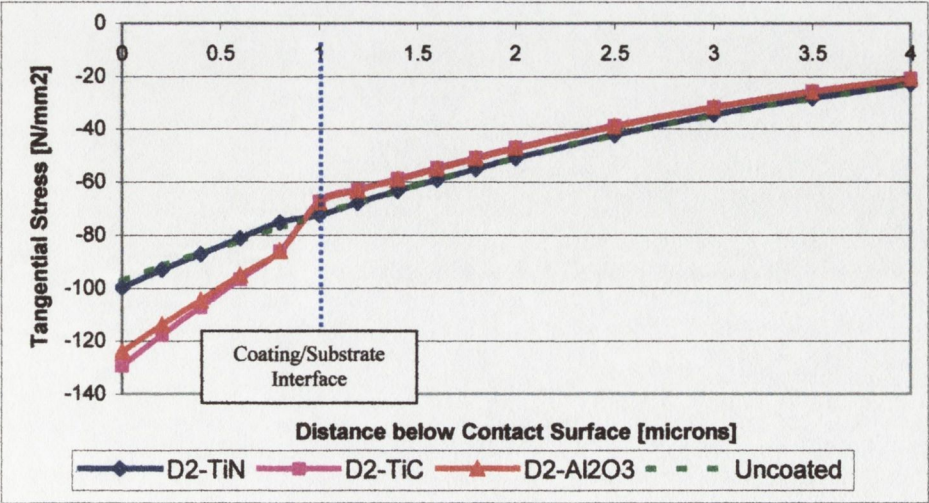


Figure 7.2.3b. Variation of σ_x through single-coated D2 substrate ($[t/a]=0.2$)

System (D2-XXX)	First Discontinuity (XXX-D2)
D2-Al ₂ O ₃	18.14 N/mm ²
D2-TiC	18.70 N/mm ²
D2- TiN	2.75 N/mm ²

Table 7.2.3c. Stress Discontinuity between coating and substrate interfaces ([t/a]=0.2)

7.2.4 Variation of σ_x through thick single-coated substrates ([t/a]=0.8)

As seen in Figure 7.2.4a, all the coated tools are subjected to less stress acting on the surface of the tool when compared to the magnitude of the stress acting on the surface of the uncoated tool TiN is subjected to the least. This is due to the relative stiffness of all the materials being studied. As can be seen from Figure 7.2.4a and Table 7.2.4a, there is quite a significant decrease in the magnitude of the stress discontinuity between the coating and the WC substrate for a thicker coating than compared to a thin coating (Figure 7.2.3a and Table 7.2.3a). As shown previously, the magnitude of stress decreases with increasing distance below the surface of the applied pressure. So as the coating is now thicker, the stress measured just above the coating/substrate interface has decreased relative to that for a thin coating measured at the same position. However the WC-TiN tool performs marginally better with a smaller stress discontinuity between the coating and substrate, which is due to the relative stiffness of the two moduli. Again the effect of the discontinuity was to cause an increase in the compressive tangential stress in the substrate at the interface as shown by the downward movement of all curves from the green dashed uncoated curve in Figure 7.2.4a. However the discontinuities were larger for the thin coating than for the thick coating systems.

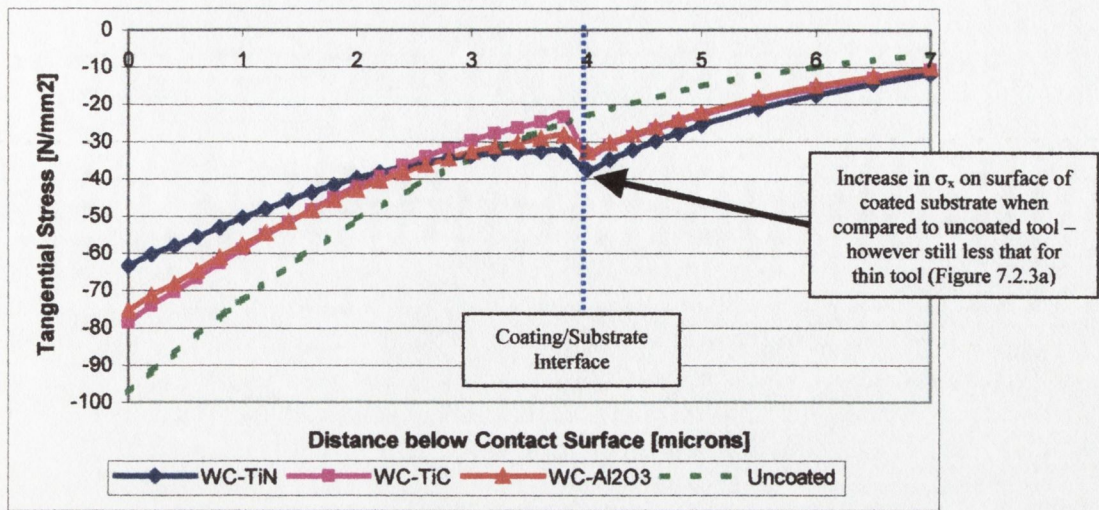


Figure 7.2.4a. Variation of σ_x through single-coated WC substrate ($[t/a]=0.8$)

System (WC-XXX)	First Discontinuity (XXX-WC)
WC-Al ₂ O ₃	-4.943 N/mm ²
WC-TiC	-9.952 N/mm ²
WC- TiN	-4.481 N/mm ²

Table 7.2.4a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.8$)

For the case of the D2 tool steel substrate as shown in Figure 7.2.4b and Table 7.2.4b, initially there are greater stresses in both the Al₂O₃ and TiC coatings due to the relative stiffness of these materials when compared to the less stiff TiN. However, the magnitudes of the stress discontinuities for the thicker coatings have greatly increased compared to those with a single thin coatings (Figure 7.2.3b and Table 7.2.3c). Again the D2-TiN tool performs better due to the similarity of elastic moduli between the TiN and D2 tool steel (250MPa and 200MPa respectively). For the first time, the TiC and Al₂O₃ coatings undergo tensile tangential stresses along the interface, which can lead to fracture and detachment of the coatings. This is due to a combination of both the thickness and the stiffness of the coating on top of a softer D2 substrate. These stress discontinuities were such as to cause a decrease in the compressive tangential stress in the substrate at the interface as shown by the upward movement of all curves from the green dashed uncoated curve in Figure 7.2.4b.

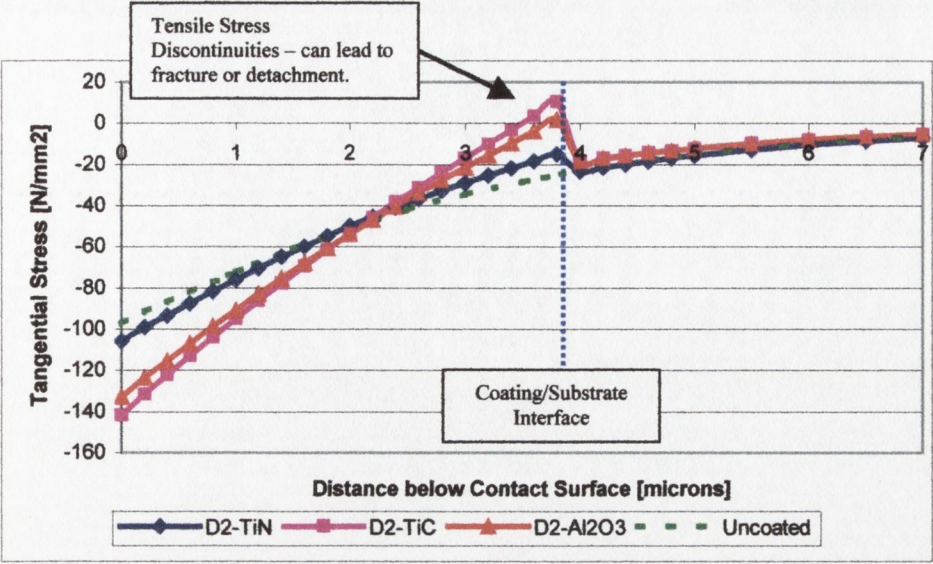


Figure 7.2.4b. Variation of σ_x through single-coated D2 substrate ($[t/a]=0.8$)

System (D2-XXX)	First Discontinuity (XXX-D2)
D2-Al ₂ O ₃	-20.4215 N/mm ²
D2-TiC	-28.73 N/mm ²
D2- TiN	-8.22 N/mm ²

Table 7.2.4b. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.8$)

7.2.5 Variation of σ_x along top of single-coated tool

Figure 7.2.5a shows the magnitude of the tangential stress, on the surface of the single-coated WC substrate decreasing with increased distance from the centre of contact of the load at $x=0$ (along line 2 on Figure 7.2). Again the contact half-width for all models is $5\mu\text{m}$. What is interesting to note is the decreased stress on the TiN-coated WC substrate at $x=0$ compared to the other two coatings due to the lower modulus of TiN relative to TiC and Al_2O_3 . The maximum tangential stress for all tools occurs at a distance of $4.5\mu\text{m}$ from the centre of contact so a $[x/a]$ ratio of 0.9 is obtained (as a is $5\mu\text{m}$ for all models). This value is maintained for all single-coated models, with both WC and D2 substrates, for both thin and thick coatings.

The location of this maximum stress is of importance. The author has found that if either the contact half-width, a , the applied Hertzian pressure distribution, or the thickness

of the coating, t , is varied, the maximum stress will always occur at an $[x/a]$ ratio of 0.9. As previously explained in Section 5.3, the width and height of the model has to be 30 times greater than the contact half-width, a . As the Hertzian pressure exerts itself of the half-width, the tangential stresses increase along the top of the coating. As the coating is elastic, its resistance to deformation exerts an opposite tangential force on the force wanting to deform it. The combination of both these forces, acting on the same point in opposite directions, will result in a maximum stress occurring at a $[x/a]$ ratio of 0.9 for all models.

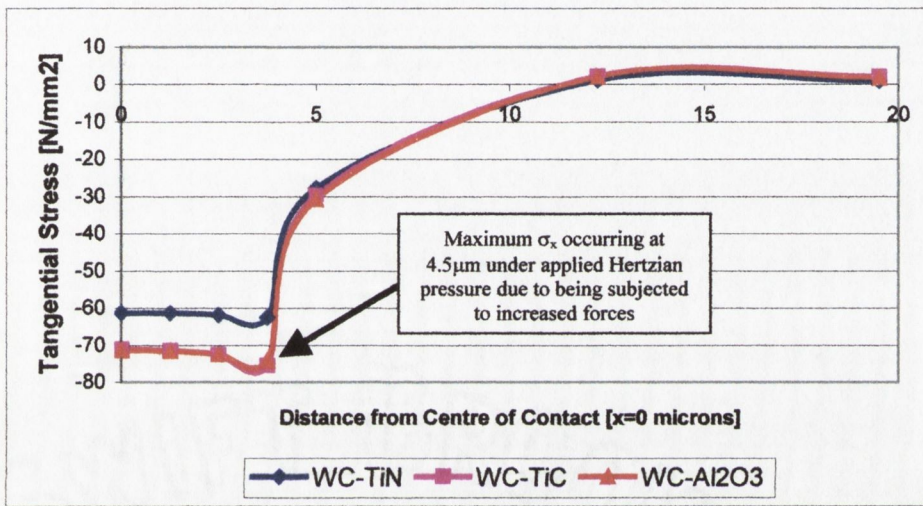


Figure 7.2.5a. Tangential Stress along top of single-coating ($[t/a]=0.2$)

Figure 7.2.5b shows the tangential stress results, but this time along the top of the single-coating with an increase in the thickness ratio ($[t/a]$ increasing from 0.2 to 0.8). This again shows the maximum stress on the coating surface to be found at a $[x/a]$ ratio of 0.9, with the magnitude of this maximum stress increasing with increasing thickness. At a distance of approximately $12\mu\text{m}$ from the centre of contact, for both thin and thick single-coated WC substrates, the nature of the stress changes from compressive to tensile and finally trails off to zero at a distance of about $23\mu\text{m}$ from the centre of contact. These tensile stresses can cause fracture to occur on the coating surface which leads to tool failure.

TiN again is subjected to the least tangential stress on the tool surface due to its lower stiffness relative to the other two coating materials. Again, as in the case of thin single-coated tools, the maximum tangential stress occurs at a distance of $4.5\mu\text{m}$ from the centre of contact.

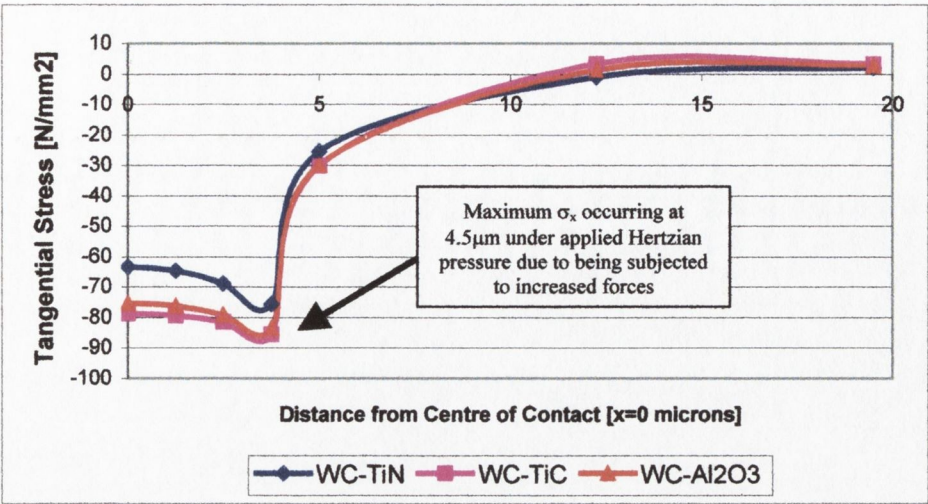


Figure 7.2.5b. Tangential Stress along top of single-coating ($[t/a]=0.8$)

7.2.6 Variation of σ_x along coating/substrate interface

Figure 7.2.6a shows the effect that the contact loading has at the interface between the single-coating and the substrate for a $[t/a]$ value of 0.2. What is interesting to note is that with the thin single-coating, there is relatively little difference in the magnitude of this stress for all coated tools, and in all cases the maximum value occurs on the centre of contact line (where $x=0$). For thin single-coated tools, this indicates that coating stiffnesses have little effect on the interfacial tangential stresses.

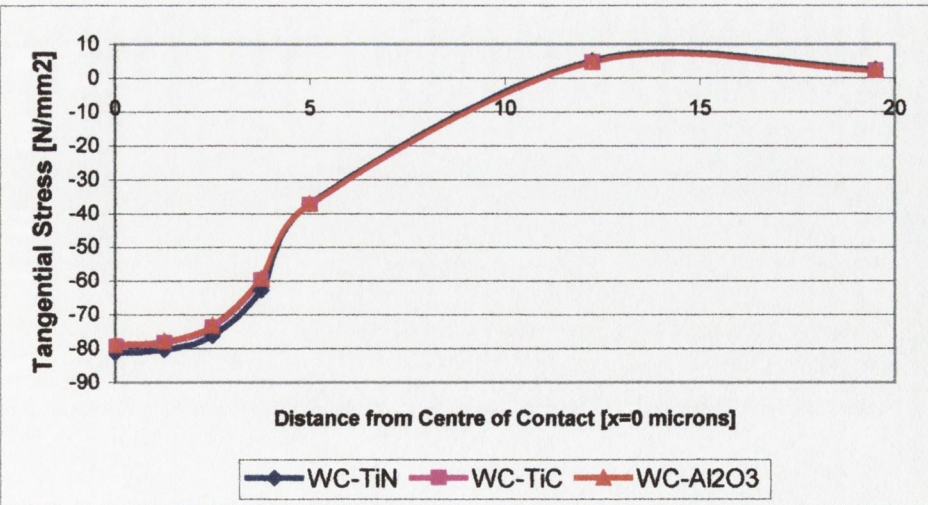


Figure 7.2.6a. Tangential Stress along coating/substrate interface ($[t/a]=0.2$)

Figure 7.2.6b however shows that increasing the thickness of the coating does indeed have a dramatic effect on the interfacial tangential stresses. As the thickness ratio is increased from $[t/a]$ of 0.2 to 0.8, the magnitude of the interfacial stress decreases. The maximum value for all single-coated substrates again occurs along the centre of contact line ($x=0$). However, the maximum stress along the TiN-coated substrate decreases by over 250%. This is primarily due to the thickness of the coating, and as shown previously, the magnitude of stress decreases with increasing distance below the surface of the applied pressure. So as the coating is now thicker, the stress measured at the coating/substrate interface has decreased relative to that for a thin coating measured at the same position. Again the TiC-coated substrate has the largest interfacial stress due to its increased modulus, but even the maximum stresses acting on it have reduced by over 200%.

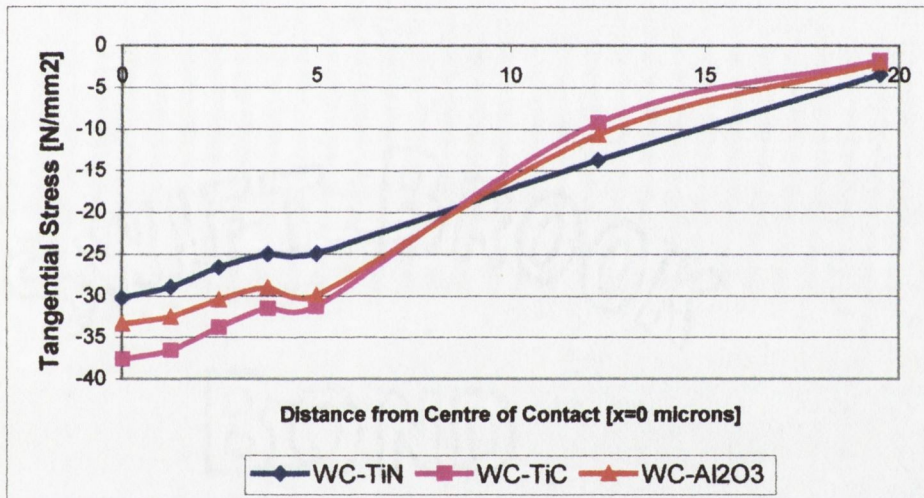


Figure 7.2.6b. Tangential Stress along coating/substrate interface ($[t/a]=0.8$)

7.2.7 Effect of thickness ratio on maximum shear stress, $\tau_{xy(\max)}$, for single-coated substrates

High tensile in-plane stresses, σ_x , contribute to brittle failure or detachment of the coating at its surface or interface whilst high compressive in-plane stresses cause buckling in the presence of interfacial cracks and local areas of poor adhesion. However in-plane shear stresses contribute to a combination of both the above mixed mode failures as well as delamination. Although, in the finite element models, the coating layers are assumed to be perfectly bonded together, in practice the coating interfaces can only sustain a certain level of stress corresponding to their interfacial strength. Therefore a detailed knowledge of the

magnitude and the distribution of the tangential and shear stresses are required for a better understanding of the failure mechanisms which may occur.

Figure 7.2.7 shows the distribution of the maximum value of the shear stresses, $\tau_{xy(\max)}$, for single-coated tools, as the thickness ratio $[t/a]$ of the coatings increases from 0.2 to 1. The contact half-width, a , for all models was $5\mu\text{m}$. As in the case of the maximum tangential stress, $\sigma_{x(\max)}$, the maximum shear stress, $\tau_{xy(\max)}$, was always greater for the case of the D2 substrate compared to the WC substrate. For the case of the WC substrate, it can be seen that there is little significant difference in the shear stress for any of the three coatings investigated, until the thickness ratio $[t/a]$ increases to one. It can also be seen that for this substrate, the magnitude of $\tau_{xy(\max)}$ decreases as the thickness ratio $[t/a]$ increases. This is contrasted to the results from the D2 substrate. As the thickness ratio increases, so does the magnitude of the maximum shear stress. This is due to the stiffness ratio $E_{\text{substrate}}/E_{\text{coating}}$. For the WC substrate, if this stiffness ratio is greater than one then the maximum shear stress decreases with an increased thickness ratio.

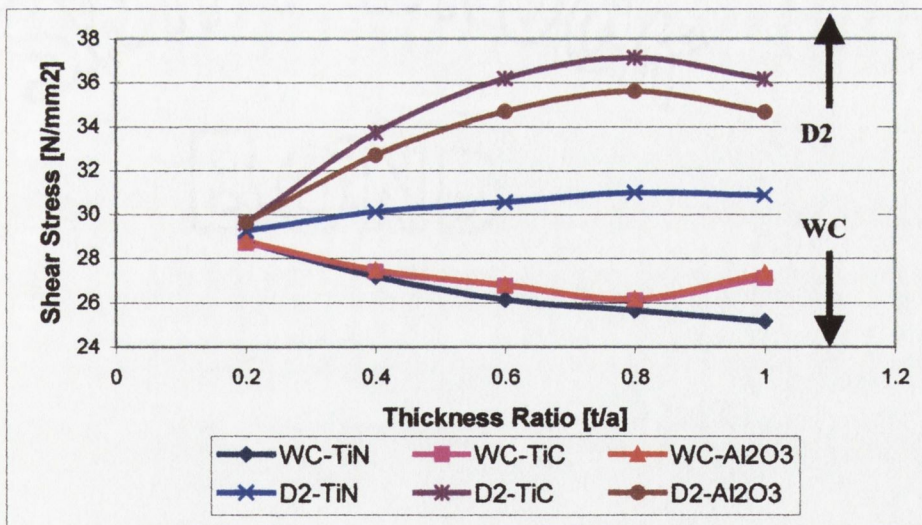


Figure 7.2.7. Effect of Thickness ratio for single-coated substrates

7.2.8 Variation of τ_{xy} along single-coated substrate interfaces

Figure 7.2.8a and 7.2.8b shows the variation of the shear stresses along the top of the single-coated substrate (*line 2 on Figure 7.2*) and at the coating/substrate interface (*line 3 on Figure 7.2*) from the centre of contact ($x=0$) for both thin and thick coatings ($[t/a]=$

0.2, 0.8 respectively). The solid lines on the graph indicate the thin coatings whilst the dashed represent the thick coatings. Only the TiN coated substrates are shown as it has been observed that all the coated substrates react very similarly under the same pressure for both thin and thick coatings. However, the TiC and Al₂O₃ single-coated tools can be found in Appendix 1.

It can be observed from Figure 7.2.8a that the shear stress along the top of the tool doesn't vary by much for both thin and thick coatings. Initially a negative shear stress is observed immediately under the contact half-width, a , which soon turns positive at a distance, x , of 4.5 μm from the centre of contact, and gradually reduces to zero over distance from the centre of contact. However, the shear stress between the interface of the coating and substrate is quite different. The shear stress for thick coatings is at all times greater than the shear stress for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings. This stress in both cases reaches a maximum at the contact half-width, $x=4.5\mu\text{m}$ which again shows the maximum positive shear stress to be occurring with a $[x/a]$ ratio of 0.9. As can be seen from the graph, the difference in the shear stress in the contact region shows the greatest variance. It can be seen that the shear stresses increase for thicker single-coatings, unlike the corresponding tangential stresses along the coating-substrate interface, which have been shown to decrease with increasing thickness (Section 7.2.6).

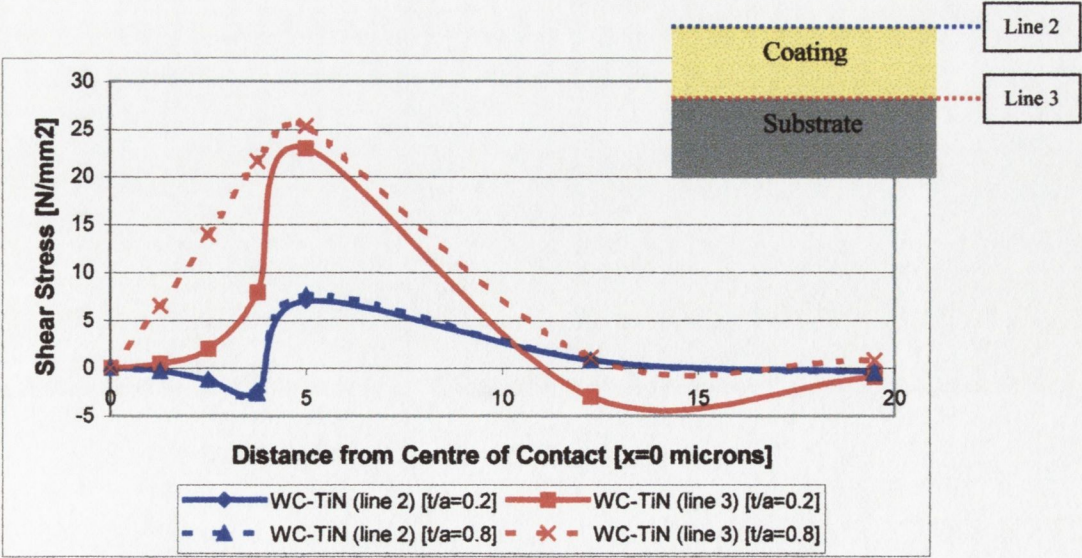


Figure 7.2.8a. Variation of τ_{xy} along single-coated WC substrate interfaces

However there are minor differences when a D2 substrate is used instead of WC. From Figure 7.2.8b, it can be seen that again that the shear stress along the top of the thin and thick coatings is much the same. Again, the greatest change in shear stress variance takes place under the contact half-width, a , but for the case of D2, there is little difference in the maximum value at $x=4.5\mu\text{m}$ for either the thin and thick coatings. Similarly, due to the decreased stiffness of the D2 substrate, the shear stresses along the coating-substrate interface are greater in magnitude relative to the WC substrate outside the contact half-width length of $5\mu\text{m}$. This again is unlike the changes in the tangential stresses along the D2 coating-substrate interface which have been shown to increase with greater thickness.

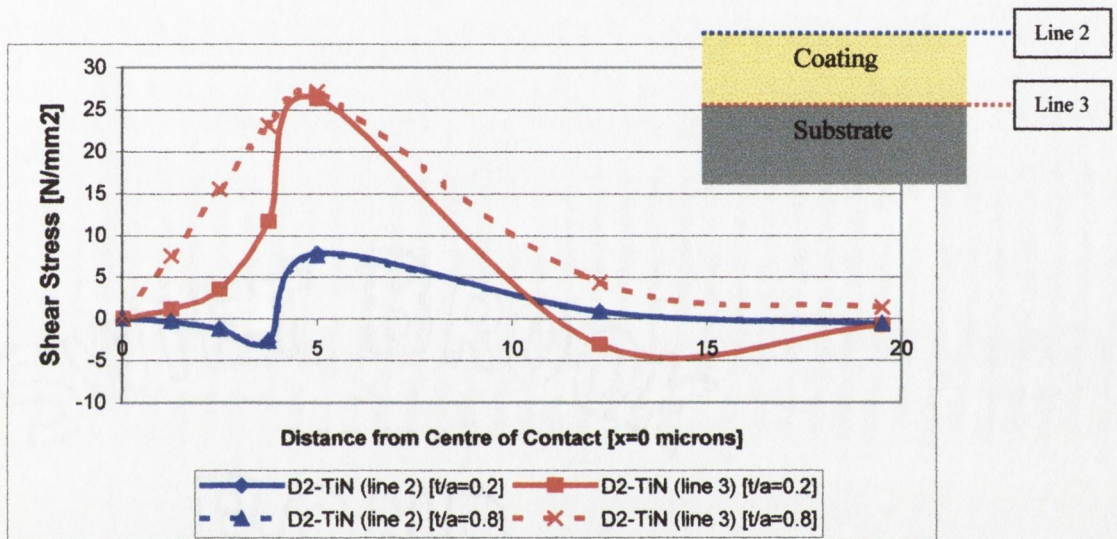


Figure 7.2.8b. Variation of τ_{xy} along single-coated D2 substrate interfaces

7.2.9 Summary of Stresses within Single-Coated Tools

Comparing the results of thin and thick single-coated tools, the following observations can be made. For all WC substrates, there is always a decrease in the magnitude of the tangential stress on the surface for both thin and thick single-coated tools when compared to an uncoated tool. For all D2 substrates, the reverse holds true and the coatings cause an increase in the magnitude of tangential stress on the surface.

For both WC and D2 substrates, there is always a tangential stress discontinuity between the interface of the coating and the substrate. The magnitude of these discontinuities depends on the relative stiffness ratio, $E_{\text{substrate}}/E_{\text{coating}}$, of the substrate and coating. For WC substrates, the greater than the value of 1 it is, the greater the

discontinuity. For D2 substrates, the smaller than the value of 1 it is, the greater the discontinuity.

For both thin and thick single-coated models, only the interface of the thick single-coated D2 substrates was subjected to tensile tangential stresses which can lead to fracture and detachment of the coatings. This was due to a combination of the thickness and the stiffness of the coating on top of a softer D2 substrate. Increasing the thickness of the coating does indeed have a remarkable effect on the interfacial tangential stresses. As the thickness ratio is increased from $[t/a]$ of 0.2 to 0.8, the magnitude of the interfacial tangential stress decreases. As the coating is thicker, the stress measured at the coating/substrate interface has decreased relative to that for a thin coating measured at the same position.

For the case of the WC substrate, it can be seen that there is little significant difference in the maximum shear stress for any of the three coatings investigated at any thickness ratio. It has also be seen from the previous ANSYS simulations, that the magnitude of $\tau_{xy(\max)}$ decreases as the thickness ratio $[t/a]$ increases.

The shear stress along the interfaces for thick coatings is at all times greater than the shear stress for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings. The difference in the shear stress under the contact half-width region shows the greatest variance between the thin and thick coating.

7.3 Stress Distributions in Double-Coated Substrates

7.3.1 Effect of thickness ratio on maximum tangential stress, $\sigma_{x(\max)}$, for double-coated substrates

Figure 7.3.1a and 7.3.1b shows the effect of the thickness ratio $[t/a]$ on double-coatings of increasing thicknesses on WC and D2 substrates respectively. All values are

taken at the centre of contact line at $x=0$ where σ_x is a maximum. The dashed line represents the stresses within an uncoated WC-Co substrate.

With double-coatings, since each WC-XXX-YYY system has an identical substrate, the stress is now related to the stiffness of the coating next to the substrate and also the stiffness of the top and bottom coatings. The greater the ratio of $E_{\text{substrate}}/E_{\text{bottomcoating}}$, the greater will be the resulting stress. Therefore the maximum tangential stress is greater for the double layer combination of coatings given by WC-TiN-TiC (2.56) than for WC-TiC-TiN (1.422). However another ratio has to be taken into account. The greater the ratio of $E_{\text{bottomcoating}}/E_{\text{topcoating}}$, the lower will the resulting stress. Thus for a ratio greater than one, a softer coating is on top and for a value less than one, a harder coating is on top. So the tangential stress is lower for the double layer combination of coatings given by WC-TiC-TiN (1.8) than for WC-TiC- Al_2O_3 (1.154). Table 7.3.1 compares the ratio of possible combinations and as can be seen, the WC-TiC-TiN (1.8) layer has the best combination of moduli. Another combination which gives good results is the WC- Al_2O_3 -TiN (1.56) due to the decreasing stiffness of the layers.

