



Dissertation aus dem Werkzeugmaschinenlabor

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**Modeling Metal Cutting Processes
Under Consideration of Elastic
Material Properties**

Modeling Metal Cutting Processes Under Consideration of Elastic Material Properties

Von der Fakultät für Maschinenwesen der
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Zusammenfassung

Die Modellierung von Zerspanprozessen beschränkt sich meist auf die Abbildung des Werkstückwerkstoffes mithilfe einer starr-plastischen oder, bei Berücksichtigung der Dehnratenabhängigkeit der Fließkurve, einer starr-viskoplastischen Formulierung. Der Einfluss des elastischen Werkstoffverhaltens wird dabei vernachlässigt. Weiterhin wird die Geometrie der Schneidkante meist vernachlässigt und stattdessen eine ideal scharfe Schneide angenommen. Ein wichtiger Grund für diese Vereinfachungen sind die daraus resultierenden stark reduzierten Rechenzeiten.

Im Rahmen der vorliegenden Arbeit wurde untersucht, inwiefern sich die Berücksichtigung der elastischen Werkstoffeigenschaften von TiAl6V4 auf die zweidimensionale Simulation der orthogonalen Drehbearbeitung dieses Prozesses auswirkt. Die Simulationsergebnisse werden anhand experimentell ermittelter Prozesskräfte, Temperaturen und der in der Werkstückrandzone verbleibenden Eigenspannungen bewertet.

Um die für die Bewertung notwendige experimentelle Datenbasis zu erzeugen, wurde ein neuartiger orthogonaler Drehprozess an einem helixförmigen Steg entwickelt. Der Vorteil des neu vorgestellten Prozesses im Vergleich zu bekannten Versuchsanordnungen liegt in der Möglichkeit der eindeutigen Zuordnung der Prozessparameter Schnittgeschwindigkeit und Vorschub zu jedem Punkt der bearbeiteten Oberfläche.

Die eingesetzten Versuchswerkzeuge wurden mit definierten Schneidkantenverrundungen versehen, sodass diese als Eingangsgröße für die Simulation bekannt waren. Während der Versuche wurden auftretende Prozesskräfte mithilfe einer Kraftmessplattform sowie Prozesstemperaturen mithilfe eines Zweifarbenpyrometers erfasst. Im Anschluss an die Zerspanversuche erfolgte die röntgenografische Bestimmung der in der Werkstückrandzone vorliegenden Eigenspannungen.

Die Modellierung des orthogonalen Drehprozesses erfolgte mittels des kommerziellen FE-Codes DEFORM 2D, Version 8.1, der Firma SFTC.

Die Simulationsergebnisse zeigen, dass das Modell eine gute Sensitivität hinsichtlich der unterschiedlichen Schneidkantenradien aufweist. Die Berücksichtigung der

elastischen Werkstoffeigenschaften hat keinen signifikanten Einfluss auf die berechneten Prozesskräfte, die Formulierung beeinflusst jedoch die Kontaktzonenlänge sowie die Umformgradverteilung in Werkstück und Span.

Die Einführung von Scherreibung in das elasto-viskoplastische Modell führt zu Fluktuationen der Schnittkraft, die als Vorstufe einer Segmentierung interpretiert werden können. Die Schnittkräfte werden im Vergleich zum Experiment über- und die Vorschubkräfte unterschätzt.

Die berechneten Eigenspannungen zeigen eine deutliche Abhängigkeit von den im Modell zugrunde gelegten Prozessparametern und weisen der Literatur entsprechende Trends auf. Die berechneten Absolutwerte der Spannungskomponenten weichen von den experimentell ermittelten Daten ab.

Die Diskussion der Ergebnisse zeigt auf, welche weiteren Schritte sowohl im Bereich der Materialbeschreibung als auch im Bereich der FEM Simulation erforderlich sind, um die Qualität der Simulationsergebnisse weiter zu verbessern.

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List of Symbols

Symbol	Unit	Description
A	m^2	apparent contact area
a_p	mm	workpiece width
A_r	m^2	real contact area
c	m/s	speed of sound
C_0	m/s	longitudinal wave velocity
c_p	J/kg·K	specific heat capacity
d	Å	atomic spacing
d_{FWHM}	μm	penetration depth based on full width of half maximum
d_ε	μm	penetration depth based on effective plastic strain
E	GPa	modulus of elasticity
f	mm	feed
F_c	N	cutting force
F_f	N	feed force
F_t	N	thrust force
h	mm	uncut chip thickness
k	N/mm^2	local shear flow stress
k_f	N/mm^2	local flow stress
l	mm	workpiece length
L_0	m	length of the specimen
m	-	shear factor
r_β	μm	cutting edge radius
t	s	time
t	mm	thickness
v_c	m/s	workpiece velocity
v_{ch}	m/s	chip velocity
z	mm	depth
ϑ	°C	temperature

Symbol	Unit	Description
Θ_0	°	Bragg angle
α	10^{-6}K^{-1}	coefficient of thermal expansion
α_k	°	pitch angle
α_0	°	clearance angle
β	°	angle of principal stresses
ε	-	effective plastic strain
ε	-	emissivity of the workpiece material
$\varepsilon_{1,2,3}$	N/mm^2	principal strains
ε_i	-	incident pulse
ε_r	-	reflected pulse
ε_t	-	transmitted pulse
$\varepsilon_x, \varepsilon_y$	-	plastic strain components
ε_n^g	-	lattice strain
$\dot{\varepsilon}$	s^{-1}	effective plastic strain rate
ϕ	°	shear angle
γ	°	rake angle
η	$\text{MPa} \cdot \text{s}$	linear damping coefficient
λ	nm	wavelength
λ	W/mK	thermal conductivity of the workpiece material
μ	-	Coulomb friction coefficient
ν	-	Poisson ratio
θ	°	angle
ρ	Mg/m^3	density
σ	N/mm^2	stress
$\sigma_{1,2,3}$	N/mm^2	principal stresses
$\sigma^{I,II,III}$	N/mm^2	residual stresses of first, second and third type
σ_n	N/mm^2	normal stress

Symbol	Unit	Description
$\sigma_{x,z}$	N/mm ²	residual stresses
τ_f	N/mm ²	shear stress (surface traction)
ω	s ⁻¹	angular velocity
ψ	°	angle
ψ	°C	temperature function

1 Introduction and Objective

Metal cutting processes represent the most common metal shaping operations followed by metal forming processes. A good indicator for the industrial significance of metal cutting processes is the value of the produced metal cutting machine tools as shown in **Figure 1-1**. In [VDW2005] current trends for the machine tool industry are identified. Among these trends, the development of useable modeling tools to support machine tool design, as well as process design is considered to be important in order to promote innovation.

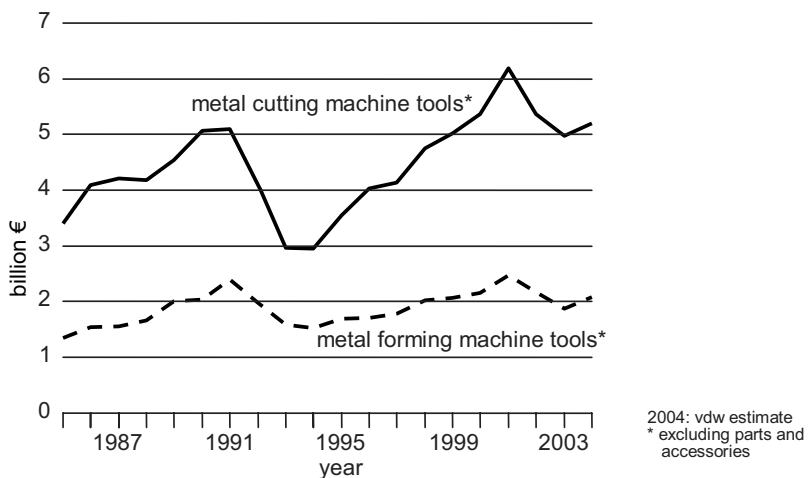


Figure 1-1: German production of metal cutting and forming machine tools excluding parts and accessories [VDW2005]

Researchers have been and still are investigating modeling methods that allow the optimization of metal machining processes. A large number of analytical models for chip formation has been developed with the objective to provide methods for the prediction of tool and workpiece behavior during the machining process as well as the process result.

The prediction of the process results provides the potential to speed up the layout of new machining processes or, respectively, to react to process changes due to new cutting tool geometries or new requirements toward the manufactured component.

The Finite Element Method (FEM) has proven to be a very powerful tool for the analysis of manufacturing processes [KLOCKE2002a]. On the macroscopic level, the behavior of the machine tool, the clamping system and the workpiece are subject to intensive research.

With respect to chip formation, the advantage of the Finite Element Method compared to analytical models originates from its capability to take into account the fundamental physical processes that govern a machining process. This allows an increase in the level of detail of the machining model. However, the application of the FEM to a machining process requires a significant amount of input data that is difficult to obtain and to some extent requires the extrapolation of acquired data [CHILDS1998].

For the previously mentioned reasons, analytical machining models are outside the scope of this work. An introduction into a variety of analytical models and also to finite element (FE) based approaches for the analysis of chip formation processes is given in [CHILDS2000].

A large amount of work has been put into the FE based examination of metal cutting processes. The efforts in the area of the three dimensional simulation of chip formation in kinematically complex machining operations such as drilling and milling have dramatically increased within the last years with promising results for the prediction of material flow. However, from a physical point of view there are still open questions that are common to complex machining operations as well as kinematically simpler processes like the orthogonal cutting process.

The impact of the machining process on the workpiece is of particular interest with respect to a component's fatigue and corrosion behavior in a technical application. Factors that influence these properties are commonly summarized under the term "Surface Integrity".

Generally, the main motivation behind the implementation of elastic workpiece properties into a FE model for machining processes is the prediction of the post process surface integrity. However, instead of attempting a "top-down" approach by trying to provide a complete solution for the prediction of surface integrity and

particularly residual stresses after machining of Ti6Al4V, this work contributes to a “bottom-up” approach by firstly providing a solid basis of experimental data and secondly examining the impact of elastic workpiece properties on the FE based modeling process and its results. Experimental machining data are compared to the modeling results based on process forces, temperatures and the impact on the workpiece rim zone.

2 State of the Art

2.1 The Finite Element Method (FEM)

In order to provide the terminology used throughout this work, the basic concepts of the Finite Element Method are outlined in this chapter. A detailed introduction into the Finite Element theory can be found in [BATHE1996] and [ZIENKIEWICZ2000].

By definition, the Finite Element Method is a method for the approximate solution of so called field problems. Field problems are mathematical problems which allow the description of a continuum's behavior by means of partial differential equations with a temporal and spatial dependence. Within a real body, the field variables, such as e.g. stress and temperature, are a function of every generic point. Consequently, mathematical problems of this kind contain an infinite number of unknowns [HUEBNER1982].

The FEM is a well established method for the computational analysis of problems that belong to the previously described category, particularly in mechanics, fluid dynamics and thermodynamics. In manufacturing, especially in the area of metal forming, the FEM has proven to be an indispensable tool in research as well as industrial applications. Despite the wide range of applications for the Finite Element Method, this chapter focuses on the concepts relevant to large deformation as it occurs in metal forming and metal cutting processes.

The basic principle of the FEM is the division of the continuum into a finite number of sub domains, the so called finite elements. Thereby, the initially complex, continuous problem is divided into a finite number of discrete and interdependent problems.

The major development of the FEM started in the late 1950s, mainly driven by activities in the field of structural mechanics. However, many of the fundamental principles were developed earlier. They originate from civil engineering and applied mathematics as well as from other disciplines. The term "Finite Element Method" was first used by [CLOUGH1960]. A more detailed description of the history of the Finite Element Method can be found in [KNOTHE1992] and [HUEBNER1982].

2.1.1 Basic Concepts of the FEM

The application of the FEM to a certain problem is also referred to as Finite Element Analysis (FEA). Generally, a Finite Element Analysis incorporates the following steps [HUEBNER1982; REDDY1993; ZIENKIEWICZ2000]:

1. *Discretization of the continuum.* During the discretization of the continuum, the domain of the solution (e.g. the workpiece in a manufacturing process) is divided into a finite number of subdomains. These subdomains are called finite elements. The geometrical shape of the applied elements depends on the nature of the problem. **Figure 2-1** gives an overview of common element shapes.

geometry / type of shape function	one dimensional	two dimensional	three dimensional
1. rectangle / linear			
2. general quadrilateral / linear			
3. rectangle / quadratic			
4. curved quadrilateral / quadratic			
5. triangle / linear			
6. triangle / quadratic			
7. curved triangle / quadratic			

Figure 2-1: Overview of common element shapes, the corresponding nodal positions and the degree of the shape function [KNOTHE1992]

In **Figure 2-2** an example for the discretization of a geometry that results in a faceted Finite Element mesh is given. It can be seen that the discretization already introduces an error due to the deviation between the actual object boundary and the boundary of the finite element mesh.

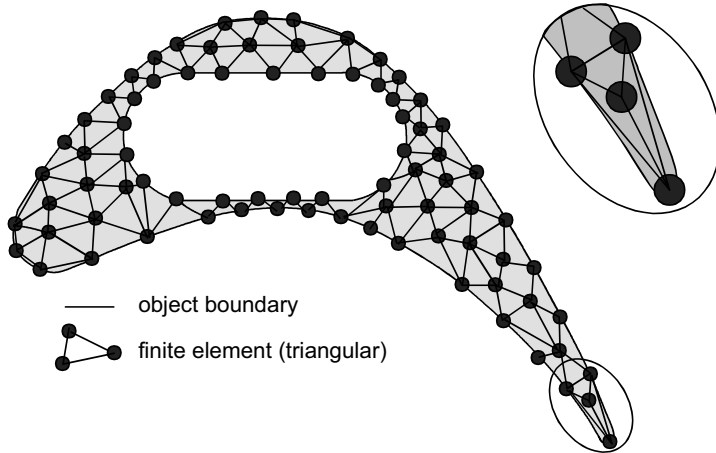


Figure 2-2: Example for a two-dimensional Finite Element mesh with linear triangular elements. Discretization of a turbine blade, [HUEBNER1982]

2. *Selection of interpolation functions.* Now that the geometrical properties of the element have been determined, nodes and interpolation functions have to be assigned to the elements. The interpolation functions determine how the field variables are interpolated within each single element. Usually, polynomials are used to perform this task and the nodes are used as pivots of the respective polynomial. Depending on the selected element type, the degree of the polynomial that is utilized for interpolation purposes can vary (Figure 2-1).
3. *Determination of the element properties.* Subsequent to the discretization of the domain and the selection of the interpolation functions, the element properties have to be determined. They describe the elements' behavior from a physical point of view. In other words, the element properties represent the link between the primary unknowns of the system (e.g. displacements) and the secondary unknowns (e.g. stresses). In order to obtain the element properties, different approaches can be taken. The most common one is the variational approach in which a functional is extremized. The minimization of the potential energy is a common approach.

4. *Assembly of the element equations.* In order to find the properties of the complete system represented by the elements, the matrix equations of the elements are combined to form the matrix equations of the system. The assembly procedure relies on the fact that the state variables at the common nodes of adjacent elements have the same value.
5. *Solution of the system equations.* The set of simultaneous equations resulting from the assembly process is solved numerically in order to obtain the field variables. This process involves the application of numerical integration procedures which require the evaluation of integrals at discrete points within an element, the so-called integration points. A common numerical integration scheme is the Gaussian quadrature [ZIENKIEWICZ2000]. The state variables are calculated at the integration points and interpolated to other locations within the element as required.

As mentioned before, the basic steps of a Finite Element Analysis are always the same. However, based on certain assumptions that are implied within the FE code, software packages can be divided into different categories as described in the following paragraphs.

2.1.2 Lagrangian and Eulerian View of the Continuum

The discretization of the continuum can be based on various approaches. The most common views of the continuum are the Lagrangian and the Eulerian, [BATHE1996].

The Lagrangian view assumes that the nodes of an element move with the continuum. Traveling on a node, a spectator would observe the changes of one particle's state variables during the examined physical process. A disadvantage of the Lagrangian approach is the distortion of the Finite Element mesh during large displacements within the continuum. Large plastic deformations of the continuum as they occur in metal forming and metal cutting, therefore require an appropriately frequent regeneration of the Finite Element mesh. This procedure is called remeshing. Subsequent to the generation of a new mesh, a complete remeshing cycle requires the interpolation of all state variables from the old mesh to the newly generated mesh. Depending on the number of remeshing cycles that are performed, this procedure introduces a more or less significant error to the simulation which

occurs due to the smoothening of the state variables.

The Eulerian view assumes that the particles of the continuum move through a fixed mesh. A spectator on a node of an Eulerian mesh would observe the states of all particles that pass his position. This approach is particularly suitable for the analysis of stationary processes and is frequently applied in the area of fluid mechanics.

The ALE ("Arbitrary Lagrangian Eulerian") approach is becoming of increasing interest for large deformation problems and is emerging as an alternative to the two previously mentioned approaches. This approach represents a combination of both, the Lagrangian and the Eulerian approach and neither requires the mesh to adhere to the material, nor does the mesh need to be fixed in space. Independently from the material movement, the mesh is continuously moved in order to maintain an optimal element shape [KOBAYASHI1989; WU2003; BAYOUMI2004]. While the ALE approach is promising, it is still subject to development within commercial codes.

A Lagrangian approach has been chosen for the machining simulations performed within this work. With reference to machining simulations, the advantage of the Lagrangian approach compared to the Eulerian approach is that the chip geometry is a result of the simulation and it is not necessary to assume an initial chip geometry.

2.1.3 Explicit and Implicit Time Integration

Many FE codes that are designed to handle large plastic deformation utilize a so-called implicit time integration scheme, whereas codes that are tailored towards highly dynamical processes use explicit integration schemes.

Explicit integration schemes treat the examined physical process as a dynamical problem which can be divided into time steps. The unknowns at the time $t+\Delta t$ are calculated from the values available at the time t . In most cases, the central difference scheme is used for that purpose. This scheme is stable, if the time step Δt is smaller than the time an elastic wave that propagates through the material needs to travel a distance that correspond to the smallest element edge length in the model. In other words, the largest possible time step depends on the speed of sound within the respective material. For solids the speed of sound can be calculated by:

$$c = \sqrt{\frac{E}{\rho}}, \quad (2.1)$$

Consequently, the maximum time step that is possible in the calculation depends on the modulus of elasticity E and the density ρ of the material. The resulting time step can be in the range of microseconds which can lead to a large number of calculation steps. By artificially increasing the material's density, which is often referred to as "mass-scaling", or artificially reducing process time, the user can increase the possible time step size and thereby reduce computation time. Mass effects caused by these manipulations have to be properly compensated in order to maintain the validity of the simulation results [ROLL1993; CHUNG1998].

Implicit schemes are not subjected to the limitations mentioned above. Implicit solvers are designed to determine the solution at the time $t+\Delta t$ considering the values of the unknown quantity at the time t and at the time $t+\Delta t$ [HUEBNER1982]. This requires the solution of a non-linear system of equations by means of iterative methods such as the Newton-Raphson algorithm. Compared to the explicit schemes, the time step can be increased by a factor of up to 1000. However, the iterative solution of the non-linear system of equations is time consuming [ROLL1993; ZIENKIEWICZ2000] and all mass effects are neglected.

2.1.4 Thermo-Mechanical Coupling

Machining processes involve large deformations at high rates. The dissipation of plastic and frictional work within real machining processes leads to a change of the temperature field within tool and workpiece. Mechanical and thermal calculations have to be coupled in order to account for thermal processes during a large deformation analysis. In metal forming and metal cutting this allows not only to calculate tool and workpiece temperatures, but also to take the temperature dependence of the material properties into account.

The thermal and the mechanical calculations can either be performed simultaneously or they can be conducted in a staggered approach. A simultaneous calculation requires the solution of a fully coupled system of equations for both, the mechanical and the thermal processes. The more common staggered approach relies on a

purely mechanical calculation which is followed by a thermal calculation that allows the determination of the material properties under the respective conditions. These material properties represent the input for the next mechanical calculation step.

2.1.5 Non-Linearities

Generally, those problems involving a linear dependence between the applied loads and the system response are referred to as linear problems. From a physical point of view, however, every real system is non-linear. In many cases these non-linearities can be neglected. The analysis of large deformation processes like metal cutting and metal forming, however, does not allow to neglect the non-linearities.

Non-linearities can be divided into three categories:

- Material non-linearities,
- Geometric non-linearities and
- Boundary non-linearities.

The term “material non-linearities” refers to problems with a non linear stress-strain relationship as it can be observed when, for example, the elastic limit of a material is exceeded and the material deforms plastically.

Geometric non-linearities occur in large deformation problems. A simple example is the large deformation of a beam during which the change of the beam’s geometry due to large displacements gains significant influence on the further deformation of the beam.

Boundary non-linearities can be observed in problems that involve changes of the contact situation between two objects or changes of the external loading conditions.

Manufacturing processes like metal forming and metal machining usually involve all three types of non-linearities. Among other features, the FEM’s ability to handle non-linearities qualifies the method for the analysis of these processes.

2.1.6 Solid Formulation and Flow Formulation

Depending on the objective of a FE analysis, two different types of FE formulation can be distinguished, **Figure 2-3**. The first type considers the material elastic until

the elastic limit is exceeded; subsequently, the material is considered plastic. This “solid formulation” is also frequently referred to as “elastoplastic formulation”. The second type of formulation considers the material rigid until plastic deformation begins. Usually, this formulation is referred to as the “flow-formulation”, or “rigid-plastic formulation” [KOBAYASHI1989; WU2003].

The flow formulation is used to reduce computation time if the amount of plastic deformation outweighs the amount of elastic deformation significantly. However, if elastic effects such as springback and residual stresses are of interest, the solid formulation has to be used, [ROLL1993; WU2003].

Strain rate dependence as captured in the viscoplastic or, respectively, the elastic-viscoplastic formulation is important to take into account for processes that are conducted at elevated temperatures or, respectively, processes that involve high strain rates, such as machining processes.

Within this work, an elasto-viscoplastic formulation is used to model the machining process.

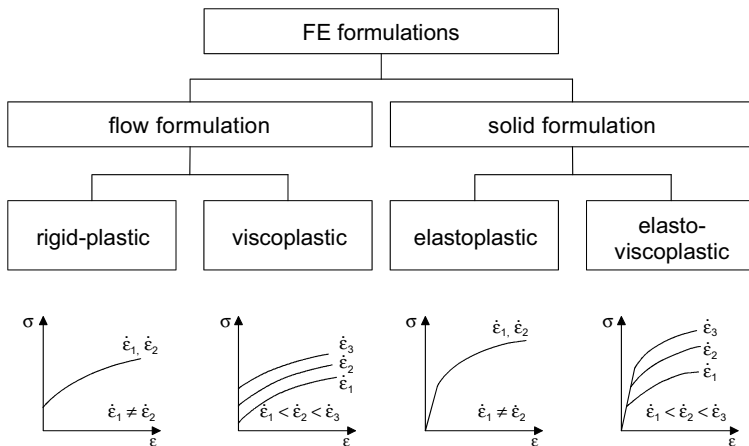


Figure 2-3: FE formulations for large strain problems; based on [ROLL1993]

2.2 The FEM in Manufacturing

As stated previously, the Finite Element Method is a universal tool for a large variety of applications. Topics such as metal forming, metal cutting with defined and undefined cutting edge, electro-erosion and tribology have been and are being examined by means of this tool [KLOCKE2002a]. Due to the increase in computational power and the availability of advanced software tools, the simulation of manufacturing chains and consequently the information exchange between different simulation tools also are of increasing interest [STRAUBE2004].

The following chapters give an overview over the application of the FEM in metal forming and in machining with a geometrically defined cutting edge. Due to the focus of this work, the latter will be presented more detailed.

2.2.1 The FEM in Metal Forming

Metal forming can be considered the first field for the application of the FEM in manufacturing. In the meantime the FEM has evolved into an indispensable tool throughout the metal forming industry that helps the design engineer to reduce development time and cost. Due to its capability to describe complex shapes, material behavior and boundary conditions a variety of commercial software packages has been developed. Typical applications involve die and mold design for hot and cold forging operations as well as for virtually all processes in sheet metal processing.

In the mid 1980s, the FEM began to be very successful for axisymmetric applications such as the forging of discs or the cold forging of cylindrical fasteners. These applications allowed the application of two-dimensional models assuming plane strain conditions and consequently were the foundation to the FEM's industrial success. Nowadays, parts can be modeled in three dimensions including multiple forming stages, heating and cooling cycles, elastic tools and complex material behavior (**Figure 2-4**). Modern codes offer thermo-mechanical coupling so that the interaction between plastic deformation and temperature is taken into account.