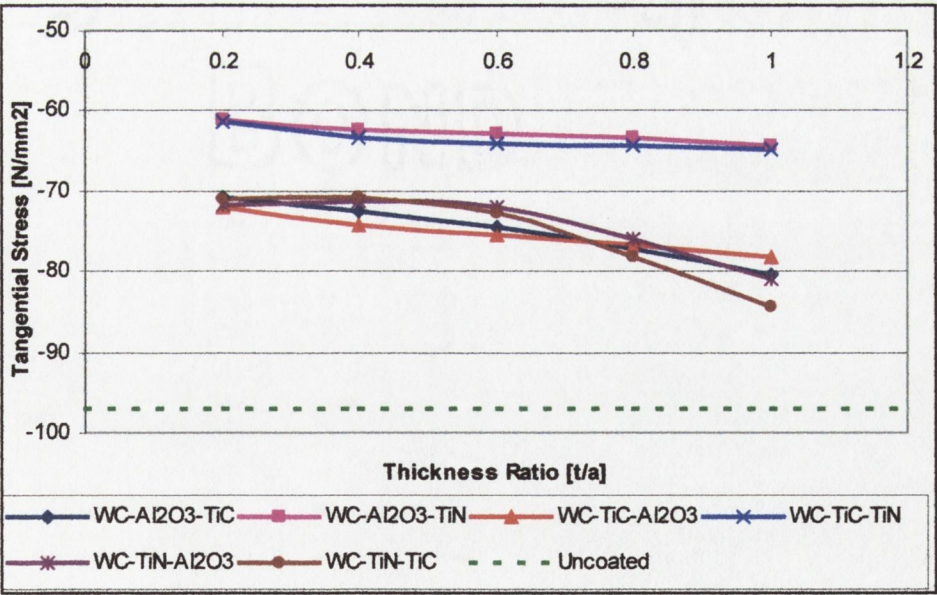


Figure 7.3.1a. Effect of Thickness ratio for double-coated WC substrates.

Combination	$E_{\text{substrate}}/E_{\text{bottomcoating}}$	Combination	$E_{\text{bottomcoating}}/E_{\text{topcoating}}$
WC-Al ₂ O ₃	1.641	Al ₂ O ₃ -TiC	0.867
WC-TiC	1.422	Al ₂ O ₃ -TiN	1.560
WC-TiN	2.560	TiC-Al ₂ O ₃	1.154
D2-Al ₂ O ₃	0.513	TiC-TiN	1.800
D2-TiC	0.444	TiN-Al ₂ O ₃	0.641
D2-TiN	0.800	TiN-TiC	0.556

Table 7.3.1. Ratio of Moduli for Double-Coated Substrates

As mentioned in the Section 7.2.1, if the modulus ratio for WC coated substrates increases past 1.4, the effect of the $[t/a]$ ratio is minimised. However, as shown in Table 7.3.1, as four of the possible $E_{\text{bottomcoating}}/E_{\text{topcoating}}$ combinations for double-coated tools have modulus ratio values lower than this, there is an increase in σ_x for increasing $[t/a]$ ratios as shown in Figure 7.3.1a.

Having a substrate of D2 tool steel instead of WC again changes all the stresses due to the reduced stiffness of D2 compared to WC. As can be seen in Figure 7.3.1b, again the D2-TiC-TiN and D2-Al₂O₃-TiN double-coated substrates have the lowest stress, however the magnitude of this tangential stress has increased relative to the WC substrate due to the softer D2 substrate. Again, if the modulus ratio for D2 coated substrates decreases past 0.5, the effect of the $[t/a]$ ratio is minimised. However, as shown in Table 7.3.1, as all of the possible $E_{\text{bottomcoating}}/E_{\text{topcoating}}$ combinations for double-coated tools have modulus ratio values greater than this, there is an increase in σ_x for increasing $[t/a]$ ratios as shown in Figure 7.3.1b. Only two double-coated combinations, D2-TiN-Al₂O₃ (0.641) and D2-TiN-TiC (0.556), are close to this maximum ratio value and hence their curves are more linear than the other four.

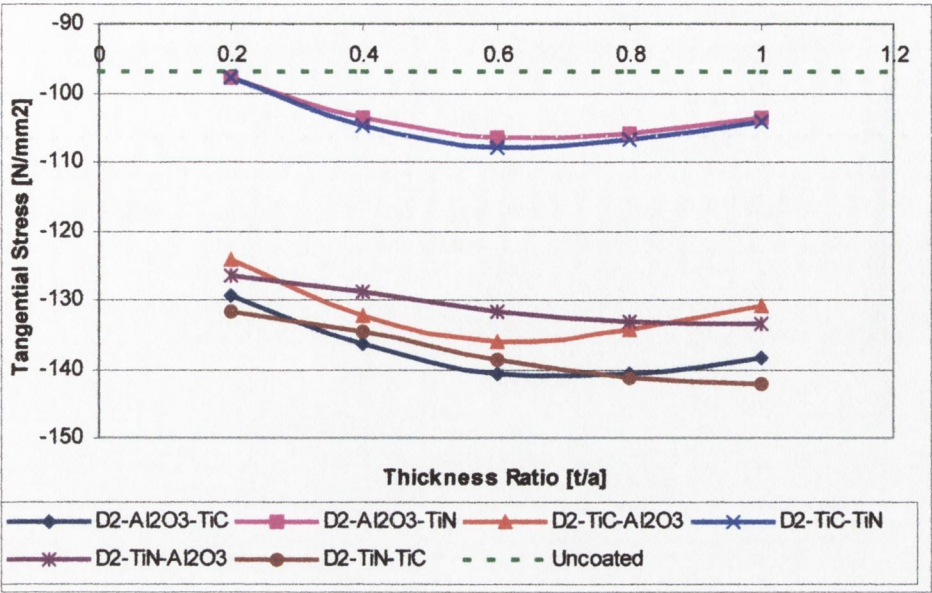


Figure 7.3.1b. Effect of Thickness ratio for double-coated D2 substrates.

7.3.2 Variations of σ_x through double-coated substrates ($[t/a]=0.2$)

It can be seen from Figures 7.3.2a and 7.3.2b, and Tables 7.3.2a and 7.3.2b, that the discontinuities are greater between the middle coating and the substrate than between the top coating and the middle coating. However, with the addition of a second coating, the stress discontinuities for the D2 tool steel substrate are greater than that of the WC substrate. A compressive stress discontinuity is more favourable than a tensile one, because tensile stresses will tend to fracture the coatings and this is the more predominant failure mode. So what is desirable is a compressive stress discontinuity in both the first and second locations as indicated in Figure 7.3.2a and 7.3.2b. For the WC range of substrates all the discontinuities are compressive however only two tools have a negative first discontinuity which helps minimise the effects of the second discontinuity. These are the WC-Al₂O₃-TiN and the WC-TiC-TiN inserts which cause the jump of stress from top of the tool to the substrate surface to be gradually minimised. An insert which has a top coat of TiN causes the stress at the first discontinuity to be negative whilst an insert having a middle coating of TiN causes the second discontinuity with the WC substrate to be positive, as shown by Table 7.3.2a with the WC-TiN-Al₂O₃ and WC-TiN-TiC inserts. All the double-coatings have an effect on the substrate, which causes it to be even more compressive just below its interface with the coating, which is shown by the downward movement of all curves from the green dashed line in Figure 7.3.2a.

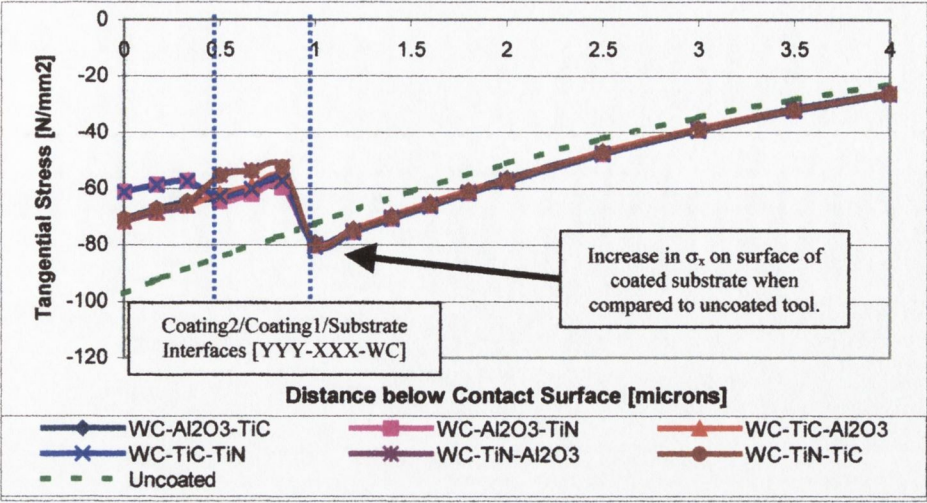


Figure 7.3.2a. Variations of σ_x through double-coated WC substrates ($[t/a]=0.2$)

System (WC-XXX-YYY)	First Discontinuity (YYY-XXX)	Second Discontinuity (XXX-WC)
WC-Al ₂ O ₃ -TiC	0.78 N/mm ²	-20.41 N/mm ²
WC-Al ₂ O ₃ -TiN	-7.26 N/mm ²	-20.86 N/mm ²
WC-TiC-Al ₂ O ₃	4.24 N/mm ²	-22.97 N/mm ²
WC-TiC-TiN	-5.57 N/mm ²	-23.38 N/mm ²
WC-TiN-Al ₂ O ₃	10.8 N/mm ²	-27.62 N/mm ²
WC-TiN-TiC	9.29 N/mm ²	-27.74 N/mm ²

Table 7.3.2a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.2$)

The differences in the stress discontinuities are more pronounced with the D2 coated substrate. This is due to the relative stiffness of the base substrate and hence the first discontinuity at the interface between the two coatings was greater. Again only two tools gave a negative tangential stress at the first discontinuity. These were the D2-TiC-TiN and the D2-Al₂O₃-TiN. This top coating of TiN again causes this negative first stress discontinuity. However, as shown in Table 7.3.2b, because of the decreased stiffness of the D2 substrate, having a TiN middle coating will result in a smaller second stress discontinuity due to the favourable stiffness ratios outlined in Section 7.2.3. However this middle coat of TiN results in the magnitude of the first discontinuity increasing.

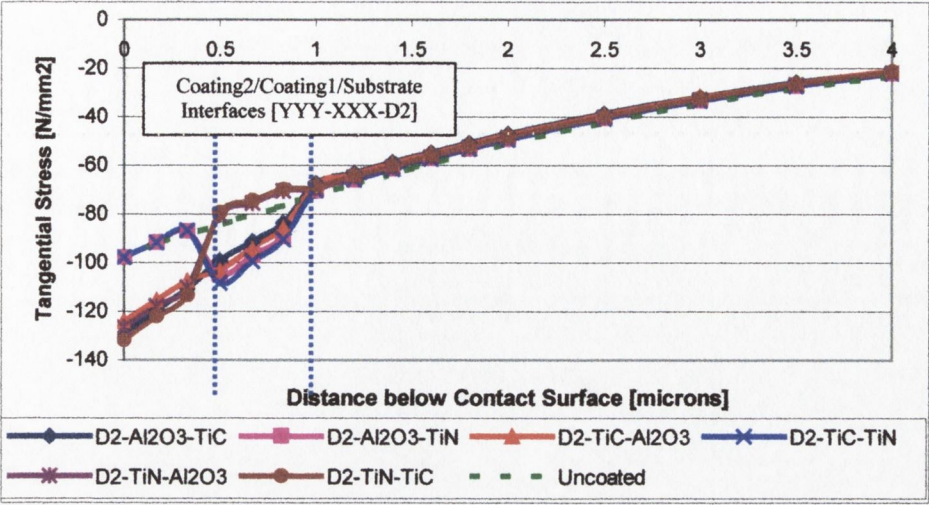


Figure 7.3.2b. Variations of σ_x through double-coated D2 substrates ($[t/a]=0.2$)

System (D2-XXX-YYY)	First Discontinuity (YYY-XXX)	Second Discontinuity (XXX-D2)
D2-Al ₂ O ₃ -TiC	11.60 N/mm ²	16.22 N/mm ²
D2-Al ₂ O ₃ -TiN	-18.08 N/mm ²	18.93 N/mm ²
D2-TiC-Al ₂ O ₃	4.80 N/mm ²	17.43 N/mm ²
D2-TiC-TiN	-21.56 N/mm ²	20.31 N/mm ²
D2-TiN-Al ₂ O ₃	29.78 N/mm ²	1.05 N/mm ²
D2-TiN-TiC	33.62 N/mm ²	0.92 N/mm ²

Table 7.3.2b. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.2$)

7.3.3 Variations of σ_x through double-coated substrates ($[t/a]=0.8$)

As can be seen from Figure 7.3.3a and Table 7.3.3a, there is quite a significant decrease in the size of the stress discontinuity between the double-coating and the WC substrate for a thick coating compared to a thin coating. As shown previously, the magnitude of stress decreases with increasing distance below the surface of the applied pressure. So as the coating is now thicker, the stress measured just above the coating/substrate interface has decreased relative to that for a thin coating measured at the same position. What is interesting to note is that the tangential stress for thick double-coated WC substrates is much smaller than the stress for a thin double-coated WC substrate but also the stress at the top of the *substrate* is 56% lower due to these thicker

coatings (Thin = -79.82 N/mm² & Thick = -34.83N/mm²). Again the double-coatings has an effect which causes the WC substrate to be more compressive just below its interface with the coating as shown by the downward movement of all curves from the green dashed uncoated curve in Figure 7.3.3a.

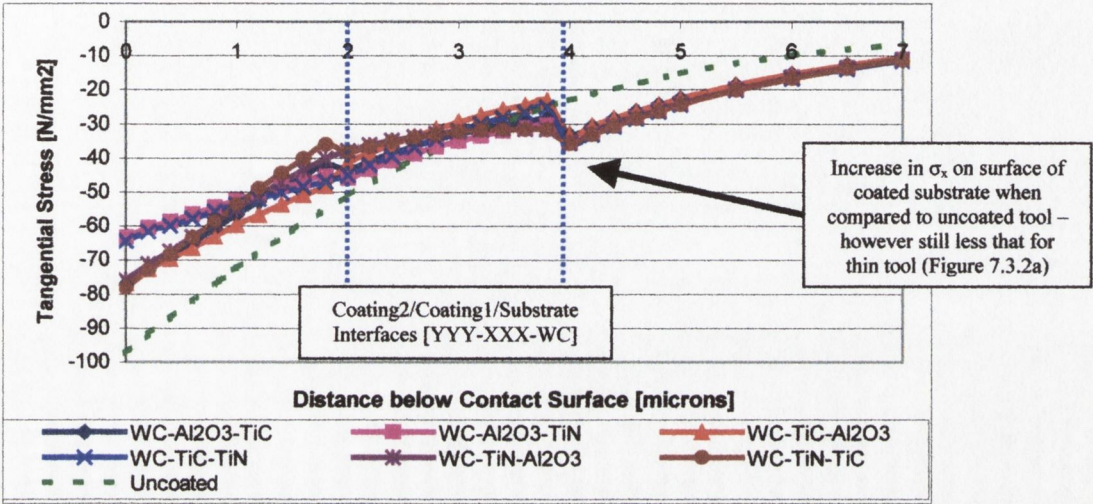


Figure 7.3.3a Variations of σ_x through double-coated WC substrates ($[t/a]=0.8$)

System (WC-XXX-YYY)	First Discontinuity (YYY-XXX)	Second Discontinuity (XXX-WC)
WC-Al ₂ O ₃ -TiC	-1.31 N/mm ²	-4.60 N/mm ²
WC-Al ₂ O ₃ -TiN	-0.81 N/mm ²	-5.23 N/mm ²
WC-TiC-Al ₂ O ₃	6.66 N/mm ²	-9.88 N/mm ²
WC-TiC-TiN	1.79 N/mm ²	-10.57 N/mm ²
WC-TiN-Al ₂ O ₃	2.25 N/mm ²	-3.91 N/mm ²
WC-TiN-TiC	-2.25 N/mm ²	-4.14 N/mm ²

Table 7.3.3a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.8$)

Comparing a thick double-coated WC substrate to a thick single-coated WC substrate shows that the combined stress discontinuities for the double-coated tool are only marginally greater than the discontinuities for the single-coated tool. Again the magnitude of the tangential stress on the top of the *substrate* is smaller for the double-coated tool. Both these effects indicates the benefits of a double-coat over a single-coat.

In the case of the D2 tool steel substrate as shown in Figure 7.3.3b, the magnitudes of the stress discontinuities have greatly decreased compared to those with a single thin coating. Both the D2-TiC-Al₂O₃ and the D2-TiC-TiN tools undergo tensile tangential stresses which can lead to fracture and detachment of the coatings. This is due to a combination of the thickness and the stiffness of the coating on top of a softer D2 substrate. Therefore these tools would not be favourable due to the arrangement of these coatings however the magnitude of these tensile stresses would hardly be enough to cause fracture to occur.

Increasing the thickness of the coating has the effect of lowering the first stress discontinuity between the two coatings. However the discontinuities between the bottom coating and the substrate change from being positive to being negative. These stress discontinuities were such as to cause a decrease in the compressive tangential stress in the substrate at the interface as shown by the upward movement of all curves from the green dashed uncoated curve in Figure 7.3.3b.

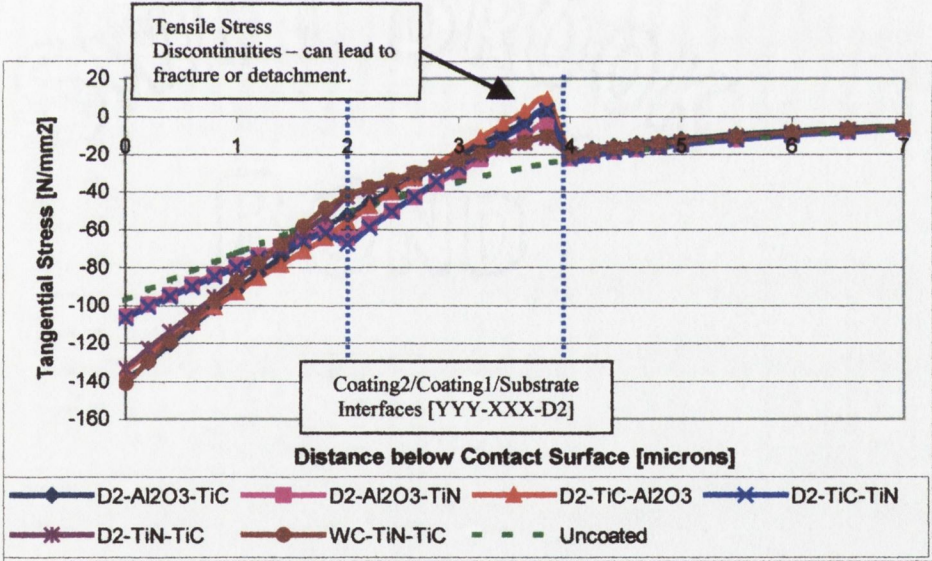


Figure 7.3.3b Variations of σ_x through double-coated D2 substrates ($[t/a]=0.8$)

System (D2-XXX-YYY)	First Discontinuity (YYY-XXX)	Second Discontinuity (XXX-D2)
D2-Al ₂ O ₃ -TiC	5.21 N/mm ²	-20.71 N/mm ²
D2-Al ₂ O ₃ -TiN	-4.90 N/mm ²	-18.06 N/mm ²
D2-TiC-Al ₂ O ₃	8.92 N/mm ²	-28.35 N/mm ²
D2-TiC-TiN	-5.58 N/mm ²	-25.44 N/mm ²
D2-TiN-Al ₂ O ₃	10.22 N/mm ²	-8.91 N/mm ²
D2-TiN-TiC	6.86 N/mm ²	-8.98 N/mm ²

Table 7.3.3b. Stress Discontinuity between coating and substrate interfaces ([t/a]=0.8)

7.3.4 Variation of σ_x along the interfaces of double-coated substrates

Figure 7.3.4a shows the magnitude of the tangential stress on the various interfaces of the WC-TiC-TiN tool, all decreasing, with increased distance from the centre of contact of the load. Again the contact half-width for all models is 5µm. Only results from a WC-TiC-TiN double-coated system are presented here. The rest of the double-coated systems are included in Appendix 2. The maximum tangential stress is seen to occur at the coating/substrate interface along the centre of contact line where x=0.

This maximum compressive stress drops off rapidly along lines 2 and 3 due to the favourable modulus ratio. At approximately 12µm on the thin double-coated WC substrate, the magnitude of the stress changes from compression to tension, for all interfaces, and trails off to zero at a distance of about 25µm from the centre of contact. These tensile stresses can cause fracture to occur on the coating surface which leads to tool failure however compressive stress are by far the more dominant in both models.

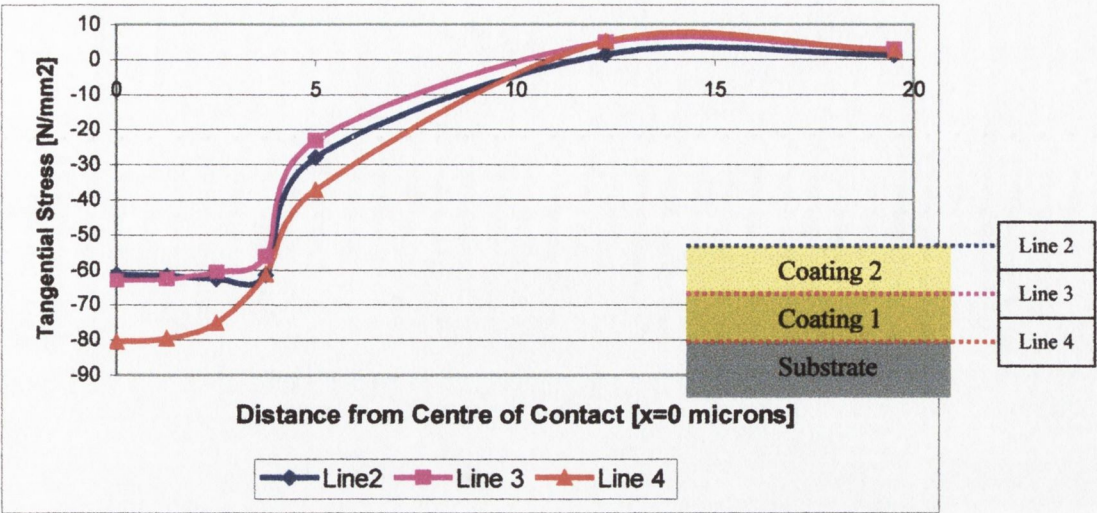


Figure 7.3.4a. Tangential Stress along interfaces of WC-TiC-TiN ($[t/a]=0.2$)

Figure 7.3.4b shows that increasing the thickness of the coating does indeed have a dramatic effect on the tangential interfacial stresses. As the thickness ratio is increased from $[t/a]$ of 0.2 to 0.8, the magnitude of the interfacial tangential stresses decrease. The maximum value of stress for all interfaces (except along the surface) occurs along the centre of contact line ($x=0$), and decreases rapidly to the contact half-width length, a . Along the contact surface, maximum tangential stress occurs at a distance of $4.5\mu\text{m}$ from the centre of contact so a $[x/a]$ ratio of 0.9 is obtained (*as a is $5\mu\text{m}$ for all models*). This value is maintained for all double-coated models, with both WC and D2 substrates, for both thin and thick coatings. As the Hertzian pressure exerts itself of the half-width, the tangential stresses increase along the top of the coating. As the coating is elastic, its resistance to deformation exerts an opposite tangential force on the force wanting to deform it. The combination of both these forces, acting on the same point in opposite directions, will result with a maximum stress occurring at a $[x/a]$ ratio of 0.9 for all models.

Due to the thick coating, the tangential stress decreases, however the magnitude of the tangential stress of the surface of the double-coated tool remains roughly the same. Again, at approximately $12\mu\text{m}$ on the thick double-coated WC substrate, the magnitude of the stress changes from compression to tension, only for two of the interfaces. Due to the thickness of the coatings, the stress at the coating/substrate interface remains compressive throughout the length of the interface.

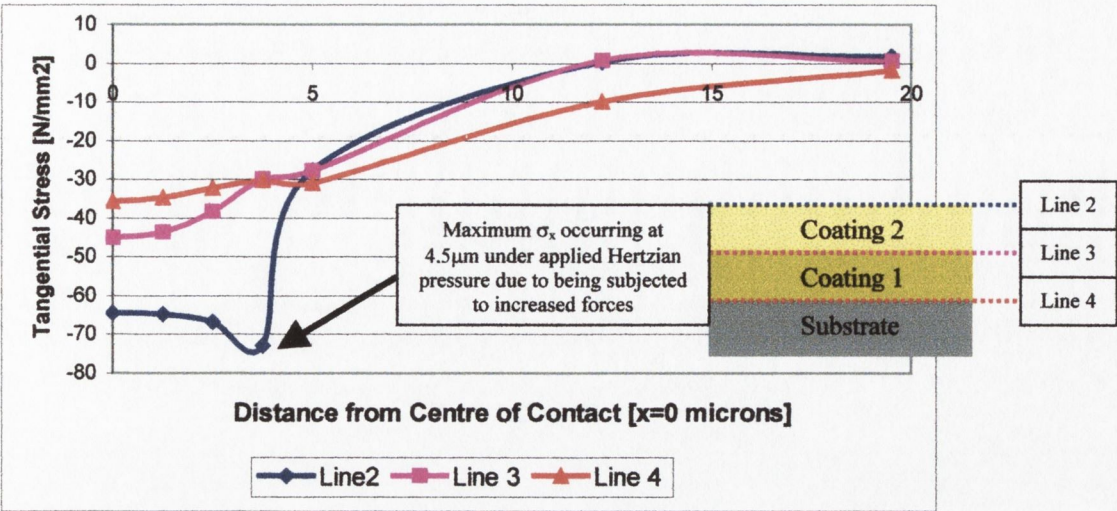


Figure 7.3.4b. Tangential Stress along interfaces of WC-TiC-TiN ([t/a]=0.8)

7.3.5 Effect of thickness ratio on maximum shear stress, $\tau_{xy(max)}$, for double-coated substrates

Figure 7.3.5 shows the influence of the substrate stiffness of the magnitude of the maximum shear stress along with the stiffness ratio of the top and bottom coating layers. With the addition of a second coat, and the total coating thickness remaining the same as for the single-coated models, the maximum shear stress is reduced for those coated tools with a favourable coating/coating and coating/substrate ratio. The tools which will perform the best will be the WC-TiC-TiN and the WC-Al₂O₃-TiN due to the continually decreasing modulus from the substrate to the top coat. WC-TiC-Al₂O₃ also performs well due to this decreasing modulus. This trend indicates that using TiC for the bottom coat exhibits a decrease in the maximum shear stress for an increasing thickness ratio. By comparison, if TiN forms this inner coat, there is an increase in $\tau_{xy(max)}$ as $[t/a]$ increases. This also holds true if the inner coat is less stiff than the top coat.

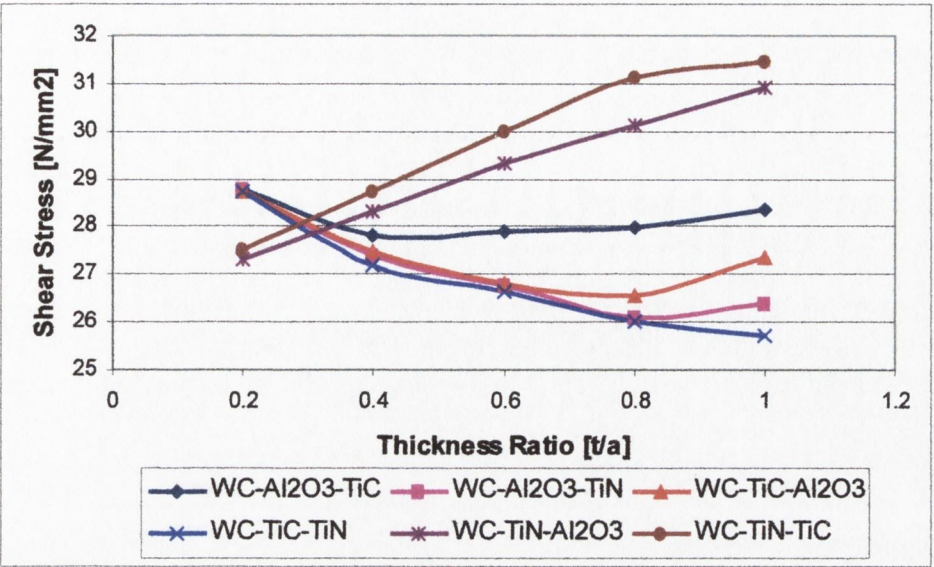


Figure 7.3.5. Effect of Thickness ratio for double-coated substrates

7.3.6 Variation of τ_{xy} along double-coated substrate interfaces

Figure 7.3.6a and 7.3.6b shows the variation of the shear stresses along the top of the double-coated substrate (line 2), the coating_{top}/coating_{bottom} interface (line 3) and the coating_{bottom}/substrate interface (line 4) from the centre of contact ($x=0$) for both thin and thick coatings ($[t/a]=0.2, 0.8$ respectively). The solid lines on the graph indicate the thin coatings whilst the dashed represent the thick coatings.

Again, as seen in Figure 7.3.6a, the maximum shear stress takes place a distance $4.5\mu\text{m}$ from the centre of contact which gives a $[x/a]$ ratio of 0.9. Again the interface with the greatest shear stress is that of the coating_{bottom}/substrate (line 4). The stress along the top of the double-coated substrate (line 2) is somewhat similar to that seen for the single-coated substrate while the stress for the coating_{top}/coating_{bottom} interface (line 3) is somewhere between both. Again the shear stresses increase for thicker double-coatings unlike the corresponding tangential stresses along the coating_{bottom}/substrate interface which have been shown to decrease with increasing thickness. The shear stress is generally high over a greater portion of the interface for thick coatings and only concentrated within a narrow band for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings.

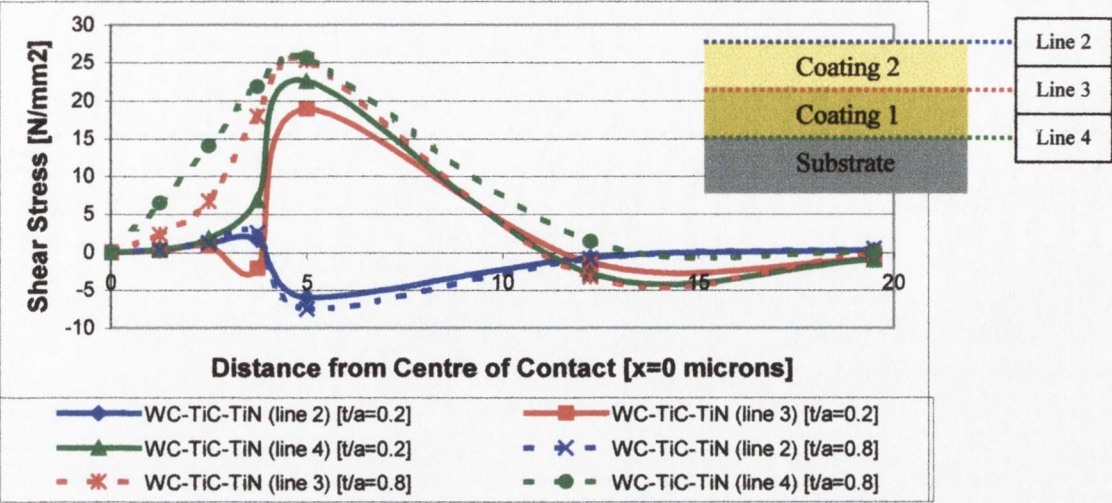


Figure 7.3.6a. Variation of τ_{xy} along double-coated WC-TiC-TiN interfaces

Again as shown by the differences in the curves from Figures 7.3.6a and 7.3.6b, the stiffness ratios between any two adjacent materials causes the shear stress to fluctuate. Any system having a top coating of TiN will have the lowest stress along the coating_{top}/coating_{bottom} interface (line 3) as long as it is on top of a more stiff material. In the case of thicker coatings, the maximum shear again occurred in the WC-TiC-Al₂O₃ system and the lowest for the WC-TiN-Al₂O₃ which follows the same pattern to the distribution of the tangential stress along the interfaces.

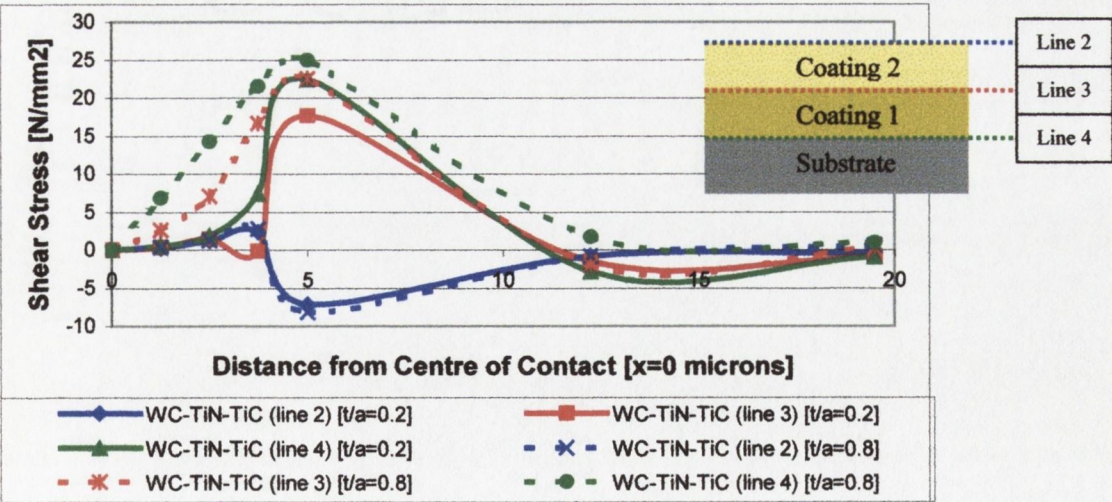


Figure 7.3.6b. Variation of τ_{xy} along double-coated WC-TiN-TiC interfaces

7.3.7 Summary of Stresses within Double-Coated Tools

Comparing the results of thin and thick double-coated tools, the following observations can be made. If the modulus ratio for WC coated substrates increases past 1.4, the effect of the $[t/a]$ ratio is minimised. However, four of the possible $E_{\text{bottomcoating}}/E_{\text{topcoating}}$ combinations for double-coated tools have modulus ratio values lower than this, so there is an increase in σ_x for increasing $[t/a]$ ratios for these four combinations.

The tangential stress discontinuities are greater between the middle coating and the substrate than between the top coating and the middle coating for thin coatings. An insert which has a top coat of TiN causes the stress at the first discontinuity to be negative whilst an insert having a middle coating of TiN causes the second discontinuity with the WC substrate to be positive

There is quite a significant decrease in the size of the tangential stress discontinuity between the double-coating and the WC substrate for a thick coating compared to a thin coating. What is interesting to note is that the combined tangential stress discontinuities for thick double-coated WC substrates are much smaller than the discontinuities for a thin double-coated WC substrate but also the tangential stress at the top of the *substrate* is 56% lower due to these thicker coatings

Comparing a thick double-coated WC substrate to a thick single-coated WC substrate shows that the combined tangential stress discontinuities for the double-coated tool are only marginally greater than the discontinuities for the single-coated tool. Again the stress on the top of the substrate is lower for the double-coated tool. Both these effects indicate the benefits of a double-coating over a single-coating. Increasing the thickness of the coating has the effect of lowering the first stress discontinuity between the two coatings.

The maximum shear stress for a double-coated insert is reduced for those coated tools with a favourable coating/coating and coating/substrate ratio as the thickness ratio, $[t/a]$, increases. The tools which will perform the best will be the WC-TiC-TiN, the WC-Al₂O₃-TiN and the WC-TiC-Al₂O₃ due to the continually decreasing modulus from the

substrate to the top coat. This trend indicates that using TiC for the bottom coat exhibits a decrease in the maximum shear stress for an increasing thickness ratio. By comparison, if TiN forms this inner coat, there is an increase in $\tau_{xy(\max)}$ as $[t/a]$ increases. This also holds true if the inner coat is less stiff than the top coat.

Again the interface with the greatest shear stress is that of the coating_{bottom}/substrate. The shear stresses increase for thicker double-coatings unlike the corresponding tangential stresses along the coating_{bottom}/substrate interface which have been shown to decrease with increasing thickness. The shear stress is generally high over a greater portion of the interface for thick coatings and only concentrated within a narrow band for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings.

7.4 Stress Distributions in Multi-Coated Substrates

7.4.1 Effect of thickness ratio on maximum tangential stress, $\sigma_{x(\max)}$, for multi-coated substrates

The same approach as that used for double-coated tools can be used for multi-coated systems. With multi-coatings, since each WC-XXX-YYY-ZZZ system has an identical substrate, the stress is now related to the stiffness of the coating next to the substrate and also the stiffness of the top, middle and bottom coatings. Figures 7.4.1a shows the effect of the thickness ratio $[t/a]$ on multi-coatings of increasing thicknesses on a WC substrate. All values are taken at the centre of contact line at $x=0$ where σ_x is a maximum. The dashed line represents the stresses within an uncoated WC-Co substrate.

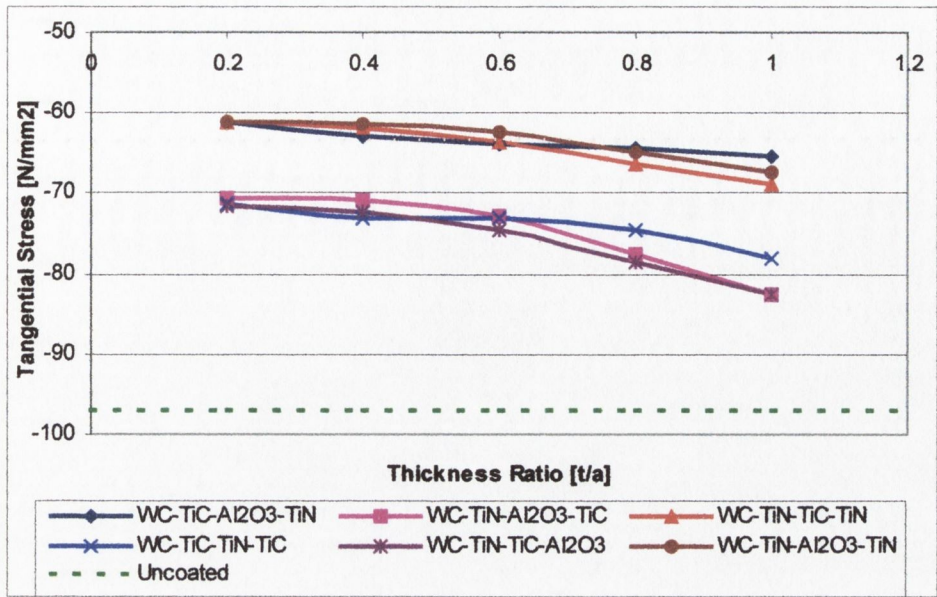


Figure 7.4.1a. Effect of Thickness ratio for multi-coated WC substrates.

Again, as the stiffness of the coatings decrease relative to the substrate, the stresses within the tool also decrease. That is, WC-TiC-Al₂O₃-TiN will have the lowest maximum tangential stress due to the decreasing modulus. In all cases, the maximum tangential stress within any coating/substrate systems with a top coating of TiN can be seen to increase as the coating thicknesses increase.

7.4.2 Variations of σ_x through multi-coated substrates ([t/a]=0.2)

The complex interaction between the various coatings and substrate materials makes analysis of the stress distribution changes difficult due to the three interfaces in a multi-coated system. It can be seen that if TiN is used as a middle layer, a compressive stress distribution takes place for both substrates. The change in stress from the bottom layer to the substrate is also influenced strongly by the substrate stiffness. In the case of the WC substrate, the discontinuities were such as to cause an increase in the compressive tangential stress in the substrate at the interface as shown by the downward movement of all curves from the green dashed uncoated curve in Figure 7.4.2a.

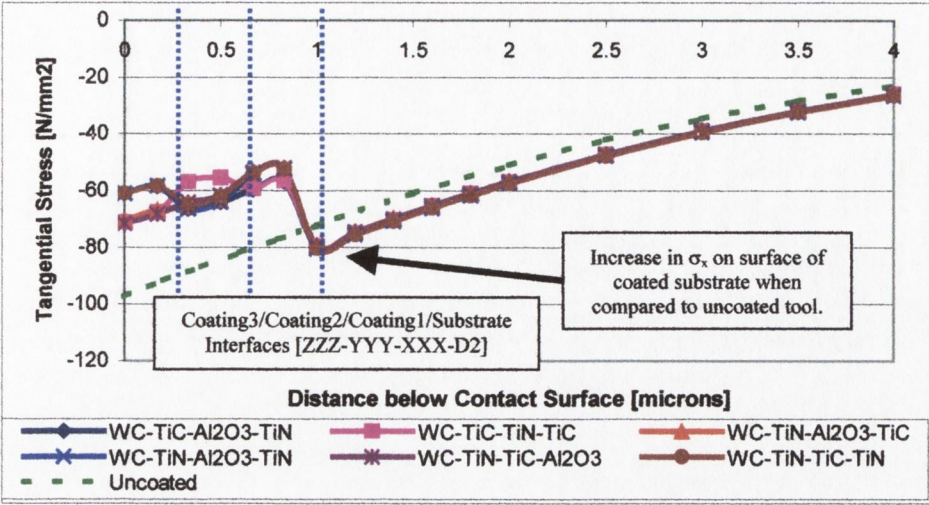


Figure 7.4.2a Variations of σ_x through multi-coated WC substrates ($[t/a]=0.2$)

System (WC-XXX-YYY-ZZZ)	First Discontinuity (ZZZ-YYY)	Second Discontinuity (YYY-XXX)	Third Discontinuity (XXX-WC)
WC-TiC-Al ₂ O ₃ -TiN	-7.98 N/mm ²	4.68 N/mm ²	-23.26 N/mm ²
WC-TiC-TiN-TiC	10.76 N/mm ²	-3.94 N/mm ²	-23.21 N/mm ²
WC-TiN-Al ₂ O ₃ -TiC	1.18 N/mm ²	10.02 N/mm ²	-27.48 N/mm ²
WC-TiN-Al ₂ O ₃ -TiN	-7.95 N/mm ²	10.27 N/mm ²	-28.00 N/mm ²
WC-TiN-TiC-Al ₂ O ₃	4.01 N/mm ²	8.15 N/mm ²	-27.51 N/mm ²
WC-TiN-TiC-TiN	-6.49 N/mm ²	8.50 N/mm ²	-28.08 N/mm ²

Table 7.4.2a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.2$)

7.4.3 Variations of σ_x through multi-coated substrates ($[t/a]=0.8$)

The effect of increasing the thickness of the multi-coatings on the WC substrate has the result of decreasing all discontinuities at the first, second and third interfaces. The added advantage is that the tangential stress at the top of the *substrate* with the thick multi-coatings decreases by 52% compared to that with the thin multi-coatings (Thin = -75.17 N/mm² & Thick = -35.78 N/mm²). For the thick multi-coated tools, the discontinuities were such as to cause an increase in the compressive tangential stress in the substrate at the interface as shown by the downward movement of all curves from the green dashed uncoated curve in Figure 7.4.3a.

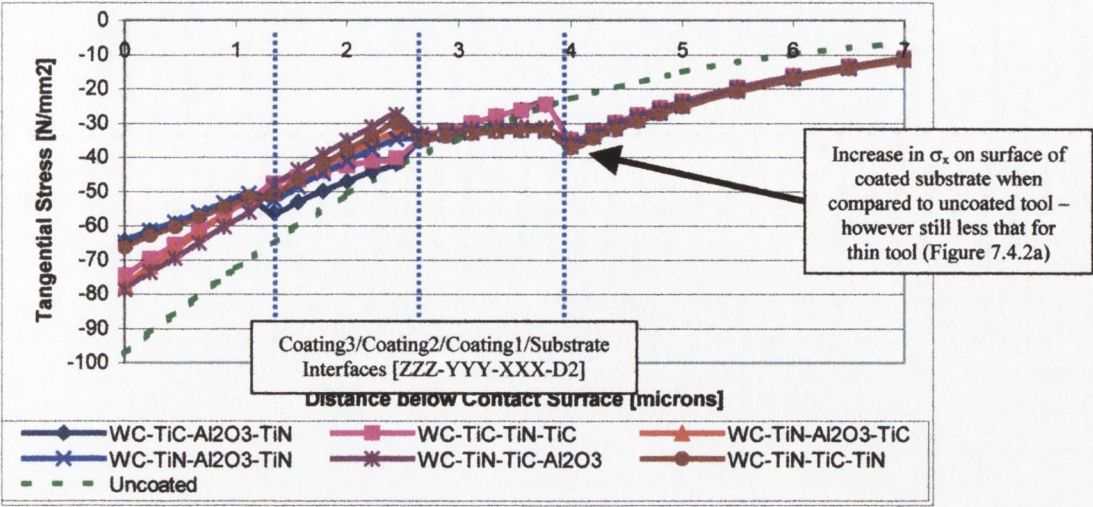


Figure 7.4.3a. Variations of σ_x through multi-coated WC substrates ($[t/a]=0.8$)

System (WC-XXX-YYY-ZZZ)	First Discontinuity (ZZZ-YYY)	Second Discontinuity (YYY-XXX)	Third Discontinuity (XXX-WC)
WC-TiC-Al ₂ O ₃ -TiN	-3.76 N/mm ²	2.52 N/mm ²	-10.18 N/mm ²
WC-TiC-TiN-TiC	4.86 N/mm ²	2.55 N/mm ²	-10.19 N/mm ²
WC-TiN-Al ₂ O ₃ -TiC	1.20 N/mm ²	0.88 N/mm ²	-3.81 N/mm ²
WC-TiN-Al ₂ O ₃ -TiN	-1.83 N/mm ²	0.97 N/mm ²	-4.56 N/mm ²
WC-TiN-TiC-Al ₂ O ₃	7.72 N/mm ²	0.89 N/mm ²	-3.99 N/mm ²
WC-TiN-TiC-TiN	0.66 N/mm ²	1.00 N/mm ²	-4.88 N/mm ²

Table 7.4.3a. Stress Discontinuity between coating and substrate interfaces ($[t/a]=0.8$)

7.4.4 Variation of σ_x along interfaces of multi-coated substrates

Figure 7.4.4a shows the magnitude of the tangential stress on the various interfaces of the WC-TiC-Al₂O₃-TiN tool, all decreasing, with increased distance from the centre of contact of the load. Only results from a WC-TiC-Al₂O₃-TiN multi-coated system are presented here. The rest of the multi-coated systems are included in Appendix 3. Again the contact half-width for all models is 5 μ m. The maximum tangential stress is seen to at the coating/substrate interface along the centre of contact line where $x=0$. This maximum compressive stress drops off rapidly along lines 2, 3 and 4 due to the favourable modulus ratio. At approximately 12 μ m on the thin multi-coated WC substrate, the magnitude of the stress changes from compression to tension, for all interfaces, and trails off to zero at a

distance of about $27\mu\text{m}$ from the centre of contact. These tensile stresses can cause fracture to occur on the coating surface which leads to tool failure however compressive stress are by far the more dominant in both models.

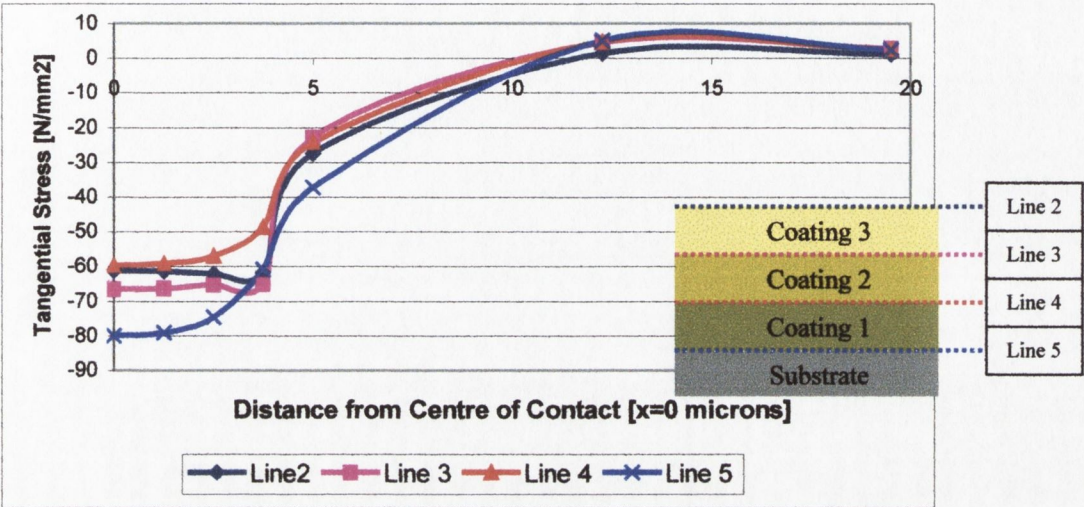


Figure 7.4.4a. Tangential Stress along interfaces of WC-TiC-Al₂O₃-TiN ([t/a]=0.2)

Figure 7.4.4b shows that increasing the thickness of the coating does indeed have a dramatic effect on the tangential interfacial stresses. As the thickness ratio is increased from $[t/a]$ of 0.2 to 0.8, the magnitude of the tangential stress decreases. The maximum tangential stress for all tools occurs at a distance of $4.5\mu\text{m}$ from the centre of contact so a $[x/a]$ ratio of 0.9 is obtained (as a is $5\mu\text{m}$ for all models). This value is maintained for all single-coated models, with both WC and D2 substrates, for both thin and thick coatings.

The location of this maximum stress is of importance. As previously explained in Section 5.3, the width and height of the model has to be 30 times greater than the contact half-width, a . As the Hertzian pressure exerts itself on the half-width, the tangential stresses increase along the top of the coating. As the coating is elastic, its resistance to deformation exerts an opposite tangential force on the force wanting to deform it. The combination of both these forces, acting on the same point in opposite directions, will result with a maximum stress occurring at a $[x/a]$ ratio of 0.9 for all multi-coated models.

Due to the thick coating, the tangential stress decreases however the magnitude of the tangential stress of the surface of the multi-coated tool remains roughly the same. Again, at approximately $12\mu\text{m}$ on the thick double-coated WC substrate, the magnitude of

the stress changes from compression to tension, only for two of the interfaces. Due to the thickness of the coatings and the distance below the surface, the stress at the coating_{middle}/coating_{bottom} and coating_{bottom}/substrate interface remains compressive throughout the length of the interface.

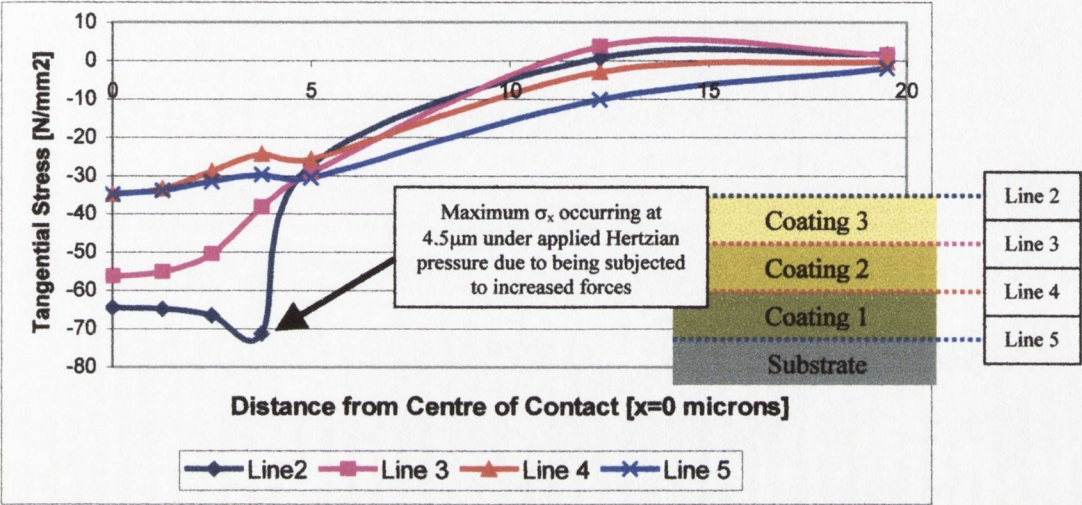


Figure 7.4.4b. Tangential Stress along interfaces of WC-TiC-Al₂O₃-TiN ([t/a]=0.8)

7.4.5 Effect of thickness ratio on maximum shear stress, $\tau_{xy(max)}$, for multi-coated substrates

The same approach used for double-coated tools is used for multi-coated systems. With multi-coatings, since each WC-XXX-YYY-ZZZ system has an identical substrate, the stress is now related to the stiffness of the coating next to the substrate and also the relative stiffnesses of the top, middle and bottom coatings. Figure 7.4.5 shows the effect of the magnitude of the shear stress on multi-coatings with an increasing thickness ratio $[t/a]$. Again, if the stiffness of the coatings decrease so $WC > XXX > YYY > ZZZ$, then a decrease in the maximum shear stress occurs for an increasing thickness ratio. The tool with the highest stiffness ratio is the WC-TiC-Al₂O₃-TiN so therefore it has a corresponding lower interfacial shear stress. Again, any tool having a bottom coating of TiC exhibits a decrease in the maximum shear stress for an increasing thickness ratio. By comparison, if TiN forms this inner coat, there is an increase in $\tau_{xy(max)}$ as $[t/a]$ increases.

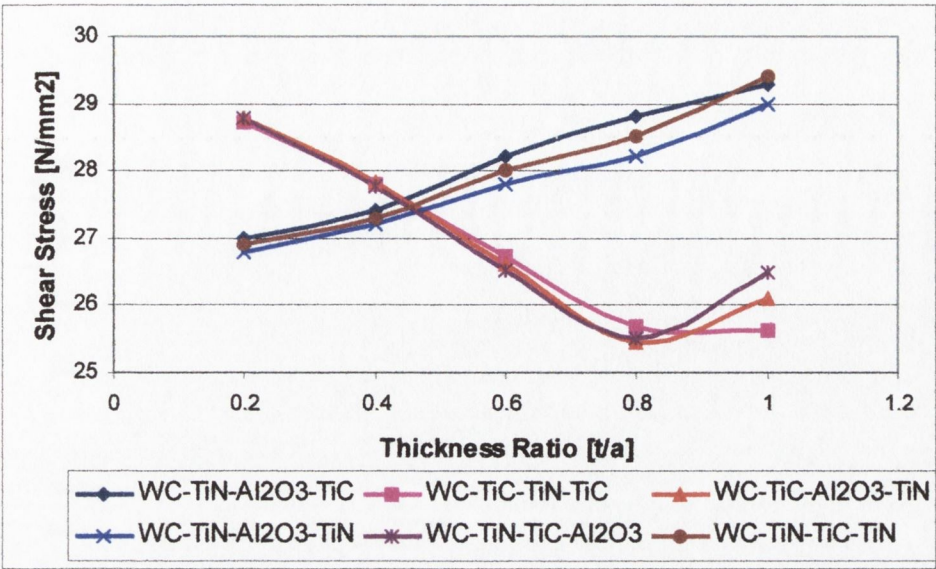


Figure 7.4.5. Effect of Thickness ratio for multi-coated substrates

7.4.6 Variation of τ_{xy} along multi-coated substrate interfaces

Figure 7.4.6a shows the variation of the shear stresses along the top of the multi-coated (WC-TiC-Al₂O₃-TiN) substrate (line 2), the coating_{top}/coating_{middle} interface (line 3), the coating_{middle}/coating_{bottom} interface (line 4) and the coating_{bottom}/substrate interface (line 5) from the centre of contact ($x=0$) for both thin and thick coatings ($[t/a]=0.2, 0.8$ respectively). The solid lines on the graph indicate the thin coatings whilst the dashed represent the thick coatings.

Again, as seen in Figure 7.4.6a, the maximum shear stress takes place a distance $4.5\mu\text{m}$ from the centre of contact which gives a $[x/a]$ ratio of 0.9. Again the interface with the greatest shear stress is that of the coating_{bottom}/substrate (line 5). The stress along the top of the multi-coated substrate (line 2) is somewhat similar to that seen for the single- and double-coated substrates. Again the shear stresses increase for thicker multi-coatings on the coating_{bottom}/substrate interface unlike the corresponding tangential stresses along the same interface which have been shown to decrease with increasing thickness. The shear stress is generally high over a greater portion of the interface for thick coatings and only concentrated within a narrow band for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings.

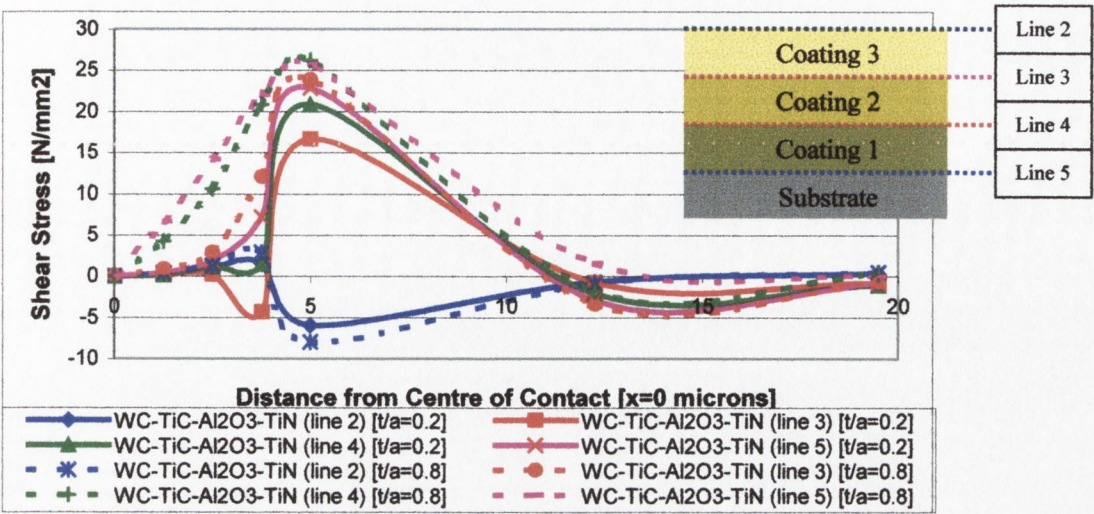


Figure 7.4.6a. Variation of τ_{xy} along double-coated WC-TiC-Al₂O₃-TiN interfaces

7.4.7 Summary of Stresses within Multi-Coated Tools

As with the double-coated tools, as the stiffness of the coatings decrease relative to the substrate, the maximum tangential stresses within the tool also decrease. That is, WC-TiC-Al₂O₃-TiN will have the lowest maximum tangential stress due to the decreasing modulus. In all cases, the maximum tangential stress within any coating/substrate systems with a top coating of TiN can be seen to increase as the coating thicknesses increase.

The effect of increasing the thickness of the multi-coatings on the WC substrate has the result of decreasing all the tangential stress discontinuities at the first, second and third interfaces. Another advantage is that the stress in the substrate below the interface with the thick multi-coatings decreases by 52% compared to that with the thin multi-coatings (Thin = -75.17 N/mm² & Thick = -35.78 N/mm²). For the thick multi-coated tools, the discontinuities were such as to cause an increase in the compressive tangential stress in the substrate at the interface.

The shear stresses increase for thicker multi-coatings on the coating_{bottom}/substrate interface unlike the corresponding tangential stresses along the same interface which have been shown to decrease with increasing thickness. The shear stress is generally high over a greater portion of the interface for thick coatings and only concentrated within a narrow band for thin coatings. This spread of greater values of the in-plane shear stresses along the interface of a thick coating can lead to a greater probability of causing local delamination with thicker coatings.

8.0 FE Modelling of Orthogonal Metal Cutting

8.1 FE Model Verification with Experimental Data

Before FEA modelling of the orthogonal metal cutting process was performed, verification of the FEA modelling technique was required. This was achieved by comparing the output parameters from FORGE2 with those obtained from the experimental work for both a coated and uncoated insert. The experimental tests involved orthogonal machining performed on a precipitation hardened Inconel 718 billet as outlined in Section 5.4. The cutting force components from this experimental test were measured and compared to the force components from the FEA model. The two models presented for comparative purposes are “best case” models where the results compare very favourably. Figure 8.1a illustrates the variation in cutting forces obtained from machining Inconel 718 with an uncoated H13A tool while Figure 8.1b illustrates the variation using a coated GC1015 tool. In both cases the results were averaged after steady state machining took place, which was after about 3 seconds in both diagrams.