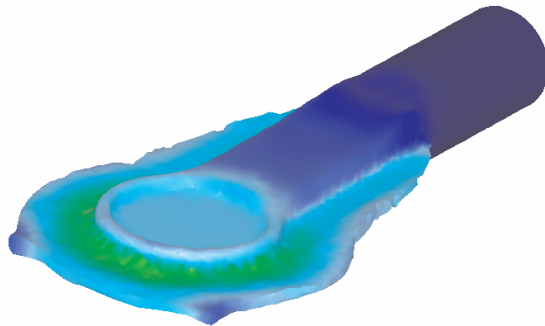


Figure 2-4: Forging simulation of an automotive part (effective strain distribution)

FE codes that are designed for metal forming applications are able to cope with highly non-linear problems of continuum mechanics. The non-linearities in metal forming arise from large deformations, non-linear material behavior and non-linear boundary conditions such as frictional contact.

Despite the fact that modern FE packages can handle elasto-viscoplastic material behavior, the workpiece in the majority of all forming simulations is considered rigid-plastic or rigid-viscoplastic in order to increase computational efficiency. This approach is sufficient as long as elastic springback or residual stresses of a component are not in the focus of interest [WU2003].

The most commonly implemented formulation in FE packages aimed at metal forming is the Updated Lagrangian (UL) formulation, which uses the latest (updated) configuration of the continuum (i.e. the workpiece) as a reference configuration for the further calculation.

Generally, the material properties required for forming simulations can be determined experimentally. Among other properties like thermo-physical material properties, the flow stress significantly influences the quality of a simulation.

The flow stress description, also referred to as the material description, constitutes the behavior of the material flow stress as a function of effective plastic strain ε , effective plastic strain rate $\dot{\varepsilon}$ and temperature T . In metal forming, the description of the flow stress as a function of these variables is commonly used, and in most cases

the required data can easily be obtained experimentally. This can for instance be done by means of standard compression tests. For many materials the required data has been determined and is available in literature [DOEGE1986; BLECK1999].

Typical strains and strain rates as they occur in common metal forming processes are listed in **Table 2-1**.

Process	Strain [-]	Strain rate [s^{-1}]
Extrusion	2-5	$10^{-1} - 10^2$
Forging / rolling	0.1-0.5	$10^0 - 10^3$
Sheet metal forming	0.1-0.5	$10^0 - 10^2$

Table 2-1: Overview over effective plastic strains and strain rates for common forming processes [ALEXANDER1985; KALPAKJIAN1997]

2.2.2 The FEM in Metal Cutting

A comprehensive bibliography concerning the research activities in the area of FE simulation of machining processes for the years 1976-2002 is given in [MACKERLE1998] and [MACKERLE2003]. The following summary represents a brief description of the developments in metal cutting simulation while **Figure 2-5** gives a graphical overview over the research activities based on the number of publications.

Soon after the Finite Element Method emerged, researchers started to utilize this method for the analysis of machining processes. [ZIENKIEWICZ1971] showed the FEM's capability to calculate the plastic deformation of the workpiece material in the primary shear zone using a simplified small strain model. This model assumes a pre-shaped chip and does not take into account friction and the rate or, respectively, temperature dependency of the material behavior. A few years later, [SHIRAKASHI1976] presented an improved model that takes into account friction at the tool workpiece interface as well as the variation of the material behavior with strain, strain rate and temperature. This model also starts out with a pre shaped chip, however, the chip shape is changed iteratively until it agrees with the calculated plastic flow. This so-called Iterative Convergence Method (ICM) has since been further developed, [CHILDS2000]. [USUI1982] developed a metal cutting model that relies on a rate independent material behavior and a geometrical separation criterion at the tool tip. [IWATA1984] used an Eulerian approach and provided a steady state

model with rigid-plastic material behavior. This model also includes friction and strain hardening effects. [STRENKOWSKI1985] developed a non-steady state elasto-plastic Lagrangian model using the commercial general purpose code NIKE2D. The model captures the chip formation process beginning with the first workpiece contact. Material separation is achieved by means of a maximum strain criterion. Variation of flow stress due to the process conditions as well as thermo-mechanical coupling are not included.

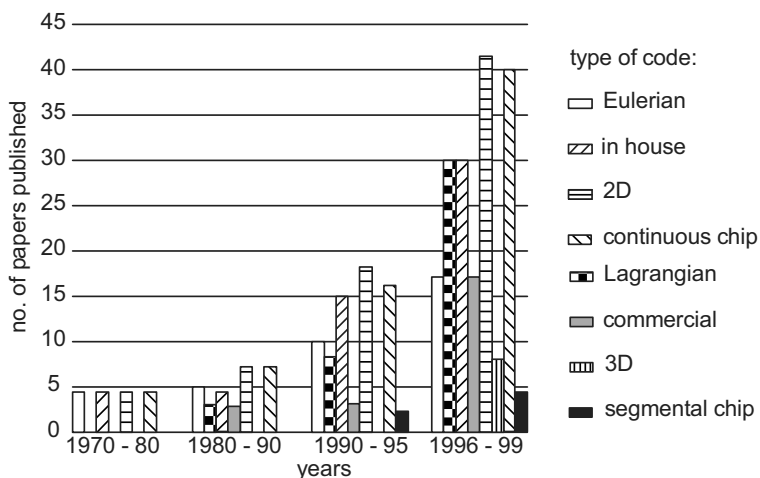


Figure 2-5: Trends in modeling chip formation processes [NG2001]

Subsequent to the early phase during which researchers had to struggle with severe restrictions concerning the capabilities of the FE codes as well as the available computational power, research activities started to become more oriented towards practical applications. A series of papers by [SHIH1993] and [SHIH1993; SHIH1995; SHIH1996a; SHIH1996b] focused on Finite Element modeling aspects as well as experimental verification including the analysis of residual stresses induced by the machining process. [OBIKAWA1997] introduced a model for discontinuous chip formation in machining of brass. Separation of the elasto-plastic material is induced by a combination of a geometrical criterion and an empirical failure criterion. The increase in computing power in the beginning of the 1990s resulted in an increase in

research activities (Figure 2-5) and promoted the expansion of the two dimensional analysis to three dimensions. An early example is given by the work of [MAEKAWA1993]. As Figure 2-5 shows, the late 1990's saw an increase in the use of commercially available FE codes due to significantly increased capabilities of these codes. [CERETTI1996], [SHET2000] and [KLOCKE2000; KLOCKE2001a; KLOCKE2001b; KLOCKE2002b; KLOCKE2002d] analyzed orthogonal metal cutting processes using commercial metal forming or, respectively, general purpose codes. In [KLOCKE2002c], an overview over FE applications in metal forming and metal cutting is given, including a three dimensional model for the chip formation in drilling. The simulation of chip formation in high speed cutting (HSC) processes was focus of the works of [PLÖGER2002; WESTHOFF2002] while [HOPPE2003] examined the chip formation numerically and experimentally at conventional and at high cutting velocities. The transition from two to three dimensional models and the application of the FEM to milling processes is focus of the work by [SÖHNER2003]. FEM based tool wear prediction in machining has been the subject of recent works using the calculated state variables as input data for analytical tool wear models within the FEM code [XIE2004; YEN2004b]. [BÄKER2004] focuses on the chip formation in orthogonal high speed cutting of Ti6Al4V with an emphasis on the material behavior. [YEN2004a] examined the influence of the actual cutting edge geometry, which is neglected in many publications on the predicted process variables. According to [BARGE2005], numerical parameters such as mesh size mainly influence the chip morphology and have a less significant influence on the calculated forces and field variables.

2.2.2.1 Elasto-Plastic FE Models

Most approaches to include elastic workpiece properties in a modeling approach aim at the prediction of surface integrity or, respectively, subsurface residual stresses due to machining. A major share of these works are based on empirical or analytical approaches [LIU1976a; LIU1976b; MATSUMOTO1999; JACOBUS2000]. [CHILDS1980] stresses the significance of elastic effects in chip formation and suggests an approach by means of the slip line field analysis. Only few numerical models take into account elastic material properties which have an impact on surface

integrity. Early approaches to model residual stresses utilize experimentally determined cutting forces and temperatures which were applied as external loads, without considering chip formation [LAJCZOK1980; NATARAJAN1983].

[STRENKOWSKI1985] used an effective plastic strain criterion to implement chip separation and the criterion was the main influencing factor on the residual strain and residual stress formation. According to the FEM model presented by [OBIKAWA1997], continuous chip formation produces larger residual stresses than in discontinuous chip formation. However, the effects of strain rate, temperature and the disengagement of the cutting tool were neglected. [LIU2000] examined the effect of sequential cuts on a machined surface and found that their model showed an influence of previous cuts on the residual stresses in the machined surface. However, the results were not compared to experimental data. The impact of elastic material properties on other aspects of machining such as process forces is mostly neglected. [SHET2003] examines the formation of residual stresses and strains in orthogonal cutting using ABAQUS. He uses a stress based chip separation criterion and a pre-distorted mesh. An approach for the simulation of the process sequence casting-machining including an elasto-plastic machining model as well as methods for the required data transfer between different software packages were presented by [MESSNER2003].

Figure 2-6 shows a three dimensional elasto-plastic model for a milling process of the wrought aluminum alloy AA7075.

The shown model represents a three dimensional elasto-plastic model that was set up with the objective to generate a state of stress that was to be passed on to the next link in the process sequence. A verification of the calculated stresses did not take place.

While the calculation of material flow for complex processes such as drilling or milling delivers promising results, residual stresses due to machining have not been thoroughly investigated yet by means of three dimensional models.

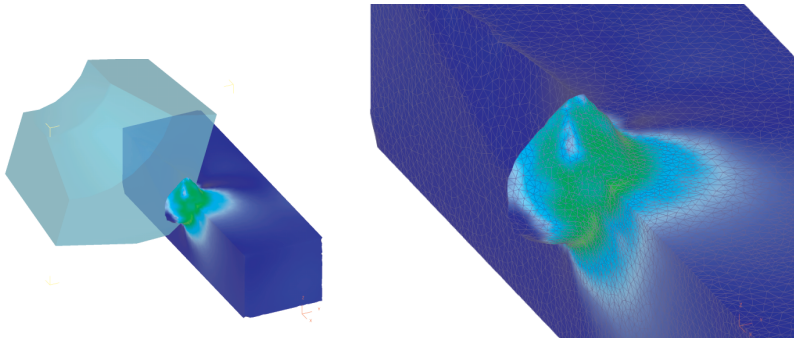


Figure 2-6: Three-dimensional elasto-plastic model of a milling process [MESSNER2003]

The reason for this is that three dimensional models introduce new challenges concerning numerical methods as well as computational power. The object representing the workpiece shown in Figure 2-6 contains more than 85,000 linear tetrahedral elements with a resulting computation time of several days. Therefore, the approach of this work is based on two dimensional models.

2.2.2.2 *Approaches to Model Chip Formation*

Chip formation in a machining process can be modeled in three different ways [VAZ2000]. The first option is to assume a line of separation along the projected cutting path and then to introduce a “crack” by separating the pre-defined nodes on the cutting path if a certain geometrical or a physical criterion is fulfilled. [USUI1982] demonstrated the application of a purely geometrical criterion.

This approach has some disadvantages. The separation line has to be defined very accurately with respect to the tool tip. Even with an ideally sharp cutting edge, the movement of the split nodes has to be controlled in order to ensure that one node passes the cutting edge and the other node moves up along the rake face. In [BÄKER2004] this is achieved by means of two auxiliary surfaces which determine whether a node moves into the chip or becomes part of the mesh that represents the machines surface.

Another possibility is the application of a physical node splitting criterion which is for

instance based on fracture mechanics without prescribing a line of separation [ATKINS2003]. This criterion is applied to all nodes or, respectively, elements regardless of their position within the model. [ZHANG1999] examines various criteria and concludes that even if a criterion and its corresponding threshold value have been determined for a material under certain cutting parameters, the threshold value can change when varying the cutting parameters.

The third approach relies on pure plastic deformation and considers the machining process a “high-speed forming” process. In this case, chip formation is solely based on the material description. The implementation of a failure criterion to influence the chip mode is still possible as demonstrated in [CERETTI1999].

Since all known separation criteria for machining simulations are more or less arbitrarily selected [MADHAVAN2000], for the simulations performed in this work the pure deformation approach has been chosen.

2.2.2.3 *Friction in Metal Cutting Simulations*

The analytical description of friction in metal cutting has been subject to research for many years. For contacts where the real area A_r of contact is only a small fraction of the apparent area of contact A , the friction rules Amontons postulated yield reasonable results:

1. The friction coefficient is independent of the applied load.
2. The friction coefficient is independent of the apparent area of contact.

From a tribological point of view, however, in metal cutting extreme conditions occur. With increasing normal stress in the contact zone between tool and chip, the apparent area A increases (**Figure 2-7**) and can become equally large compared to the real area A_r . In this case, Amontons' rules do not capture the ratio of normal force and shear force [YANG2002].

Within the last three decades, a large variety of friction models for metal cutting has been developed [SHIRAKASHI1974; IWATA1984; ELDRIDGE1991; SEKHON1993; WU1996; OZEL2004]. Among these, the analytical approaches to describe friction conditions in machining are suitable to be implemented into a FE model.

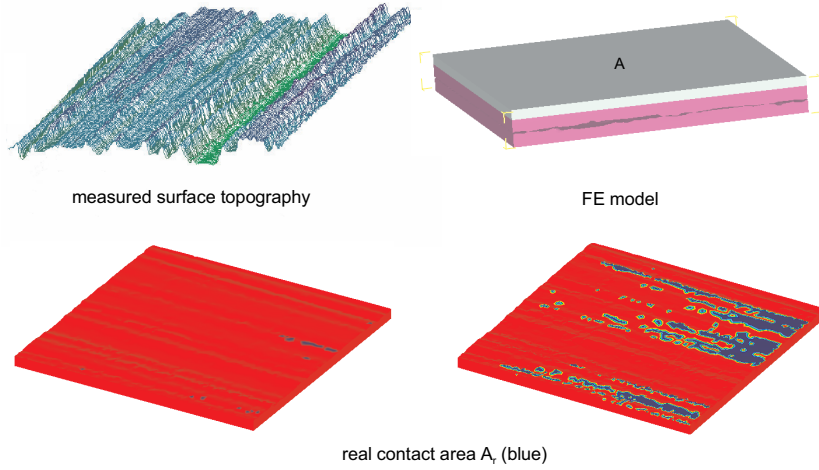


Figure 2-7: Apparent contact area A (0.01 mm^2) and real contact area A_r ; contact between two ground surfaces at nominal force of $0,1 \text{ N}$ (lower left) and $4,5 \text{ N}$ (lower right) [KLOCKE2004]

In many analytical and numerical models, the shear stress (traction) τ_f acting on the tool rake face is assumed to either follow the Coulomb law

$$\tau_f = \mu \cdot \sigma_n \quad (2.2)$$

with μ as the Coulomb friction coefficient and σ_n as the normal stress on the rake face, or the shear friction law

$$\tau_f = m \cdot k \quad (2.3)$$

with m as the constant shear factor and

$$k = \frac{k_f}{\sqrt{3}} \quad (2.4)$$

as the local shear flow stress of the workpiece material while k_f represents the local flow stress of the workpiece material.

[ZOREV1963] suggests a stress distribution in the chip contact zone on the tool rake face as shown in **Figure 2-8**. The normal stress shows a maximum at the cutting edge and then decreases exponentially until it reaches zero at the end of the contact zone (A-C).

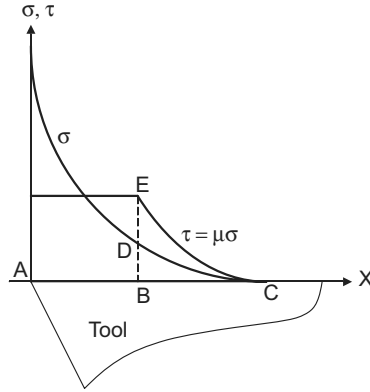


Figure 2-8: Normal and shear stress distribution along the rake face as suggested by [ZOREV1963]

The rake face is divided into two regions; the sticking and the sliding region. In the sticking region (A-B) the frictional shear stress (traction) is assumed to be equal to the local shear flow stress of the workpiece material so that equations (2.3) and (2.4) yield:

$$\tau_f = \frac{k_f}{\sqrt{3}}. \quad (2.5)$$

In the sliding region (B-C) within ZOREV's model, the shear stress obeys the Coulomb law with a coefficient of friction that is independent of the normal stress. The lengths of the sticking region and the sliding region have to be prescribed.

ZOREV's model as well as equations (2.2) and (2.3) have been extensively used in FE modeling of machining processes [SHIH1995; SHATLA1999].

Despite the amount of work that has been done on friction in metal cutting, uncertainty in the description of the friction conditions between tool and workpiece remains. This uncertainty is due to the lack of experimental methods that allow the

verification of the friction models. In order to do so, one has to determine the normal stress distribution and the shear stress distribution along the tool rake face or, respectively, the cutting edge. One approach to experimentally obtain stress distributions along the tool rake face in machining processes is the split tool method as described in [GORDON1967] and [CHILDS2000].

Despite the availability of advanced friction models for machining, most researchers have chosen to rely on a Coulomb law, equation (2.2) or, respectively, the shear friction law, equation (2.3). Two significant reasons for this approach are the dependency of the advanced laws on constants which are difficult to determine as well as the negative numerical impact of these friction models which may result in convergence issues.

2.2.2.4 *Properties of the Workpiece Material*

Flow Stress Description

From a physical point of view, machining of ductile metals can be considered a localized forming process, or respectively, a wedge indentation that takes place at high strain rates [TÖNSHOFF1995; MADHAVAN2000]. The workpiece material deforms plastically, and in the shear zone it experiences strain rates with a magnitude of between 10^4 s^{-1} during standard machining processes and 10^6 s^{-1} in high speed machining [HOPPE2003; BÄKER2004]. In contrast to most forming operations, in metal cutting, however, strongly localized plastic deformation occurs. The locally occurring strains and strain rates exceed the conditions that can experimentally be reproduced in a manner that allows the acquisition of strains, stresses and temperatures. In order to provide experimental flow stress data for the use in machining models, mainly two different approaches have been taken. One approach is the conduction of high speed compression tests, such as the Split-Hopkinson-Pressure-Bar test and the extrapolation of the acquired data [LEE1998; TREPPMANN2001; EL-MAGD2003]. A typical test set up is shown in **Figure 2-9**. The specimen is held between two straight bars, the incident bar and the transmitter bar and a furnace allows to heat the specimen up to the desired temperature. A third, shorter bar is pneumatically accelerated and its velocity is measured by sensors. The

striker bar hits the incident bar and induces a mechanical wave which propagates through the bar. The wave reaches the specimen and causes it to deform or to fracture while part of the wave is reflected back into the incident bar. The specimen response modifies the incident wave and the modified wave propagates through the transmitter bar.

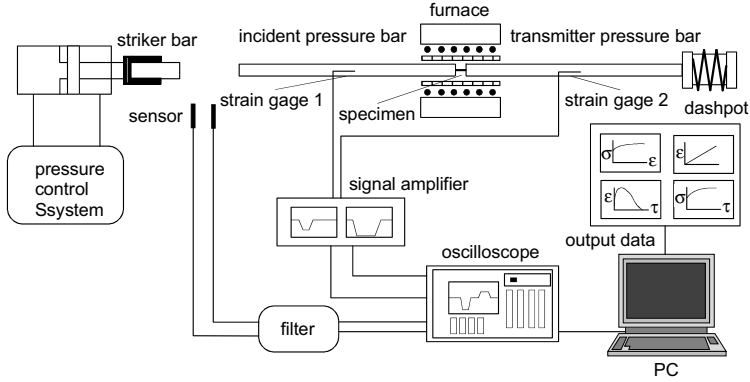


Figure 2-9: Split-Hopkinson-Pressure-Bar setup [LEE1998]

Based on the one-dimensional elastic wave propagation theory the recorded pulses ε_i , ε_r and ε_t can be utilized to determine the average strain ε , strain rate $\dot{\varepsilon}$ and stress σ within the specimen assuming a uniform deformation:

$$\varepsilon = \left(\frac{2C_0}{L_0} \right) \int \varepsilon_t dt, \quad (2.6)$$

$$\dot{\varepsilon} = \frac{2C_0 \varepsilon_r}{L_0} \quad \text{and} \quad (2.7)$$

$$\sigma = E \frac{A}{A_0} \varepsilon_t \quad (2.8)$$

with

ε_i , ε_r , ε_t : recorded incident, reflected and transmitted pulse,

C_0 : longitudinal wave velocity,

- L_0 : length of the specimen,
- E : Young's modulus of the split bar and
- A, A_0 : cross sectional area of the split bar and of the specimen.

The experimentally acquired data can then be described by means of constitutive equations [GUO2003].

The second approach for an experimental determination of flow stress data for machining simulations is an inverse analysis technique based on actual machining experiments as e.g. suggested by [SHATLA2001b; SHATLA2001a; SARTKULVANICH2004]. Rather than using the results of compression tests and extrapolating them by means of an empirical equation, this method assumes an analytical model, such as presented in [OXLEY1989], in order to predict process forces and temperatures. The process forces are compared to forces which have been acquired experimentally. The flow stress is assumed to follow a constitutive equation and the coefficients of the respective equation are changed until predicted and measured process forces agree within a specified error tolerance. Constitutive equations to describe the behavior of metals at high strains, strain rates and temperatures can be found in [SWIFT1952; JOHNSON1983; EL-MAGD2003].

When choosing the inverse method, material specific flow stress parameters have to be determined iteratively. In [SHATLA2001b; SHATLA2001a; SARTKULVANICH2004] this is done by minimizing the following errors:

- the error between predicted and experimental process forces,
- the error between the predicted and experimentally determined ratios of primary shear zone thickness and length and
- the error between the predicted and experimentally determined ratios of secondary shear zone thickness and cut chip thickness.

These minimization objectives are formulated as a multi-dimensional minimization problem which is solved by means of algorithms such as the Downhill-Simplex algorithm [PRESS1992].

The advantage of the inverse approach is that the material data is determined while the material is subjected to machining conditions. Consequently, no data extrapolation is required. However, all assumptions made in the analytical model potentially reduce the accuracy of the obtained flow stress curves. Another source of error is the experimental determination of primary and secondary shear zone thickness and the cut chip thickness as an input for analytical models.

In this work, material data originating from Split-Hopkinson-Bar tests has been used. Details on the material model that was chosen for the simulations conducted in this work are given in chapter 5.2.

Properties of the Titanium Alloy Ti6Al4V

Ti6Al4V is a titanium alloy which is common for many applications, mainly in the aeronautics, space and medical sector. With a share of 50 % among all produced titanium alloys, Ti6Al4V is the most widely used alloy [PETERS2002]. Since the 1950's, demand for titanium alloys has significantly increased. Their increased significance is mainly founded on these properties:

- Titanium alloys have a low density of about 4.5 g/cm^3 which leads to an almost 40 % weight reduction compared to a steel component with the same dimensions.
- The high specific strength of titanium alloys which, in combination with their low density, results in a breaking length of about 20 km compared to a breaking length of 6 km for a construction steel [SÄGLITZ1994].
- The excellent corrosion resistance when exposed to corrosive media such as sulfuric acid and sea water [SÄGLITZ1994].

Titanium can occur in two different crystallographic modifications which are only stable within a certain temperature interval. Below the β -transus temperature of $882 \text{ }^\circ\text{C}$, pure titanium occurs as a hexagonal close packed phase which is referred to as the α -phase. Above this temperature, the microstructure changes to the body centered cubic β -phase.

Depending on the alloying additions of a titanium alloy, three different categories of titanium alloys can be distinguished. According to their microstructure they are

referred to as α , $\alpha+\beta$ and β alloys.

As **Table 2-2** shows, the alloy properties are mainly determined by its additions and the resulting microstructure. Compared to the β phase, the α phase shows a low cold formability, low ductility, anisotropic mechanical properties and a high creep resistance.

	α	$\alpha + \beta$	β
density	+	+	-
strength	-	+	++
ductility	-/+	+	+/-
fracture toughness	+	-/+	+/-
creep strength	+	+/-	-
corrosion resistance	++	+	+/-
weldability	+	+/-	-
cold formability	--	-	-/+

Table 2-2: Properties of α , ($\alpha+\beta$) and β titanium alloys [PETERS2002]

As it is to be expected, Ti6Al4V as an ($\alpha+\beta$) alloy offers a balanced set of properties such as a high specific strength, a good heat treatability and a high thermal strength.