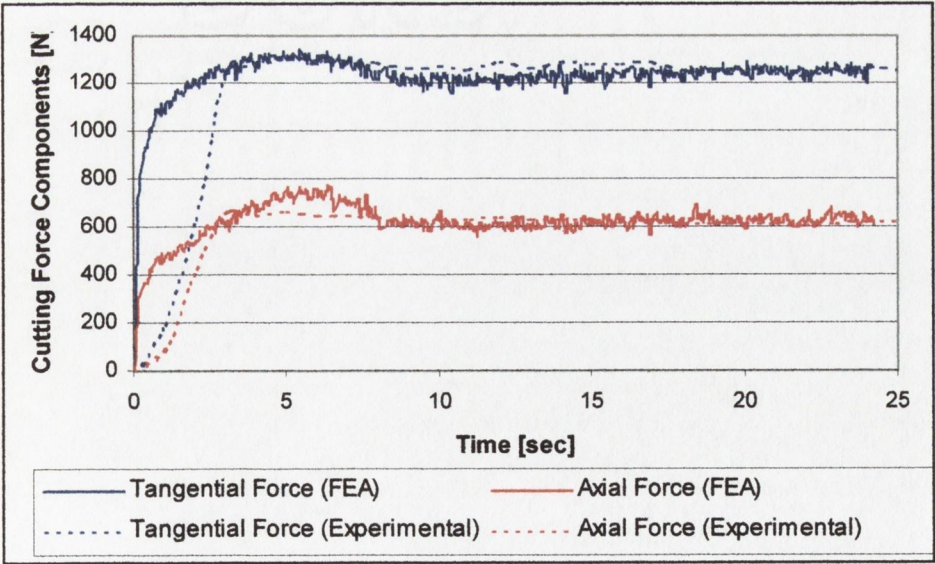


Figure 8.1a. Cutting Forces obtained from experimental work and FEA when machining Inconel 718 at 20m/min with an uncoated Sandvik H13A cutting tool.

The experimental cutting forces from the above figure were already presented in Figure 6.2a. The average tangential cutting force measured from the experimental tests was 1271N and that predicted by FORGE2 was 1229N for the same cutting conditions, giving a difference of 3.5% for the uncoated tool. The average axial force component from the experimental work was found to be 634N and the average force from the FORGE2 analysis was predicted to be 620N again giving a difference of 2.5%. These differences are quite acceptable given the variation in both the experimental and FORGE2 force measurements.

The above simulation was re-run with a single-coated TiN tool (GC1015). As shown in Figure 8.1b, the average tangential cutting force measured from the experimental tests was 1249N and that predicted by FORGE2 was 1205N for the same cutting conditions, giving a difference of 4% for the single-coated tool. The axial force component from the experimental work was averaged to be 624N and the force from the FORGE2 analysis was again averaged to be 598N again giving a difference of 4.5%.

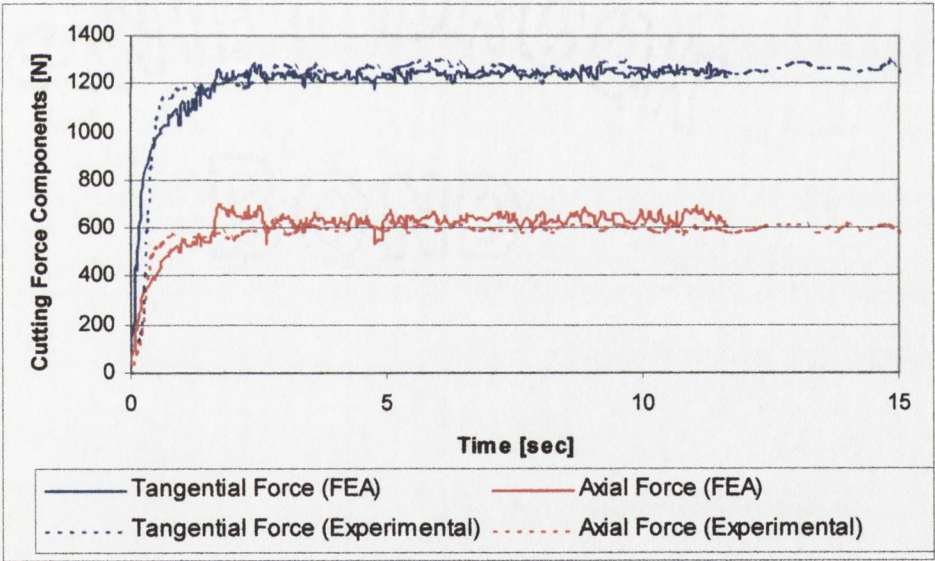


Figure 8.1b. Cutting Forces obtained from experimental work and FEA when machining Inconel 718 at 20m/min with a coated Sandvik GC1015 cutting tool.

Both the above comparative studies show that FE can indeed be used to simulate orthogonal machining operations using coated and uncoated carbide cutting tools .

8.2 FORGE2 Meshing difficulties during Orthogonal Machining

The main difficulty encountered during the finite element modelling of the orthogonal machining process was having element penetration occurring on the nose radius of the cutting tool or on the entrance/exit of the chipbreaker. Figure 8.2a and 8.2b illustrates this problem with both a rigid and deformable cutting tool.

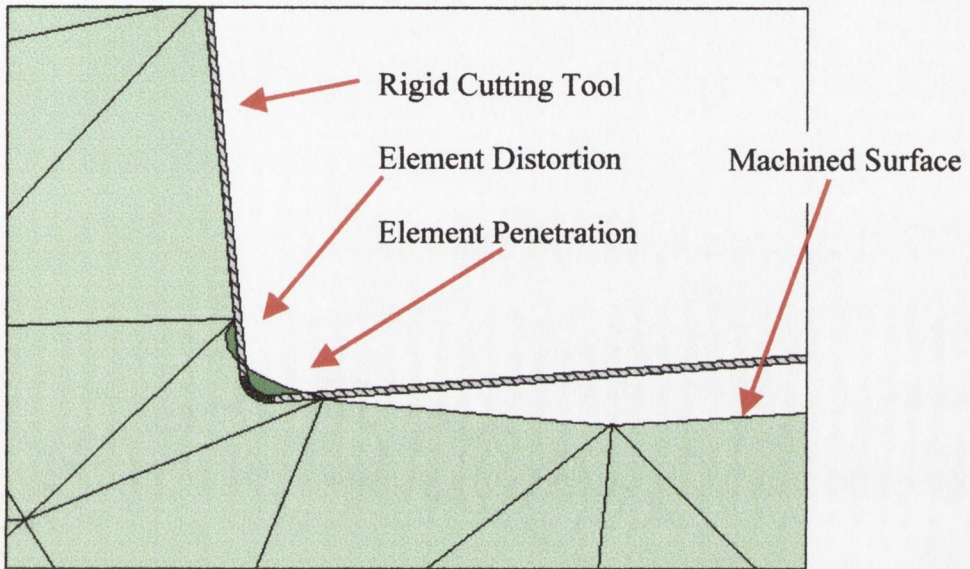


Figure 8.2a. Element Penetration of Rigid Cutting Tool.

For the finite element simulation to calculate the stiffness matrix, and hence solve for all nodal unknowns, it must be able to calculate the nodal spatial gradients. These gradients describe how the nodal displacements vary across any element. The nodal displacements are mapped onto a “parent shape element” using a *Jacobian* matrix to calculate the spatial gradients. In order that the *Jacobian* matrix can be constructed, the element nodes must run counter-clockwise. Once the workpiece penetrates the tool, any excessive element distortion creates a negative *Jacobian* matrix [J] which causes the simulation to crash.

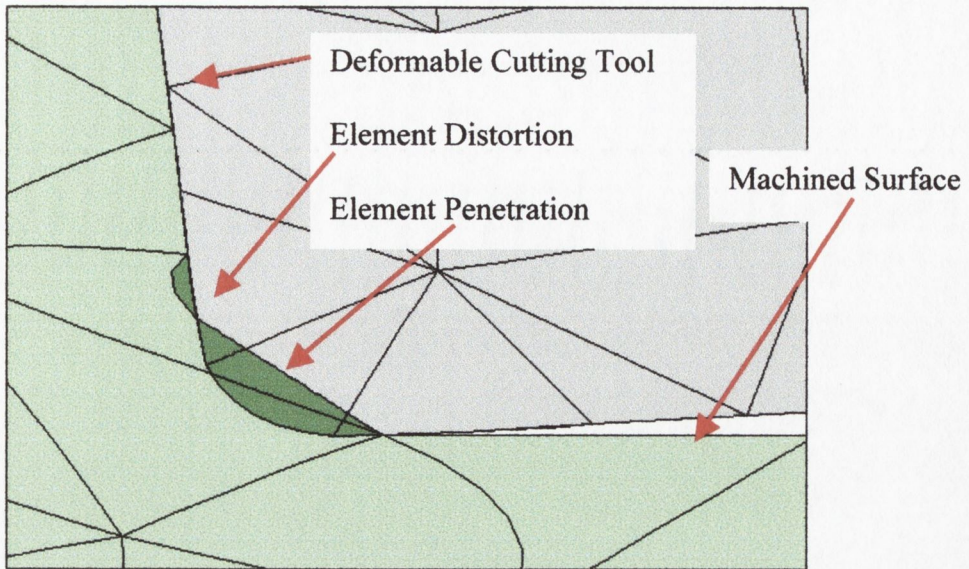


Figure 8.2b. Element Penetration of Deformable Cutting Tool.

In order to overcome the problem of negative *Jacobian* matrices, the procedures listed below had to be implemented with all FORGE2 models.

1. A corner fillet radius of $5\mu\text{m}$ was placed on the cutting edge of the tool as in practice this edge is not perfectly sharp. The effect of this change to the corner edge reduced element penetration from the workpiece into the cutting tool.
2. In the FORGE2 package, the *precision* of each new mesh created after the workpiece remeshed was reduced from 0.04% (default) to 0.001%. The *precision* is a keyword which defines the difference in volume between the over-discretised boundary and the new mesh boundary. So by reducing the *precision*, the remeshing process allowed nodes to be placed in corners and around the nose radius of the tool so as to minimise penetration.
3. The remeshing procedure is triggered by various factors, among them is the “rate of penetration”. The global penetration rate (*taux de penetration global*), was reduced from 0.05 (default) to 0.001 and the local penetration rate (*taux de penetration local*), was reduced from 0.01 to 0.0001. This initiated remeshing at nearly every increment and ensured that penetration was kept to a minimum. These penetration rates are calculated by the volume percentage of an element from the workpiece passing into the cutting tool.

4. The curvature of the elements (*courbure*) was reduced from 0.25 to 0.1 so as to prevent the element side from spreading around the edge of the cutting tool. The curvature is defined as the product of the length and height of the element.
5. A floating mesh box (*zone*) was placed around the nose radius of the cutting tool in order to ensure that all elements inside the box had sides of 0.05 units (Figure 8.2c). This ensured that the size of elements are minimised around the nose radius, where the chances of element penetration are greatest.
6. The refinement rate (*taux de raffinement*) of the mesh around the remainder of the workpiece (outside the mesh box), was reduced from 1.3 to 1.0.

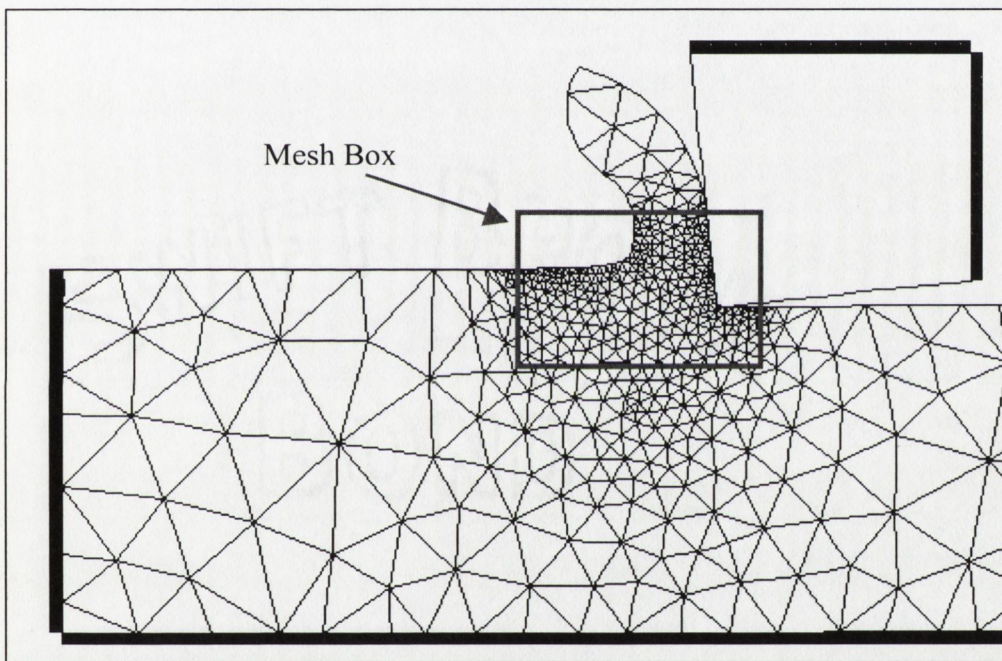


Figure 8.2c. Mesh Box location.

8.3 Variation in Cutting Tool Angle

8.3.1 Rigid Tools without chipbreaker

The cutting tool rake angle was varied from -6° to 6° in order to establish the effect of rake angle when machining Inconel 718. Figure 8.3.1a illustrates the effect of material flow with a change of rake on rigid tools. As can be seen, the amount of material

deformation and flow for a negative rake tool is much larger than for a positive rake tool and so consequently the stresses and temperatures are expected to be higher also.

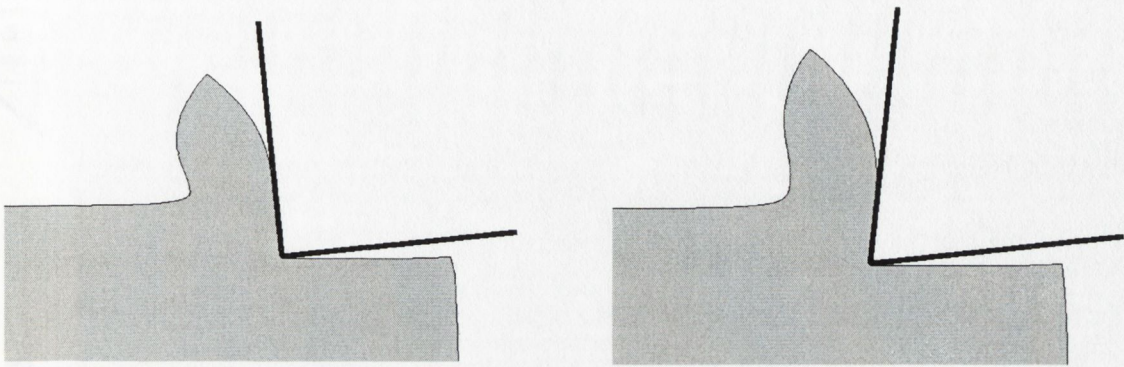


Figure 8.3.1a. Effect of Rake angle on Material Flow.

Measuring the amount of abrasive wear on the rake face of a tool is important in any metal cutting simulation. FORGE2 allows measurement of this variable with the use of a wear vector. The length of the vector denotes the amount of wear on the tool and as previously outlined in Section 5.2.5, is dependent on the normal stress and tangential sliding velocity. As can be seen schematically in Figure 8.3.1b, and graphically in Figure 8.3.1c, the abrasive wear on the negative rake tool is greater than that of the positive rake tool in both its magnitude and also in its distance along the rake face. This is due to the increased normal stress on the rake face of negative rake tools compared to that of positive rake cutting tools, and also because the chip remains longer in contact with the negative rake tool than for the positive one. For both tools, the wear function has a parabolic distribution over the rake face.

The wear is a maximum on the rake face just past the nose radius where the normal stress is expected to be a maximum, which is illustrated by the single vector on the cutting nose. This is due to the combined effects of friction and deformation associated with the primary shear zone. However the wear drops off just after this local maximum due to a “dead metal” zone where the material has effectively stopped flowing. This region is commonly referred to as a stagnation point. The effect of this dead metal zone was to reduce wear locally due to a decrease in the material velocity. However these low material velocities, combined with high normal stresses promotes the formation of a built-up-edge (BUE).

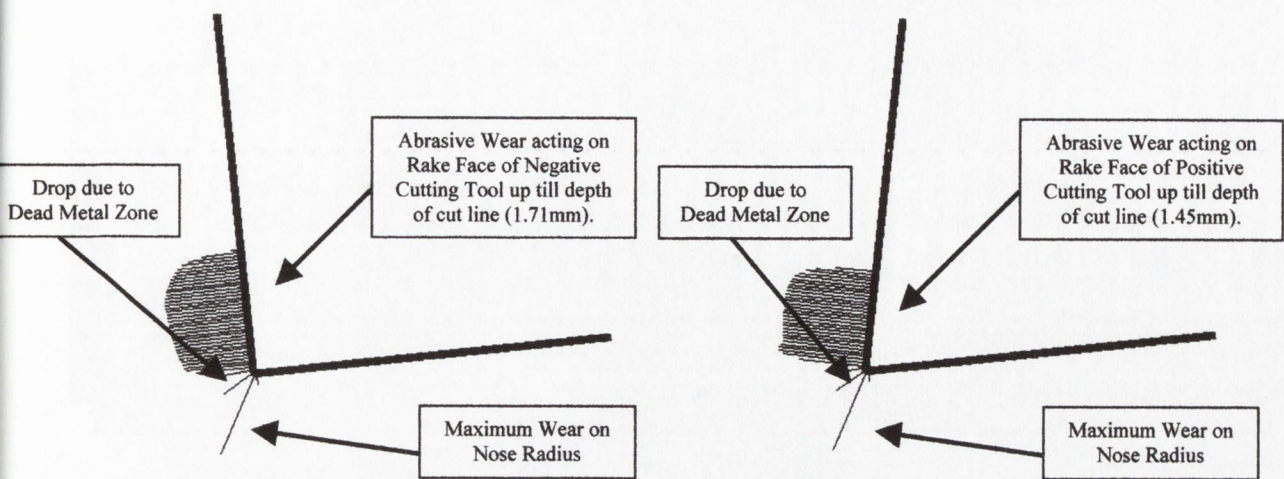


Figure 8.3.1b. Wear Vectors on Negative and Positive Rigid Tools.

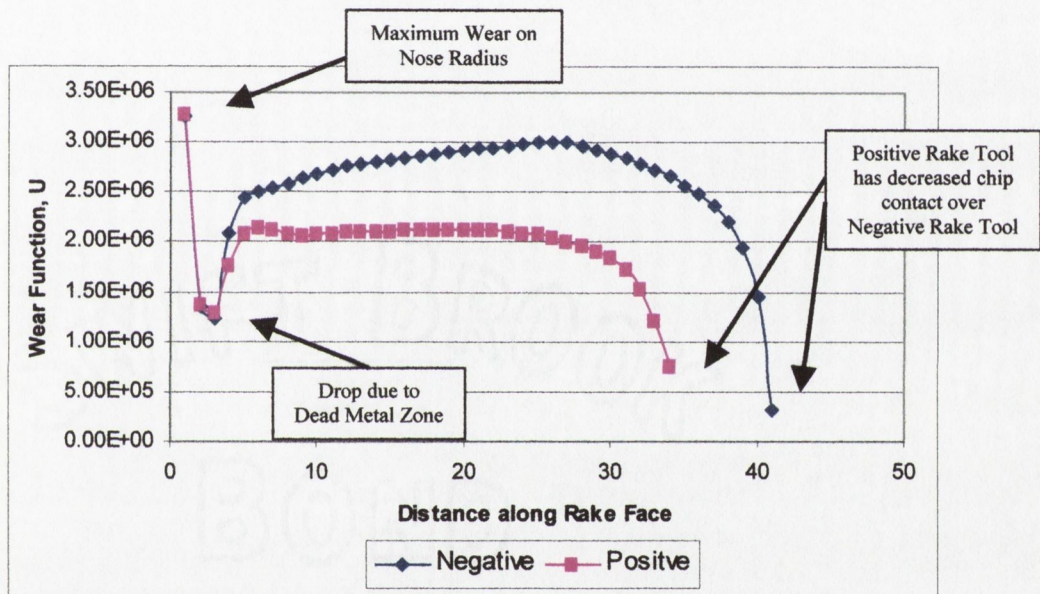


Figure 8.3.1c. Variation in Wear Function along rake face for Rigid Tools.

The increased chip/tool contact length associated with the negative rake cutting tool can be detrimental to tool life since the temperatures generated in the shear zone, and passed away in the chip, can only affect the cutting tool while the chip is in contact with it. Table 8.3.1 lists the differences in output parameters for the two cutting tool geometries. The fourth column in the table illustrates the percentage difference in the output parameter. If the value is negative it shows that the parameter for the negative rake tool is higher in magnitude. It can be seen that the amount of material deformation was considerably larger for the negative rake tool.

	Negative	Positive	% Difference
Chip/Tool Contact Length [mm]	1.709	1.45	-17.9
Tangential Cutting Force, Ft [N]	185.4	153.6	-20.7
Max. Effective Stress [MPa]	1281.4	1247.8	-2.7
Max. Effective Strain	3.9247	2.9597	-32.6
Max. Strain Rate	5742.9	7067.9	18.7
Max. Temperature [°C]	416.89	362.83	-14.9

Table 8.3.1. Machining output parameters for negative and positive rake rigid tools.

The maximum effective strain was greater for the negative rake tool than from the positive rake tool as a result of the increased amount of material deformation. This caused the temperatures generated due to viscoplastic deformation to be correspondingly greater for the negative rake tool.

8.3.2 Rigid Tools with –QM type chipbreaker

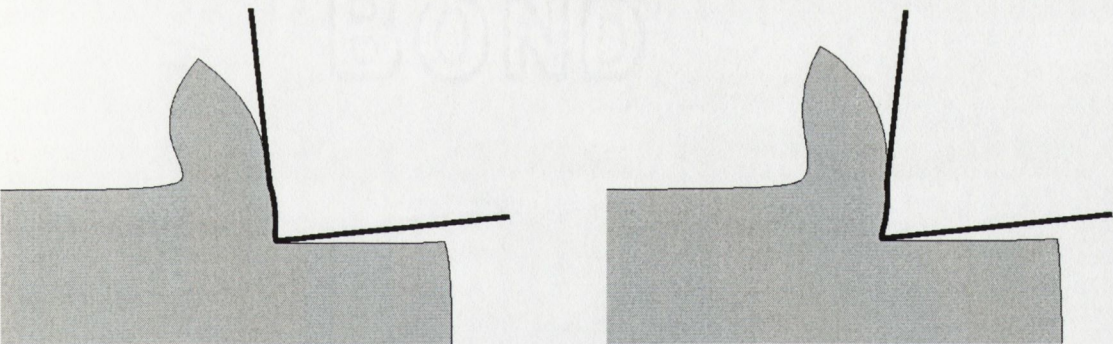


Figure 8.3.2a. Effect of Rake angle with included chipbreaker on Material Flow.

A Sandvik –QM type tool, on which a chipbreaker was present, replaced the flat rake face rigid tools. The effect of the chipbreaker was to curl the chip which caused it to leave the rake face quicker as shown in Figure 8.3.2a. This in turn lowered the length that the abrasive wear acted along the upper rake face due to the decreased chip/tool contact however the magnitude of this wear increased. As can be seen in both Figure 8.3.1b and 8.3.2b, the negative rake tool sustains the most abrasive wear both in magnitude and in terms of wear distance along the rake face due to the chip being in contact with the rake face for longer. However with the addition of a chipbreaker, this wear decreases over a lot

of the rake face with only wear at the entrance, middle and exit of the chipbreaker being high. Again, the wear just past the nose radius is a maximum due to high normal stresses and temperatures from the primary shear zone.

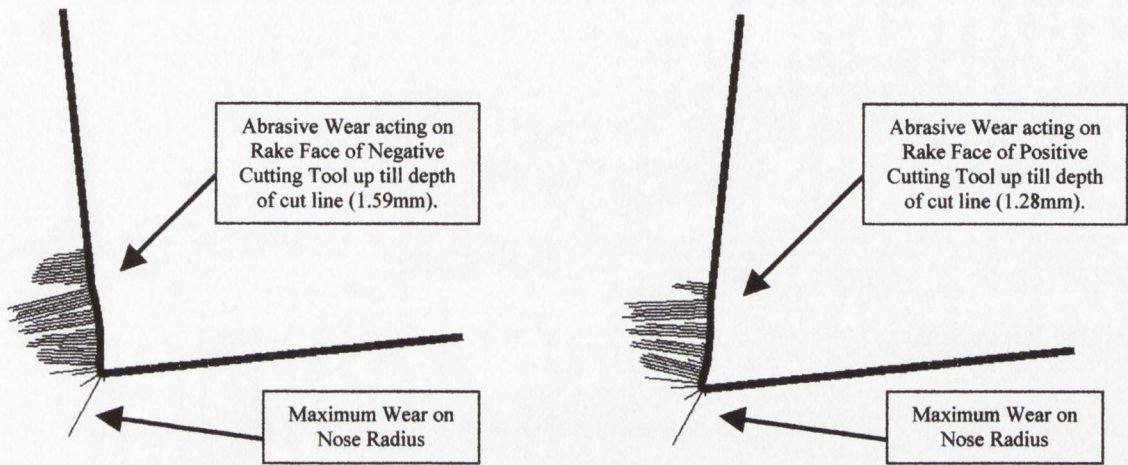


Figure 8.3.2b. Wear Vectors on Negative and Positive Rigid Tools with chipbreakers.

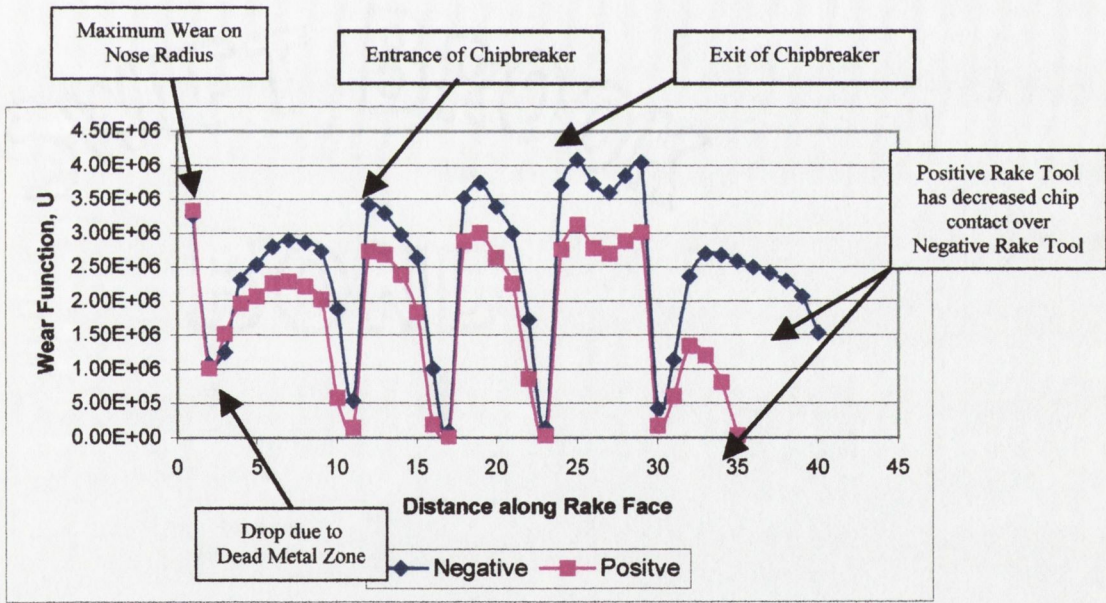


Figure 8.3.2c. Variation in Wear Function along rake face for Rigid Tools with chipbreakers.

Table 8.3.2 lists the differences in output parameters for the two cutting tool geometries. The fourth column in the table illustrates the percentage difference in the output parameter. For the case of the tools with chipbreakers, because of increased material flow due to chip curling, the maximum effective strain increases for both the negative and positive tools when compared to tools with flat rake faces (Table 8.3.1). However this

again caused the temperatures generated due to viscoplastic deformation to be greater for the negative rake tool.

	Negative	Positive	% Difference
Chip/Tool Contact Length [mm]	1.59	1.278	-24.4
Tangential Cutting Force, Ft [N]	187.2	159.6	-17.3
Max. Effective Stress [MPa]	1257.3	1305.2	3.7
Max. Effective Strain	4.1971	3.2719	-28.3
Max. Strain Rate	5412.9	3814.1	-41.9
Max. Temperature [°C]	410.8	367.4	-11.8

Table 8.3.2. Machining output parameters for negative and positive rake rigid tools.

8.3.3 Deformable Tools without chipbreaker

Figure 8.3.3 illustrates the effective stress contour plot for both a negative and positive rake deformable cutting tool. It can be seen that the location of the maximum effective stress is different for each cutting tool geometry. The location of the maximum effective stress for the positive rake cutting tool occurred just in front of the cutting tool nose radius (on rake face), whereas for the negative rake tool, the maximum effective stress occurs behind the tool nose radius (on flank face). However, even though the magnitude of this stress is much smaller for the positive rake compared to the negative rake tool, this smaller stress acts over a greater area of the nose of the cutting tool. As Inconel 718 is an abrasive material, the positive tool would not have the strength to machine it without the cutting tool nose failing. However the negative rake tool has a stronger cutting edge because the location of the maximum effective stress, during steady state machining, occurs behind the nose of the tool.

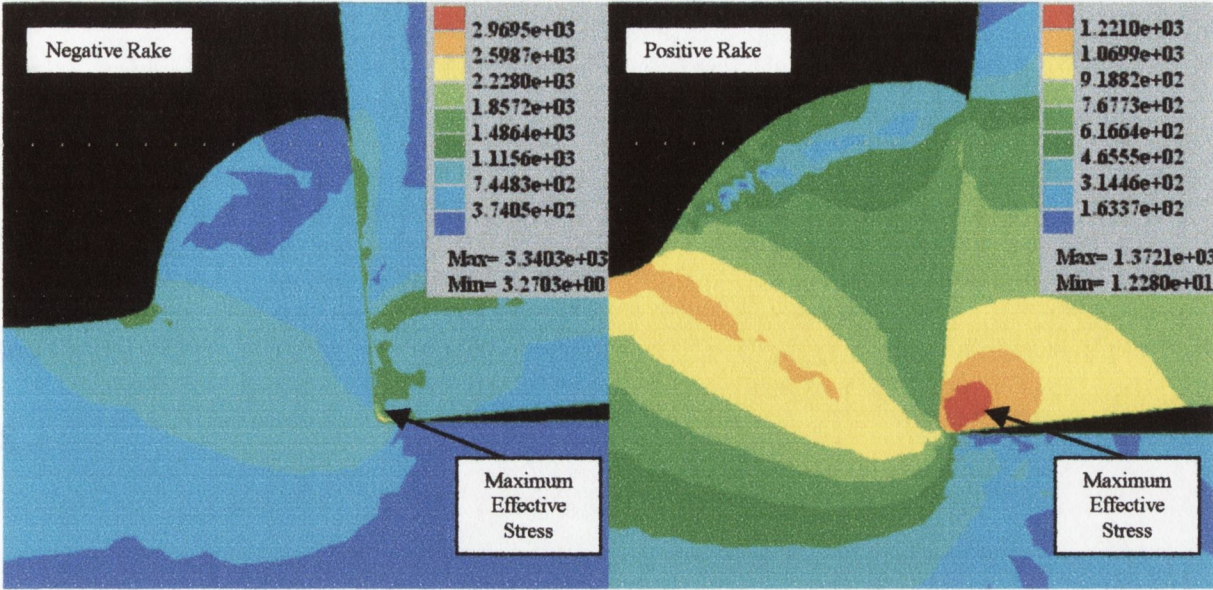


Figure 8.3.3a. Variation in effective stress distribution for negative and positive rake cutting tools.