The composition of the workpiece material used in the experimental part of this work is shown in **Table 2-3**.

element	Al	V	C	Fe	O ₂	N ₂	H ₂	Ti
weight %	6,25	3,85	0,04	0,14	0,092	0,013	0,004	balance

Table 2-3: Chemical composition of the workpiece material as utilized in the experiments

Machinability of Ti6Al4V

Titanium was and partly still is considered to be difficult to machine [CHE-HARON2005]. Within the last years, however, titanium alloys like Ti6Al4V have been thoroughly investigated and machining strategies for these alloys have been developed. Using conventional cutting strategies, reasonable production rates and manufacturing cost can be achieved if certain characteristics of the material are taken

into account. However, compared to a low carbon free machining steel, the machinability of Ti6Al4V is considered poor [DONACHIE2000]. Among others, the reduced machinability is a result of these characteristics:

- Titanium is a poor conductor of heat so that the heat generated during the machining process has a larger impact on tool life.
- Titanium has a strong chemical reactivity with materials in the cutting tools at operating temperature which can lead to smearing, galling and chipping on the machined surface and a rapid destruction of the cutting tool.
- Titanium and its alloys are susceptible to surface damage which can result in reduced fatigue life [DONACHIE2000].

Chip Formation Mode in Machining Ti6Al4V

In machining, chip formation can take place in different modes. The three most commonly observed chip formation modes are mentioned below.

Discontinuous chips are common for brittle materials that fracture under the high strains imposed to the material during machining, such as cast iron.

Continuous chips occur when machining ductile materials with a low tendency to a strain induced embrittlement and the process is not affected by vibration (chatter).

Segmented chips, also referred to as *serrated chips*, exhibit regions of low deformation separated by thin regions of severe deformation. These strongly deformed regions are referred to as shear bands. This chip formation mode is common to TiAl6V4 under all cutting conditions. **Figure 2-10** shows a SEM picture of a segmented chip resulting from machining TiAl6V4. The triangularly shaped segments are clearly visible.

The mechanism behind this type of chip formation has not been identified. Mainly two theories are competing for an explanation of this mechanism: One theory states that cracks are initiated at the free surface and lead to a chip segmentation. The second theory is based on a shear localization mainly caused by strain softening, or, respectively, thermal softening of the workpiece material [KÖNIG1990; TÖNSHOFF1995; GENTE2002; BÄKER2004; HUA2004].

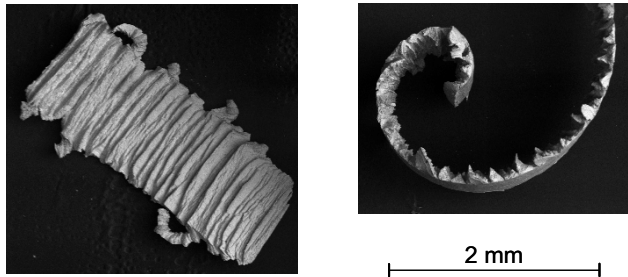


Figure 2-10: Segmented chip originating from an orthogonal cutting process (Ti6Al4V, $v_c=100$ m/min, $f=0.1$ mm)

2.3 Experimental Verification of Machining Models

In order to evaluate a model's capabilities, experimental data is needed. With respect to machining, researchers mainly rely on measured process forces, the chip geometry and process temperatures. Since the plastic deformation and the residual stress state subsequent to the machining process will be examined as well, these characteristics will also be discussed within this chapter.

2.3.1 Process Forces in Orthogonal Cutting

In order to reduce the complexity of the experimental set ups as well as the complexity of their models, researchers frequently examine the orthogonal cutting process. This process is rarely used in industrial applications, however, it is particularly suitable for the examination of the physics of machining processes. The orthogonal cutting process has a significant advantage when experimentally examining properties of the workpiece surface layer, particularly residual stresses. Compared to other turning processes such as external turning, material flow is less complex. Depending on the feed rate and the resulting overlap of the tool trajectory, the surface experiences repetitive deformation. The absence of feed marks simplifies the state of stress in the surface region and improves the accuracy of the residual stress measurements in the surface region of the workpiece. The influence of geometrical effects such as feed marks on residual stress measurements are significant and are thoroughly discussed in [PLÖGER2002].

The orthogonal cutting process is considered to be the simplest machining process (**Figure 2-11**). In orthogonal processes, the tool cutting edge (A-D) is perpendicular to the direction of the workpiece velocity v_c and to the side face of the indicated workpiece.

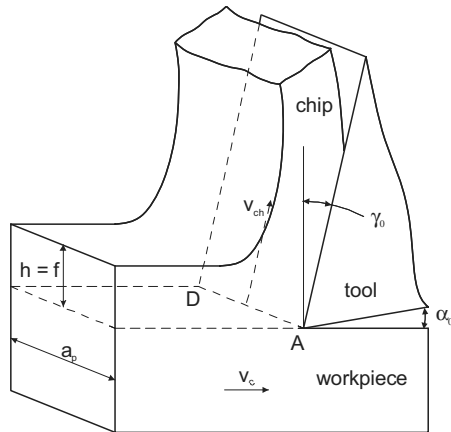


Figure 2-11: Orthogonal chip formation after [CHILDS2000]

If the width a_p of the workpiece is considered large compared to the feed f , i.e. $a_p \gg f$, plane strain conditions can be assumed for the material flow. **Figure 2-12** shows Mohr's circles for a state of plane stress (left) and for a state of plane strain (right). For a state of plane strain, the principal stresses σ_1 , σ_2 and σ_3 are unequal to zero. The principal strain ϵ_3 , however, is equal to zero. In contrast to a state of plane strain, in a state of plane stress the principal stress is equal to zero, while the principal strain ϵ_3 is unequal to zero.

With reference to the orthogonal cutting process, the plane strain condition implies that there is no material flow in the out of plane direction, and the flow in every plane parallel to the section modeled is identical.

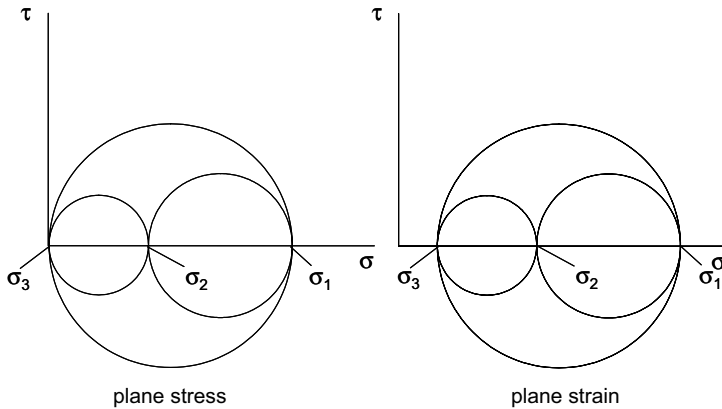


Figure 2-12: Mohr's circle for a state of plane stress (left; $\sigma_3 = 0$; $\varepsilon_3 \neq 0$) and a state of plane strain (right; $\sigma_3 \neq 0$; $\varepsilon_3 = 0$)

Since thrust force and feed force act in the same direction, in orthogonal cutting, only two process force components are distinguished: the cutting force F_c in the direction of the workpiece velocity and the feed force F_f perpendicular to the workpiece velocity. The orthogonal cutting process can be fully described in two dimensions as shown in **Figure 2-13**.

Generally, three zones of plastic deformation can be identified in orthogonal cutting:

- The primary shear zone (1) extending from the tool tip to the free surface,
- the secondary shear zone (2) extending along the tool rake face in the area of chip contact and
- the tertiary shear zone (3) extending from the tool tip into the generated surface.

Figure 2-14 gives an overview over machining processes that are referred to as orthogonal cutting processes.

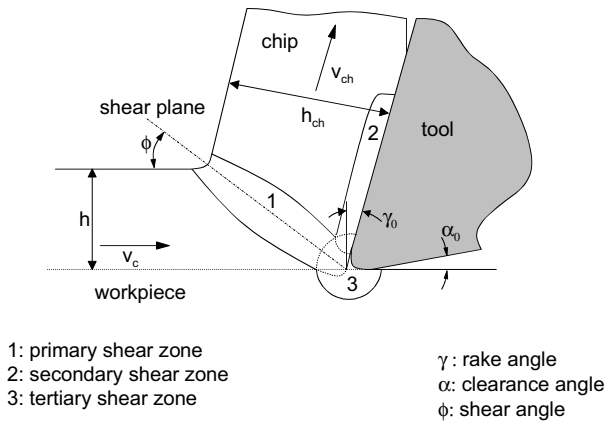


Figure 2-13: Two-dimensional cross section of an orthogonal cutting process and commonly used notation [TÖNSHOFF 1995]

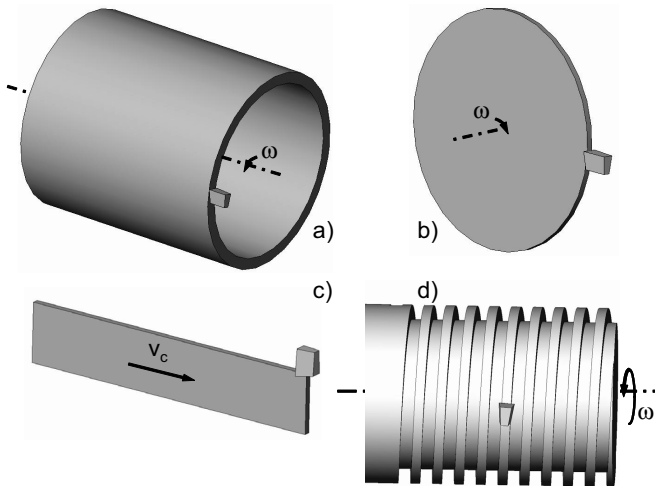


Figure 2-14: Conventional orthogonal cutting processes (a-c) and newly suggested orthogonal cutting process (d)

The machining of a tube, Figure 2-14a, is considered an orthogonal process under the assumption that the ratio of the tube diameter to the wall thickness is large so that the variation in cutting velocity over the tube's wall thickness can be neglected.

Figure 2-14b illustrates the recessing of a disc while Figure 2-14c shows a planing operation. The configuration shown in Figure 2-14d is a newly suggested set-up and will be described in detail in chapter 3.

2.3.2 Cutting Temperatures

Most machining models are evaluated only based on measured process forces because these can be easily acquired. Temperature measurements are more difficult to conduct than force measurements and, therefore, only within the last years have become part of the experimental verification of machining models [HOPPE2003].

Various methods to determine temperatures during machining processes have been reported in literature. Generally, one can distinguish between methods with time resolution and those without time resolution [MÜLLER2004]. Latter are e.g. techniques based on temperature sensitive paint as described by [SCHALLBROCH1943; ROSETTO1971], or powders with a defined melting point [LOCASTO1989]. Techniques that do not allow time resolution are not suitable for the verification of most machining models because simulated process times are usually so short that a steady state is not reached.

The methods that allow a time resolution can be divided into thermo-electrical methods and radiation methods. Commonly used thermo-electrical techniques are inserted thermo couples and the tool-workpiece thermo couple. When using thermo-couples, the time resolution is strongly influenced by the response time of the thermo couple and by the heat transfer between thermo couple and the target surface.

Radiation based methods that are commonly known are thermographical methods and pyrometric methods. Pyrometric methods are contactless methods that allow the determination of a object's absolute temperature by measuring the radiation emitted by the respective object. Thermographical methods are imaging methods that visualize infrared radiation emitted by an object. Thermographical methods only deliver relative temperature values and, compared to pyrometric methods, their response time is increased [MÜLLER2004].

Due to their capability to measure absolute temperatures without contact and due to the possibility to use fiber optics to access the measuring point, pyrometric methods

currently are the first choice for temperature measurements in machining.

2.3.3 Residual Stresses

Since the experimental determination of residual stresses due to machining and their numerical prediction is more complex than the determination and prediction of cutting forces or temperatures, residual stresses have been neglected in the majority of machining related numerical investigations. However, residual stresses can play an important role when it comes to a component's surface integrity.

The term "surface integrity" was coined for the surface or sub-surface properties that significantly influence a component's function [FIELD1971]. The key factors included in this term are:

- surface roughness,
- microstructure and
- residual stresses.

Surface integrity has a direct impact on a component's behavior concerning fatigue, corrosion and wear resistance. Residual stresses are of major importance in this context because the mentioned properties can be improved by compressive stresses or compromised by tensile stresses [BRINKSMEIER1982; LEIS1996; YEN2004b]. Components that are subjected to highly dynamical loads can fail prematurely if the manufacturing process, in machining particularly the finishing operation, leaves an undesired state of stress [CAPELLO2005].

Residual stresses are considered to be mechanical stresses which occur in all polycrystalline solids without the presence of external loads. In manufacturing, common causes for the formation of residual stresses are for instance local heat exposure or non-uniform plastic deformation. The state of residual stress within a body depends on its thermal, metallurgical and mechanical state. The complex interactions between these three influencing factors are visualized in **Figure 2-15**.

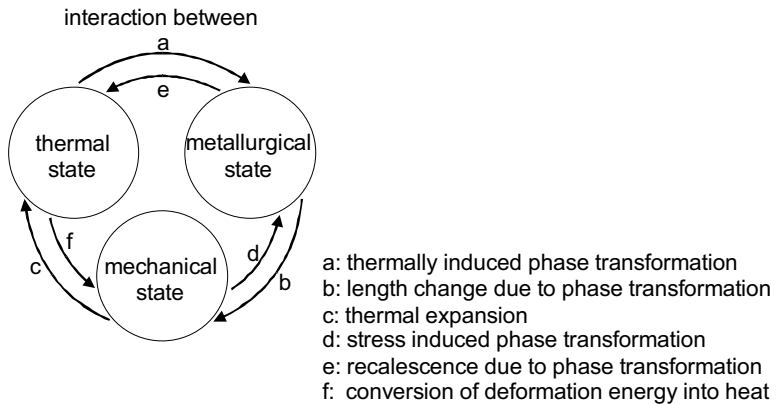


Figure 2-15: Influencing factors on the formation of residual stresses and their interaction after [PRECKEL1988]

One criterion for the classification of residual stresses is their compressive or tensile nature. Generally, positive values represent tensile stresses while negative values stand for compressive stresses.

An additional criterion for the classification of residual stresses is the spatial resolution [MACHERAUCH1973; HAUKE1983; TEKKAYA1986]. According to this classification, three types of residual stresses exist, **Figure 2-16**.

Type I residual stresses, also referred to as macroscopic residual stresses, extend nearly homogeneously over several grains. Forces and momentums resulting from these stresses are in equilibrium within the body. If this equilibrium is disturbed, the result is a macroscopic dimensional change.

Type II residual stresses are homogeneous within a grain and are in equilibrium over a "sufficient number of grains" [MACHERAUCH1973]. The Type II residual stresses can be a result of anisotropic properties of the grains of a single phase material, or they can be caused by different properties of the phases within multi phase materials. Disturbing the Type II stress equilibrium may result in macroscopic dimensional changes of the body [KANDIL2001].

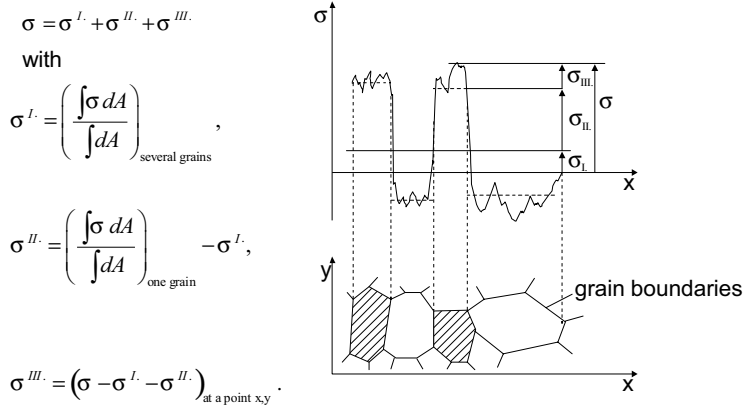


Figure 2-16: Residual stresses of first, second and third order [HAUK1983]

Type III residual stresses are inhomogeneous over a few inter atomic distances. They exist within a grain and occur e.g. around dislocations or other defects in the crystal lattice [KANDIL2001].

Frequently, Type II and Type III residual stresses are referred to as microscopic residual stresses. In practice, residual stresses always occur as a combination of both, macroscopic and microscopic residual stresses [MÜNKER1995].

2.3.3.1 Surface Integrity and Residual Stresses in Machining

Elastic material properties are considered to contribute mainly to surface integrity, in particular to residual stresses. [HENRIKSEN1951] stated that residual stresses in machining are mainly due to the deformation of grains in the surface layer and that thermal effects are negligible. [OKUSHIMA1971] questioned this statement. He introduced the hypothesis that mechanical effects (deformation) contribute to compressive stresses while thermal effects promote tensile stresses. According to [LIU1976a] and [LIN1991] thermal effects were negligible for sharp tools. [LIU1976b] suggested that the thermal influence on stress formation increases with the tool's flank wear. [KÖNIG1993] compared surface turning and grinding processes with

respect to surface integrity. [JACOBUS2000] conducted experimental orthogonal and oblique cutting tests and proposed an analytical thermo-elastoplastic model for the prediction of residual stresses after machining annealed AISI 4340 steel and agreed with other authors that thermal effects move the stress level to the tensile regime while deformation causes the opposite effect. He also stated that an increase of the cutting edge radius leads to an increase of magnitude and depth of the plastic deformation normal to the newly generated surface. Additionally he found that positive plastic strains near the surface layer contribute to compressive residual stresses and vice versa. [PLÖGER2002] examined the influence of high speed cutting (HSC) on surface layer properties of the steel Ck45N. [GUERVILLE2001] states that under non-abusive machining conditions, compressive residual stresses are obtained when machining Ti6Al4V, and that the stress level is significantly influenced by the cutting conditions. The author also states that the affected surface layer is between 50 μm and 100 μm thick. [CHE-HARON2005] also examines the effect of machining on the surface integrity of Ti6Al4V. He observes severe plastic deformation of the grains in the surface layer after prolonged dry machining of Ti6Al4V as well as an increase in surface roughness with increasing tool wear. Residual stresses are not examined in his work.

For machining conditions in conventional cutting, authors agree on the absence of phase transformations when machining steel [CAPELLO2005]. When machining titanium alloys under abusive conditions, however, so-called white layers of transformed material are reported [GUERVILLE2001].

2.3.3.2 *Determination of Residual Stresses*

Since stress is not directly measurable, residual stresses can only be determined indirectly. Among other methods [KANDIL2001], the most common approaches for the determination of residual stresses are the so-called hole drilling method (**Figure 2-17**) and the X-Ray diffraction (**Figure 2-18**). The hole drilling method is based on the determination of a material's relaxation when material is removed and a new equilibrium is established. Most commonly, a strain gage rosette as shown in Figure 2-17 is applied to the workpiece surface. Subsequently, a hole is machined in the workpiece through the center of the strain gage rosette and the strain gages register

the locally relieved surface strains. These strains represent the basis for the calculation of the stresses that existed previously to the machining process. The stress calculation is performed by means of formulae which have been derived from experimental analysis in combination with FE simulations [KANDIL2001]. The hole drilling method can be applied to examine parts with a uniform stress distribution as well as parts with a non-uniform stress field by drilling a hole with several depth increments. The method's application to non-uniform stress distribution, however, requires a more complex calculation and a careful selection of the employed evaluation algorithm [GRANT2002].

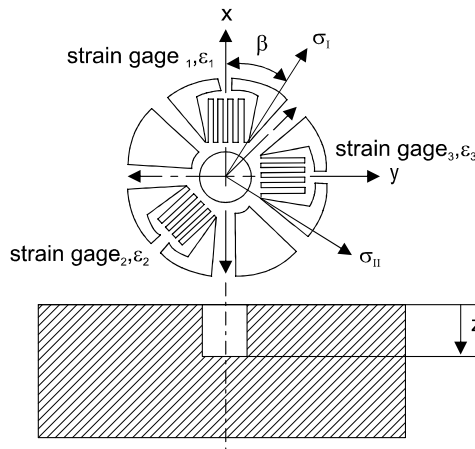


Figure 2-17: Determination of residual stresses by means of the hole drilling method [MÜNKER1995]

This method provides an inexpensive way to determine residual stresses, but even though it can be applied to inhomogeneous stress fields as well, it is not suitable for the application in inhomogeneous stress fields with high gradients as they occur in machined surfaces.

The stress determination by means of X-Ray diffraction has been applied for the determination of all residual stresses contained in this work. It relies on a change in the spacing of the lattice planes of a polycrystalline material due to elastic deformations. The spacing of the distorted lattice corresponds to the applied load. In

order to determine the spacing of the distorted lattice, the specimen is irradiated with X-Rays which are diffracted by the crystal lattice. A detector moves around the specimen and detects the diffracted X-Rays. The intensity of the diffracted X-Rays is recorded in correlation to their angular position. **Figure 2-18** shows a plane within a crystal lattice of a single grain. The plane is defined by the Miller indices (hkl). Two parallel incident X-Ray beams with the wavelength λ which arrive on the specimen surface under an angle of $\Psi + \Theta$ or, respectively, on the lattice plane (hkl) under an angle of Θ , are reflected at adjacent lattice planes. The angle between the reflected beams and the specimen surface is the angle $\Theta - \Psi$.

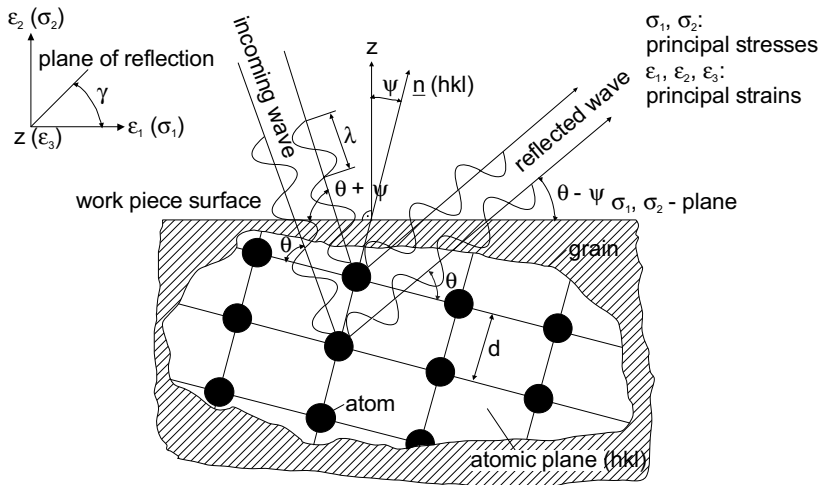


Figure 2-18: Determination of residual stresses by means of x-ray diffraction [TEKKAYA1986]

If the wavelength λ is an exact multiple of the path difference between the two reflected beams, constructive interference occurs. This condition is expressed in the Bragg equation:

$$n\lambda = 2d \sin \Theta ; n = 1, 2, 3, \dots \quad (2.9)$$

Differentiation of equation (2.9) yields:

$$\partial \theta = -(\tan \Theta) \frac{\partial d}{d}. \quad (2.10)$$

Relation to the undistorted lattice and substitution of the differentials ∂ by finite increments Δ leads to:

$$\varepsilon_n^g = \frac{\Delta d}{d_0} = \frac{(d - d_0)}{d_0} = -\Delta \Theta \cdot \cot \Theta_0. \quad (2.11)$$

With $\Delta \Theta = \Theta - \Theta_0$ equation (2.11) can also be written

$$\varepsilon_n^g = -(\Theta - \Theta_0) \cdot \cot \Theta_0. \quad (2.12)$$

Equation (2.12) represents the basic equation that allows the calculation of the lattice strain ε_n^g based on X-Ray diffraction. If the Bragg angle Θ_0 for an undistorted lattice is known, the micro strains normal to the interference plane can be calculated from the offset $\Delta \Theta$ of the interference lines. In conjunction with the theory of elasticity the macro stresses that correspond to the respective micro strains are calculated [TEKKAYA1986].

Despite x-ray diffraction itself belongs to the non-destructive methods for residual stress determination, the workpiece has to be destroyed in many cases. This is due to two reasons. One reason is that stationary diffractometers are restricted in the size of the specimens they can accommodate. Mobile diffractometers as they are becoming increasingly popular can overcome this restriction to a large extent, however, if only the surface stresses are of interest. The second reason that makes a destruction of the workpiece virtually inevitable becomes effective if a depth profile of the residual stresses is to be determined. If this is the case, layers of workpiece material have to be removed so that a sequence of surface measurements can be performed. The stress relaxation that takes place due to the material removal also has to be taken into account when the stresses are calculated from the determined micro-strains. The most common method to remove material without inducing stresses in the specimen is electro-chemical polishing which allows the reproducible material removal in small steps with a magnitude of 10 μm or less per step as it is required for the determination of residual stresses with high gradients.

3 Experimental Set Up

In order to generate a data basis for the comparison of simulation results to experimental data, orthogonal cutting tests are utilized. The set-up has to be capable of providing in-process forces as well as temperatures. Additionally, post-process residual stresses in the surface layer shall be determined. The commonly known experimental set-ups for orthogonal turning processes as they have been presented previously (p. 31, Figure 2-14b,c) are suffering from a significant draw-back: If not only process forces during the machining process, but also residual stresses after the machining operation are of interest, the conventional orthogonal set-ups can not guarantee constant cutting conditions for the whole machined surface. This is due to the fact that the tool enters and exits the workpiece gradually. Consequently, the material removal during these two phases is not constant. Since the entry and, more importantly, the exit point of the tool can not be easily determined, these configurations can not guarantee that the post process stress measurements will be performed at locations that were machined under representative process conditions.

Among the widely used orthogonal machining set ups shown in Figure 2-14 the logical choice for an experimental set-up would be a planing or a shaping process in accordance with Figure 2-14a. However, planing or shaping machines have become rare and the sudden tool engagement can obstruct force measurements. Lathes, in contrast, are commonly available and turning processes allow a smooth tool entry or tool exit. In order to be able to measure residual stresses as a result from controlled machining conditions in turning, a new workpiece design for orthogonal turning as described in the following chapter is used during the experimental investigations.

3.1 Workpiece

Instead of using a tubular, disc shaped or bar shaped workpiece and appropriate tools, a workpiece equipped with a helical, threadlike feature is suggested (**Figure 3-1**, left). The advantage of this helical collar is the possibility to clearly identify a region with constant cutting conditions so that the tool entry and –exit phases do not interfere with the post process examination of the workpiece. Furthermore, this type of cutting process can easily be implemented on a standard lathe.

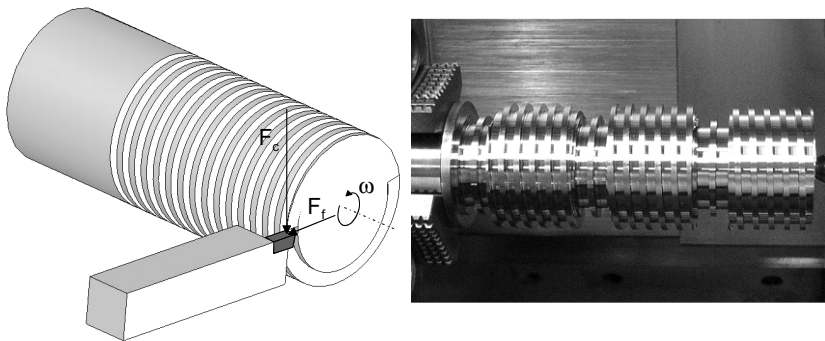


Figure 3-1: Helical orthogonal cutting. Basic principle (left) and actual workpiece (right) with helical as well as circular collars

The workpiece used in the experiments is shown on the right side of **Figure 3-1**. As the figure shows, the workpiece contains three helical and three ring shaped collars with a width of 2.5 mm each. The ring shaped collars can be used to compare the results of a “standard” orthogonal cutting test, the recessing of a disc, to the results of the orthogonal cutting tests with a helical collar.

3.2 Tools and Machine Tool

In order to achieve orthogonal cutting conditions for the helical cutting process, the tool has to be inclined so that the inclination angle compensates the helix pitch. For the workpieces used throughout this work, the tool inclination angle is 2.5° . **Figure 3-2** shows the standard grooving tool holder used for recessing of discs as well as the modified holder used for the helical turning experiments. The latter has been machined to match the desired inclination angle.

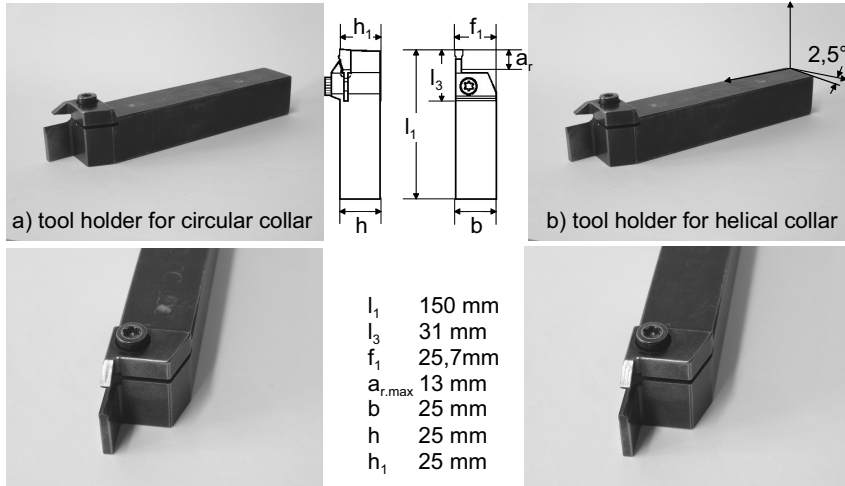


Figure 3-2: Tool holders used for recessing of circular collars (discs) and for helical turning.

All experiments were conducted utilizing uncoated carbide grooving inserts as shown in **Figure 3-3**. These particular inserts are available as blanks with a zero degree rake angle and without edge preparation. The insert has a clearance angle of 10° and a flat rake face without chip breaker. Since these inserts have not been subjected to any edge preparation process, their cutting edge shows a burr-like material formation. In order to generate the desired nominal cutting edge radii of $10\ \mu\text{m}$, $20\ \mu\text{m}$ and $30\ \mu\text{m}$, the cutting inserts are first ground conventionally in order to remove the extra material along the cutting edge. Subsequently, the inserts are ground by means of a drag grinding process.

In drag grinding, the workpiece, i.e. the cutting insert, is moved through an abrasive medium which, in this case, consists of loose corundum grits with an average size of $20\ \mu\text{m}$. By adjusting the number of revolutions per minute, the depth of the moving cutting insert in the abrasive medium and the process time, the material removal can be controlled.

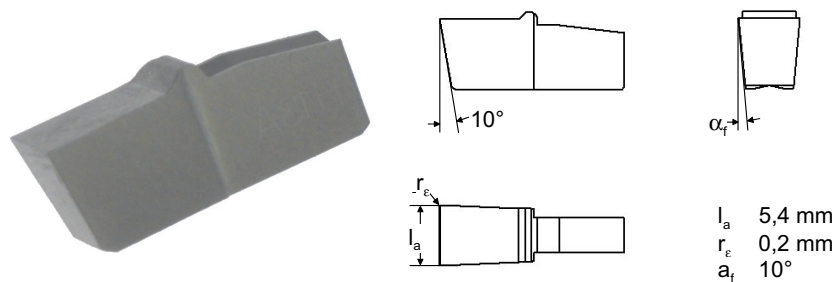


Figure 3-3: Uncoated carbide cutting insert utilized in the experiments (Sandvik Coromant N151.2-540-40-3B H13A)

Figure 3-4 shows the drag grinding machine type “Walther Trowal M-TMD” that was used for the cutting edge preparation. As the figure shows, the tool holder was protected with self-adhesive tape in order to prevent the exposure of the holder to the abrasive media.

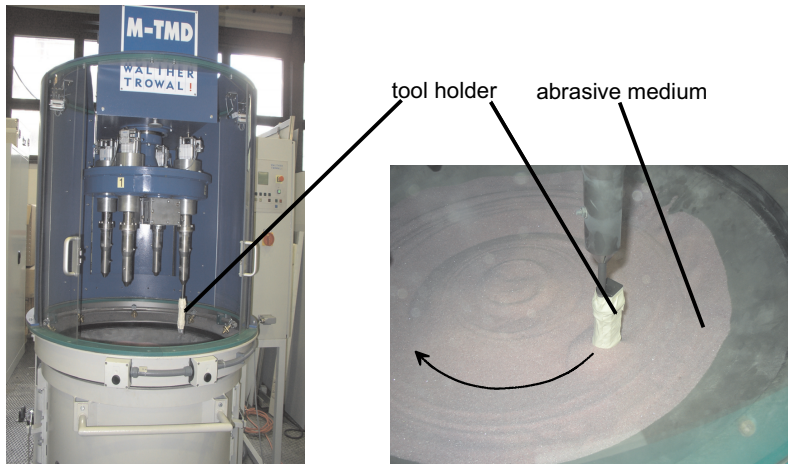


Figure 3-4: Cutting edge preparation by means of drag grinding

In **Table 3-1** the process parameters that were used to generate the desired cutting edge radii are listed.

r_β [μm]	depth [mm]	time [s]	rpm
10	50	45	10
20	180	60	10
30	180	140	10

Table 3-1: Process parameters for the cutting edge preparation by drag grinding

In addition to the listed process parameters for drag grinding, the inclination angle of the cutting insert also influences the result. It was found that a positive effective rake angle of 45° produced the best results concerning the quality of the cutting edge radius. As the SEM pictures in **Figure 3-5** indicate, the drag grinding process yields a homogeneous material removal along the full width of the cutting edge. This is of particular importance because the cutting edge radii used in the experiments have to be as close as possible to the radii used in the FE model.

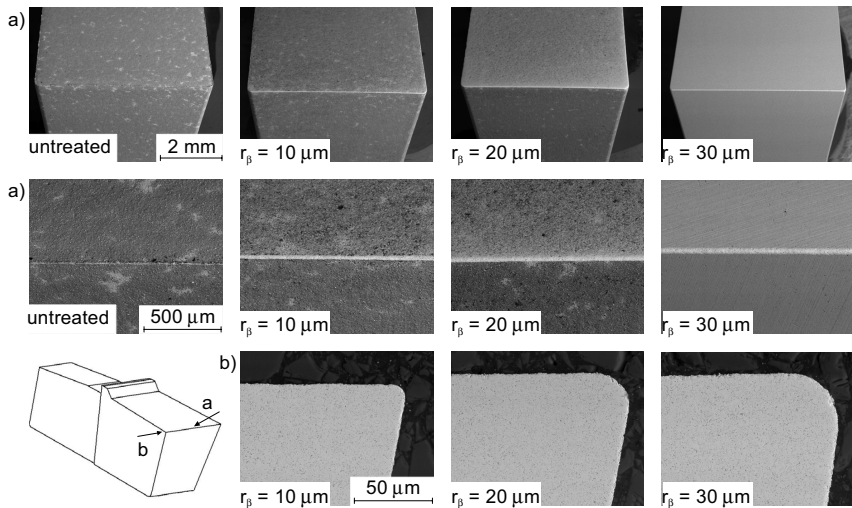


Figure 3-5: Cutting edge conditions as obtained from the drag grinding process. a) view on the cutting edge b) cross sectional view of the cutting edge.

3.3 Temperature and Force Measurements

As shown in **Figure 3-6**, the general experimental set up consists of a lathe, a cutting tool and a data acquisition system including a three component force dynamometer,

a charge amplifier and a personal computer. In order to be able to compare not only experimental and modeled process forces but also the range of the occurring temperatures, a two color pyrometer was used to determine the chip surface temperature during machining. The short response times facilitate temperature measurements despite the high heating rates that can reach 10^7 K/s in high speed machining [DUFFY1992].

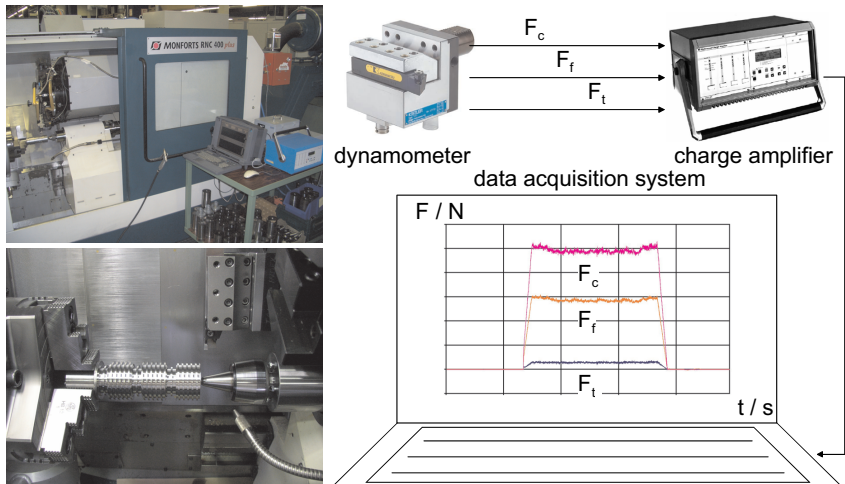


Figure 3-6: Experimental set up for orthogonal cutting tests

Generally, the optical fiber that forwards the wave spectrum from the measuring spot to the analyzing unit can be placed at various locations. Three different mounting cases are shown in **Figure 3-7**.

In order to avoid the weakening of the cutting insert caused by the guidance bore in set-up b) and c) as well as an influence of material particles which could block the fiber, the temperature measurements in this work were conducted according to set-up a). To examine whether surface temperatures reach the β -transus temperature of Ti6Al4V, results from [MÜLLER2004] obtained by means of setup b) will also be taken into account.

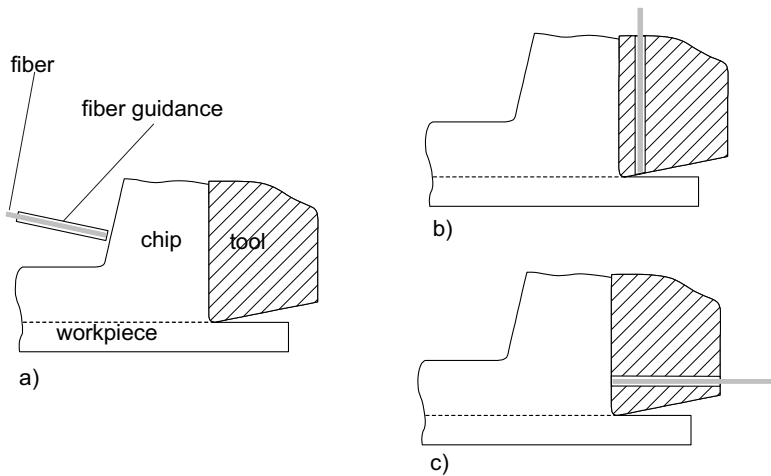


Figure 3-7: Typical set-ups for pyrometric temperature measurements on a) the free chip surface, b) the generated surface and c) the bottom of the chip

Figure 3-8 shows the experimental set-up including the components required for the temperature measurements. The optical fiber with a diameter of 0.2 mm is protected by a liner with an outer diameter of 0.4 mm and a PTFE¹ hose which guides the fiber into the machine.

The fiber is positioned in the middle of the cutting edge and 0.2 mm above the rake face. The distance from the cutting edge is 0.2 mm.

Details on the major components of the experimental set up are given in **Table 3-2**.

¹ polytetrafluoroethylene

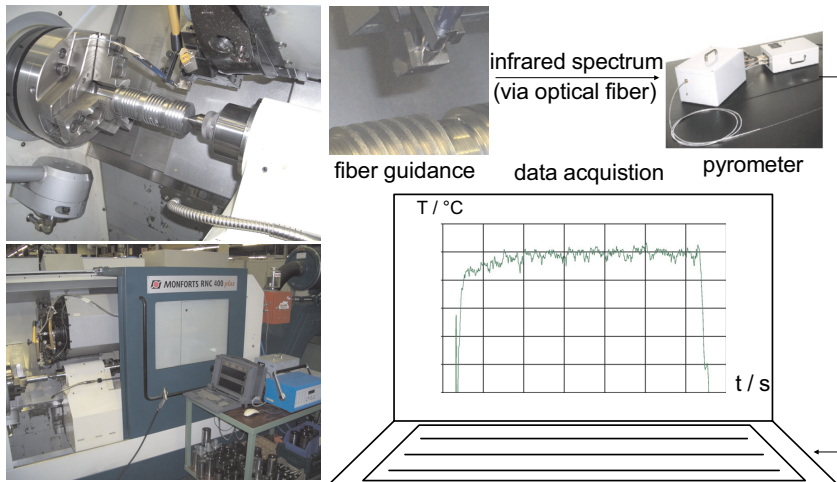


Figure 3-8: Set up for the determination of the chip surface temperature

Components of the experimental set-up	
Machine Tool	Monforts RMC 400 plus
Cutting Insert	Sandvik N151.2-540-40-3B H13A
Dynamometer	Kistler 9121
Charge Amplifier	Kistler 5019B
Two Color Pyrometer	Fire I
DAQ Software	Labview

Table 3-2: Overview over the experimental set up

4 Orthogonal Cutting Tests

The experimental plan for the conducted cutting tests consists of nine sets of cutting conditions as shown in **Table 4-1**. The cutting conditions are chosen to cover a large range of process conditions. At the same time, the experimental cutting conditions or, respectively, the process set up has to be suitable to be captured in the FE model. This requirement results in restrictions concerning the cutting edge radius and the feed (uncut chip thickness). The cutting edge radius has to be large enough to be represented by an FE mesh and the uncut chip thickness should be in the lower range of commonly used process parameters in order to reduce the total size of the FE model. This will be further discussed in chapter 5.

In order to provide statistically valid data, the cutting tests corresponding to each parameter set were repeated three times. In addition to the helical orthogonal cutting tests, orthogonal cutting tests on circular collars were performed to verify that the helical test is indeed comparable to a “standard” orthogonal cutting test.

All experiments were conducted without the use of coolant in order to simplify the model for the machining process.

Parameter Set	r_β [μm]	f [mm / rev]	v_c [m / min]
1, 2, 3	10	0.1	25, 50 100
4, 5, 6	20	0.1	25, 50 100
7, 8, 9	30	0.1	25, 50 100

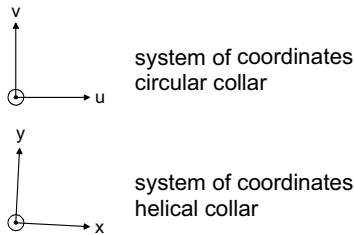
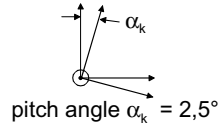
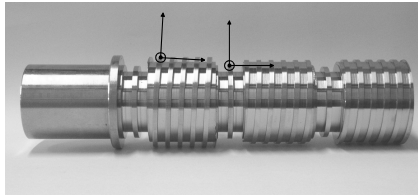
Table 4-1: Cutting conditions for the helical and circular orthogonal cutting tests.

4.1 Forces and Temperatures

In conventional orthogonal cutting both, the feed force F_f as well as the thrust force F_t act in the same direction along the longitudinal tool axis, whereas in helical orthogonal cutting the feed force F_f acts along the workpiece axis.

Since the coordinate system in helical orthogonal turning is rotated by 2.5° relative to the coordinate system of the force dynamometer, the measured force components in helical turning have to be subjected to a simple coordinate transformation, **Figure 4-1**. This coordinate transformation results in slight changes of the cutting force F_c .

and the feed force F_f . The third force component then is reduced to values around 0 N as they were observed in orthogonal turning of the circular collar.



$$u = x * \cos \alpha_k + y * \sin \alpha_k$$

$$v = -x * \sin \alpha_k + y * \cos \alpha_k$$

Figure 4-1: Coordinate transformation for the helical orthogonal cutting tests

An overview of the acquired forces in helical and conventional orthogonal turning is given in **Figure 4-2**. Each data point represents an average value of three measurements. The figure shows that the forces obtained from helical turning are in very good agreement with the forces that occurred in the conventional orthogonal cutting process. Consequently, the helical orthogonal turning process is a suitable substitute for conventional orthogonal turning processes as they have been previously described. From this point on, only forces from helical cutting will be considered within this work.

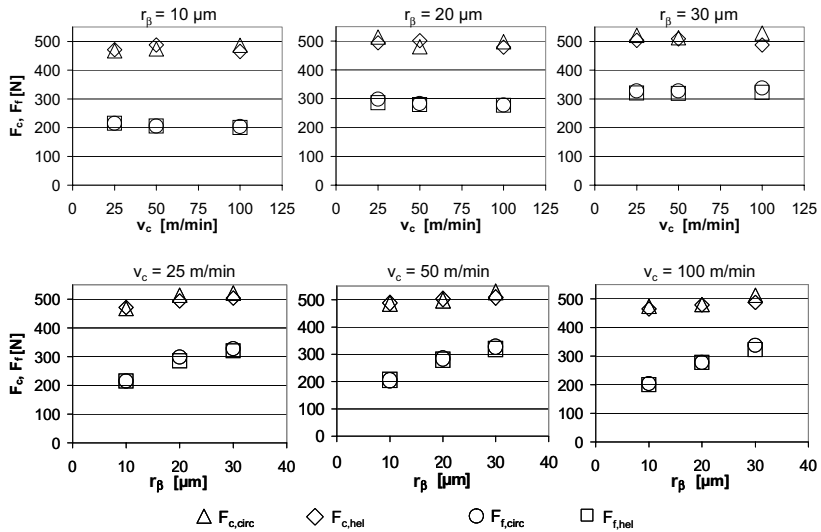


Figure 4-2: Overview over the acquired forces in helical (hel) and conventional (circ) orthogonal turning on a circular collar, $f = 0.1 \text{ mm}$

The first row of diagrams in Figure 4-2 shows that within the chosen range of process parameters the cutting forces F_c as well as feed forces F_f for each cutting edge radius r_c do not change significantly with respect to the cutting velocity. As the second row of diagrams in Figure 4-2 indicates, however, cutting force as well as feed force are clearly influenced by the cutting edge radius.

The temperature measurements performed on the free chip surface in the vicinity of the chip root indicate a temperature increase to approximately 500°C . The temperatures measured at the free chip surface did not significantly vary within the range of the chosen cutting parameters. The measurements are in agreement with the measurements performed by [MÜLLER2004]. In the experimental temperature measurements conducted by [MÜLLER2004], temperatures at the bottom of the chip, where the maximum temperatures in machining occur, did not exceed 850°C for a cutting velocity of 100 m/min . The temperatures determined for the generated surface under the same cutting conditions did not exceed 650°C . Based on these measurements it is assumed for this work that the newly generated surface has not

undergone the phase transformation from $\alpha + \beta$ to β , which for this alloy takes place at the β -transus temperature of 980 °C. Therefore, phase transformations will not be taken into account within this work.

4.2 Chip Thickness

The thickness of the generated chips was determined by means of a micrometer. Chip segmentation was not considered during the measurements so that the determined values represent the overall thickness of the chip from the bottom to the tip of the segments. Each value displayed in **Figure 4-3** represents an average of five measurements.

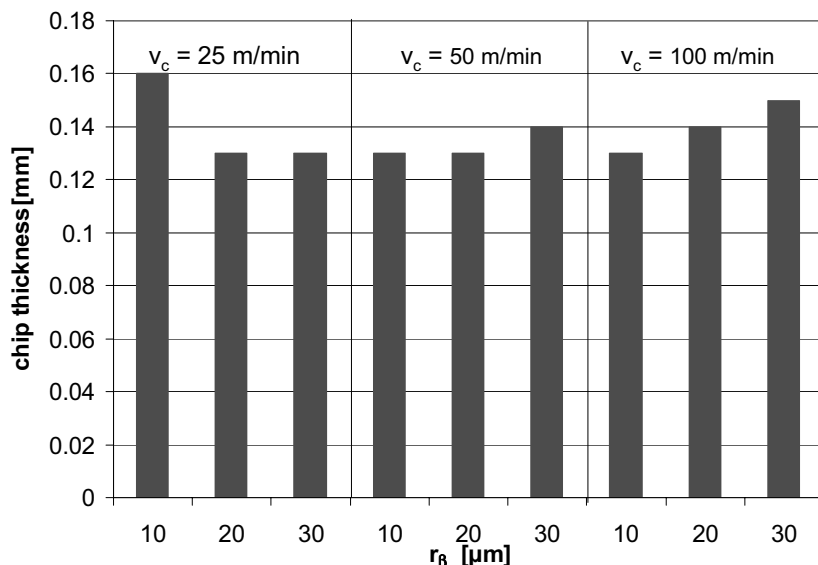


Figure 4-3: Determined chip thickness

4.3 Post Process Examination of the Cutting Inserts

Subsequent to the cutting experiments, the used cutting inserts were examined by means of a Scanning Electron Microscope (SEM). Purpose of this examination was to determine whether the used inserts show any significant geometrical change of the

cutting edge as it could be caused by excessive tool wear, by chipping of the cutting edge or by material that adheres to the cutting tool.