Figure 8.3.3b illustrates the effect of the temperature distribution throughout the workpiece machined with negative and positive rake tools. It can be seen that the distribution of the maximum temperature is different for both tools. The maximum temperature for the negative rake tool occurs in a region in front of the cutting tool nose and up along the rake face and remains in this area over the length of the cut. The maximum temperature for the positive tool extends behind the nose region due to the sharpness of this tool.

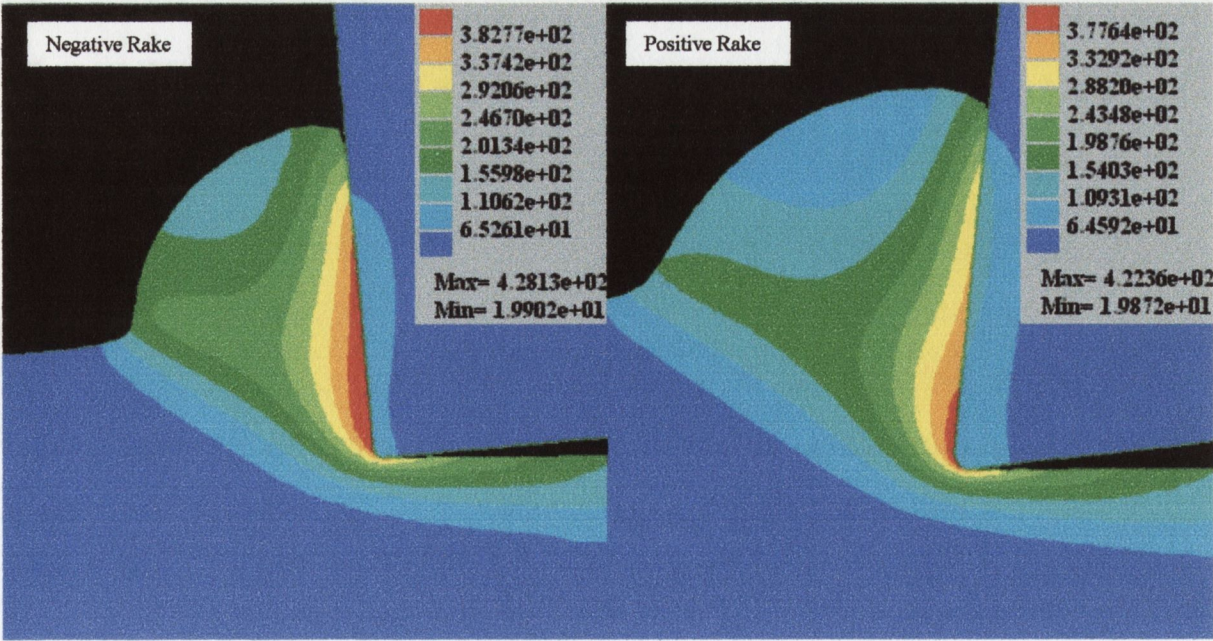


Figure 8.3.3b. Variation in temperature distribution for negative and positive rake cutting tools.

There is little difference in the maximum temperature generated in the workpiece for both the negative and positive rake simulations however the maximum temperature within the tools themselves differs. The temperature within the negative tool reaches about 110°C whilst the positive tool has a maximum temperature of 154°C. This increased temperature lowers the hot hardness of the tool, and combined with the location of the maximum effective stress at the nose of the tool, is one of the reasons why positive rake tools fail when machining Inconel 718.

8.4 Variation of machining parameters on Metal Cutting Simulations

During an orthogonal metal cutting operation, there are many parameters that can be changed to effectively change the outcome of the process. Amongst these are depth of cut, cutting speed, feed rate, lubricant and cutting edge conditions. Some of these parameters have been modelled to provide an indication of their influence on the machining parameters.

8.4.1 Effect of Depth of Cut increasing during Metal Cutting

Table 8.4.1 lists the variation in output parameters from FORGE2 when the depth of cut is changed from 0.8mm to 1.2mm and then to 1.6mm during an orthogonal metal cutting simulation. These values were taken to emulate the depths of cut used in the experimental tests. The tool was initially modelled as an uncoated tungsten carbide insert with a cutting speed of 20m/min.

	0.8mm	1.2mm	1.6mm
Chip/Tool Contact Length [mm]	1.87	2.3	2.83
Tangential Cutting Force, Ft [N]	1390	1731	2110.2
Max. Effective Stress [MPa]	3340.3	4140.6	4698.8
Max. Effective Strain	5.12	6.78	8.17
Max. Strain Rate	4.90E+04	3.47E+04	2.29E+04
Max. Temperature [°C]	502.3	551.8	617.8

Table 8.4.1. Machining output parameters with increasing depth of cut.

As can be observed from Table 8.4.1, all the output parameters, with the exception of strain rate, increase with an increase in the depth of cut. What is interesting to note is that the maximum temperature at the 1.6mm depth of cut increased to 617.8°C which would have decreased the hot hardness of the cutting tool and the wear rates would increase appropriately.

8.4.2 Effect of Cutting Speed increasing during Metal Cutting

Table 8.4.2 lists the variation in output parameters from FORGE2 when the cutting speed is changed from 20m/min to 40m/min and then to 60m/min during an orthogonal metal cutting simulation. These values were taken to simulate the conditions use in the experimental tests. The tool was initially modelled as an uncoated tungsten carbide insert with a depth of cut of 0.8mm.

	20m/min	40m/min	60m/min
Chip/Tool Contact Length [mm]	1.87	1.84	1.83
Tangential Cutting Force, Ft [N]	1390	1240.3	1168.6
Max. Effective Stress [MPa]	3340.3	3527.4	3778.9
Max. Effective Strain	5.12	6.32	7.37
Max. Strain Rate	4.90E+04	4.57E+04	4.14 E+04
Max. Temperature [°C]	502.3	587.4	656.0

Table 8.4.2. Machining output parameters with increasing cutting speed.

It can be observed from Table 8.4.2 that the maximum temperature in the workpiece increases considerably as the cutting speed is increased from 20m/min to 60m/min. In each case the maximum value of temperature occurs in localised zones of intense heat which would lower the tool life considerably as the hot hardness of the tool would decrease with large increases in temperatures. A parameter which decreases with increased cutting speeds is the tangential cutting force which has already been shown experimentally to decrease for uncoated deformable tools in Section 6.2. Again the maximum effective stress and effective strain both increase for increased cutting speeds.

8.4.3 Effect of Coolant during Metal Cutting

While some experimental tests were performed using a lubricant, the majority of the experimental work was performed without coolant. FEA simulations were performed in order to establish the effect of the lubricant/coolant on the machined surface and tool stress/strain behaviour. The effect of coolant on the exterior of the workpiece and tool can be simulated by increasing the convective heat transfer coefficient (FORGE2 keyword *alphan*) for all the boundary surfaces. Table 8.4.3 lists the variation in output parameters from FORGE2 when the heat transfer coefficient is changed from 1000W/mK to 2000W/mK and then to 3000W/mK during an orthogonal metal cutting simulation. The tool was initially modelled as an uncoated tungsten carbide insert with a depth of cut of 0.8mm and a cutting speed of 20m/min.

	1000W/mK	2000W/mK	3000W/mK
Chip/Tool Contact Length [mm]	1.87	1.86	1.86
Tangential Cutting Force, Ft [N]	1390	1390	1388
Max. Effective Stress [MPa]	3340.3	3325.2	3320.9
Max. Effective Strain	5.12	6.02	6.89
Max. Strain Rate	4.90E+04	4.77E+04	4.45 E+04
Max. Temperature [°C]	502.3	488.2	467.4

Table 8.4.3. Machining output parameters with increasing boundary head transfer coefficient.

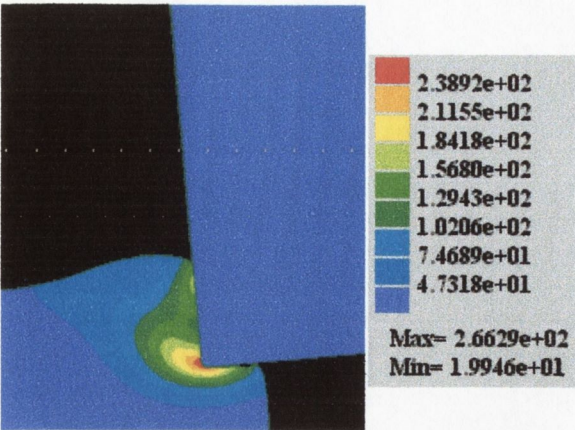
As can be seen from Table 8.4.3 the variation in machining output parameters, with increasing heat transfer coefficient, is minimal. The maximum cutting temperature on the rake face of the tool was expected to decrease however this drop in temperature was only 7% with $k = 3000\text{W/mK}$. When machining Inconel 718 dry, the workpiece material will have a higher shear flow strength and will therefore require more energy to shear the material. Consequently a decrease in the cutting forces would also be anticipated. However the simulations have not indicated any substantial decrease in stress or temperature distribution in the cutting zone. This compares favourably with experimental results presented in Section 6.4.3.

8.5 Temperature Distribution in workpiece over length of cut

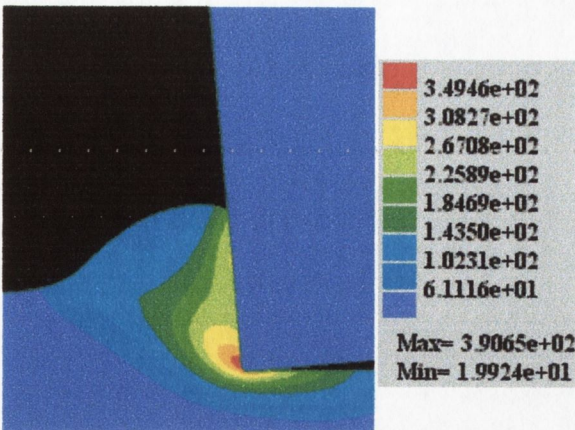
Figure 8.5a illustrates the progression of the temperature distribution within the Inconel 718 workpiece as it is machined with an uncoated tungsten carbide insert. As can be seen the maximum temperature always occurs around the nose radius for the start of the cut, and as the distance into the cut increases, the region of maximum temperature extends up the rake face of the tool. Figure 8.5b shows the increase in maximum temperature during the machining operation and once steady state machining is reached, remains constant at about 500°C.

However the temperature can be seen to be increasing in the tool due to prolonged contact with the hot chip. The increase in tool temperature, from 20°C at the start of the test to 179°C when steady state machining conditions have been reached can lead to the tool failure by lowering its deformation resistance. In each case the maximum value of temperature occurs in localised zones of intense heat which would lower the tool life considerably as the hot hardness of the tool would decrease with large increases in temperatures. These localised zones of intense heat also correspond to areas where the abrasive wear of the tool is high.

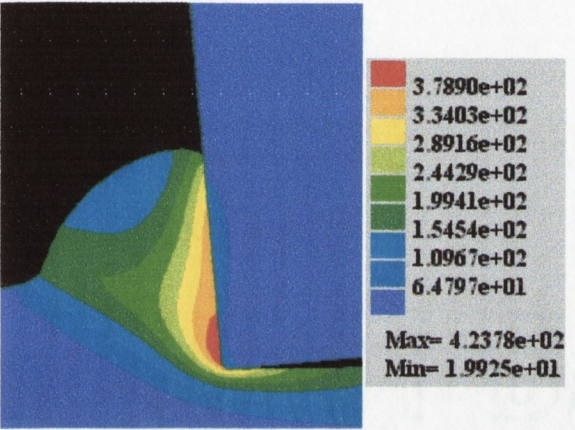
Figure 8.5a shows the temperature in the tool until steady state machining has been reached for the formation of just one chip. However if machining is continued and a second chip is formed, the initial tool temperature will be 179°C, and as machining takes place, the temperature in the tool will further increase. The results in the following sections present the stresses in the tool and workpiece up to the formation of this first chip.



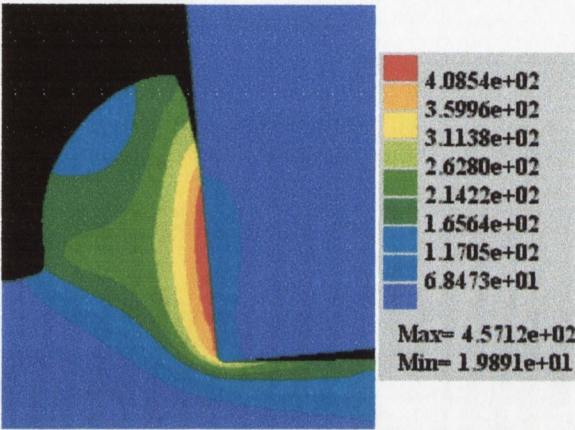
Cut Distance: 0.5mm



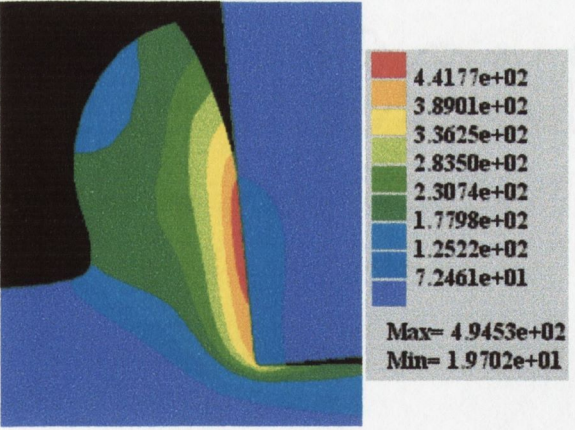
Cut Distance: 1.0mm



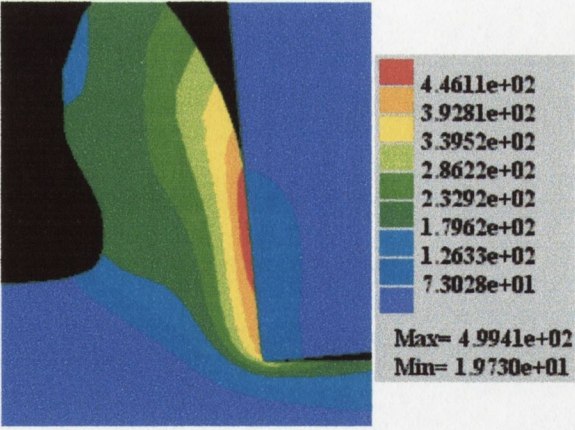
Cut Distance: 1.5mm



Cut Distance: 2.0mm



Cut Distance: 2.5mm



Cut Distance: 3.0mm

Figure 8.5a. Temperature Distribution in workpiece until steady state machining conditions have been reached.

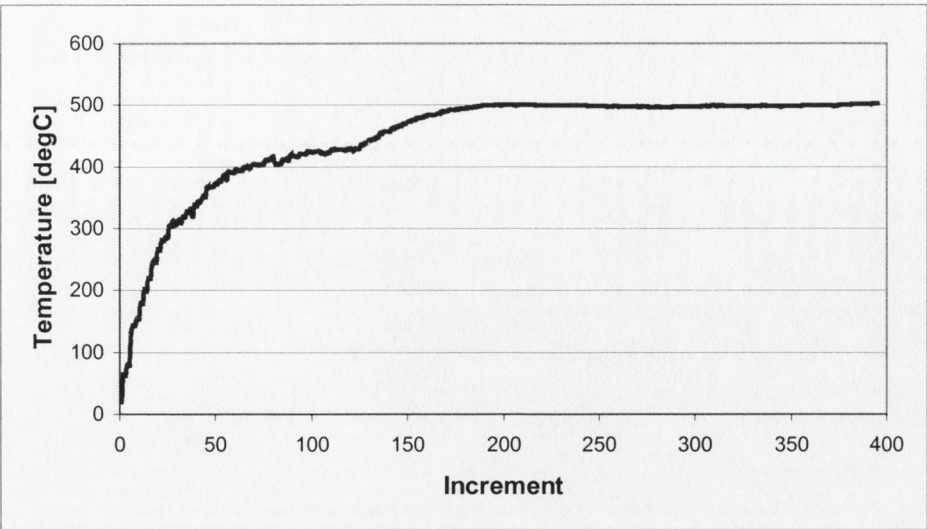


Figure 8.5b. Maximum Temperature in workpiece for Uncoated Cutting Tool.

8.6 Effect of single-coated cutting tools on machining parameters

Other research, outlined in Section 3.3, has presented results of FE simulations involving uncoated substrates, however little attention has been given in the past to the behaviour of ceramic multi-coatings. The results presented in this section refer to three different types of coatings (TiN, TiC, Al₂O₃) that were used in the FE simulations, in a series of combinations to determine the tool that would result in the lowest cutting temperature, with the lowest maximum effective stresses in the tool and the lowest effective strains in the workpiece. *To the author's knowledge, this is one of the first analyses to numerically examine the effect of machining parameters on coated deformable cutting tools.*

8.6.1 5µm single-coated WC substrates

FE simulations were performed using three different single-coated substrates. WC-TiN, WC-TiC and WC-Al₂O₃ were chosen to match tools used experimentally. In all cases the single-coated tools experimentally out-performed their uncoated competitors as was shown earlier in Section 6.2. In experimental tests, uncoated tools failed after 3 minutes compared to 10 minutes for single-coated tools. Table 8.6.1 lists the variation in the predicted output parameters from FORGE2 when the depth of cut was 0.8mm with a

cutting speed of 20m/min during an orthogonal metal cutting simulation. All the single-coated models had a coating thickness of 5µm.

	Uncoated	WC-TiN	WC-TiC	WC-Al ₂ O ₃
Chip/Tool Contact Length [mm]	1.45	1.45	1.47	1.45
Tangential Cutting Force, Ft [N]	1337.9	1291.3	1307.8	1275.0
Max. Effective Stress [MPa]	5881.6	4979.4	5362.8	5150.3
Max. Effective Strain	8.6559	8.6845	8.7268	8.7008
Max. Strain Rate	2228.3	4973.6	4769.1	4818.5
Max. Temperature [°C]	496.9	499.1	499.2	499.8
Max. Temperature in Tool [°C]	147.6	127.6	131.1	117.2

Table 8.6.1. Machining output parameters with different 5µm single-coated substrates

In all the simulations, the maximum effective stress was found to occur in the substrate near the corner of the nose radius. Figure 8.6.1a compares the WC-TiN coated tool with an uncoated WC substrate. The maximum stress in the tool decreases from 5881.6 to 4979.4 MPa due to the addition of this single coat of TiN. These high stresses, combined with high temperatures, result in the premature wear of a tool. In fact it can be seen from Table 8.6.1, that with the addition of a 5µm single-coating for any WC substrate, the temperature and effective stress in the tool decreases relative to an uncoated tool. It can also be seen from Table 8.6.1 that there is very little variation in the predicted cutting forces for any of the three single-coated cutting tools. Furthermore given that the variation in the magnitude of the output forces from FORGE2 simulations can be large, these variations are considered negligible.

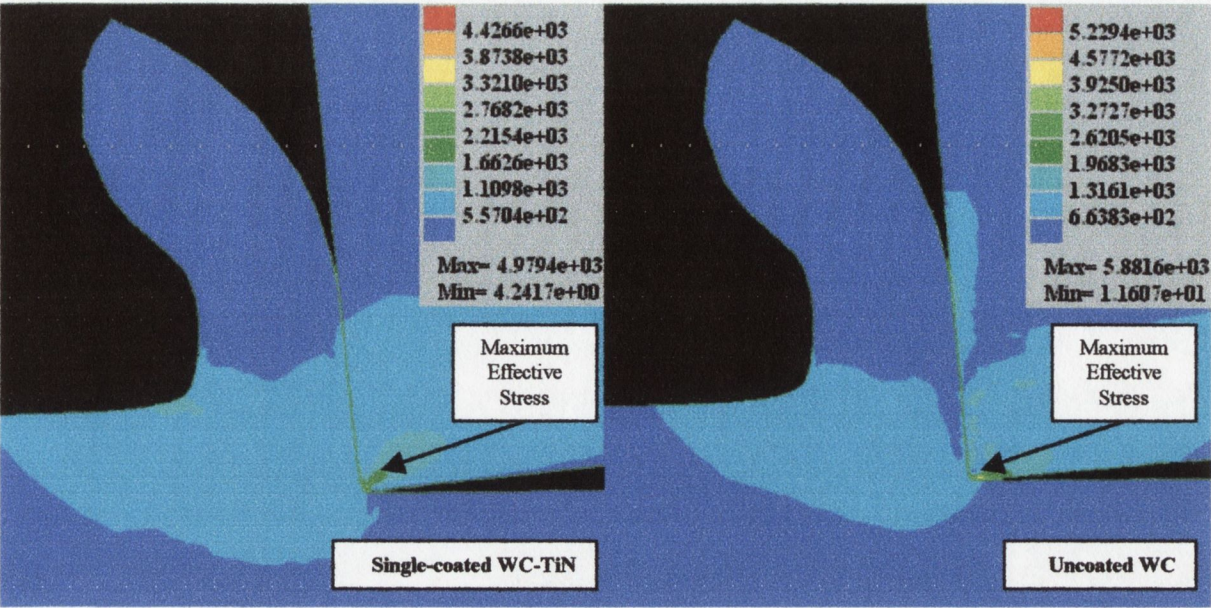


Figure 8.6.1a. Variation in effective stress for single-coated and uncoated tool.

The location of the maximum effective stress in the substrate, is important in influencing the life of a tool. Machining with an uncoated tool usually results in high stresses combined with high temperatures which weaken the tool by decreasing the hot hardness, and the stresses can cause plastic deformation or fracture of the cutting edge. With the addition of a single-coating of TiN, TiC or Al₂O₃, it can be seen from Table 8.6.1 that the stresses and temperatures all decrease for these coated tools relative to the uncoated tool. However the stress and temperature in the workpiece remains the same. This result indicates that the coating is effective in protecting the substrate.

The Al₂O₃ coating performed better than the other two coatings in regard to keeping the maximum heat of the cutting tool to a minimum (117°C). The reason for this is due to its low thermal conductivity which makes it harder for the heat to enter the cutting tool and causes it to be passed out in the chip or else remain in the workpiece. Figure 8.6.1b illustrates the difference between the maximum temperature in an uncoated substrate and one coated by Al₂O₃. In both case the maximum temperature occurs in the chip and both temperatures are quite similar at approximately 500°C. It can be seen however, that a much greater area in the region of the nose of the uncoated tool is affected by the heat, and also a greater amount of heat is conducted into the tool by the chip. The Al₂O₃ coat protects the tool from this heat conduction as shown on the contour plot of Figure 8.6.1b.

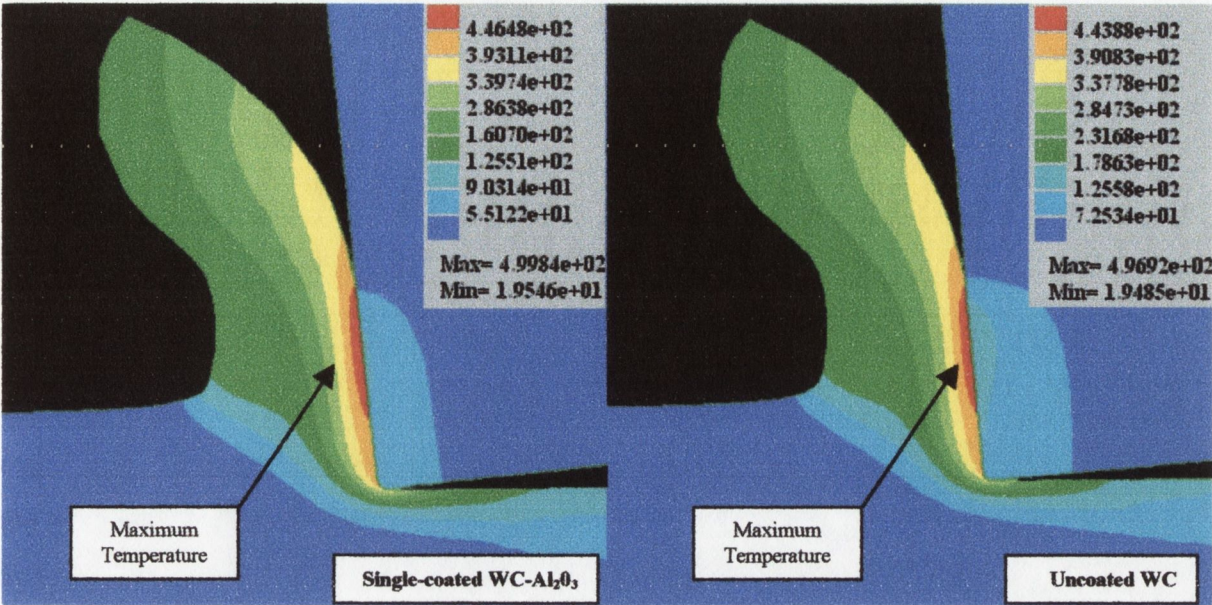


Figure 8.6.1b. Variation in tool temperature with an Al₂O₃ coating

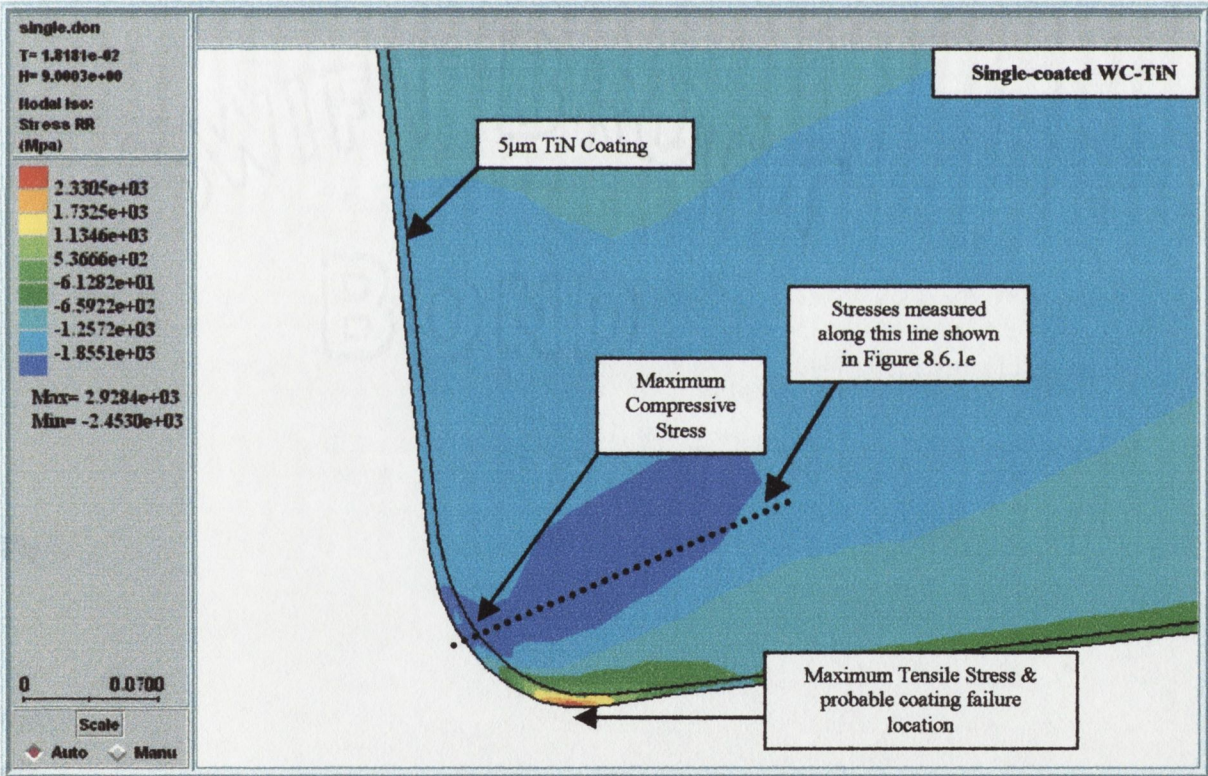


Figure 8.6.1c. Tangential stress distribution for single-coated WC-TiN tool

Figure 8.6.1c shows the distribution of tangential stress over the nose radius of the tool for a cutting speed of 20m/min and a depth of cut of 0.8mm. For clarity the workpiece and chip have been removed from the plot. The maximum compressive tangential stress occurs on the outside of the cutting nose in contact with the workpiece whilst the

maximum tensile tangential stress occurs on the rake face at the interface of the substrate and coating. This tensile stress is of concern because tensile stresses can cause fracture of the coatings and so this is a potential location of coating failure.

Figure 8.6.1d shows the tangential stress variations over the rake face, around the nose radius, and along the flank face for both the outside of the coating and also along the coating/substrate interface, based on the stress distribution shown in Figure 8.6.1c. There is little difference in the compressive tangential stresses along the rake face for a thin $5\mu\text{m}$ coating, however the stress changes in both magnitude and sign around the cutting nose of the tool. It can be seen the difference in the maximum tensile stress at the outside of the tool and along the coating/substrate interface differs greatly.

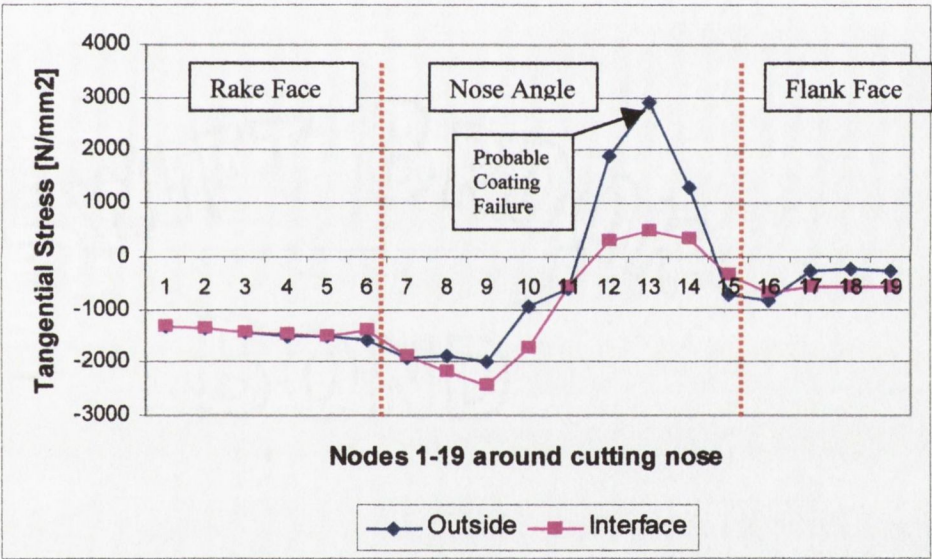


Figure 8.6.1d. Tangential stress distributions along single-coated WC-TiN interfaces

The tangential stress in the $5\mu\text{m}$ coating was found to be 2928N/mm^2 on the outside of the coating due to the high stresses within the primary shear zone and this drops off to 485N/mm^2 on a similar location along the interface between the coating and substrate. The difference in the maximum compressive stress of 2453N/mm^2 also occurs within the cutting nose region but this time occurs along the interface between the coating and substrate. It can also be seen that the compressive stresses along the outside of the cutting tool are also quite high at 2005N/mm^2 which are caused by the chip sliding over the rake face of the tool. What is interesting to note is that the maximum compressive and tensile stresses occur within $50\mu\text{m}$ of each other and both are within the coating. Due to the

complex interaction of these stresses within the cutting nose, probable coating failure would occur at the location where the tensile tangential stresses are a maximum.

Figure 8.6.1e shows how the tangential stress varies through the coating and into the substrate for all coated tools. The line along which the stresses were measured is shown in Figure 8.6.1c (dashed line). It can be seen that there is a drop in stress just past the coating/substrate interface and this stress discontinuity acts as a stress concentration where the maximum compressive tangential stress is found.

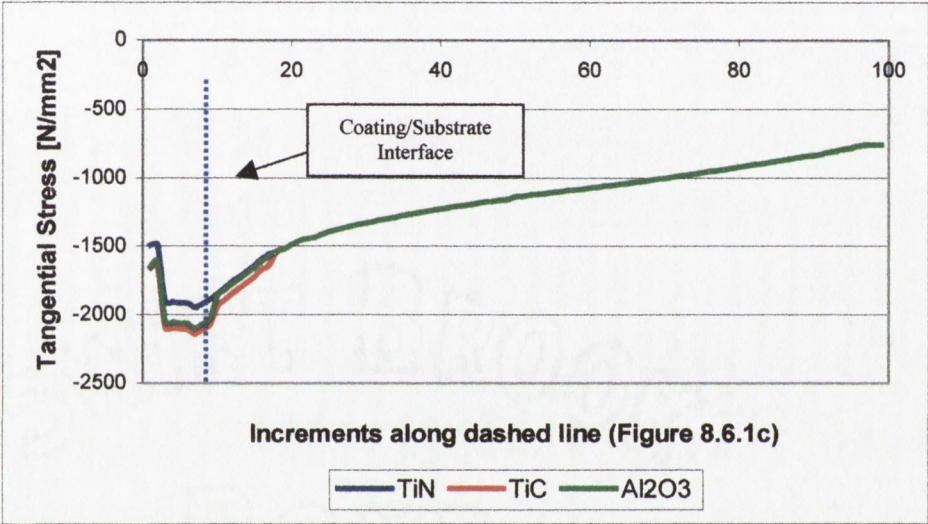


Figure 8.6.1e. Tangential Stress discontinuities through coating/substrate interface

The stress discontinuities observed are quite similar to those predicted by the ANSYS package, shown earlier in Figure 7.2.3a, except in magnitude as only a pressure of 100N/mm^2 was applied. It was shown that the stiffness of the coating is of utmost importance in determining the magnitude of the resulting tangential stresses. As TiC has the largest Youngs modulus (i.e. the stiffest material), the discontinuity through that coating is the largest but as soon as it passes through the interface and into the substrate, the stresses decrease rapidly to a value similar to the substrate stresses of the other single-coated tools.

Figures 8.6.1f shows the distribution of hydrostatic stress in the nose radius region for the WC-TiN single-coated tool. Again for clarity, the workpiece and chip have been removed. It can be observed that a maximum tensile hydrostatic stress exists just behind

the nose radius of the tool. A secondary maximum also occurs along the rake face of the tool within the TiN coating. This area indicates a probable location of coating failure for the WC-TiN insert. As can be seen from Figure 8.6.1f, the maximum compressive hydrostatic stress occurs on the flank face of the tool, just past the nose radius. These high compressive stresses tend to close any voids or cracks existing in the coating but they can cause buckling of the coating from the substrate.

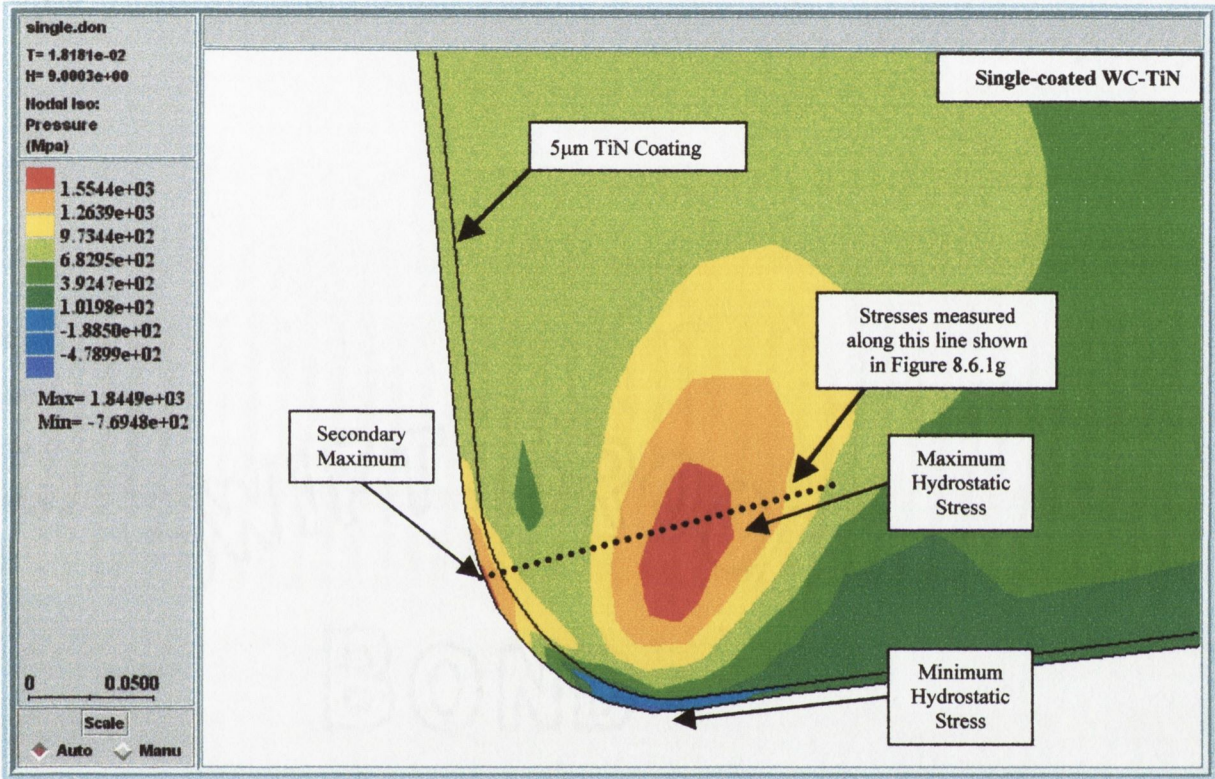


Figure 8.6.1f. Hydrostatic stress distribution for single-coated WC-TiN tool

Figure 8.6.1g shows the hydrostatic stress varies through the coating and into the substrate for all coated tools. The line along which the stresses were measured is shown in Figure 8.6.1f (dashed line). Again tensile stress discontinuities can be observed at the interface between the coating and substrate.

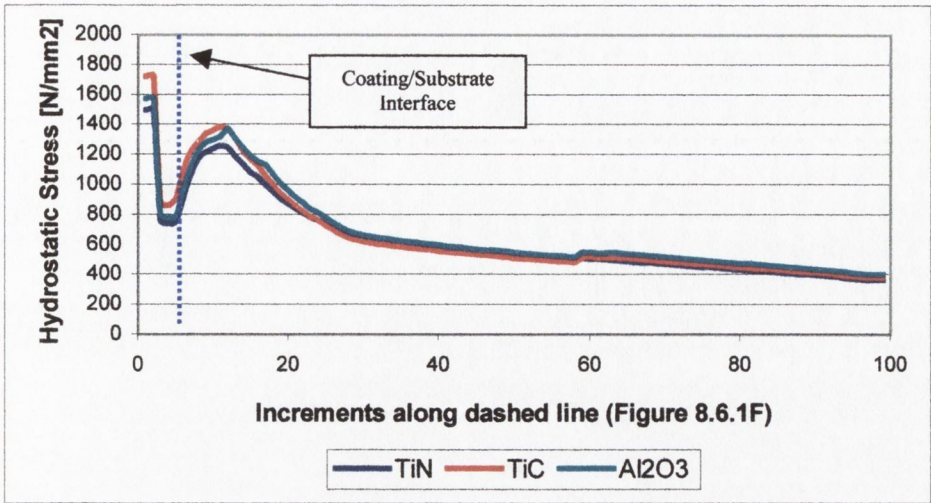


Figure 8.6.1g. Hydrostatic Stress discontinuities through coating/substrate interface

Figure 8.6.1h illustrates the distribution of shear stresses along the top of the coating and also at the coating/substrate interface for a single-coated WC-TiN insert, assuming a cutting speed of 20m/min and a depth of cut of 0.8mm. The chip and the workpiece have been removed for clarity. It can be seen that a maximum negative shear stress exists on the nose radius at the interface of the coating and substrate.

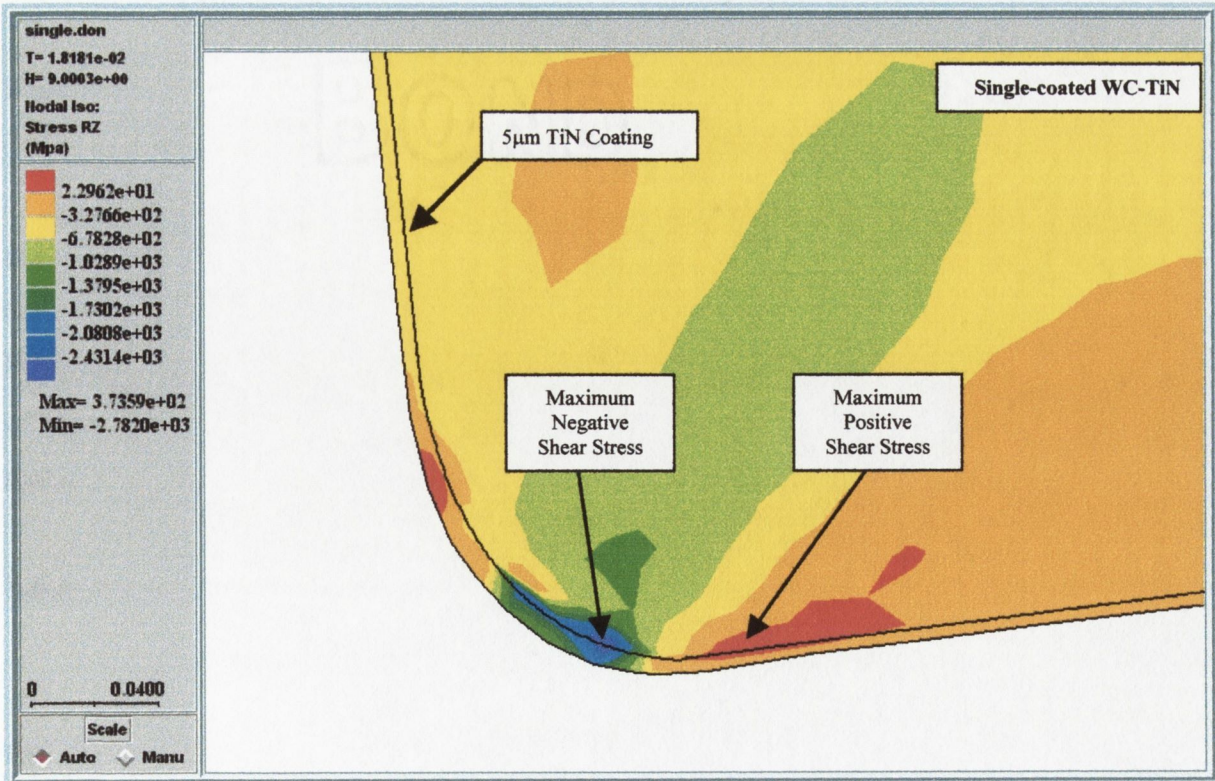


Figure 8.6.1h. Shear stress distribution for single-coated WC-TiN tool

Figure 8.6.1i shows the shear stress variations for the plot shown in Figure 8.6.1h over the rake face, around the nose radius and then up the flank face for both the outside of the coating and also along the coating/substrate interface. There is little difference in the negative shear stresses along the rake face for a thin $5\mu\text{m}$ coating, however the stress changes considerably around the cutting nose of the tool. As can be seen the magnitude of the maximum negative shear stress is larger along the interface than it is along the outside of the coating.

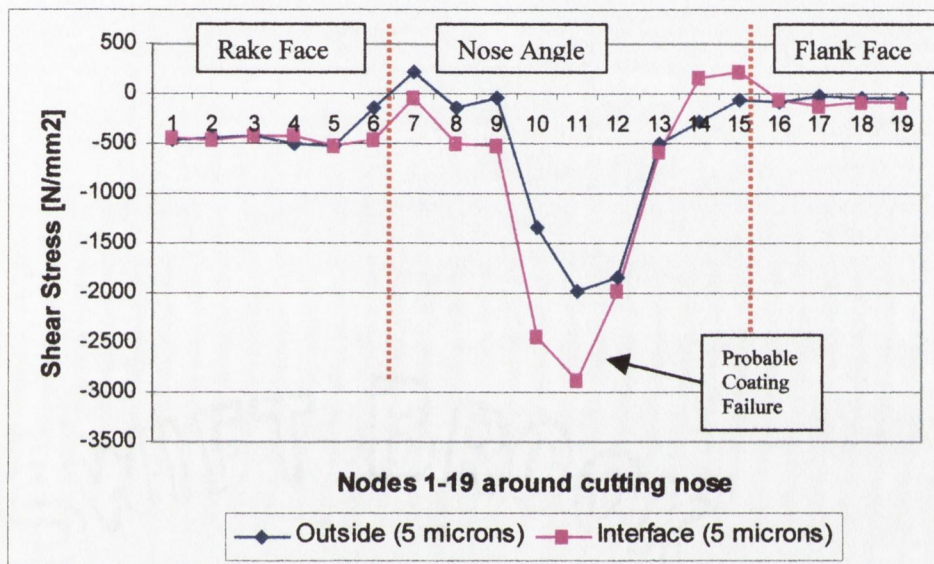


Figure 8.6.1i. Shear stress distributions along single-coated WC-TiN interfaces

The shear stress in the $5\mu\text{m}$ coating to is 2872N/mm^2 along the interface between the coating and substrate. The maximum negative shear stress for both the interface and the top of the coating occurs at the same distance along the tool within the cutting nose region. Positive shear stresses are observed in two locations. The maximum positive shear stress occurs along the interface just past the nose radius and on the flank face whilst a secondary maximum occurs on the rake face on the outside of the coating.

8.6.2 Maximum stress locations for single-coated tools

The distribution and location of the maximum compressive and tensile stresses on the tool can be confusing. Figure 8.6.2 is a representation of a WC-TiN insert and shows the location of each of the maximum stresses. It is interesting to note that most of the maximum tensile stresses occur just past the nose radius on the flank face on either the

interface of the coating/substrate or on top of the coating. The maximum compressive stresses occur on the opposite side of this nose radius on the flank face.

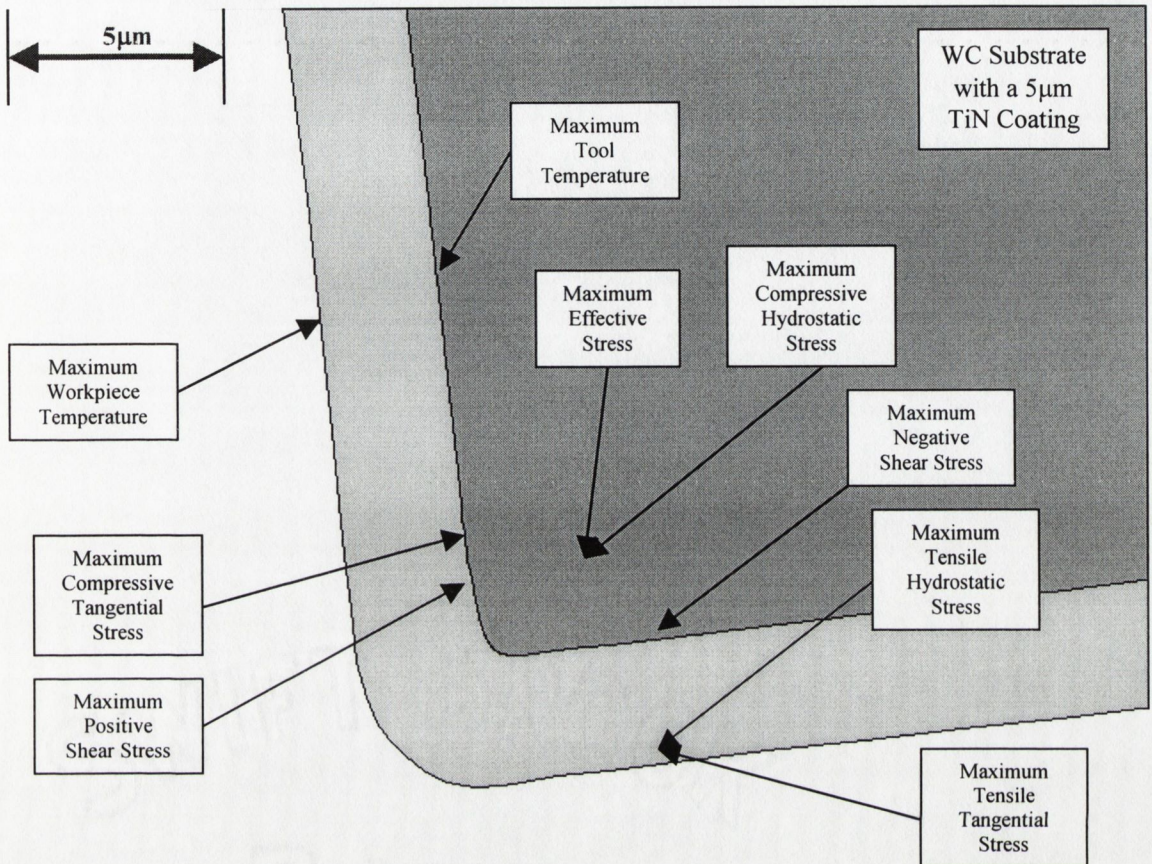


Figure 8.6.2. Location of maximum stresses for single-coated WC-TiN tool

As this nose radius area is only about $20\mu\text{m}$ square, it is quite small for all the maximum compressive and tensile stresses to occur within. This is primarily due to the effect of the primary shear zone in the workpiece and as machining continues, the material from the shear zone slides up the rake face of the tool. This causes further energy to be expended in friction by contact as the chip slides relative to the tool. Sticking friction occurs towards the bottom of the rake face and sliding friction at the top. As the tool is not perfectly sharp - it has a small corner radius, rubbing occurs and this generates frictional heating which further affects the stresses in the region. Strain hardening occurs in the workpiece below the tool which causes plastic deformation to spread to a depth below that of the undeformed chip.

The effect of friction is to increase the temperature in the tool. The maximum temperature occurs in the substrate on the opposite side of the coating where the maximum

workpiece temperature occurs. Both the maximum tensile tangential and shear stresses can cause fracture of the coating to occur and so the probable location of tool failure will be along the interface just past the nose radius on the flank face of the tool. The compressive stresses can help close any cracks, voids or defects in the coating, however high compressive stresses can cause buckling of the coatings to occur, and this too can lead to failure.

8.6.3 10µm single-coated WC substrates

Changing the thickness of the coating had an effect on the stresses in the vicinity of the nose radius. All simulations mentioned previously were rerun, but this time the coating thickness was increased to 10µm to study the effect of coating thickness. Table 8.6.3a lists the variation in output parameters from FORGE2 when the depth of cut is 0.8mm and the cutting speed is 20m/min during an orthogonal metal cutting simulation.

	Uncoated	WC-TiN	WC-TiC	WC-Al ₂ O ₃
Chip/Tool Contact Length [mm]	1.45	1.44	1.47	1.45
Tangential Cutting Force, Ft [N]	1337.9	1274.6	1326.8	1283.8
Max. Effective Stress [MPa]	5881.6	4856.2	5019.3	4917.0
Max. Effective Strain	8.6559	8.5579	8.6358	8.6555
Max. Strain Rate	2228.3	4974.1	4922.8	4947.2
Max. Temperature [°C]	496.9	498.3	496.6	503.0
Max. Temperature in Tool [°C]	147.6	115.6	117.0	104.4

Table 8.6.3a. Machining output parameters with different 10µm single-coated substrates

As can be seen in Table 8.6.3a, increasing the thickness of the coatings does little to change stress or strains in the workpiece compared to those obtained from the simulations with the original 5µm coatings (Table 8.6.1). However what does decrease is the maximum temperature in all tools. Again Al₂O₃ performs better due to its lower thermal conductivity and hence the heat finds it hard to pass into the substrate. The cutting forces and maximum effective stress again are quite similar with those obtained when machining Inconel 718 with a 5µm coated tool (Table 8.6.1).

As previously shown in the ANSYS simulations, as the thickness of the coatings increase, the tangential stresses decrease along the interface. Figure 8.6.3a shows that on both the top of the 10μm coating and along the coating/substrate interface, the tangential stresses decrease significantly relative to the stresses for a 5μm coating.

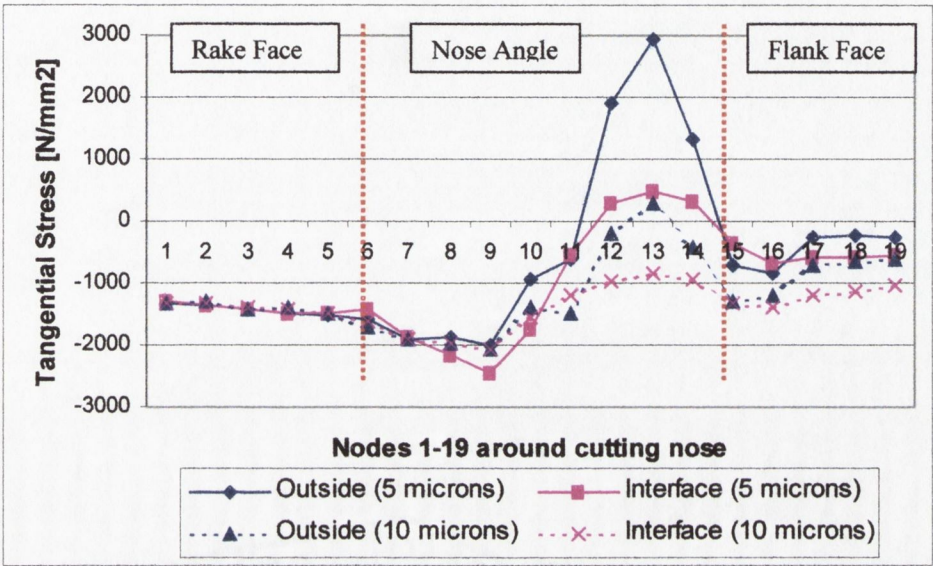


Figure 8.6.3a. Tangential stress distributions along single-coated WC-TiN interfaces

What is interesting to note is that the maximum compressive tangential stress is roughly the same for both coating thicknesses but the maximum tensile tangential stress decreases dramatically. This is important because it is this type of tensile stress which can cause fracture to occur between the coating/substrate interface.

It can be seen from Figure 8.6.3a that with a thick coating, the interfacial tangential stress is always compressive and the stress along the outside of the tool reaches a maximum tensile stress of 276N/mm² just past the nose radius on the flank face. Table 8.6.3b indicates the maximum tensile and compressive stresses for both coating thicknesses for a single-coated WC-TiN tool.

	5µm Thickness		10µm Thickness	
	Along Top of Coating	Along Coating/Substrate Interface	Along Top of Coating	Along Coating/Substrate Interface
Maximum Compressive Tangential Stress	2005N/mm ²	2453N/mm ²	2061N/mm ²	2054N/mm ²
Maximum Tensile Tangential Stress	2928N/mm ²	485N/mm ²	276N/mm ²	Always Compressive

Table 8.6.3b. Maximum Tensile and Compressive Tangential stresses for different coating thicknesses along single-coated WC-TiN interfaces

Again Al₂O₃ performed better than the other coatings in regard to keeping the maximum heat of the cutting tool to a minimum (104°C). The reason for this is its lower thermal conductivity which makes it more difficult for the heat to enter the cutting tool and causes it to be passed out in the chip or else remain within the workpiece. Figure 8.6.3b illustrates the difference between the maximum temperatures in two models, one with a 5µm coating of Al₂O₃ and the other with a 10µm coating. In both cases the maximum temperature occurs in the chip and both temperatures are quite similar at approximately 500°C. It can be seen that the substrate of the 5µm coated tool is more affected by the heat from the chip than the 10µm coated tool.

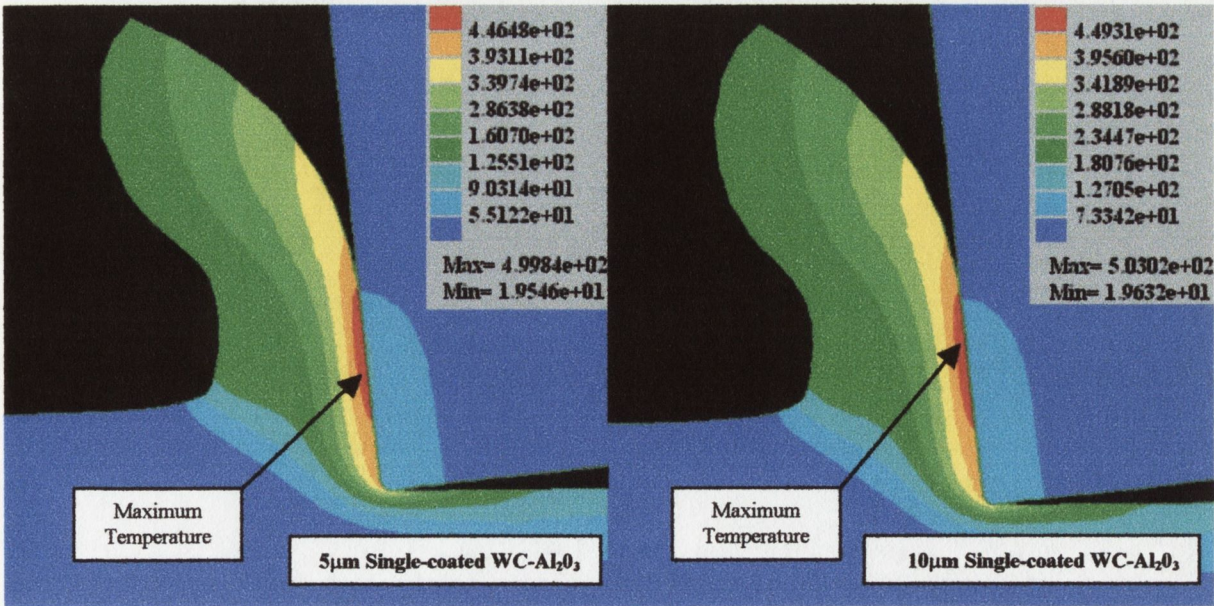


Figure 8.6.3b. Variation in tool temperature with a 5µm and 10µm Al₂O₃ coating

However the effect of increasing the thickness are not completely beneficial to the cutting tool. As can be seen in Figure 8.6.3c, as the thickness of the coating is increased from 5µm to 10µm, the interfacial shear stresses also increase. Again the maximum negative shear stress occurs at the interface between the coating and substrate at the nose radius of the cutting tool and is increased relative to the 5µm coating. The shear stress along the outside of the coating for both the 5µm and 10µm coating thickness models is mostly unchanged though it does change slightly over the nose radius. The most important area is the high change in the maximum value of the positive shear stresses. Again it occurs on the flank face just past the nose radius, however this time the stress remains positive all along the flank face coating/substrate interface. This increases the likelihood of coating fracture.

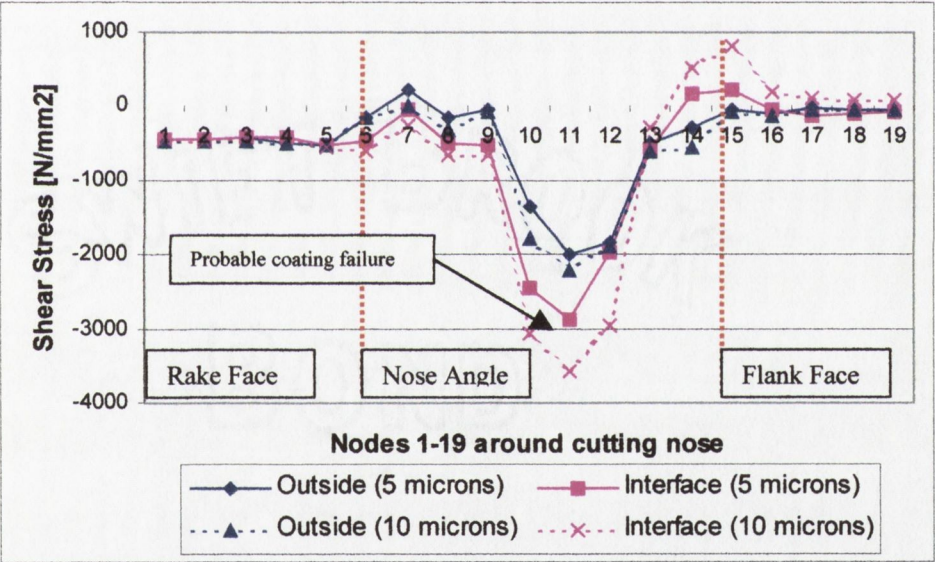


Figure 8.6.3c. Shear stress distributions along single-coated WC-TiN interfaces

Table 8.6.3c indicates the maximum positive and negative shear stresses for both coating thicknesses for a single-coated WC-TiN tool.