Figure 4-4 shows SEM pictures of cutting inserts with all three utilized edge radii after helical orthogonal cutting with 25 m/min. The figure indicates that the cutting edges seem to be completely intact, i.e. no chipping occurred. However, all three cutting edges show material that adheres to the cutting insert.

The cutting inserts shown in **Figure 4-5** were employed in helical orthogonal cutting with a cutting velocity of 50 m/min. The figure indicates that no noticeable damage to the cutting edge has taken place.

The pictures of the inserts used with a cutting velocity of 100 m/min do not indicate any damage either, **Figure 4-6**. Again, workpiece material has adhered to the cutting tool. The insert with a cutting edge radius of 20 μm shows a nearly clean cutting edge with only a small amount of adhered material.

Generally, it can be stated that an increase in the cutting edge radius leads to an increased tendency to collect workpiece material. An explanation for this tendency lies within the increased area of a cutting edge with a large cutting edge radius that provides an increased stability to adhered material compared to a cutting edge with a small radius.

The pictures indicate that even though the cutting edge geometry itself remains unchanged during the process, adhesions could have occurred while machining which might have changed the effective cutting edge radius. This change of the effective cutting edge radius in return, can lead to a change of the processing conditions that can not be taken into account by the model.

Based on these findings it is assumed for the machining model that the cutting edge geometry does not change during tool engagement.

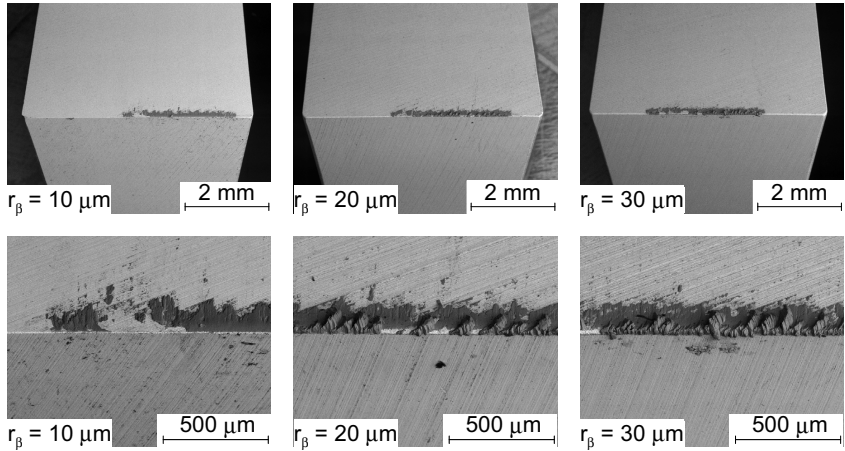


Figure 4-4: SEM pictures of cutting inserts with all three nominal edge radii after the machining process at $v_c = 25 \text{ m/min}$

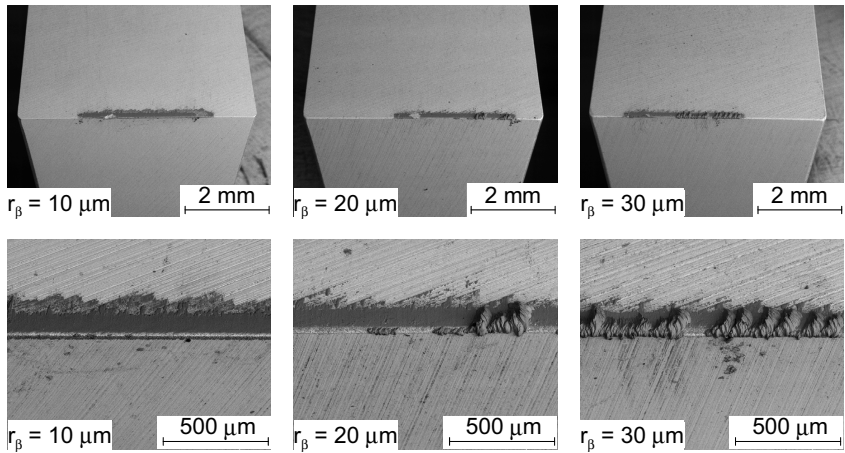


Figure 4-5: SEM pictures of cutting inserts with all three nominal edge radii after the machining process at $v_c = 50 \text{ m/min}$

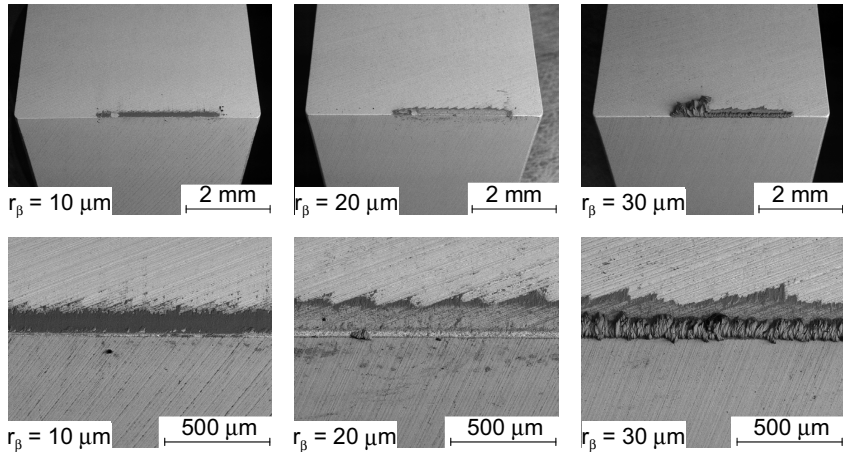


Figure 4-6: SEM pictures of cutting inserts with all three nominal edge radii after the machining process at $v_c = 100 \text{ m/min}$

4.4 Microstructure and Residual Stresses Subsequent to Machining

The impact of the machining process on the workpiece surface layer is evaluated by means of micrographs and residual stress measurements.

Figure 4-7 shows micro sections of the workpiece surface layer subsequent to machining with a cutting speed of 25 m/min and 100 m/min using an untreated cutting edge. The micro sections have been placed orthogonally to the direction of the cutting velocity. On the micrographs the α -phase appears white while the β -phase shows as a dark seam around the α phase.

The micrographs show a bi-modal microstructure consisting of a globular α -phase embedded in a matrix of lamellar α and β . While the globular microstructure leads to increased fatigue strength, the lamellar mode increases creep resistance and fracture toughness [PETERS2002]. For the previously mentioned applications, the observed bi-modal microstructure represents a well balanced compromise.

In **Figure 4-7**, no indication for severe plastic deformation can be found. As expected, the surface grains are cleanly cut and no signs for a material flow orthogonal to the direction of the cutting velocity can be identified.

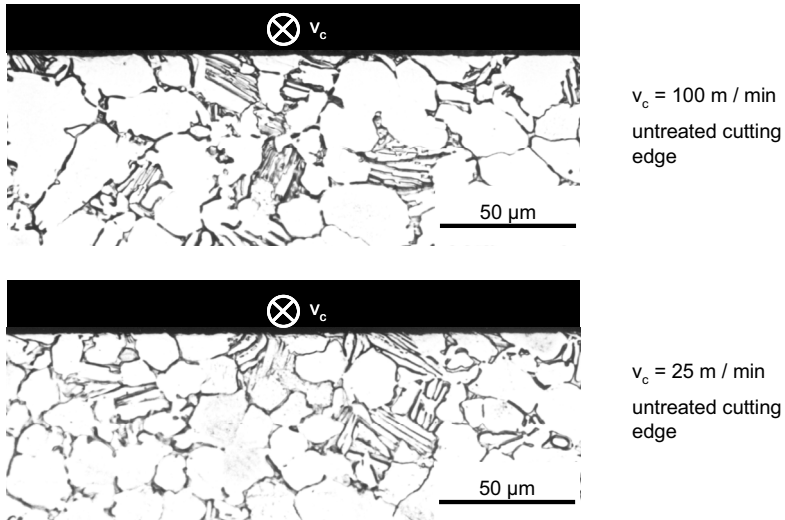


Figure 4-7: Micro section orthogonal to the direction of the cutting velocity after machining with an untreated cutting edge ($f = 0.1 \text{ mm}$)

Figure 4-8 shows micro sections for the same cutting conditions as in **Figure 4-7**. In contrast to **Figure 4-7**, however, the micro sections were taken parallel to the direction of the cutting velocity. The pictures show a deformed surface layer for both cutting conditions. It can be observed that the penetration depth of the plastic deformation is higher at a cutting velocity of 25 m/min compared to a cutting velocity of 100 m/min . The corresponding micro sections for a cutting velocity of 50 m/min are very similar to those for a cutting velocity of 100 m/min and therefore are not displayed.

A white layer or any other indications for phase transformations were not observed for the whole range of cutting parameters.

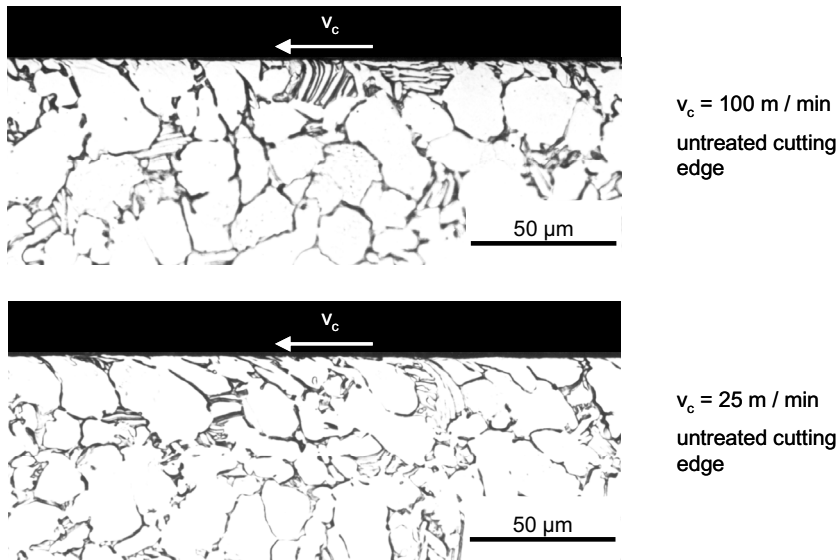


Figure 4-8: Deformed grains in the workpiece surface layer. Micro section parallel to the direction of the cutting velocity after machining with an untreated cutting edge ($f = 0.1 \text{ mm}$)

While the penetration depth of the plastic deformation can not be quantified based on the micrographs, the x-ray diffraction measurements allow a more detailed analysis of the extent of the plastic deformation by means of the “full width at half maximum” (FWHM) value of the intensity determined at different angles during the measurements. The FWHM is a measure for the micro-stresses (type III-stresses) or, respectively, the dislocation density and consequently can be utilized as a measure for the amount of plastic deformation.

All residual stress measurements were conducted using X-ray diffraction in combination with an electro-polishing process. In order to be able to perform the X-ray diffraction analysis, specimens were cut out of the workpiece by means of wire EDM. The specimens were then embedded within a resin. The diffractometer of the type Seifert XRD 3003 that was utilized for the residual stress determination is shown in **Figure 4-9**. According to the common notation, tensile stresses are considered to have a positive sign, while compressive stresses are marked by a negative sign.

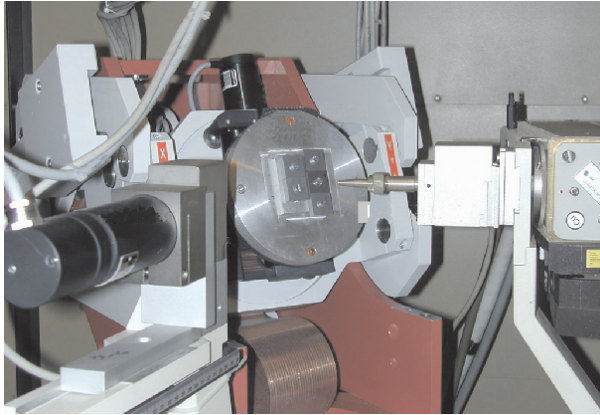


Figure 4-9: Seifert XRD3003 diffractometer

Table 4-2 gives an overview over the parameters that were used for the x-ray diffraction measurements.

x-ray diffraction parameters	
diffractometer	XRD 3000
radiation	Cu-K $_{\alpha}$
lattice plane {hkl}	{213}
interference angle 2Θ	141,7°
direction of measurements	in machining direction and orthogonal to machining direction
range of measuring angle $\sin^2\psi$	$\pm 0,7$
average x-ray penetration depth	4,5 μm
$\frac{1}{2}S_{2\{213\}}$	$11,73 \cdot 10^{-6} \text{ MPa}^{-1}$
$S_{1\{213\}}$	$-2,85 \cdot 10^{-6} \text{ MPa}^{-1}$
modulus of elasticity $_{\{213\}}$	112610 MPa
Poisson-ratio $_{\{213\}}$	0,321

Table 4-2: Parameters of the x-ray diffraction measurements

The convention for the designation of the determined stress components is given in **Figure 4-10**. The stress component determined along the direction of the cutting velocity will be referred to as σ_x while the stress component determined orthogonal to

the cutting velocity will be designated as v_c .

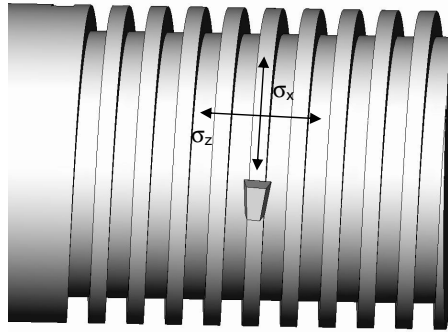


Figure 4-10: Convention for the designation of the residual stress components σ_x in cutting direction and σ_z orthogonal to the cutting direction

Figure 4-11 shows the results of residual stress measurements on surfaces that were generated with the untreated, “sharp”, cutting edge as previously shown in Figure 3-5. The results of the residual stress measurements for the tools with cutting edge preparation covering all experimental points listed in **Table 4-1** are shown in **Figure 4-12** to **Figure 4-14**.

The residual stresses shown in **Figure 4-11** were determined after machining with the untreated or “sharp” cutting edge. It can be seen that σ_x reaches a minimum stress value, i.e. a maximum compressive stress, of 500 MPa. With increasing cutting velocity, the level of σ_x shifts towards the tensile regime while remaining compressive. This behavior is in agreement with the theory that mechanical effects contribute to compressive stresses while thermal effects promote tensile stresses.

For the cutting edge radius of 10 μm , **Figure 4-12** shows significantly higher compressive stresses in x direction compared to the untreated tool. Furthermore, the dependency on the cutting speed has almost disappeared.

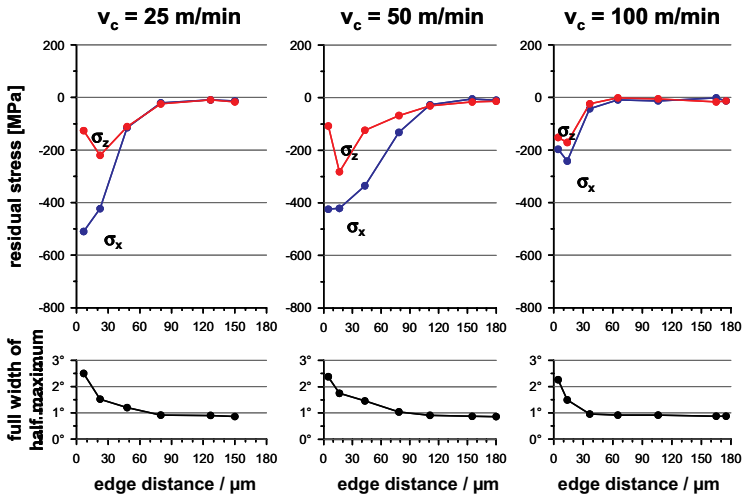


Figure 4-11: Residual stresses determined for an untreated cutting edge as shown in Figure 3-5, top left

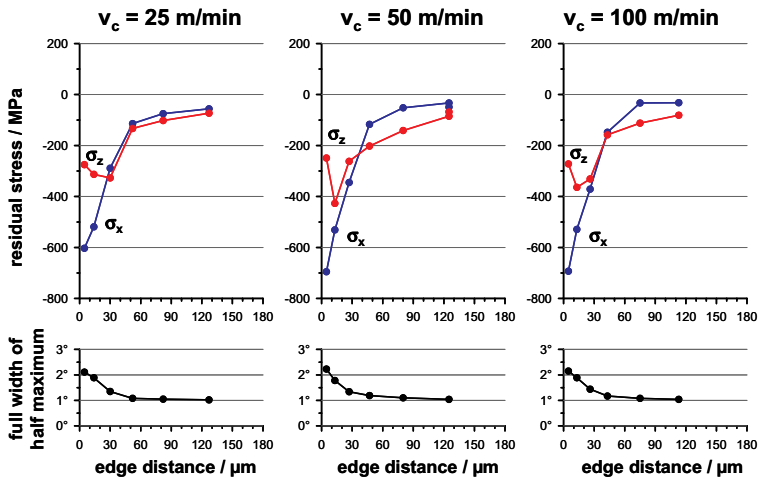


Figure 4-12: Residual stresses determined for a cutting edge radius of $r_\beta = 10 \mu\text{m}$

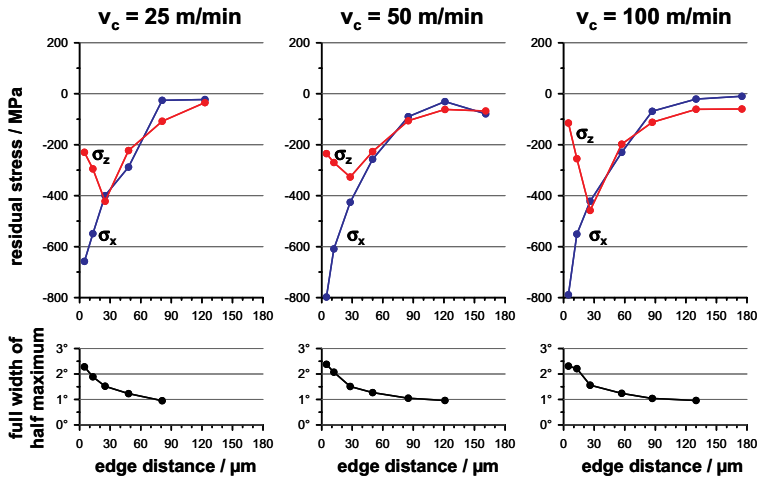


Figure 4-13: Residual stresses determined for a cutting edge radius of $r_\beta = 20 \mu\text{m}$

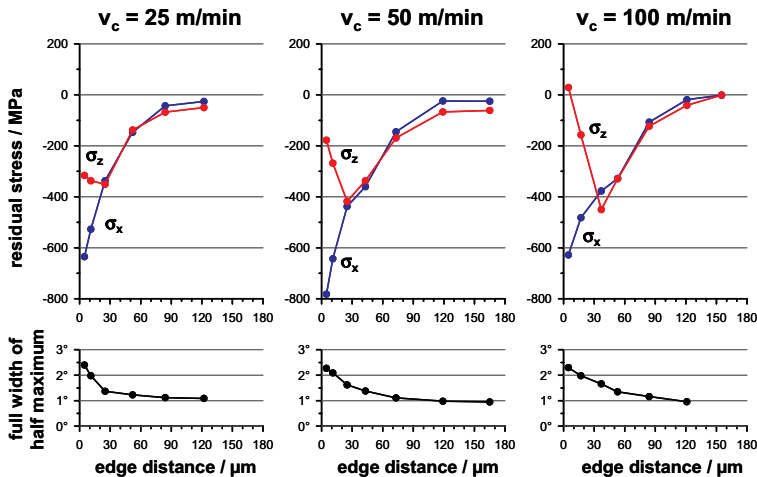


Figure 4-14: Residual stresses determined for a cutting edge radius of $r_\beta = 30 \mu\text{m}$

Among the three examined cutting velocities, the compressive stresses generated in x-direction at the cutting velocities of 50 m/min and 100 m/min reach a level of about -700 MPa, while the stresses for a velocity of 25 m/min reach about -600 MPa.

The determined stresses in x-direction for a cutting edge radius of 20 μm present a similar picture. While the stresses for the cutting velocities of 50 m/min and 100 m/min reach a highly compressive level of -800 MPa, the stresses generated at a cutting speed of 25 m/min show a value of about -650 MPa.

For a cutting edge radius of 30 μm , the determined stresses in x direction reach about -600 MPa for a cutting velocity of 25 m/min or, respectively, 100 m/min, and -800 MPa for a cutting velocity of 50 m/min.

The results of the x-ray diffraction measurements can be summarized as follows:

- The sharp cutting edge induces a low level of compressive residual stresses which moves towards the tensile regime with increasing cutting speed.
- For the sharp cutting edges, the penetration depth of the plastic deformation decreases with increasing cutting velocity.
- Cutting inserts with a defined edge radius $r_\beta \geq 10 \mu\text{m}$ generate highly compressive stresses with a penetration depth of the plastic deformation larger than 60 μm .
- The impact of the plastic deformation generated by cutting edge radii $r_\beta \geq 10 \mu\text{m}$ outweighs the thermal impact on the residual stresses so that no shift towards the tensile stress regime can be observed for the examined cutting conditions.

4.5 Summary of the Experimental Results

The comparison of process forces from helical orthogonal cutting tests with forces obtained from conventional orthogonal cutting experiments showed that helical orthogonal cutting is a fully adequate alternative to the conventional orthogonal cutting processes.

The determined cutting forces F_c as well as the feed forces F_f show a clear increase with increasing cutting edge radius while the dependency on the cutting edge radius is not as strong for F_c but clearly distinguishable. Both force components remain nearly constant with increasing cutting velocity v_c . This can be explained by the restricted range of cutting velocities that has been examined so that the resulting thermal softening is not strong enough to cause a significant decrease in the cutting force F_c .

Due to the fact that no single value for the chip thickness can be assigned to a segmented chip, the determined chip thickness does not show clear tendencies in dependence of the cutting conditions. However, the measured values can still be used as a reference values in the evaluation of the machining model.

The post process examination of all cutting inserts showed no damage or, respectively, catastrophic failure. With increasing cutting edge radius, an increased tendency of the workpiece material to adhere to the cutting insert was observed.

Optical examination of the surface layer by means of micrographs showed plastic deformation of the surface grains. The micrographs as well as the temperature measurements gave no indication of a phase transformation due to machining.

Examination of workpiece specimens by means of x-ray diffraction yielded compressive stresses for all cutting conditions in the direction of the cutting velocity as well as orthogonal to the direction of the cutting velocity. The sharp cutting insert generated the lowest level of compressive stresses and with increasing cutting velocity the stress level shifted further towards the tensile regime. Due to the severe plastic deformation caused by all cutting inserts with an edge radius of $r_\beta \geq 10 \mu\text{m}$, an increased thermal impact with a resulting shift towards tensile stresses was not observed.

5 Simulation Set Up

All simulations performed within this work were conducted using the commercially available FE package DEFORMTM 2D version 8.1. This static implicit code relies on the Updated Lagrangian formulation and was originally designed for the modeling of metal forming processes involving large plastic deformation. Based on the concept of metal cutting as a high-speed forming process involving large plastic deformation at high strains and strain rates, it was soon utilized to model machining processes [CERETTI1996].

With respect to machining, the code allows to model chip formation based on failure criteria as well as on plastic deformation with subsequent remeshing if required. The software also permits the implementation of user defined flow stress models with the flow stress as a function of strain, strain rate and temperature. Furthermore, thermo physical material data can be implemented as a function of temperature. The package offers a thermo-elasto-viscoplastic formulation [FISCHER2005a; SFTC2005].

In contrast to the dynamic explicit approach, the static implicit approach as used here does not take into account dynamic effects such as inertia. Due to the moderate cutting velocities applied throughout this work, inertia effects can be neglected. Details concerning the FE model as well as the employed material data are presented in the following sections.

5.1 FE Model

The FE model used throughout this work has to fulfill several basic requirements. The workpiece needs to be represented by an elasto-viscoplastic object whose mesh is able to capture large stress and strain gradients within the subsurface region of the workpiece. Additionally, the workpiece mesh has to be fine enough to follow the cutting edge radius when the tool deforms the workpiece plastically so that the unavoidable loss of information due to the required discretization still allows a process analysis. To fulfill these two prerequisites, mesh density has to be sufficiently high. Furthermore, the mesh density must not vary significantly throughout the workpiece because a severe variation in mesh density results in a significant

variation in the resolution of the calculated state variables.