	5µm Thickness		10µm Thickness	
	Along Top of Coating	Along Coating/Substrate Interface	Along Top of Coating	Along Coating/Substrate Interface
Maximum Negative Shear Stress	1986N/mm ²	2872N/mm ²	2208N/mm ²	3560N/mm ²
Maximum Positive Shear Stress	225N/mm ²	225N/mm ²	20N/mm ²	826N/mm ²

Table 8.6.3c. Maximum Positive and Negative Shear stresses for different coating thicknesses along single-coated WC-TiN interfaces

The benefits of thick coatings are apparent is that they lower the interfacial tangential stresses as well as lowering the temperature within the tool. The benefits of protecting the tool from high temperatures means the tool isn't weakened by decreasing hot hardness which when combined with the stresses causes plastic deformation or fracture of the cutting edge. However, a balance must be reached because as the thickness increases, so do the shear stresses which can lead to fracture of the coatings.

8.7 Effect of double-coated cutting tools on machining parameters

As with the ANSYS simulations, the coatings were organised into a series of double-coated combinations and the simulations were re-run to discover which combination would give the lowest temperature, with the lowest maximum effective stresses in the tool and the lowest effective strains in the workpiece.

8.7.1 Double-coated WC substrates

FE simulations were performed using six different double-coated substrates. WC-TiC-TiN and WC-TiC-Al₂O₃ are examined in detail here due to these tools having been used in experiment tests. In all cases the double-coated tools experimentally out-performed their uncoated competitors as shown in Section 6.2. In the experimental tests, the uncoated tools failed after 3 minutes compared to 10 minutes for single-coated tools and 12 minutes for double-coated tools. Table 8.7.1a lists the variation in output parameters from FORGE2

when the depth of cut is 0.8mm at a cutting speed of 20m/min during an orthogonal metal cutting simulation. All the double-coated models had a coating thickness of 5µm.

	Uncoated	WC-TiC-TiN	WC-TiC-Al ₂ O ₃
Chip/Tool Contact Length [mm]	1.45	1.47	1.46
Tangential Cutting Force, Ft [N]	1337.9	1307.9	1323.8
Max. Effective Stress [MPa]	5881.6	4335.4	4458.1
Max. Effective Strain	8.6559	8.0946	8.2648
Max. Strain Rate	2228.3	4716.1	4802.2
Max. Temperature [°C]	496.9	513.5	512.2
Max. Temperature in Tool [°C]	147.6	129.4	117.1

Table 8.7.1a. Machining output parameters with different 5µm double-coated substrates

Figure 8.7.1a illustrates the distribution of effective stress in a double-coated WC-TiN-TiC tool. The chip and the workpiece have been removed for clarity. For all simulations, the maximum effective stress occurs within the substrate in a region close to the corner of the nose radius. These high stresses, combined with high temperatures, can result in the premature wear of tools. In fact it can be seen from Table 8.7.1a, that with the addition of a 5µm double-coating for any WC substrate, the temperature and effective stress in the tool decreases relative to an uncoated tool. It can be seen from Table 8.7.1a that there is not much variation in the predicted cutting forces for any of the double-coated cutting tools. It can also be seen from Table 8.7.1b that the maximum effective stress within the tool is smaller for double-coatings compared to single-coatings. So initial findings suggest that a double-coating is superior – as far as it minimises the effective stresses in the tool.

	Uncoated	WC-TiN	WC-TiC	WC-Al ₂ O ₃
Max. Effective Stress [MPa]	5881.6	4979.4	5362.8	5150.3

	Uncoated	WC-TiC-TiN	WC-TiC-Al ₂ O ₃
Max. Effective Stress [MPa]	5881.6	4335.4	4458.1

Table 8.7.1b. Effective stress output parameters for single- and double-coated tools

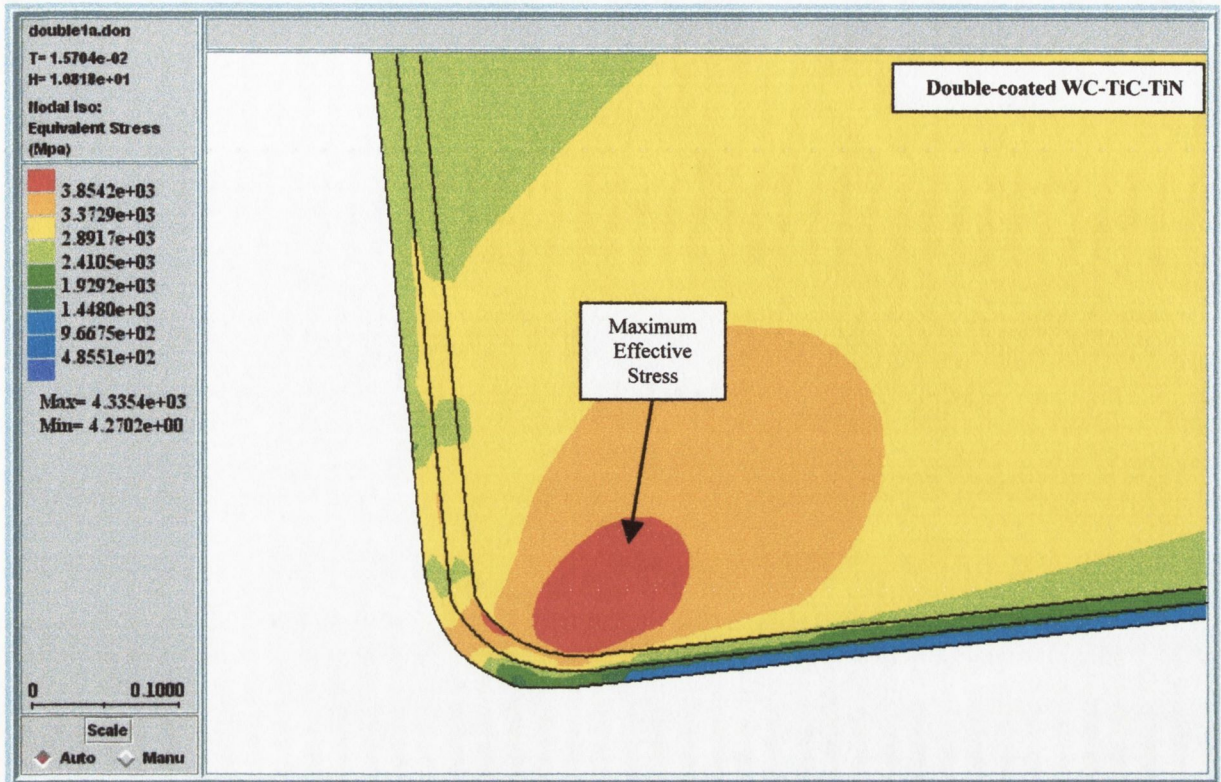


Figure 8.7.1a. Distribution of effective stress in a double-coated WC-TiC-TiN tool

Figure 8.7.1b illustrates the distribution of tangential stress in a double-coated WC-TiN-TiC tool. The chip and the workpiece have been removed for clarity. Figure 8.7.1c shows the variations along the interfaces for the tangential stress distributions for the WC-TiC-TiN tool shown in Figure 8.7.1b. It plots the values over the rake face, around the nose radius and then up the flank face for both the outside of the coating and also along the coating/coating and coating/substrate interfaces. There is little difference in the compressive tangential stresses along the rake face, however the stresses change around the nose radius of the tool. As can be seen, it is only the interface which is subjected to tensile tangential stresses along the outside of the top coating. This is different to the single-coated tool where there are tensile stresses within the substrate/coating interface which can lead to coating failure by fracture.

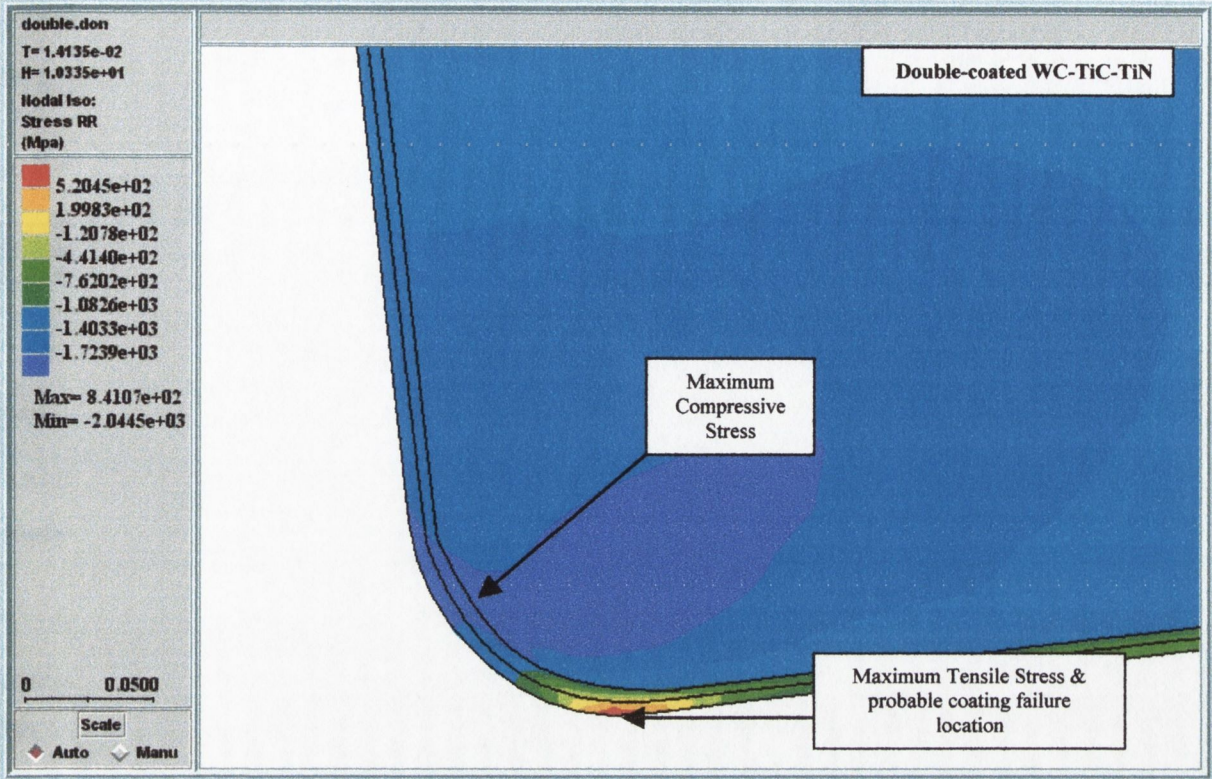


Figure 8.7.1b. Distribution of tangential stress in a double-coated WC-TiC-TiN tool

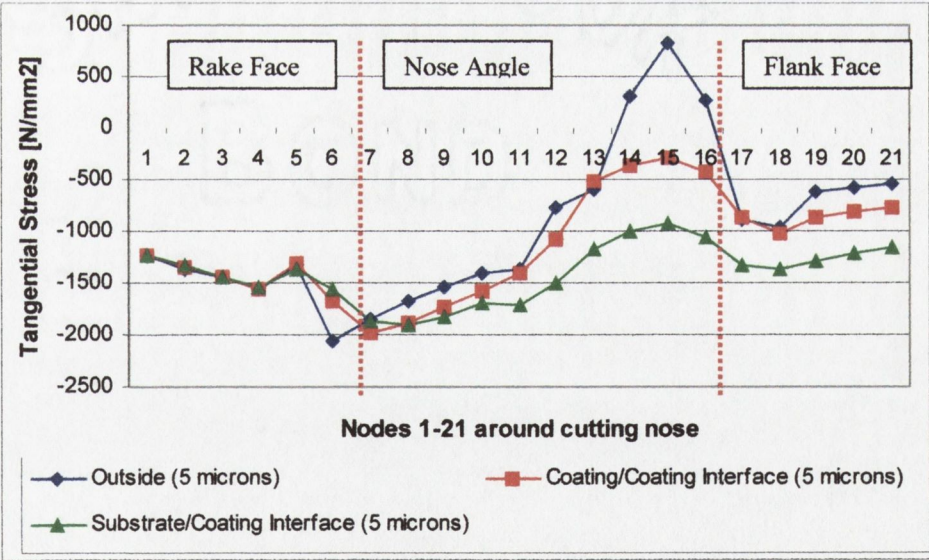


Figure 8.7.1c. Tangential stress distributions along double-coated WC-TiC-TiN interfaces

It has been previously discussed that the benefit of having an Al_2O_3 layer on any coated tool is the effect it has on maintaining the tool's hot hardness by resisting heat flow through its low thermal conductivity. As can be seen in Table 8.7.1c, the temperatures in both the single- and double-coated tool, with a top layer of Al_2O_3 , are nearly identical at

117°C. As the tool is able to retain its strength, Al₂O₃ coated tools resist the stresses in the nose radius that can affect an uncoated tool performance.

	Uncoated	WC-TiN	WC-TiC	WC-Al ₂ O ₃
Max. Temperature in Tool [°C]	147.6	127.6	131.1	117.2

	Uncoated	WC-TiC-TiN	WC-TiC-Al ₂ O ₃
Max. Temperature in Tool [°C]	147.6	129.4	117.1

Table 8.7.1c. Temperature in Tool output parameters for single- and double-coated tools

Figure 8.7.1d illustrates the distribution of shear stresses along the top of the coating and also on the coating/coating and coating/substrate interfaces for a double-coated WC-TiC-TiN insert, with a cutting speed of 20m/min and a depth of cut of 0.8mm. The chip and the workpiece have been removed for clarity. Figure 8.7.1e plots the stresses along the various interfaces and it can be seen that a maximum negative shear stress exists on the nose radius at the interface of the coating and substrate and that a maximum positive shear stress exists on the rake face, just before the corner radius.

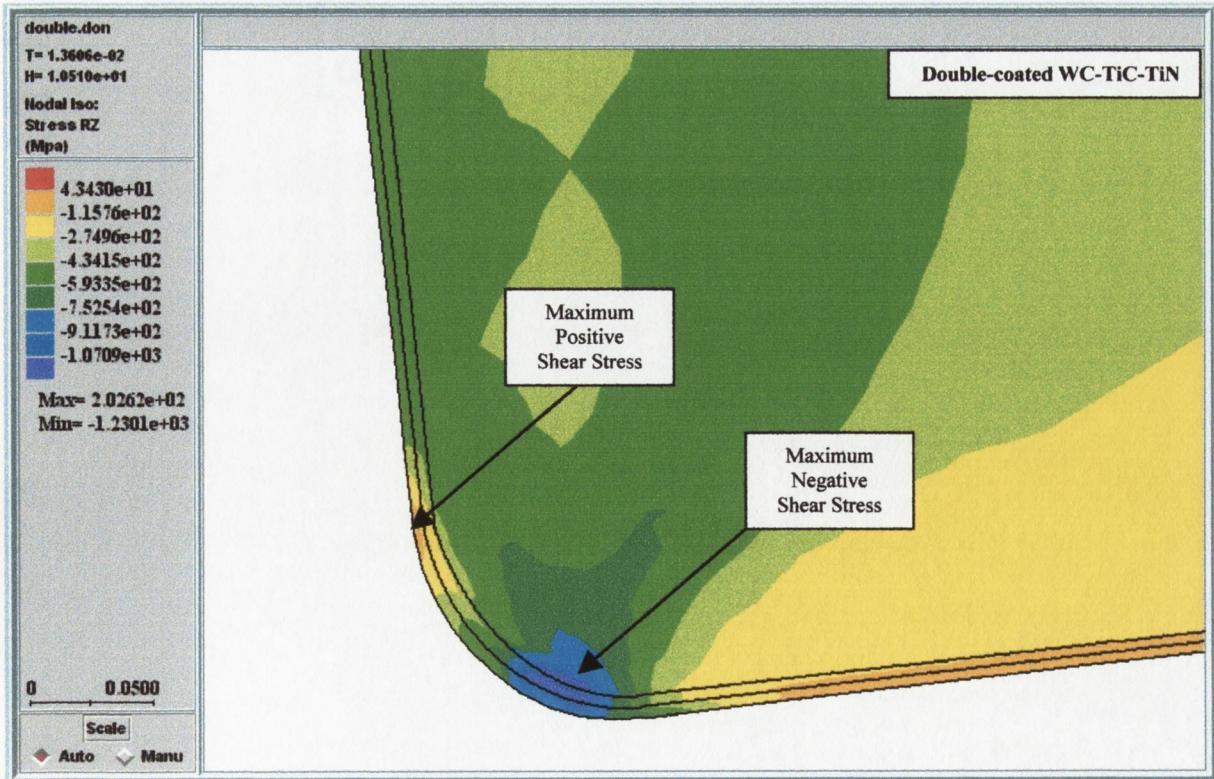


Figure 8.7.1d Distribution of shear stress in a double-coated WC-TiC-TiN tool

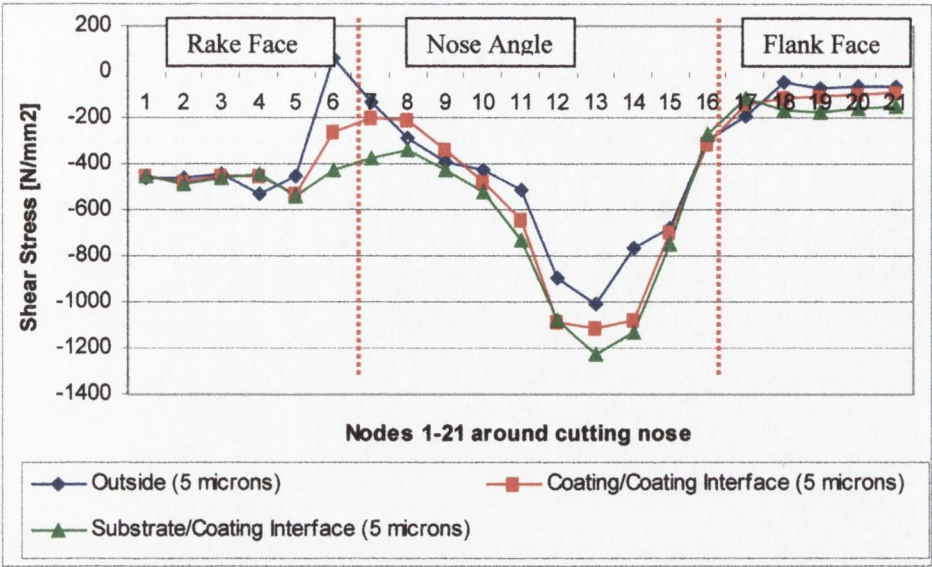


Figure 8.7.1e Shear stress distribution for double-coated WC-TiC-TiN tool

What is interesting to note is that the both the magnitude of the positive and negative shear stress decreases with a double-coating when compared to the maximum shear stresses along the interfaces of the single-coated WC-TiN tool in Figure 8.6.1h. The effect of the double-coating is to minimise the effect of the shear stresses along the interfaces and no positive shear stresses occur along the coating/coating interface or coating/substrate interface. Even though the shear stress is generally high over the same distance of the interface for single and double coatings (Figures 8.6.3c & 8.7.1e), it can be seen from Table 8.7.1d that the magnitudes for the single-coated tools is much greater. Therefore delamination would more likely be to occur along the interfaces of a single-coated tool.

	Single-Coating		Double-Coating		
	Along Top of Coating	Along Coating/Substrate Interface	Along Top of Coating	Along Coating/Coating Interface	Along Coating/Substrate Interface
Maximum Negative Shear Stress	1986N/mm ²	2872N/mm ²	1010N/mm ²	1109N/mm ²	1230N/mm ²
Maximum Positive Shear Stress	225N/mm ²	225N/mm ²	63N/mm ²	Always Positive	Always Positive

Table 8.7.1d. Maximum positive and negative shear stresses for a single-coated WC-TiN tool and a double-coated WC-TiC-TiN tool.

8.8 Effect of Cutting Speed on Coated Deformable Tools

Experimentally, when machining Inconel 718 it has been shown that as cutting speed increases, cutting forces increase dramatically for uncoated tools and decrease for coated tools. *To the author’s knowledge, this is one of the first analyses to numerically examine the effect of cutting speed on coated deformable cutting tools.*

8.8.1. Single-coated TiN substrate

It has been shown experimentally in Section 6.2, and by using FEA in as presented in Section 8.4.2, that as the cutting speed is increased for uncoated deformable tools, the output parameters also increase. The maximum value of temperature occurs in localised zones of intense heat which can lower the tool life considerably as the hot hardness of the tool would decrease with large increases in temperatures. Another parameter which increases is the tangential cutting force which is the most important and largest cutting force component.

Table 8.8.1a lists the variation in output parameters from FORGE2 when the cutting speed is changed from 20m/min to 40m/min and 60m/min during an orthogonal metal cutting simulation. These values were taken to emulate experimental tests. The tool was initially modelled as a WC-TiN single-coated insert with a depth of cut of 0.8mm.

	20m/min	40m/min	60m/min
Chip/Tool Contact Length [mm]	1.45	1.43	1.43
Tangential Cutting Force, Ft [N]	1291.3	1190.3	1076.8
Max. Effective Stress [MPa]	4979.4	5126.1	5768.0
Max. Effective Strain	8.6845	9.2547	9.8872
Max. Strain Rate	4973.6	4570.3	4145.4
Max. Temperature [°C]	499.1	567.3	675.2

Table 8.8.1a. Machining output parameters with increasing cutting speed for a single-coated WC-TiN insert

A comparative analysis was undertaken by comparing the output parameters from FORGE2 with those obtained from the experimental work for a single-coated insert at the three cutting speeds. The experimental tests involved the single point turning of a precipitation hardened Inconel 718 billet as outlined in Section 5.4. The cutting force components from this experimental test were measured and compared to the force components from the FEA model. Results from the single-coated WC-TiN insert (Sandvik GC1015) have already been presented in Figure 8.1b

This comparative study is now presented for the tangential and axial cutting forces for a cutting speed of 40m/min. As shown in Figure 8.8.1a, the average tangential cutting force measured from the experimental tests was 1093N and that predicted by FORGE2 was 1116N for the same cutting conditions, giving a difference of 2.5% for the single-coated tool. The averaged axial force component from the experimental work was 498N and the averaged force from the FORGE2 analysis was 483N, giving a difference of 3%. In both cases the results were averaged after steady state machining took place, which was after 3 seconds in Figure 8.8.1a.

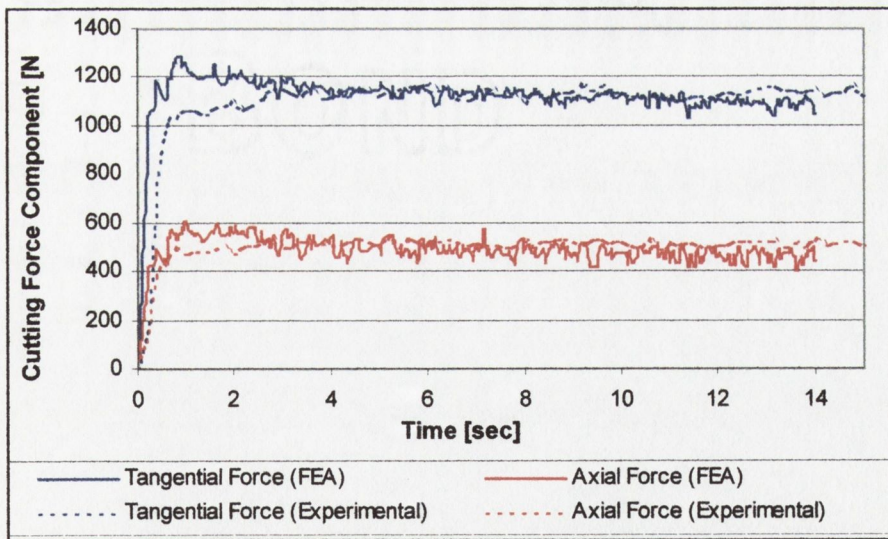


Figure 8.8.1a. Cutting Forces obtained from experimental work and FEA when machining Inconel 718 at 40m/min with a coated Sandvik GC1015 cutting tool.

In tabular form, the comparative study yielded the following result:

	Experimental [N]	FE Simulation [N]	% Difference
Tangential 20m/min	1249	1205	4
Tangential 40m/min	1093	1190.3	7
Axial 20m/min	624	598	4.5
Axial 40m/min	498	518	4

Table 8.8.1b. Effect of cutting speed on cutting forces for single-coated WC-TiN tool

Both the comparative studies show that FEA can indeed be used to successfully simulate orthogonal machining operations using coated carbide cutting tools under a range of cutting speeds. The FEA simulations also demonstrated successfully that as the cutting speeds increase, so does the cutting temperature which can lead to a decrease in the tools hot hardness. This results in wear increasing on both the flank and rake faces of the tool.

8.10 Effect of Chipbreakers on Coated Deformable Tools

A Sandvik –QM type tool, on which a chipbreaker was present, replaced the flat rake face deformable tools. The effect of the chipbreaker was to curl the chip which caused it to leave the rake face quicker. It was found that with an added chipbreaker, the stresses and temperatures were reduced for WC-TiC insert. Figure 8.10a shows the distribution of effective stress through the cutting nose of a single-coated TiC substrate for a depth of cut of 0.8mm, cutting speed 20m/min and a length of cut of 5mm. It can be seen that the magnitude of the maximum effective stress decreases due to the presence of a chipbreaker, and also due to the additional area on which the cutting force is acting.

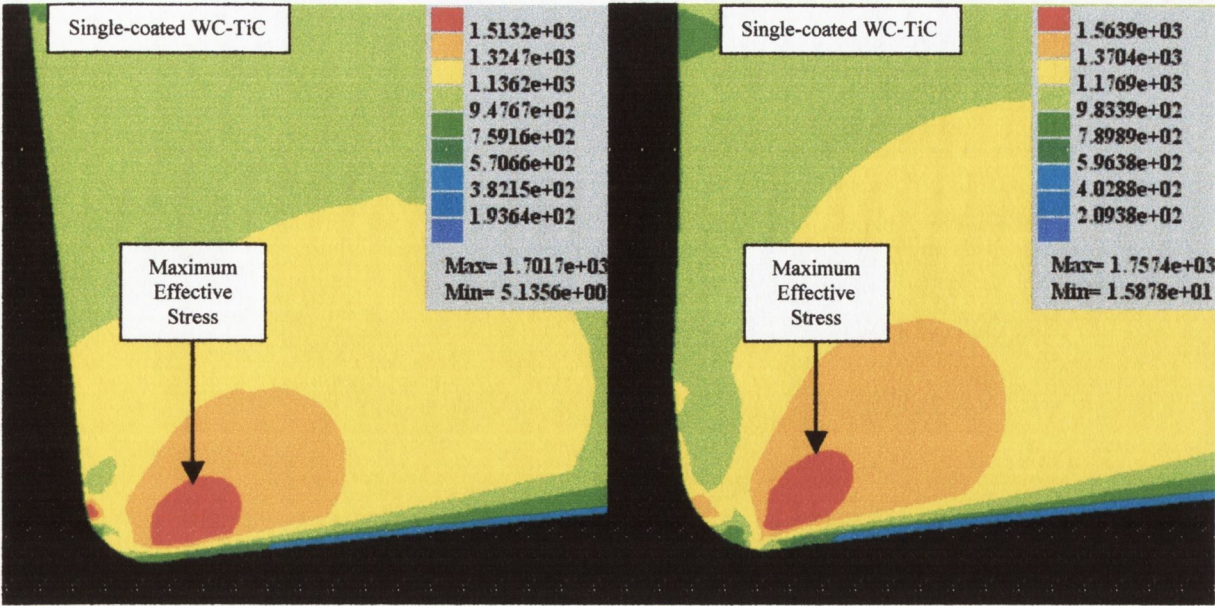


Figure 8.10a. Variation in Stress Distribution due to addition of Sandvik –QM chipbreaker

Table 8.10 lists the variation in output parameters from FORGE2 when a chipbreaker is added to the geometry of the tool and all other variables are held steady during an orthogonal metal cutting simulation. As can be seen from Table 8.10, the chip/tool contact length decreases slightly with the addition of a chipbreaker. The tangential cutting force also decreases due to the relief the chipbreaker gives the metal cutting operation as it encourages the material to flow. The cutting temperatures are again very similar as are the maximum temperatures in the tool. TiC, unlike Al₂O₃, does not have a low thermal conductivity and heat finds it easier to pass through the coating.

	WC-TiC	WC-TiC (-QM)
Chip/Tool Contact Length [mm]	1.45	1.37
Tangential Cutting Force, Ft [N]	1561.8	1452.4
Max. Effective Stress [MPa]	1701.7	1757.4
Max. Effective Strain	6.8279	7.1726
Max. Strain Rate	4621.9	4054.6
Max. Temperature [°C]	542.3	535.9
Max. Temperature in Tool [°C]	135.4	133.7

Table 8.10. Machining output parameters for effect of a –QM chipbreaker for a single-coated WC-TiC insert

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PART V
Conclusions

9.1

9.0 Conclusions

Numerous research programs have numerically, experimentally and analytically examined the stress and temperature distributions within an orthogonal machining operation using an uncoated deformable tool. However to the author's knowledge, the use of multi-coated tools has not been documented previously. Therefore the aim of this thesis was to analyse the effect of the machining process on coated carbide cutting tools and provide details of the stress distributions within the tool and possible explanations for coating failures at various key points on the coated tool. The work program focused on the machining of Inconel 718 using a selection of commonly available uncoated and multi-coated cemented carbides. The general conclusions are detailed under two headings, these being experimental and analytical.

9.1 Experimental Conclusions

- At a cutting speed of 20m/min, all coated and uncoated tools were within ISO limits of 0.3mm flank wear. As the speed was increased to 40m/min, uncoated tools failed after 3 minutes, whilst the coated tools were still within limits after 10 minutes. This indicates at lower speeds, the protective properties of the coatings are not utilised.
- After 11 minutes of machining Inconel 718 at 40m/min, the single-coated Widia HK15 fails the ISO limit of 0.3mm flank wear when the TiN coating fails, and starts to wear similarly to an uncoated tool. Both the multi-coated tools still remain within ISO limits after 12 minutes of machining at 40m/min. Both these tools contained a layer of Al_2O_3 which protected them from the high temperatures associated with machining Inconel 718.

- Measurements of the overall cutting force showed that increases in tool wear are broadly reflected by increases in cutting force. Due to the lack of protection afforded by the uncoated tools, these inserts initially sustain the greatest forces and over time, gradually break down.
- There was an overall drop in the magnitude of the cutting forces with increasing cutting speed from 20m/min to 40m/min for all tools tested. The decrease in both the radial and axial force components is quite significant but with little changing in the tangential component.