The workpiece is represented by a mesh of 6000 linear quadrilateral plane strain elements. The elements used for the simulations that were carried out have a smallest edge length between $4\ \mu\text{m}$ and $2\ \mu\text{m}$.

The software allows the use of so called mesh density windows that allow to control mesh density throughout an object. This approach potentially reduces computation time by reducing the overall number of required elements. However, since the distribution of the state variable throughout the whole workpiece is of interest, the resulting mesh density gradients might obstruct a comparison of the simulations. Instead of using mesh density windows, the software's adaptive meshing capabilities were utilized. Thereby an adaptation of the mesh density to the gradients in the state variables is achieved.

Unless stated otherwise, the two dimensional FE model shown in **Figure 5-1** is used for all simulations performed in this work. The model assumes plane strain. It contains two objects: the elasto-viscoplastic workpiece which moves in x direction with the cutting velocity v_c and the rigid tool which is fixed in x and y direction.

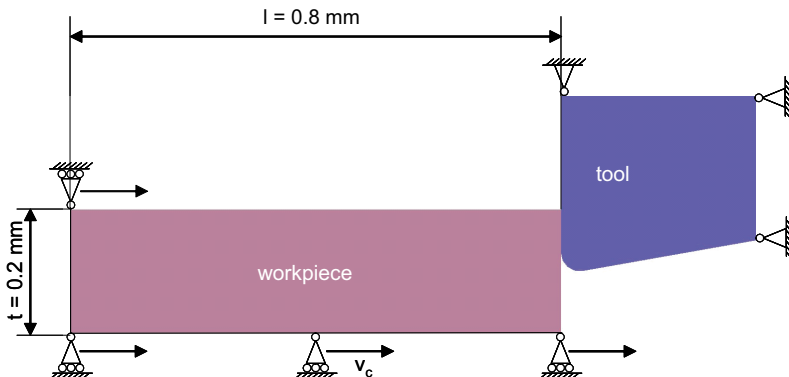


Figure 5-1: 2d FEM model for the orthogonal cutting process consisting of an elasto-plastic workpiece and a rigid tool

As mentioned before, a high mesh density is required to observe the influence of the cutting edge radius on the process. To reduce the number of remeshing cycles the

workpiece experiences as well as computation time, the workpiece was chosen to have a length of 0.8 mm and a height of 0.2 mm. The depth of cut, or uncut chip thickness, for all simulations is 0.1 mm. Preliminary simulations conducted with a workpiece height of 0.4 mm showed that the remaining workpiece thickness of 0.1 mm is sufficient to prevent a significant influence of the boundary conditions on the simulation results.

During the simulations the workpiece is moved with the cutting velocity v_c relative to the tool. This movement occurs in small time increments as they are defined through the workpiece boundary conditions.

Since chip formation in this model is solely based on deformation, the elements, particularly in the vicinity of the cutting edge become increasingly distorted so that a remeshing process is required. The software can perform the mesh generation as well as the interpolation of the boundary conditions and the data from the old mesh to the new mesh automatically. The time increment needs to be chosen carefully. In order to reduce computation time, as well as the number of remeshing cycles and the associated loss of information during data interpolation, a large time increment is desirable. However, the time increment needs to be sufficiently small so that element distortion due to the movement of the object representing the cutting tool is still acceptable. Furthermore, convergence can usually be improved if the time increment is decreased.

In order to improve comparability between the simulations, the time increment was adopted to the cutting velocity so that the number of remeshing cycles at all three cutting velocities is comparable. In **Table 5-1**, the time increments used for the respective cutting velocity are listed. It can be derived from these values that the simulated process times are very short. Since steady state variables such as the tool temperature are not in the focus of this work, the short process times modeled are sufficient. For the simulations performed, the simulated process time amounts to $1.5 \cdot 10^{-4}$ s.

Cutting velocity v_c [m/min]	Time increment Δt [s]
25	$5.0 \cdot 10^{-6}$
50	$2.5 \cdot 10^{-6}$
100	$1.25 \cdot 10^{-6}$

Table 5-1: Time increments as specified in the model for the three examined cutting velocities

The software allows to trigger remeshing by means of four different thresholds that can be specified by the user. These thresholds are:

- the maximum time increment,
- the maximum stroke increment,
- the maximum number of steps and
- the maximum Interference.

In this work, the maximum interference trigger was used. Within the presented machining model, this trigger allows the user to control the maximum overlap between an element edge and the cutting tool as shown in **Figure 5-2**. In conjunction with the element size, this trigger specifies the quality of the discretization.

For the simulations performed within this work, a maximum interference of $1 \mu\text{m}$ was specified. In addition to the user specified remeshing cycles that are triggered by the respective threshold, the software performs a remeshing cycle if severe mesh distortion is detected. The time increment may also be adjusted manually if convergence is poor within a simulation. Both procedures may lead to a slight variation of the number of remeshing cycles between the simulations. The number of remeshing cycles for all simulations was between 40 and 50.

Subsequent to the chip formation, the tool is retracted and a new equilibrium is calculated. The workpiece movement is stopped the workpiece relaxes elastically and is allowed to cool down to room temperature. This situation is shown in **Figure 5-3**.

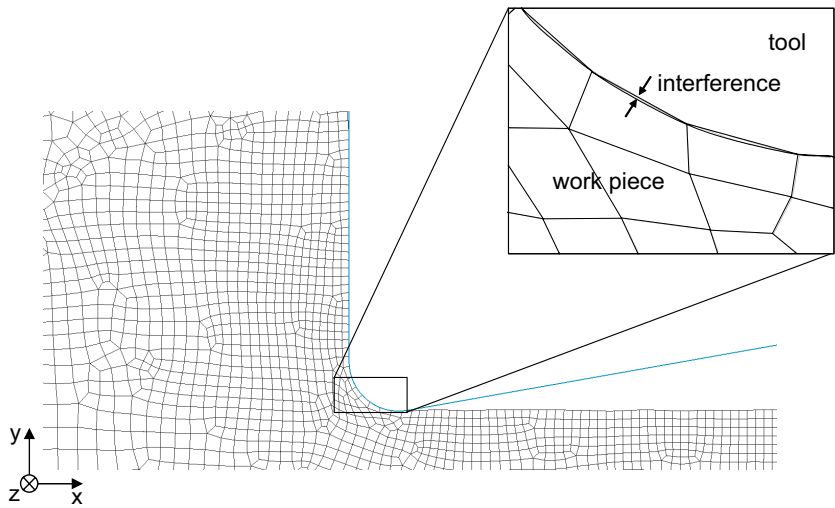


Figure 5-2: Interference between workpiece and cutting tool. Mesh density is reduced for presentation clarity

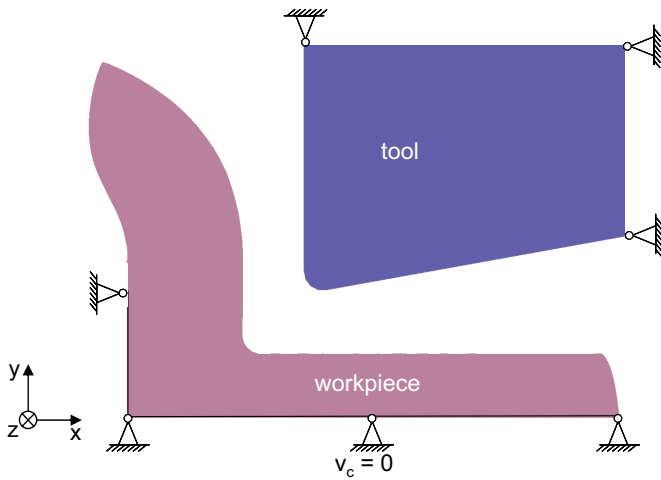


Figure 5-3: Model after the retraction of the cutting tool

5.2 Material Properties

Material properties are among the most influential factors within every FE simulation. The quality of the material properties significantly determines the simulation results. While many properties can be easily determined experimentally, others, such as the flow stress, can be acquired only for a small range of process conditions and have to be extrapolated so that they can be utilized in a machining simulation.

5.2.1 Flow Stress

A variety of flow stress descriptions for the behavior of Ti6Al4V at high strains strain rates and temperatures have been suggested by material scientists. However, so far no description has been accepted by researchers as the one that perfectly describes the material behavior of Ti6Al4V.

Two flow stress descriptions for Ti6Al4V taken from literature are used throughout this work. The first description is suggested by [EL-MAGD2003]. From now on this description will be referred to as “material description 1”. The material is to be described as a function of flow strain, strain rate and temperature:

$$\sigma = F(\varepsilon, \dot{\varepsilon}, T). \quad (5.1)$$

The dependency $\sigma(\varepsilon)$ can be described by a strain hardening power law according to Hollomon:

$$\sigma = K \cdot \varepsilon^n. \quad (5.2)$$

The influence of the velocity in the range of low strain rates is considered by an exponential function:

$$\sigma = \sigma(\varepsilon, T) \cdot \dot{\varepsilon}^m. \quad (5.3)$$

Damping effects are taken into account by introducing a linear damping coefficient η :

$$\sigma = \sigma(\varepsilon) + \eta \cdot \dot{\varepsilon}. \quad (5.4)$$

During plastic deformation at high temperatures, stress relaxation takes place due to creep effects. The unrelaxed flow stress as a function of strain, strain rate and temperature is given by:

$$\sigma_0 = \left(K \cdot \left(\frac{\varepsilon}{0.1} \right)^n \cdot \dot{\varepsilon}^m + \eta \cdot \dot{\varepsilon} \right) \cdot \psi(T) \quad (5.5)$$

The temperature function Ψ is determined based on experimental data which leads to

$$\psi(T) = e^{\left(\frac{T+273}{T_1} \right)} + A^* \cdot e^{\left(\frac{T+273}{T_2} \right)}. \quad (5.6)$$

Taking into account stress relaxation yields:

Description 1:

$$\sigma = \frac{\sigma_0}{\left(1 + \left(\frac{\sigma_0}{\sigma^*} \right)^\nu \cdot e^{\left(\frac{T+273}{T^*} \right)^\nu} \cdot \frac{\dot{\varepsilon}^*}{\dot{\varepsilon}} \cdot 0.1 \right)^{\frac{1}{\nu}}}. \quad (5.7)$$

All parameters were determined by [EL-MAGD2003] for Ti6Al4V based on the experimental split Hopkinson bar data presented in the same work and are shown in **Table 5-2**.

Parameters for description 1	
K [MPa]	1199
m [-]	0.005
n [-]	0.17
η [MPa·s]	0.05995
T^* [K]	111.12
$\dot{\varepsilon}^*$ [s ⁻¹]	$2.073 \cdot 10^{-25}$
ν [-]	3.7
A^* [-]	0.519
T_1 [K]	346
T_2 [K]	1280

Table 5-2: Parameters of the constitutive equations (5.3)-(5.7) [EL-MAGD2003]

Figure 5-4 shows the flow stress behavior of Ti6Al4V as a function of effective plastic strain and temperature as predicted by material description 1 at an effective strain rate of 1000 s⁻¹. It can be seen that strain hardening as well as temperature softening

occur. **Figure 5-5** illustrates the strain rate dependency of the flow stress according to material description 1. An overestimation of the flow stress high strain rates can be observed. The impact of this extrapolated flow stress behavior on the modeling results is discussed in chapter 5.

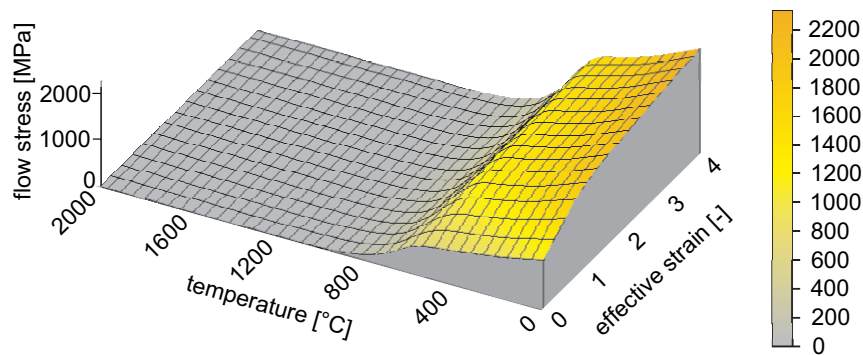


Figure 5-4: Flow stress for Ti6Al4V as a function of effective strain and temperature according to material description 1 for a strain rate of 1000 s^{-1}

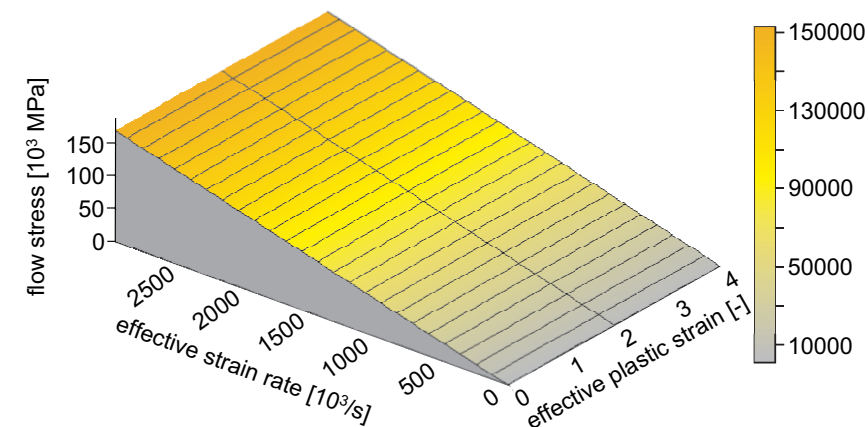


Figure 5-5: Flow stress for Ti6Al4V as a function of effective plastic strain and strain rate according to material description 1 for a temperature of $20 \text{ }^\circ\text{C}$

The second material description used in this work has been originally suggested by the same group of researchers [EL-MAGD2000; EL-MAGD2001; TREPPMANN2001]. The approach for the high speed regime uses a form suggested by [SWIFT1952]:

$$\sigma = K \cdot (B + \varepsilon)^n + \eta \cdot \dot{\varepsilon} \quad (5.8)$$

K, B, n and η are again material parameters which were determined by means of experiments. The parameters K and n are temperature dependent with the following dependency:

$$K(T) = K^* \cdot \Psi(T), \quad (5.9)$$

$$n(T) = n^* \cdot \Psi(T) \text{ and} \quad (5.10)$$

$$\Psi(T) = e^{-\left(\frac{T}{T_{MT}}\right)^\mu} \quad (5.11)$$

The constants K^* , n^* , T_{MT} and μ were also determined based on experimental data.

Like material description 1, this material description overestimates the flow stress for very high strain rates. [BÄKER2004] modified this material description by including a logarithmic strain rate dependence from a Johnson-Cook approach ([JOHNSON1983]) rather than a linear dependence. While BÄKER does not claim that this particular modification captures the real material behavior, it still reduces the error at high strain rates. The modified material description has the following form:

$$\sigma(\varepsilon, \dot{\varepsilon}, T) = \left(K(T) \cdot (B + \varepsilon)^{n(T)} \right) \cdot \left(1 + C \cdot \ln \left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \right) \right) \quad (5.12)$$

[BÄKER2004] determined the parameters C and $\dot{\varepsilon}_0$ by assuming that all other parameters could remain unchanged. Reference points at two different strain rates (10^3 s^{-1} and 10^4 s^{-1}), a plastic strain of 0.1 and a temperature of 673 K were selected. Then the constants C and $\dot{\varepsilon}_0$ were determined so that the calculated flow stress values for the original description and the modified description were the same for the two selected points.

Table 5-3 contains all parameters that were used in connection with material description 2.

Parameters for description 2		
C [-]		0.302
$\dot{\epsilon}_0^*$ [s ⁻¹]		774
K* [MPa]		2260
n* [-]		0.339
T _{MT} [K]		825
μ [-]		2
B [-]		0

Table 5-3: Parameters of the constitutive equations (5.8)-(5.12) [EL-MAGD2000; EL-MAGD2001; TREPPMANN2001; BÄKER2004]

Figure 5-6 shows the flow stress for Ti6Al4V as a function of effective strain and temperature according to material description 2 for a strain rate of 1000 s⁻¹. Again, strain hardening and temperature softening are clearly visible.

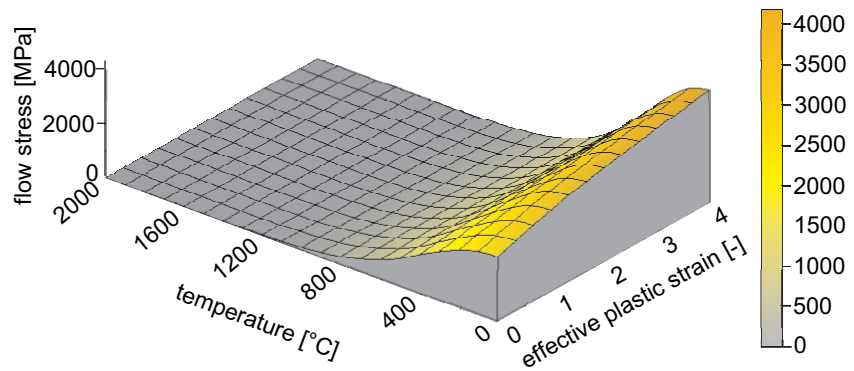


Figure 5-6: Flow stress for Ti6Al4V as a function of effective strain and temperature according to material description 2 for a strain rate of 1000 s⁻¹

However, as **Figure 5-7** illustrates, strain rate dependency is significantly lower for high strain rates when compared to material description 1.

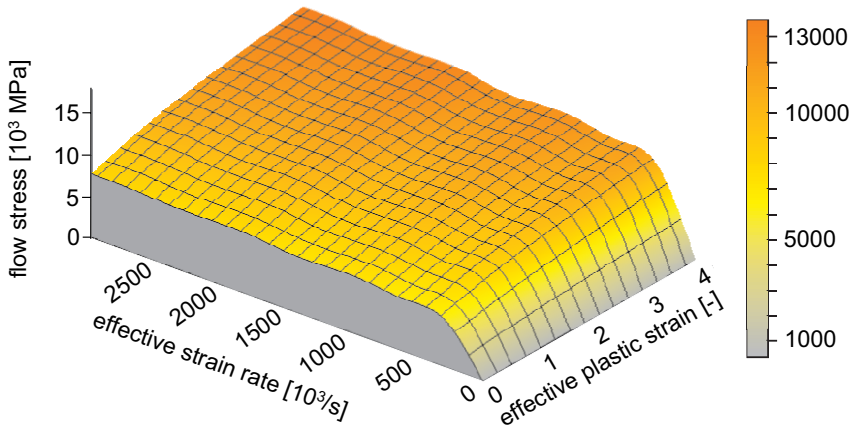


Figure 5-7: Flow stress for Ti6Al4V as a function of effective plastic strain and strain rate according to material description 2 for a temperature of 20 °C

5.2.2 Mechanical and Thermo Physical Properties

The mechanical and thermo physical material data used originates from the literature [RICHTER1988; MÜLLER2004]. Temperature dependency of heat capacity c_p , Young's modulus E , thermal conductivity λ and linear thermal expansion α should be taken into account while the influence of the linear thermal expansion will be discussed in depth later.

It is assumed that the newly generated surface has not undergone the phase transformation from $\alpha + \beta$ to β , which takes place at the β -transus temperature of 980 °C. Depending on the calculated conditions during the simulation, however, the model might require material data for temperatures that exceed the experimentally determined range. Therefore, values for temperature dependent data up to a temperature of 1500 °C are provided.

Figure 5-8 shows the specific heat capacity c_p , the thermal conductivity λ , Young's modulus E and the Poisson ratio ν as functions of temperature.

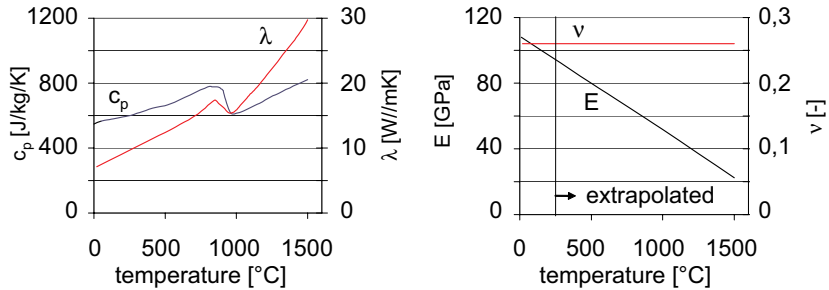


Figure 5-8: Temperature dependent material properties for Ti6Al4V. Left: Specific heat capacity and thermal conductivity, [MÜLLER2004]; right: Young's modulus and Poisson ratio, [RICHTER1988]

Figure 5-8 shows data for the linear thermal expansion coefficient and the density of Ti6Al4V as they were obtained from literature [RICHTER1988].

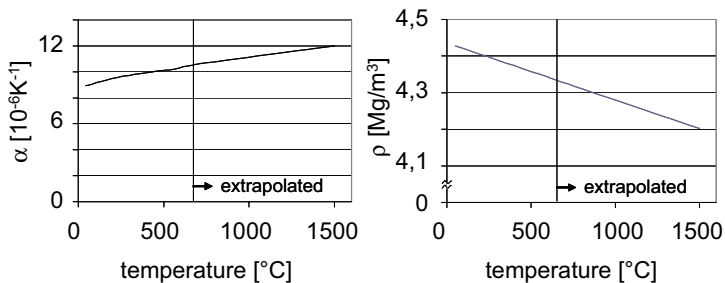


Figure 5-9: Linear thermal expansion coefficient and density of Ti6Al4V as a function of temperature, [RICHTER1988]

Thermal conductivity and specific heat capacity were determined experimentally by [MÜLLER2004]. Both properties show a decrease between 900 °C and 1000 °C followed by an increase. This behavior is due to the previously mentioned phase transformation from $\alpha + \beta$ to β .

In order to provide a sufficient temperature range of the elastic material data for the

simulations, the data for Young's modulus, Poisson ratio, linear thermal expansion coefficient and density that were determined by [RICHTER1988], were linearly extrapolated up to a temperature of 1500 °C so that phase changes are not accounted for in these data.

5.2.2.1 Influence of linear Thermal Expansion

The influence of the thermal expansion coefficient on the simulation results needs to be addressed in depth. While the plane strain assumption for orthogonal cutting from a mechanical point of view is legitimate, it is questionable whether this also applies to the concept of thermal expansion. As mentioned before, the plane strain model does not allow any material flow in the out of plane direction. Since the material can not expand in the out of plane direction, the corresponding stresses in this direction increase.

Figure 5-10 shows the stress distribution of a workpiece as it is used in the machining simulations. The object is exposed to a heat source with a temperature of 800 °C without the application of any mechanical load.

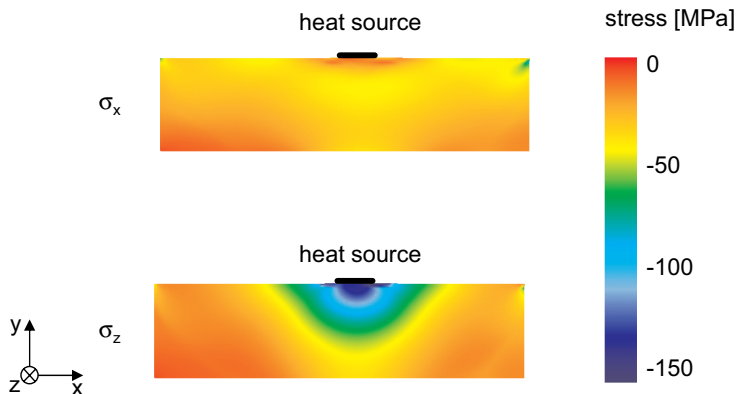


Figure 5-10: Stresses due to thermal expansion; temperature of the heat source $\vartheta = 800 \text{ °C}$

The thermal load induces stresses in x and z direction which, during a machining operation, contribute to the local stress state. During orthogonal cutting the material

flow may be considered restricted in the out of plane direction. Thermal expansion, however, is not restricted. While the stresses in x direction are in agreement with the assumption of an orthogonal process, the stresses in z direction contribute to an unrealistic state of stress which in return influences material flow.

The impact of this effect on the calculated residual stresses is shown in **Figure 5-11**. While the simulation including thermal expansion predicts tensile stresses orthogonal to the machining direction (z-direction) virtually over the whole machined surface, the simulation that does not take into account thermal expansion predicts alternating compressive and tensile stresses. The reason for the difference in the two stress predictions lies within the plane strain approach. In the plane strain model, the workpiece material can not expand in z-direction. During the machining simulation the thermal expansion contributes to the state of stress and therefore influences material flow. When the workpiece cools down after machining, the material can not contract in z-direction due to the plane strain assumption so that the stress level moves to the tensile regime.