9.2 FEA of Hertzian Pressures Conclusions

- For all coated tools, the magnitude of the stresses within the tool and the interfaces depend on the value of the stiffness ratio between adjacent layers.
- For all single, double, and multi-coated tools, the interfacial shear stresses increase with increasing coating thickness unlike the corresponding interfacial tangential stresses which have been shown to decrease with increasing thickness.
- For all single, double, and multi-coated tools, the magnitudes of the tangential stress discontinuities decreases between interfaces, due to a favourable modulus ratio and increasing coating thickness.
- The magnitude of the shear stress is generally high over a greater portion of the interface for thick coatings and only concentrated within a narrow band for thin coatings which increases the probability of delamination occurring along the interfaces of thick coatings.

9.3 FEA of Machining Conclusions

- Good correlation was obtained between the results of the experimental cutting forces and those of the FE simulations.
- The maximum temperature for the negative rake tools occurs on the rake face of the tool. Coatings are used due to the protection they afford the substrate from the heat.
- All the maximum tangential stresses and shear stresses occur around the nose radius. Failure is a possibility where shear stresses and tensile tangential stresses are a maximum.
- The maximum effective stress within the tool is smaller for double-coatings compared to single-coatings. So findings suggest that a double-coating is superior – as far as it minimises the effective stresses in the tool. Experimentally a double-coated 10 μ m tool (*Sandvik GC3105*) performed better than a single-coated 10 μ m tool (*Sandvik GC1015*) with increased tool life and decreased cutting forces.
- The maximum shear stress occurs on the coating/substrate interface for all coated tools. The magnitude of the interfacial shear stresses decrease with a double-coated tool when compared to a single-coated tool. This would suggest that delamination is more likely to occur on a single-coated tool, which is shown experimentally as the coatings fail on the single-coated tools before the double-coated tools.
- When the cutting speed increases, the cutting temperature and stresses were shown to increase and the cutting forces were shown to decrease. Experimentally, it is shown that as the cutting speed increases, the cutting force components for all coated and uncoated cutting tools decrease.

9.4 Recommendations for Future Work

The most crucial future research will be in the development of a damage criterion (*Section 3.3*) which takes into account both the coated tool and workpiece. This damage criterion will be beneficial in two ways. The first to enable fracture or delamination of the coatings over the various interfaces while the second allows crack growth in the chips which results with segmental chip formations.

Further experimental work to obtain input data for new finite element analyses can be performed by machining Inconel 718 using ceramic tools at cutting speeds of about 200m/min.

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Appendix 1

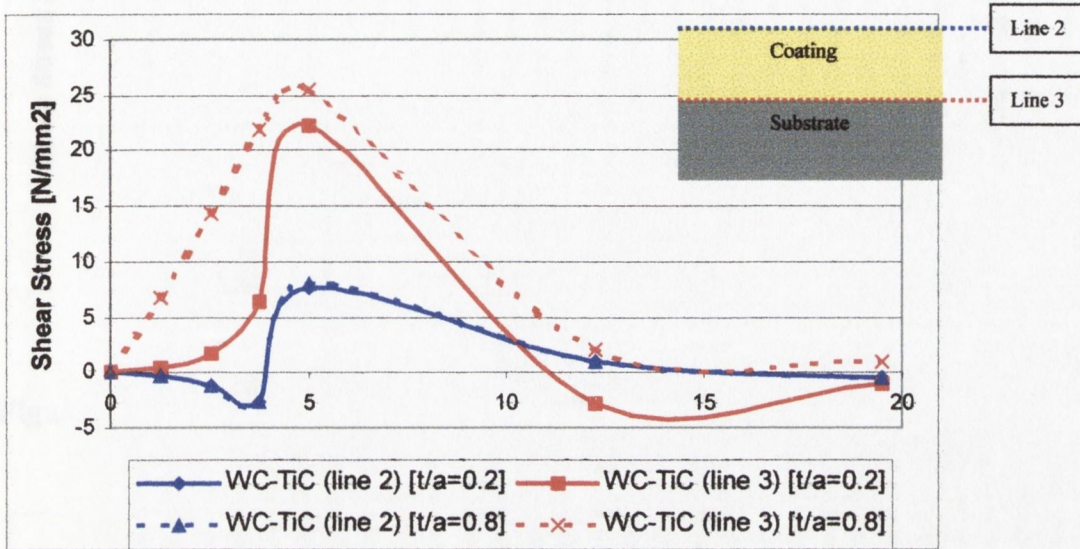


Figure app1.1. Variation of τ_{xy} along single-coated WC-TiC substrate interfaces

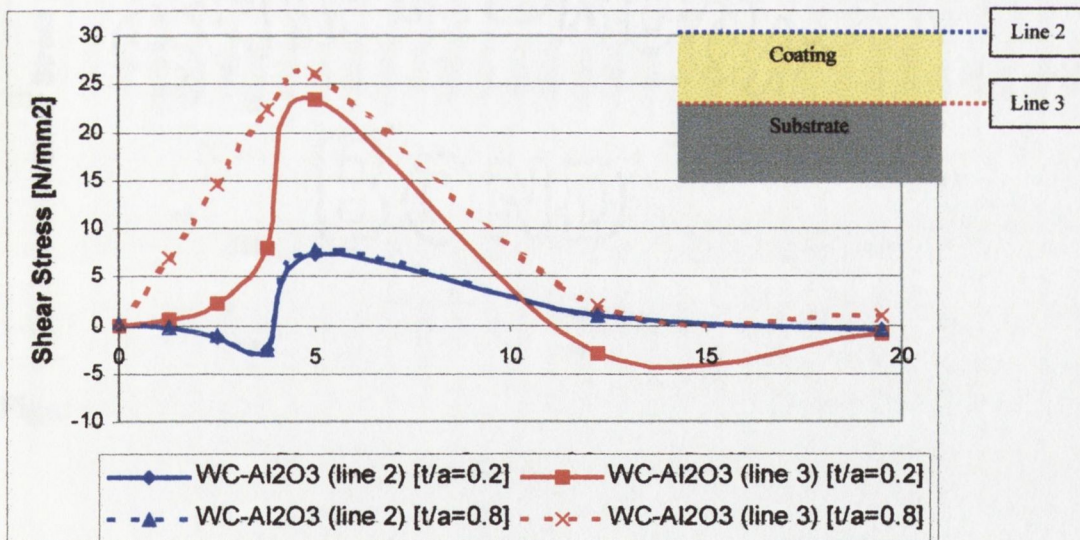


Figure app1.2. Variation of τ_{xy} along single-coated WC-Al₂O₃ substrate interfaces

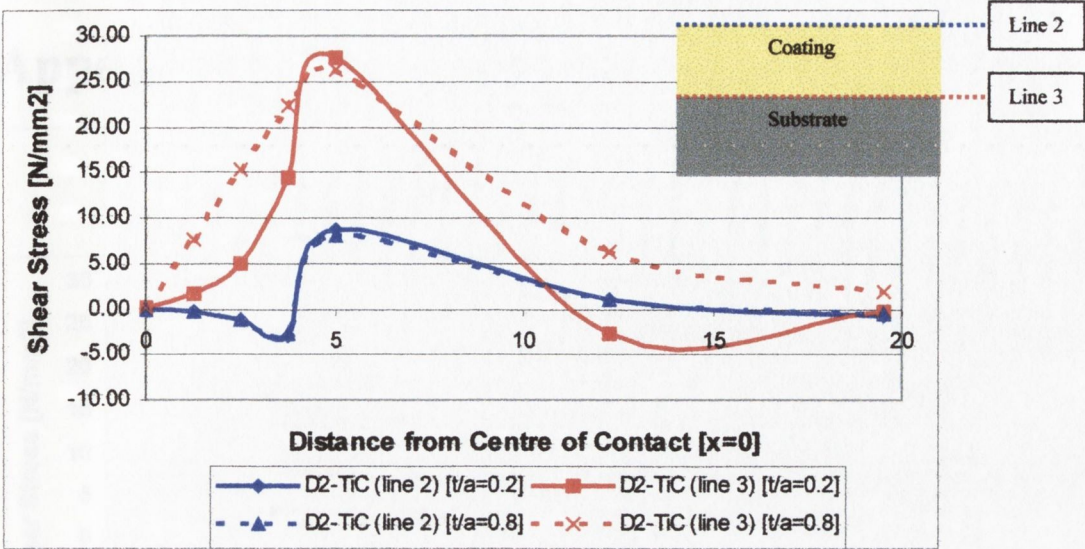


Figure app1.3. Variation of τ_{xy} along single-coated D2-TiC substrate interfaces

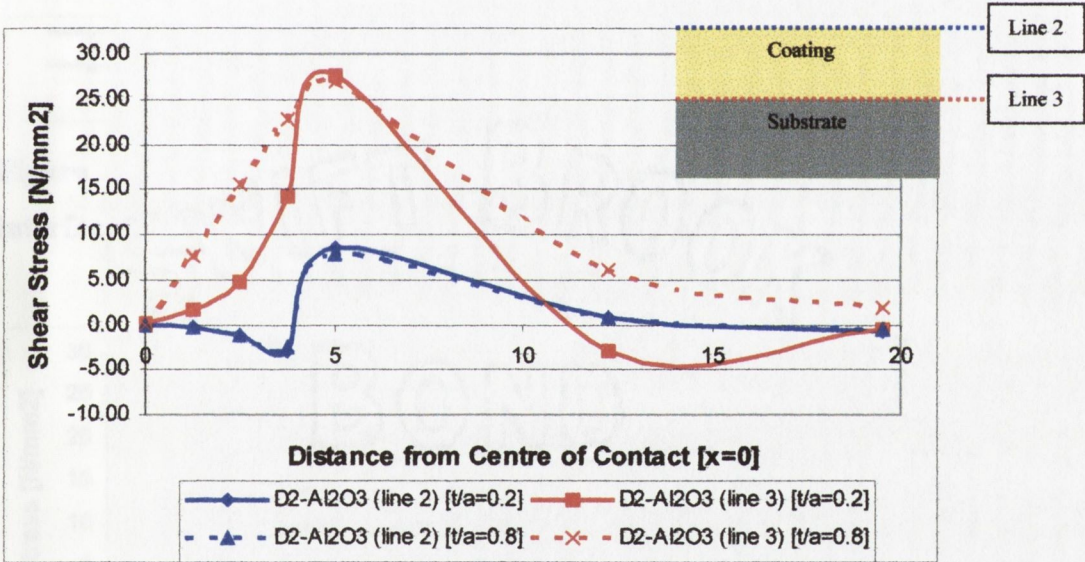
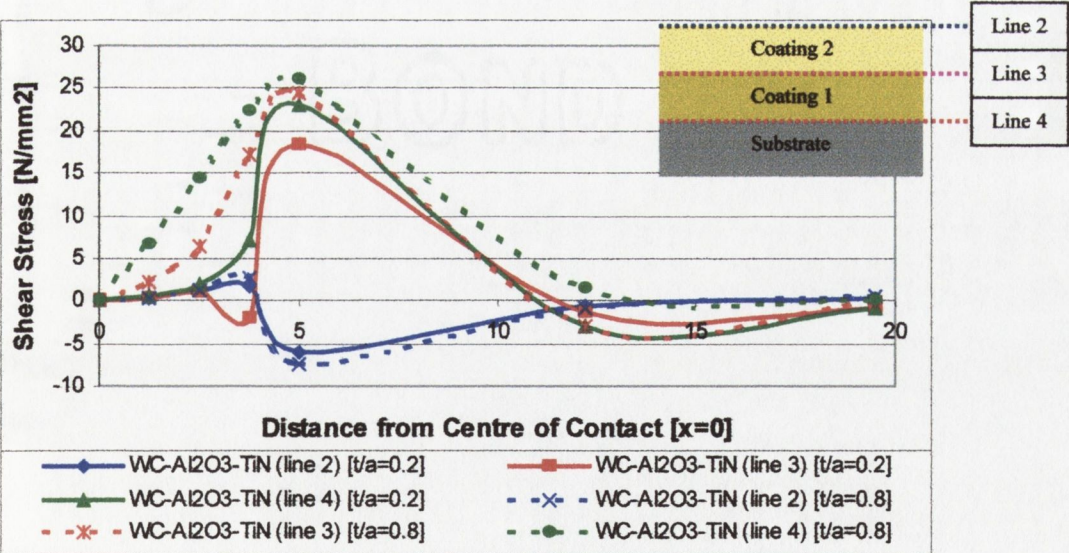
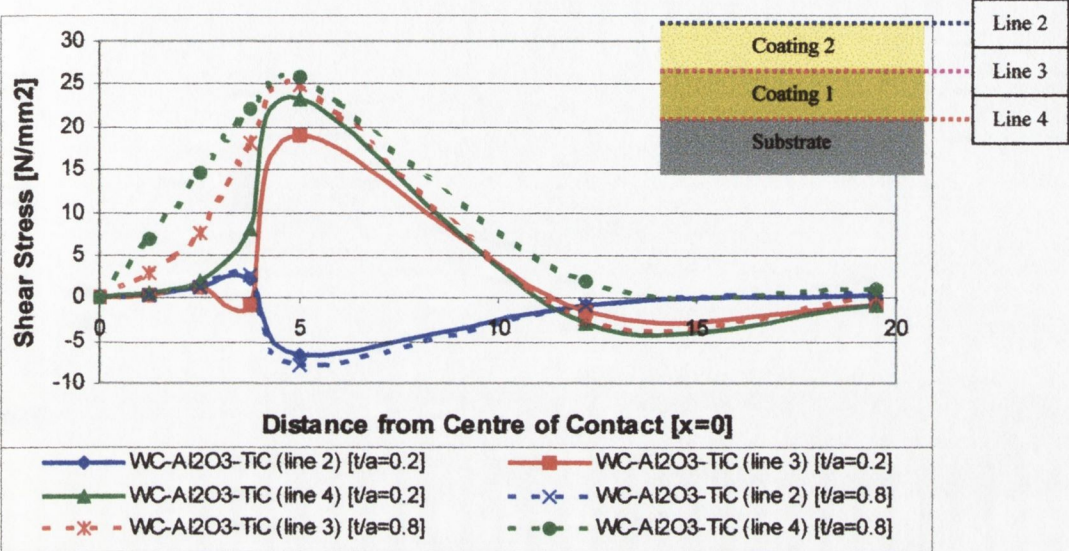


Figure app1.4. Variation of τ_{xy} along single-coated D2-Al₂O₃ substrate interfaces

Appendix 2



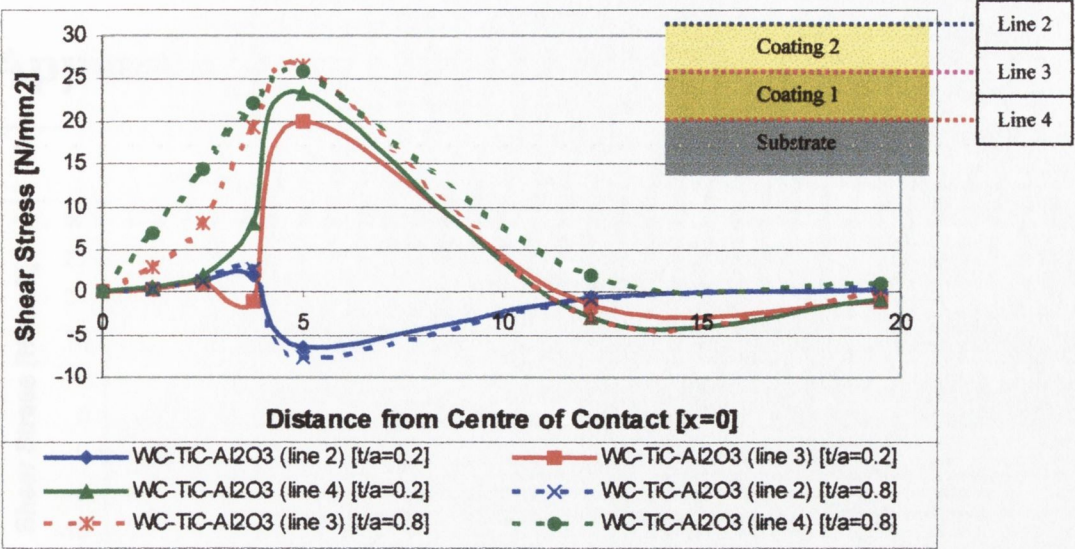


Figure app2.3. Variation of τ_{xy} along double-coated WC-TiC-Al₂O₃ substrate interfaces

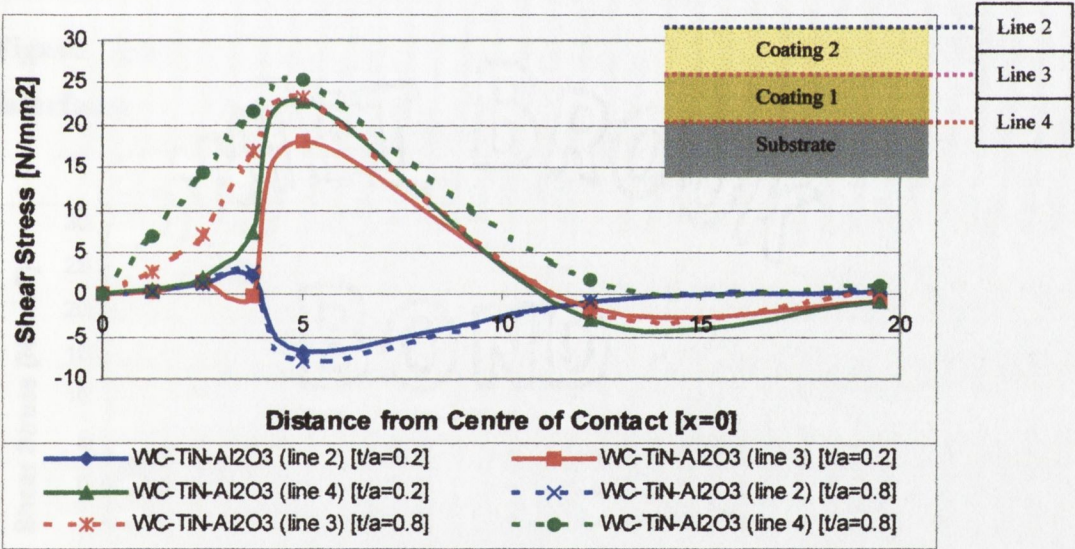


Figure app2.4. Variation of τ_{xy} along double-coated WC-TiN-Al₂O₃ substrate interfaces

Appendix 3

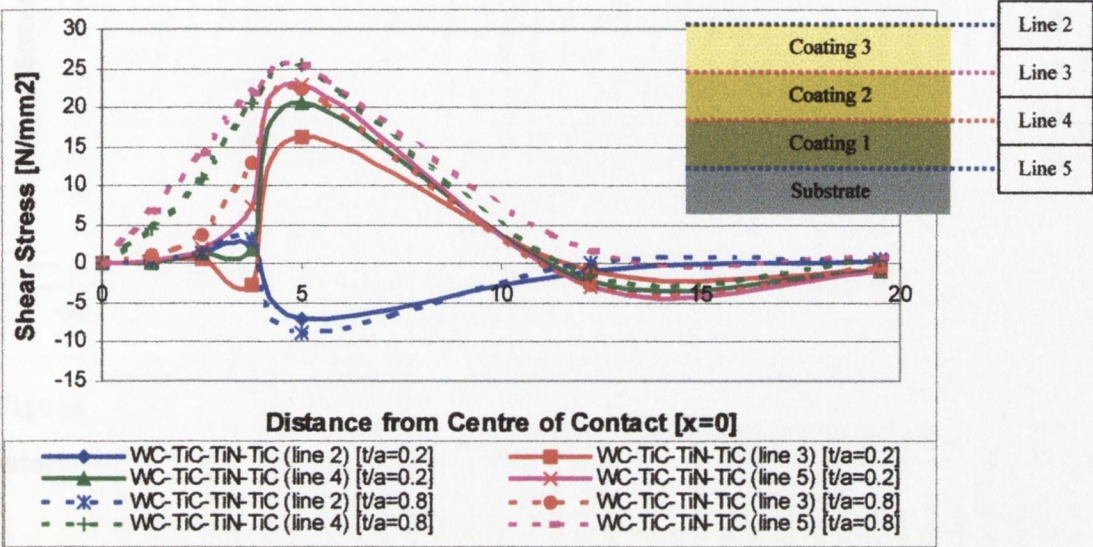


Figure app3.1. Variation of τ_{xy} along multi-coated WC-TiC-TiN-TiC substrate interfaces

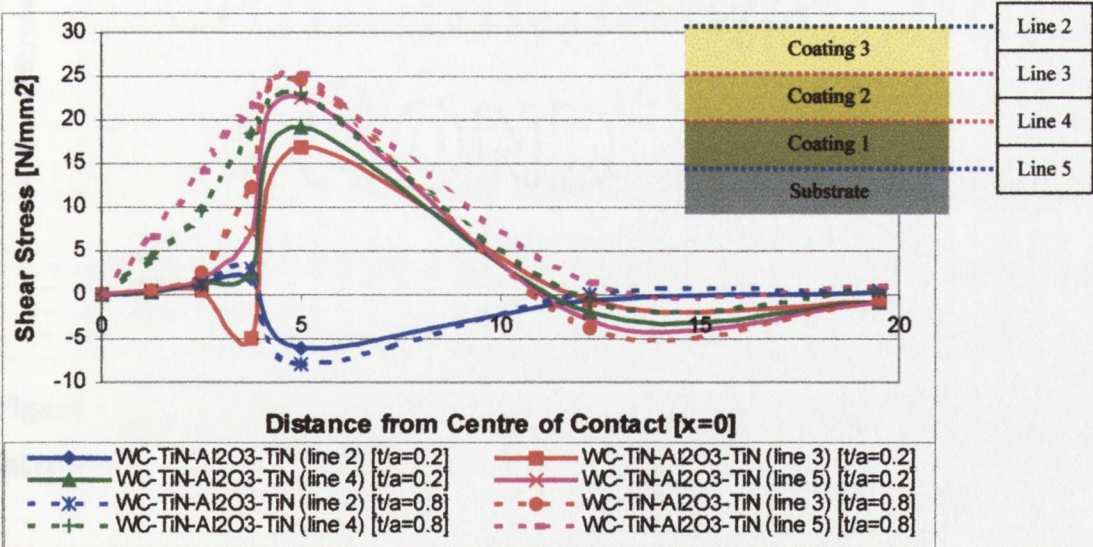


Figure app3.2. Variation of τ_{xy} along multi-coated WC-TiN-Al₂O₃-TiN substrate interfaces

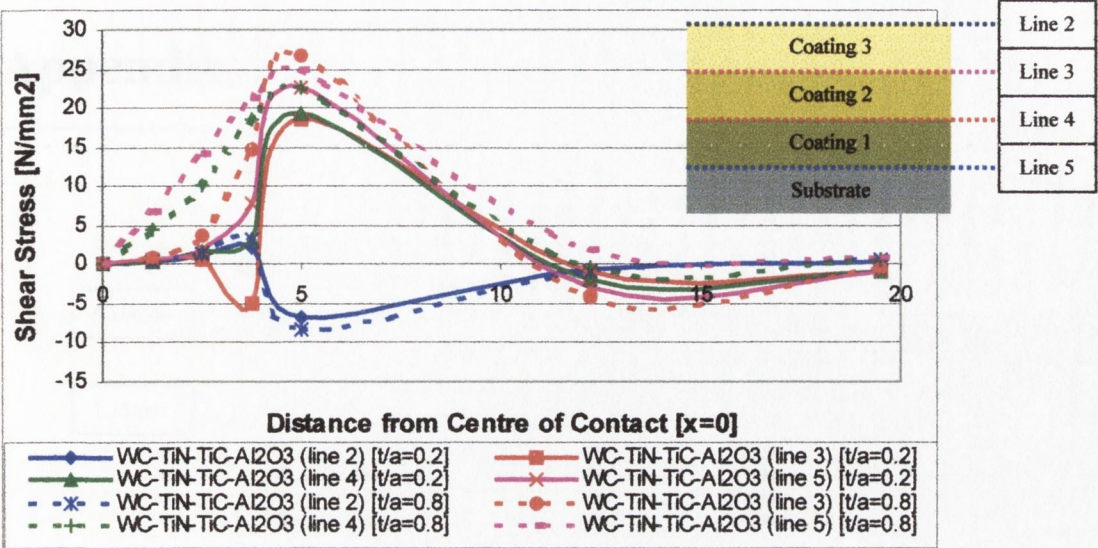


Figure app3.3. Variation of τ_{xy} along multi-coated WC-TiN-TiC-Al₂O₃ substrate interfaces

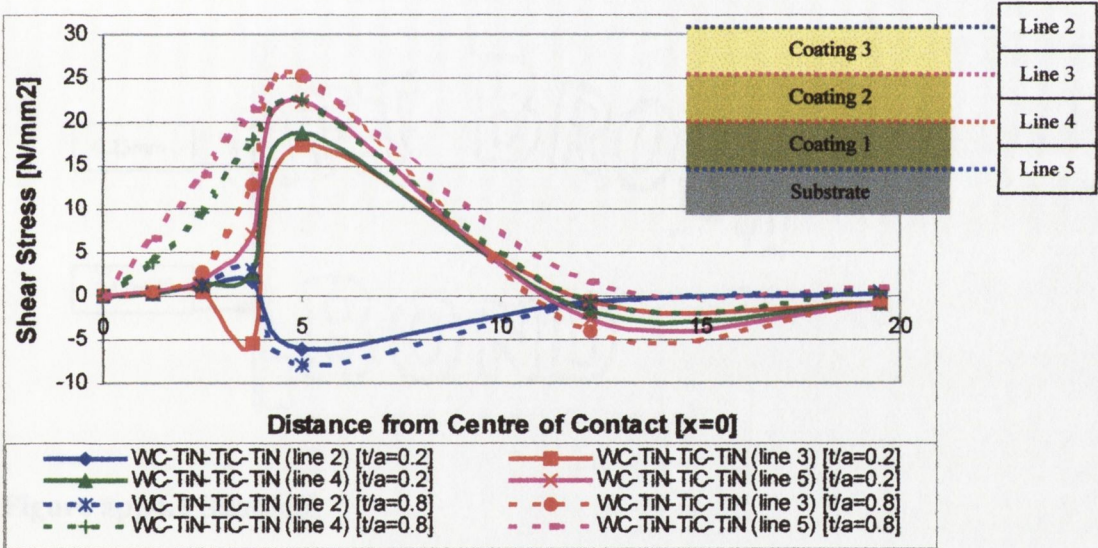


Figure app3.4. Variation of τ_{xy} along multi-coated WC-TiN-TiC-TiN substrate interfaces

Appendix 4 - Chipbreaking Geometries

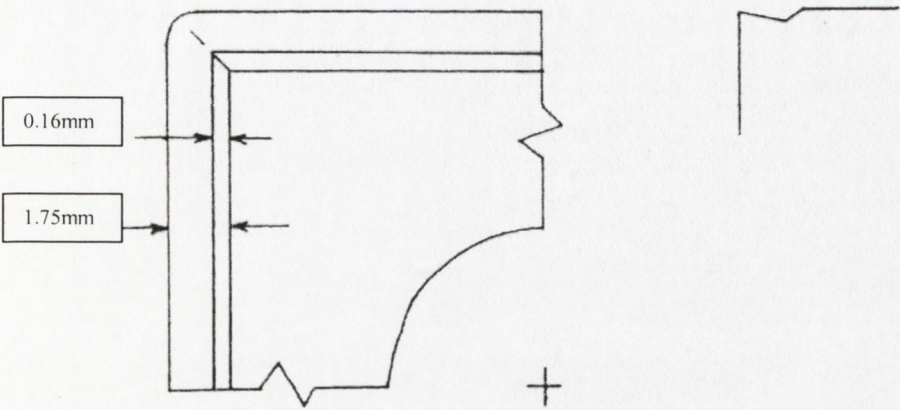


Figure app4.1 Sandvik SNMG-23

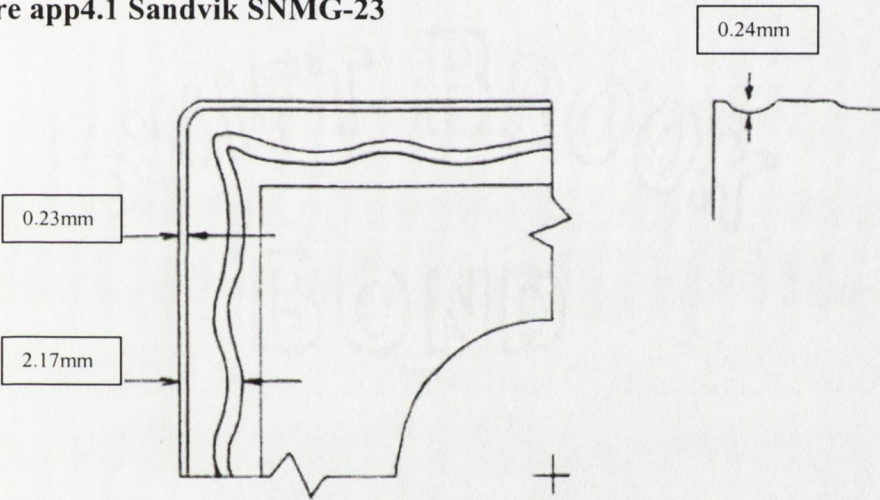


Figure app4.2 Sandvik SNMG-QM

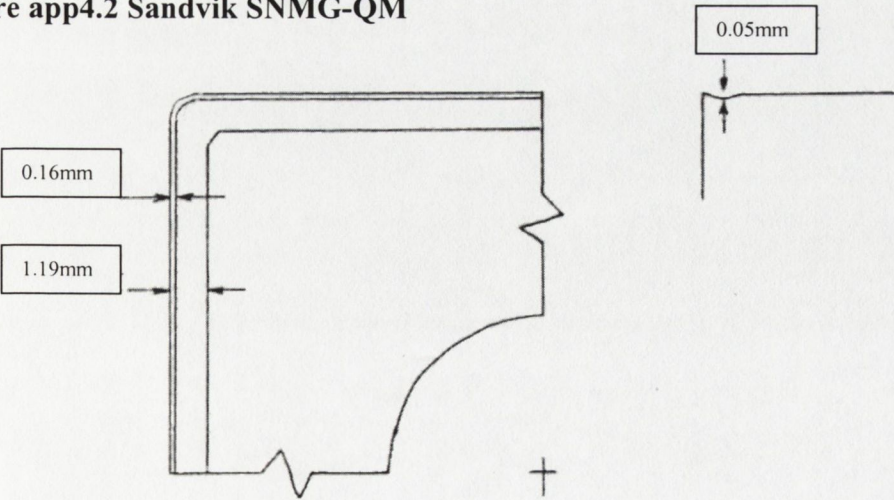


Figure app4.3 Sandvik SNMG-15