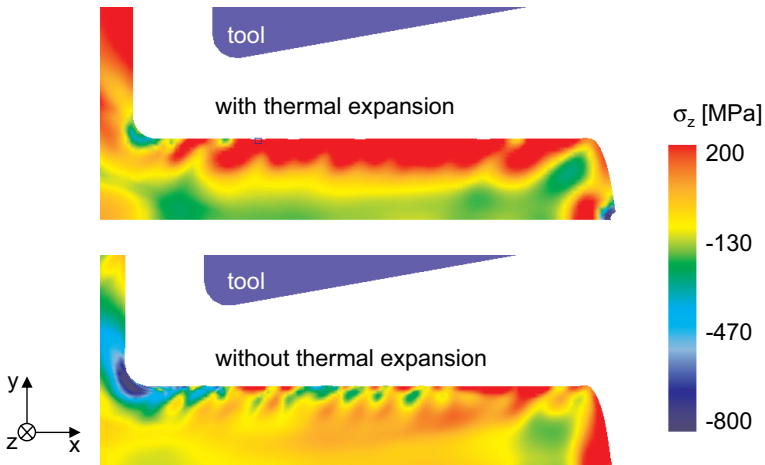


Figure 5-11: Residual stresses as predicted in z direction with thermal expansion (top) and without thermal expansion (bottom); $r_\beta = 30 \mu\text{m}$, $v_c = 25 \text{ m/min}$

A similar observation can be made when examining the residual stresses predicted in machining direction (x-direction), **Figure 5-12**. While the model predicts continuous tensile stresses in the machined surface when considering the thermal expansion, the same model without thermal expansion predicts a less homogeneous stress field.

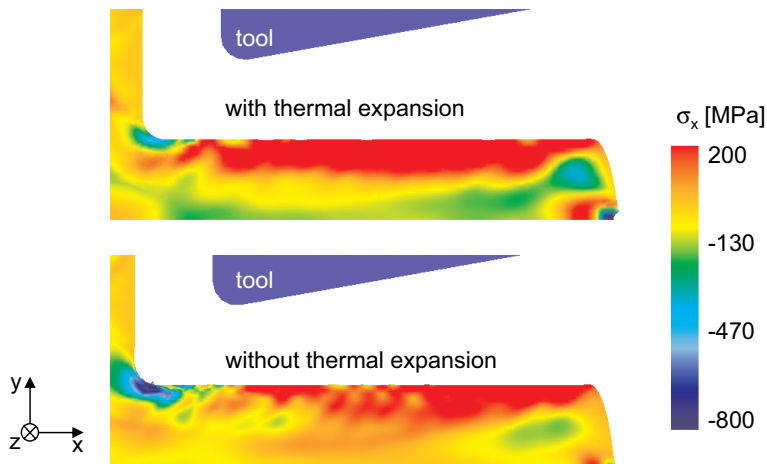


Figure 5-12: Residual stresses as predicted in x direction with thermal expansion (top) and without thermal expansion (bottom); $r_\beta = 30\ \mu\text{m}$, $v_c = 25\ \text{m/min}$

Due to the distortion of the state of stress during machining that is caused by the thermal expansion coefficient, the thermal expansion coefficient is not further taken into account. Thermal effects are still taken into account as far as the temperature influence on the plastic deformation is concerned.

Within this work, the workpiece material is considered a homogeneous material that consists of only a single phase so that the difference in material properties between the α -phase and the β -phase of this alloy is not taken into account.

Table 5-4 gives an overview over the material properties that are taken into account within the machining model.

Property	Symbol	Value
Fraction of deformation energy transformed into heat	β	0.9
Density	ρ	Figure 5-9
Poisson ratio	ν	Figure 5-8
Young's modulus	E	Figure 5-8
Emissivity	ε	0.3
Thermal conductivity	λ	Figure 5-8
Specific heat capacity	c_p	Figure 5-8

Table 5-4: Overview over the material properties used in the simulations

5.3 Friction

As discussed in chapter 2.2.2.3, significant number of models is available. Within this work friction between tool and workpiece is described using the most commonly utilized approach according to the shear factor model. Another approach that is frequently used in metal forming is a modified Coulomb law that prevents the surface traction on the tool from exceeding the local shear flow stress of the workpiece.

For the simulations conducted within this work the shear factor model was chosen because the complex friction models that can be found in literature are usually tailored to certain combinations of tool and workpiece material. Furthermore, convergence of the elasto-plastic solution is influenced negatively by the implementation of complex friction models. This can lead to a premature stop of the simulation due to non-convergence.

6 Simulation Results

6.1 Forces and Temperatures Predicted by the Rigid-Viscoplastic Model

In order to provide a basis for the variation of the FE model, the whole set of experimental process parameters is modeled using both flow stress descriptions given in chapter 5.2.1 in connection with a rigid-viscoplastic model. For these initial simulations, friction is considered to be zero. Friction will be taken into account at a later stage in this work, however.

The cutting forces yielded by the rigid-viscoplastic model using both material descriptions are shown in **Figure 6-1**. Both material models yield very similar results. The models predict continuous chips; chip segmentation can not be observed. Details on the prediction of the chip mode will be discussed later.

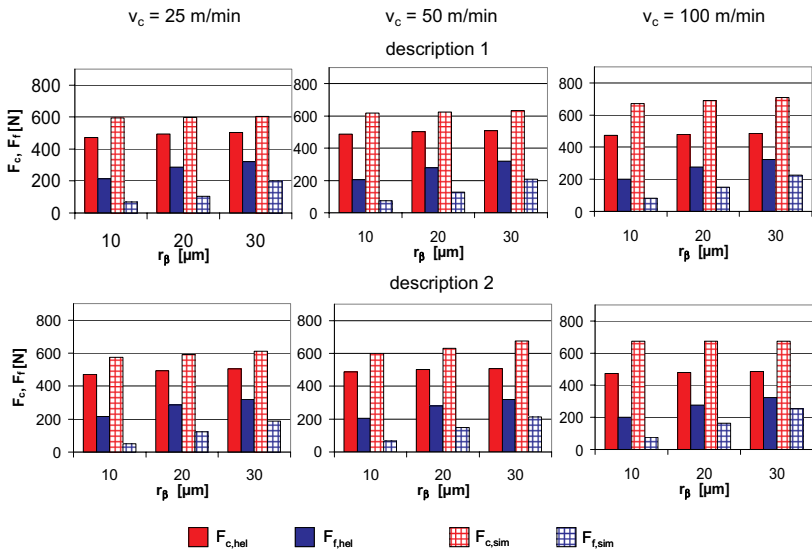


Figure 6-1: Cutting forces and feed forces determined experimentally (index “hel”) and as predicted (index “sim”) by the rigid-viscoplastic model without friction. Top: flow stress description 1; bottom: flow stress description 2

It can be recognized that cutting forces are generally overestimated, while feed forces are significantly underestimated. However, it can also be seen that the

simulation results partly follow the experimental trend. With increasing cutting edge radius, the cutting force F_c increases slightly. This trend is more prevalent in the simulations than in the experimental investigations. This could be a result of the assumption that the strain rate dependency of the flow stress is of linear nature. An increase of the cutting velocity does not cause a significant increase in the experimental cutting forces. The simulation, however, shows a clear increase of the cutting force with increasing cutting velocity.

With respect to the feed forces F_f , both material descriptions predict forces that follow the experimental trend, but are significantly lower than the experimentally determined feed forces which can be explained by the absence of friction. An increase in the cutting edge radius leads to a clearly visible increase in the feed forces which is confirmed by the experiments.

Figure 6-2 shows example curves for the process forces as they are predicted by simulations based on both flow stress descriptions. Both descriptions predict a very similar level of feed forces F_f .

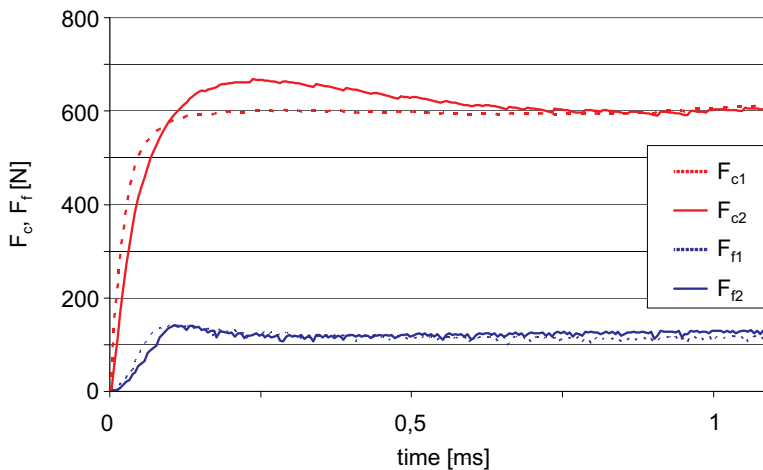


Figure 6-2: Example for the predicted curves of cutting force F_c and feed force F_f for flow stress description 1 and 2 ($r_\beta = 20 \mu\text{m}$ and $v_c = 25 \text{ m/min}$; $f = 0.1 \text{ mm}$)

With reference to the cutting forces F_c , description 1 shows a steep increase as soon

as the tool engages with the workpiece and then asymptotically approaches a constant value. Based on description 2, the cutting force F_c the initial increase of F_c is less steep, but reaches a higher value and then drops to the same level as predicted based on description 1.

Despite both flow stress descriptions yield similar results for the cutting forces, the predicted chip shape and chip curvature differs. As **Figure 6-3** shows, description 1 generates a thicker chip with the chip curvature being reduced compared to the predictions based on description 2. The maximum temperatures that were predicted are located in the chip root and amount to 720 °C for description 1 and 690 °C for description 2.

Compared to the experimental results, the calculated temperatures are underestimated which can be explained by the assumption of zero friction for these simulations.

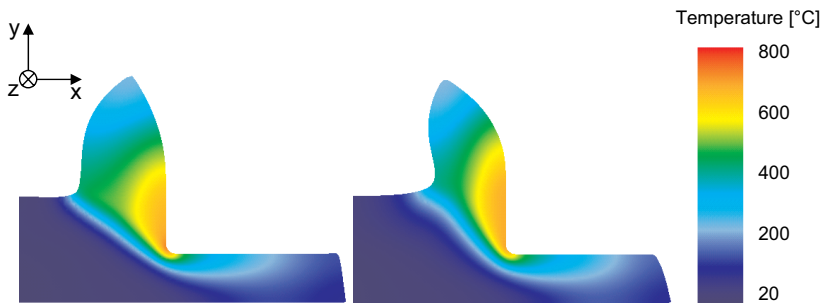


Figure 6-3: Chip shape and temperature distribution predicted by flow stress description 1 (left) and description 2 (right); ($r_\beta = 20 \mu\text{m}$ and $v_c = 25 \text{ m/min}$)

6.2 Forces and Temperatures Predicted by the Elasto-Viscoplastic Model

Subsequent to the simulations based on the rigid-viscoplastic formulation, simulations were run using an elasto-viscoplastic formulation. The process force predictions resulting from these simulations are shown in **Figure 6-4**.

Compared to the forces predicted by the rigid-viscoplastic formulation, the consideration of elastic properties does not have a significant impact on the predicted

forces. The trends are unchanged and forces vary slightly compared to the rigid-viscoplastic formulation.

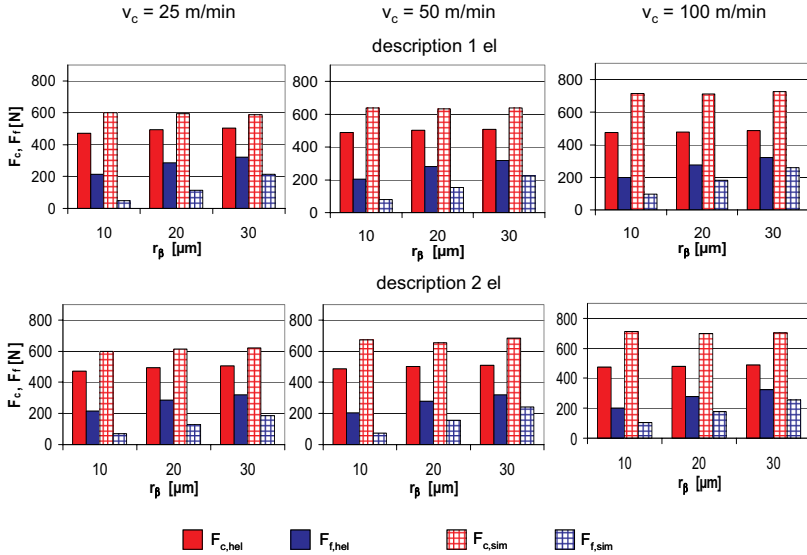


Figure 6-4: Cutting forces and feed forces as determined experimentally (index “hel”) and as predicted (index “sim”) by the elasto-viscoplastic model without friction. Top: flow stress description 1; bottom: flow stress description 2

The similarities in the predictions based on the rigid-plastic and the elasto-plastic model come to an end when chip thickness, chip curvature and the distribution of plastic strain are compared. The rigid-plastic model predicts a chip that is about 15% thinner than the one predicted by the elasto-plastic model. The predicted chip curvature of the rigid-plastic simulation is slightly higher than the one predicted by the, i.e. the chip curls with a smaller radius.

6.3 Strain Distribution

Location and absolute value of the maximum effective strain on the bottom of the chip or, respectively, in the area that represented the tool chip contact zone during machining, vary significantly. While the maximum effective strain in the plastic model occurs within the contact zone in a distance of about 0.05 mm from the chip root, the

maximum value resulting from the elasto-plastic simulation occurs in a distance of 0.17 mm (**Figure 6-5**).

The effective strains in the surface layer predicted by the elasto-viscoplastic model are lower compared to the ones predicted by the rigid-viscoplastic model. This is reasonable since in the elasto-viscoplastic model the workpiece can deform elastically before plastic deformation occurs. **Figure 6-5** illustrates this by showing the effective strain over the distance from the surface along the lines A-B or, respectively, the line E-F.

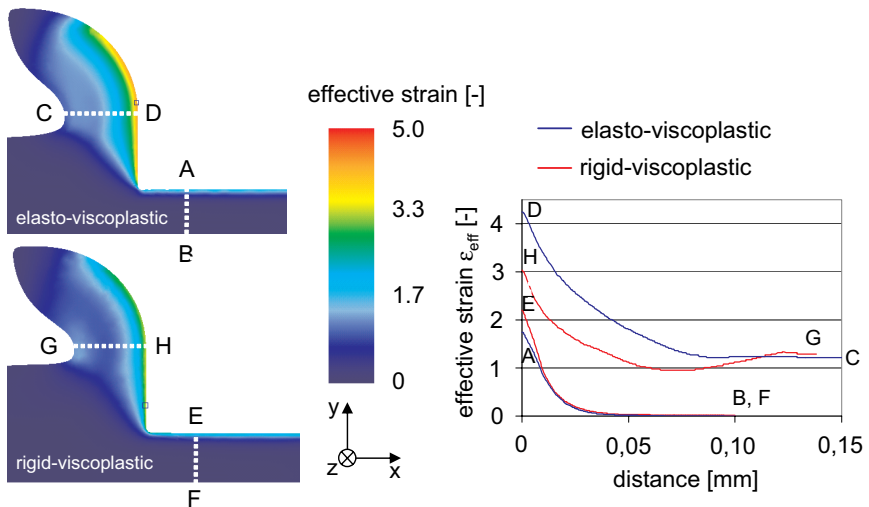


Figure 6-5: Effective plastic strain distribution predicted by the elasto-viscoplastic model and the rigid-viscoplastic model for $r_b = 10 \mu\text{m}$ and $v_c = 100 \text{ m/min}$; flow stress description 2

While the predicted strain level in the workpiece surface differs between the elasto-viscoplastic and the rigid-viscoplastic model, the predicted penetration depth of the plastic deformation can be considered equal.

With reference to the chip, the distribution of the effective strain for both models along the lines D-C or, respectively, H-G is shown in **Figure 6-5**. It can be observed that the elasto-plastic model predicts significantly higher strains on the bottom of the chip compared to the plastic model. Since the predicted temperatures for both

simulations do not show significant differences, an increased plastic deformation due to increased thermal softening can be excluded. A possible explanation is a change of the contact conditions between tool and workpiece due to the elastic contact.

Despite **Figure 6-5** only shows modeling results based on flow stress description 2, the statements given in this section are valid for both descriptions.

6.4 Contact Length and Normal Stress Distribution

The consideration of elastic material properties also influences the total contact length and the normal stress distribution along the contact zone. The total contact length consists of the contact between tool and workpiece on the rake face as well as the tool-workpiece contact at the cutting edge radius. Based on the model the total contact length is determined by the distance between the first and the last node of the workpiece that is in contact with the tool.

In **Figure 6-6** the predicted total contact length between tool and workpiece is shown for the elasto-plastic model (line A_e-B_e) and the rigid plastic model (line A_p-B_p).

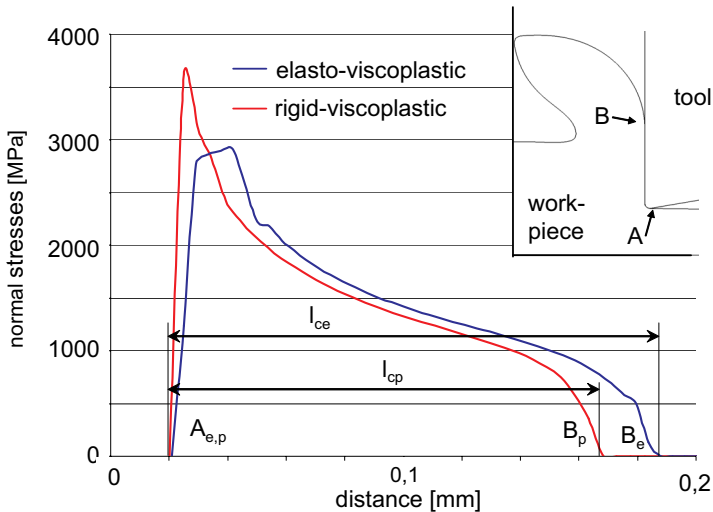


Figure 6-6: Normal stress distribution along the cutting tool and contact length l_c as predicted by the elastic-plastic (line A_e-B_e) and rigid plastic model (line A_p-B_p); $v_c = 25$ m/min, $r_{\beta} = 10$ μ m, flow stress description 2

As the figure shows, the total contact length predicted by the elasto-plastic model is larger than the one predicted by the rigid plastic model due to the elastic contact. Furthermore, the maximum normal stress predicted by the rigid plastic model is significantly higher. The peak in the normal stress distribution and the difference in the predicted total contact length can have a significant influence on the simulation results if or example the normal stress is used as an input variable for tool wear calculations.

The predictions for the total contact length at all examined cutting conditions is shown in **Figure 6-7**. It can be stated that the predicted total contact length l_c increases for all cutting conditions due to the application of the elasto-plastic model. The predicted total contact length for both formulations decreases or stagnates for all and cutting edge radii.

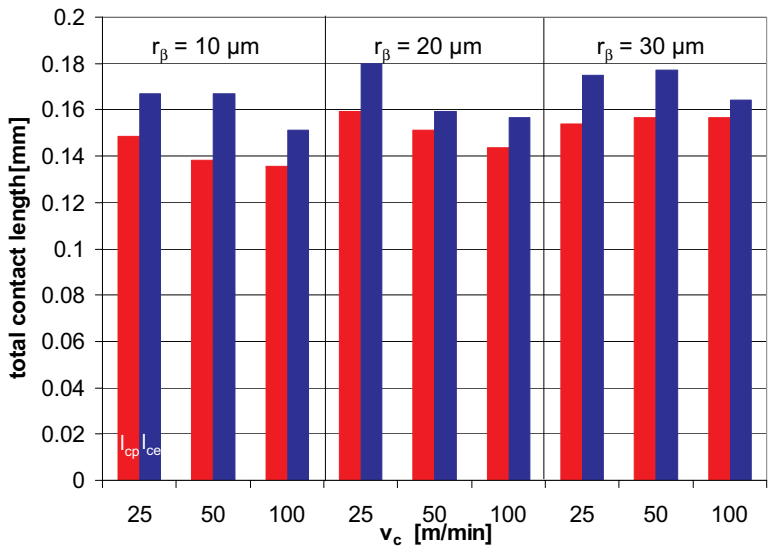


Figure 6-7: Comparison between the prediction of the total contact length l_{cp} predicted by a rigid plastic and l_{ce} by an elasto-plastic model (material description 2)

6.5 Predicted Temperatures

Figure 6-8 shows the temperature distribution for the two cutting conditions that generate the lowest or, respectively, the highest temperatures. The predicted temperatures are within the range of the experimentally determined values. The β -transus temperature is not reached in any simulation.

The difference in chip shape and chip thickness results from the flow stress description and the absence of chip segmentation.

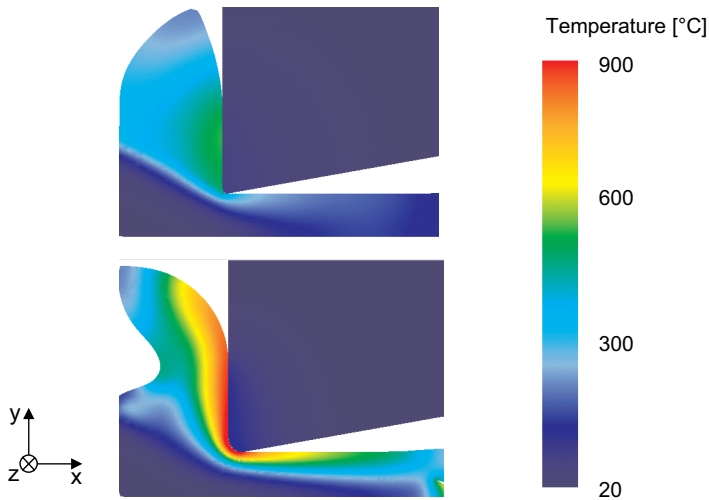


Figure 6-8: Temperature distribution in the workpiece as predicted for $r_\beta = 10\mu\text{m}$, $v_c = 25\text{ m/min}$ (top) and for $r_\beta = 30\mu\text{m}$, $v_c = 100\text{ m/min}$ (bottom)

6.6 Residual Stresses and Strains Subsequent to Machining

Residual stresses and strains predicted by the elasto-plastic model are obtained by retracting the tool from the workpiece. Subsequently, a new equilibrium is calculated.

Figure 6-9 to **Figure 6-12** give an overview over the residual strains and stresses as predicted by the simulations for a cutting edge radius of $r_\beta = 30\mu\text{m}$. The corresponding figures for the cutting edge radii of $10\mu\text{m}$ and $20\mu\text{m}$ can be found in the appendix (**Figure 9-1** to **Figure 9-8**).

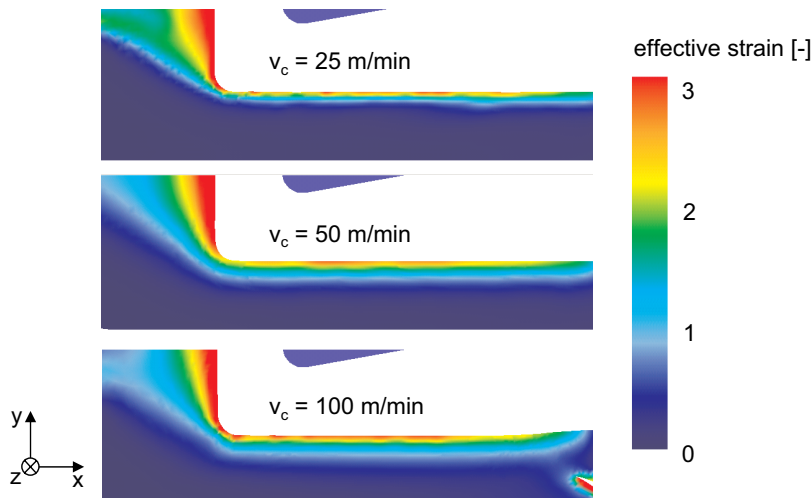


Figure 6-9: Effective strain distribution $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 30 \mu\text{m}$

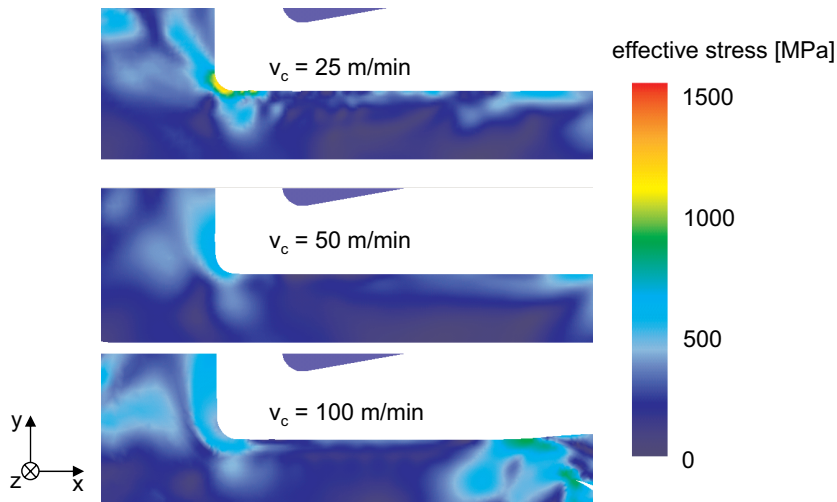


Figure 6-10: Effective stress distribution $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 30 \mu\text{m}$

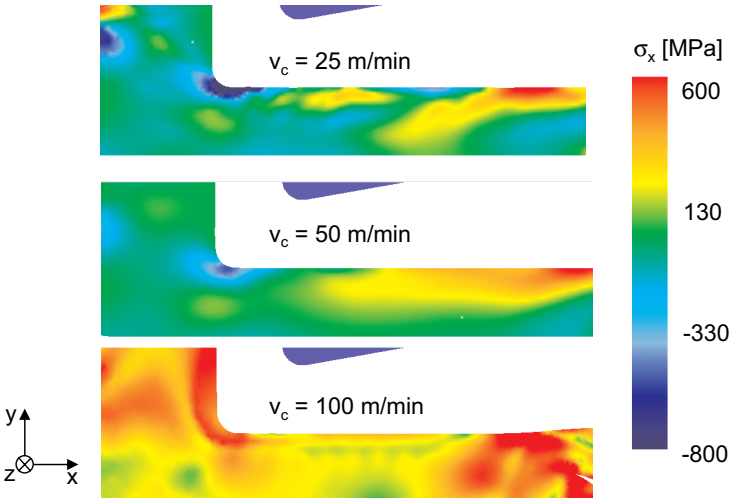


Figure 6-11: Distribution of σ_x ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 30\text{ }\mu\text{m}$

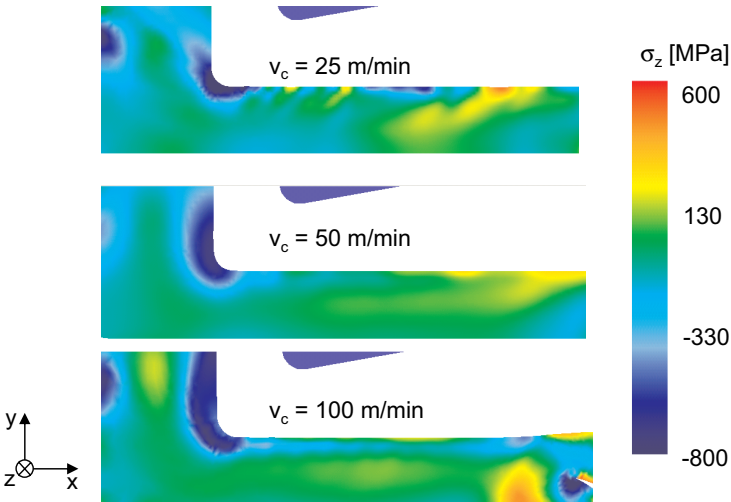


Figure 6-12: Distribution of σ_z ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 30\text{ }\mu\text{m}$

The figures show the residual effective strain and effective stress distribution as well as the distribution of σ_x (stress in machining direction) and σ_z (stress orthogonal to the machining direction).

It can be observed that the depth profile of the effective plastic strain for all cutting conditions is almost evenly distributed while the strain directly at the surface shows local peaks. An increase in the cutting velocity leads to an increased depth of the plastically deformed layer (also referred to as penetration depth) while the peak values of the effective strain at the surface also increase.

An increase of the cutting edge radius causes an increase in the absolute strain values while the penetration depth increases only slightly, **Figure 6-13**. The value of 2.8 for the effective strain was never exceeded.

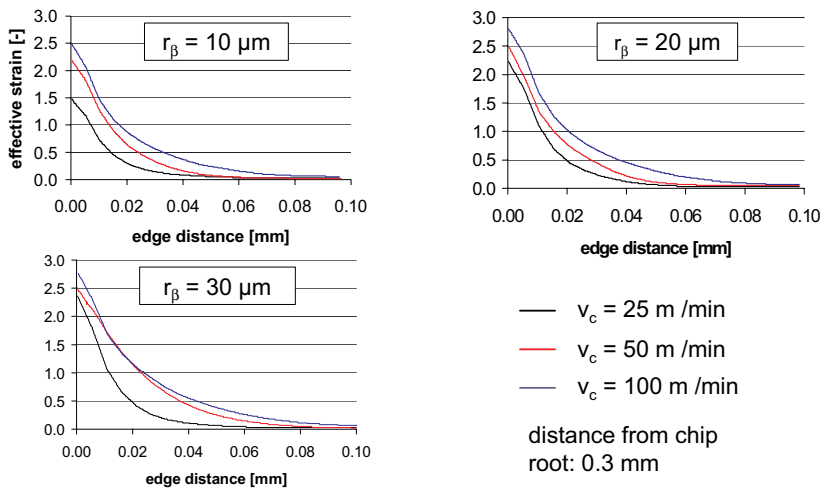


Figure 6-13: Depth profiles of the effective plastic strain for a cutting edge radius of $r_\beta = 10 \mu\text{m}$, $20 \mu\text{m}$ and $30 \mu\text{m}$ in a distance of 0.3 mm from the chip root

An increase of the cutting velocity leads to an increase in the penetration depth. The penetration depth can be compared to the „full width of half maximum“ (FWHM) determined during the residual stress measurements since this value is a measure for the plastic deformation. **Figure 6-14** shows the comparison between penetration

depth compared to the experimentally determined value for the FWHM. While the absolute values do not agree, it can be seen that similar general characteristics can be identified. The maximum cutting velocity of 100 m/min causes the highest penetration depth for the plastic deformation in the experiment as well as in the simulations.

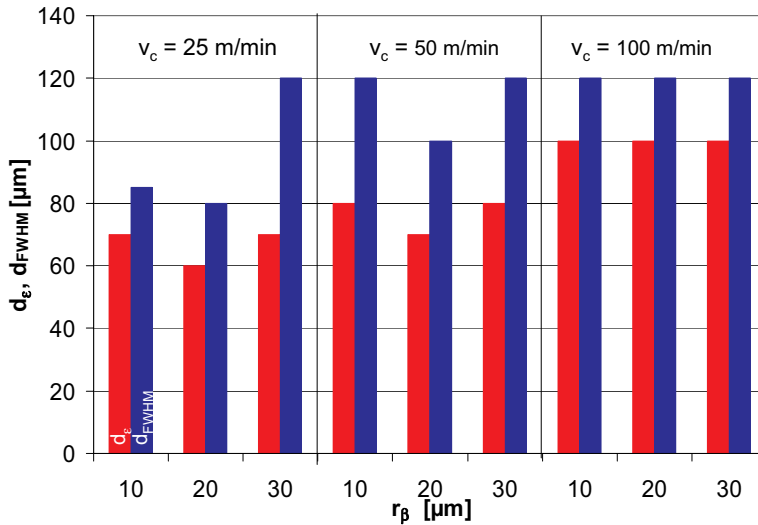


Figure 6-14: Comparison between predicted penetration depth of the effective plastic strain and the penetration depth based on the „full width of half maximum“ (FWHM) values from x-ray diffraction

The predicted effective stress distribution for all cutting edge radii at a velocity of 25 m/min is inhomogeneous throughout the workpiece surface. With increasing cutting velocity, the effective stress distribution becomes more homogeneously while the penetration depth increases. For a cutting edge radius of 10 μm this effect is most distinctive and it decreases with increasing cutting edge radius.

For all cutting conditions the chip root shows the highest concentration of effective stress which was to be expected. With increasing cutting velocity, the stress localization at the chip root decreases together with the absolute stress level in the chip root.

The distribution of the stress in machining direction (σ_x) at a cutting velocity of 25 m/min shows zones of alternating compressive and tensile stresses for all examined cutting edge radii while the stresses shift towards the compressive regime with increasing cutting edge radius. With increasing cutting velocity, the stress level moves towards the tensile regime. The highest tensile stress level can be observed for a cutting velocity of 100 m/min and a cutting edge radius of 10 μm .

Orthogonal to the machining direction, the stress σ_z shows the same tendencies in its distribution. An increase in the cutting velocity moves the predicted residual stresses towards the tensile regime while an increase in the cutting edge radius induces more compressive stresses.

The absolute stress values are strongly depending on the location due to the local material flow that is determined by the flow stress description.

6.7 Summary of the Modeling Results

Both flow stress descriptions used in a rigid-plastic model deliver nearly identical results for the predicted cutting forces while chip thickness and chip curvature differ. For both material descriptions the cutting force is overestimated while the feed force is underestimated. While both descriptions deliver similar results, this work focuses on material description 2 for two reasons:

- Description 1 yielded a very high chip thickness as soon as friction was implemented.
- While the overestimation of the flow stress for high cutting velocities does not effect the predicted process forces for the examined cutting conditions, the overestimation inhibits any shear localization.

The modeling results indicate that the elastic material properties have a significant influence on the predictions of a machining simulation. The magnitude of this influence depends on the type of information that is expected to be delivered.

The application of an elasto-plastic formulation does not cause any significant changes in the process forces. However, the distribution of plastic deformation is strongly influenced. While the level of effective strain on the workpiece surface is

reduced in the elastic model, the effective strain in the chip cross section and particularly at the bottom of the chip increases. Due to the change in plastic deformation, the chip curvature is reduced while the chip thickness remains constant.

The trends in the predicted residual strains and stresses follow the trends described in literature. However, the absolute values and the transition from compressive to tensile stresses that was predicted by the model do not agree with the experimentally determined data. While a phase transition of the workpiece material during the machining process is highly unlikely, the dual-phase characteristics of the alloy Ti6Al4V may contribute to the observed deviations. Also it should be noted that the residual stress measurements were only performed on the α -phase of the material and that the stress state of the β -phase is not known.

Since thermal expansion of the workpiece generates an unrealistic state of stress in a plane strain model, it was not taken into account, which also introduces an error into the predicted residual stresses.

The temperatures predicted by the model are in agreement with the experimentally determined temperatures.

Chip formation was modeled based on plastic deformation. The model predicts a continuous chip which can not be observed in the experiments. It was shown that a serrated chip can be generated, but the mechanisms that lead to the generation of a serrated chip are still subject to research. The introduction of artificial chip serration would merely introduce another unknown to the model which obstructs the analysis of the modeling results.

7 Discussion

7.1 Influence of Friction

The modeling results that have been presented so far did not take into account friction. As indicated before, the shear factor model will be used to incorporate friction into the model.

The implementation of the friction law in combination with description 2 results in a significant change of the process forces as shown in **Figure 7-1**.

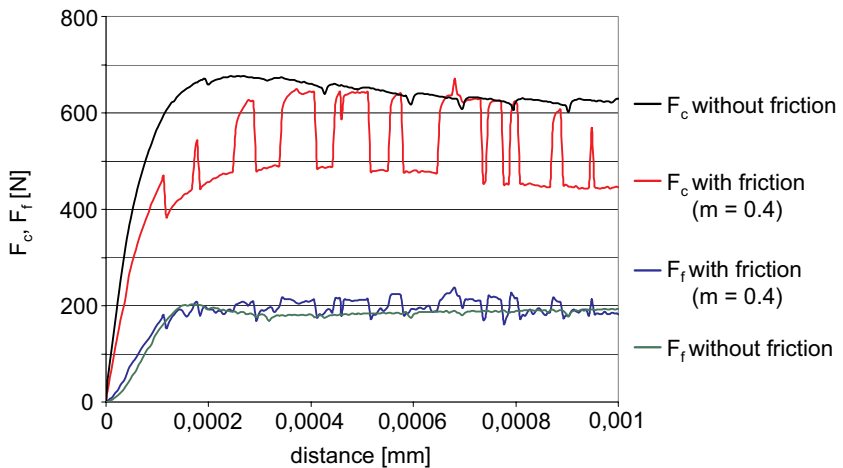


Figure 7-1: Force predictions of the elasto-plastic model with and without friction. Description 2, $v_c = 25\text{m/min}$ $r_{\beta} = 30\text{ }\mu\text{m}$, $m = 0.4$

The cutting force predicted by the model without friction is constant except from the increase caused by the upsetting of the material at the beginning of the chip formation.

When taking into account friction, the predicted cutting force and feed force fluctuate as displayed in **Figure 7-1**. This fluctuation can be observed for all cutting edge radii and for the cutting velocities of 25 m/min and 50 m/min. However, the generated chip still has to be considered continuous. The fluctuations in the process forces were not observed for the rigid-plastic model.

The examination of this effect showed that the fluctuation is independent of obvious numerical influences such as mesh density and remeshing cycles. Furthermore, this effect only occurred in the simulations based on flow stress description 2. This is reasonable, because flow stress description 1 overestimates the flow stress for high strain rates as they would occur during deformation localization and therefore inhibits localization itself. It was found that the strain rate in the shear zone of the respective simulations fluctuates in phase with the observed fluctuation in the cutting force. These results suggest that the force fluctuation is rather caused by the physical process model than by the underlying numerical procedures or boundary conditions.

As **Figure 7-2** (left) shows, the model exhibits a strong shear localization at a cutting velocity of $v_c=25$ m/min. This shear localization is similar to the one described by [BÄKER2004] in the formation of segmented chips.

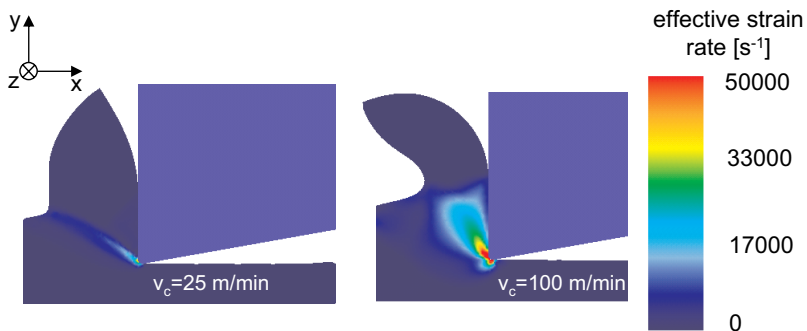


Figure 7-2: Effective strain rate at $v_c=25$ m/min and $v_c=100$ m/min; $r_\beta = 10 \mu\text{m}$

This observation was also made for a cutting velocity of $v_c = 50$ m/min. For the cutting velocity of 100 m/min the fluctuation in the cutting force could not be observed. As Figure 7-2 (right) shows, no shear localization takes place, despite the maximum predicted strain rate of $120,000 \text{ s}^{-1}$ is more than twice as high compared to the maximum strain rate predicted at a cutting velocity of 25 m/min. Additionally, the plastic deformation is spread over a considerably larger area. Obviously, for the cutting velocity of 100 m/min the material description leads to an increased flow stress that prevents localization.

The effect of friction on the model for the cutting velocities of 25 m/min and 50 m/min can be described as follows: Due to the introduction of friction into the model and the resulting fluctuation, the predicted cutting forces F_c start to fluctuate and their average value drops significantly while the average feed force increases. This fluctuation can also be seen in the distribution of the effective plastic strain, **Figure 7-3**. Therefore, this phenomenon can be considered a pre stage of chip segmentation.

The fluctuation in process forces suggests, that it is possible to determine a friction factor m that lowers the predicted average cutting force F_c while increasing the predicted feed force F_f .

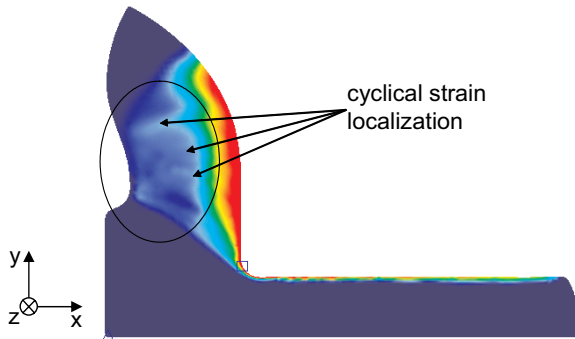


Figure 7-3: Irregularities in the distribution of the effective plastic strain; $v_c = 25\text{m/min}$, $r_\beta = 10\text{ }\mu\text{m}$

In order to determine the friction factor, a sensitivity analysis was performed. A value of $m = 0.4$ yielded better results concerning the cutting forces, but the feed forces were still underestimated so that m was increased. Despite the increased friction along the rake face leads to an increase in the predicted feed forces F_f , an increase of m up to a value of 0.7 leads to unrealistically thick chips. Due to the heavy upsetting of the chip, the average cutting force increases so that it becomes impossible to match cutting force and feed force simultaneously. The investigations performed by [HOPPE2003] showed that it is possible to fit process forces by modifying the coefficient of a Coulomb friction law. For a shear factor friction law, however, the surface traction also depends on the local flow stress so that a match of

process forces is only possible if the flow stress description captures the material behavior properly.

Figure 7-4 shows the process forces as predicted based on flow stress description 2 using a shear factor friction law with a factor of $m = 0.4$. While the average cutting force for the cutting velocities of 25 m/min and 50 m/min drop due to the described fluctuations, the cutting forces for a cutting velocity of 100 m/min do not show these fluctuations. Due to an increase of chip thickness after the implementation of friction, the cutting forces for a velocity of 100 m/min show a slight increase compared to the forces predicted without friction so that for this cutting condition the forces are significantly overestimated.

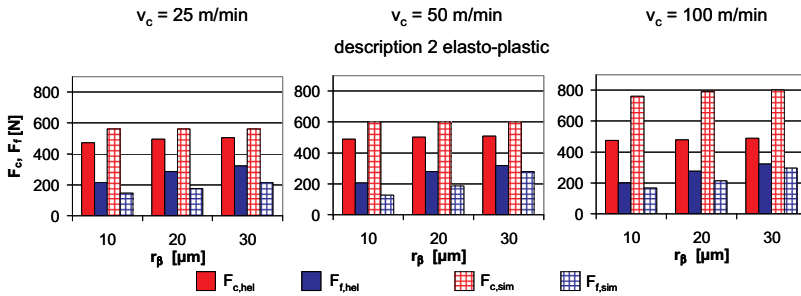


Figure 7-4: Predicted process forces based on description 2 assuming a friction factor of $m = 0.4$

With respect to the predicted feed forces the consideration of friction leads to a slight increase for all cutting conditions. Feed forces are still underestimated, however. A larger friction factor could potentially improve the accuracy of the predicted feed forces, but this would lead to a simultaneous increase in the predicted cutting forces.

The observed shear localization under certain cutting conditions can be considered the beginning of a chip segmentation as it is typical for Ti6Al4V. This hypothesis is supported by the distribution of the effective plastic strain as it is found in the chip as shown in **Figure 7-3**. However, the elasto-plastic model does not predict chips with a distinct segmentation. Together with the uncertainty in the material description as it is inherent to machining models, the lack of chip segmentation may be the explanation for the unrealistically increased chip thickness.

The influence of friction on the simulation results can be summarized as follows:

The introduction of friction to the elasto-viscoplastic model lead to an instability in the process forces which significantly reduced the average level of the cutting force. With respect to the feed force, this effect is very slight. It is not clear whether this instability is based on numerical or on physical effects. However, the nature of the observed fluctuation in the process forces indicates that a physical background is more likely. Due to the sensitivity of the solution process to the chosen friction conditions, a shear factor law was implemented. It was found that the implementation of friction lowered the average level of the cutting forces while it increased the feed forces. The described effects were very distinct for the lowest cutting velocity. They were reduced with increasing cutting velocity and could not be observed for the highest examined cutting velocity.

7.2 Influence of the Flow Formulation

Convergence of an elasto-plastic solution is generally more difficult to achieve than for a rigid-plastic formulation and the convergence of the elasto-plastic model is very sensitive to the chosen friction conditions.

The inclusion of a rate dependent material description requires the software to deal with an elasto-viscoplastic material behavior. From a physical point of view the rate dependency of an elasto-plastic material means that any stress causes plastic flow due to creep effects. On the numerical side this needs to be taken into account for example by implementing a creep model or a cut-off stress. These numerical measures again have an impact on the calculated plastic flow.

Mesh density does not have a significant impact on the calculated cutting forces. However, a strong impact of mesh density on the predicted feed forces were observed due to the ability of the mesh to capture the cutting edge radius.

In order to evaluate the tendency of the elasto-viscoplastic model to predict segmented chips, several simulations were run using manipulated flow stress data to enforce segmentation. The manipulated data incorporated strain softening as it can be seen in **Figure 7-5**.

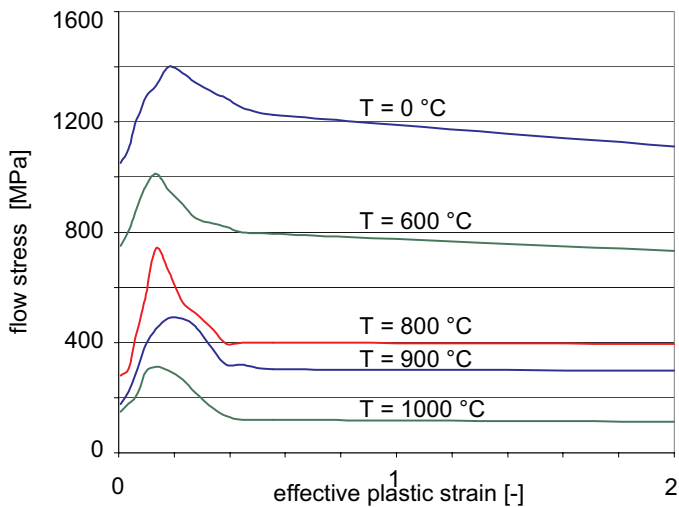


Figure 7-5: Flow stress with artificial strain softening; after [BÄKER2004]

As **Figure 7-6** indicates, chip segmentation could be achieved for the elasto-plastic model, but convergence could only be achieved for a few steps. The sensitivity of the rigid-plastic and the elastoplastic model to show chip segmentation was examined by varying the drop in the flow stress including strain softening

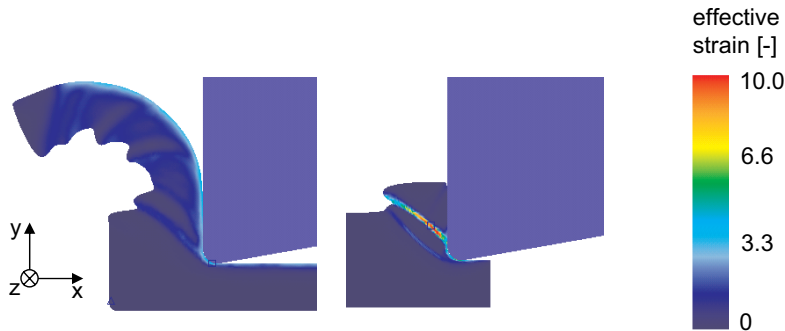


Figure 7-6: Chip formation based on manipulated flow stress data incorporating strain softening. Left: rigid-viscoplastic model; right: elasto-viscoplastic model; $r_\beta = 10 \mu\text{m}$, $v_c = 25 \text{ m/min}$

The results indicate that the rigid plastic model based on the manipulated flow stress is significantly more sensitive to shear localization and consequently to chip segmentation than the elasto-plastic model utilizing the same flow stress data. Once chip segmentation occurs in the elasto-plastic model, convergence could only be achieved for a few steps.

7.3 Discussion of the Benefit of 3D-Models

Since the plane strain assumption imposes certain restrictions concerning the implementation of thermal expansion and the neglect of edge-effects due to a widening of the chip, a three dimensional model of the orthogonal cutting process could improve the predictions. However, a three dimensional approach also introduces new challenges on the numerical side.

Figure 7-7 indicates how a three dimensional model for an orthogonal cutting process could be set up. The figure also demonstrates that meshing or, respectively remeshing is more difficult to implement. Despite the fact that the software contains a highly capable mesh generator, the object edges become irregular.

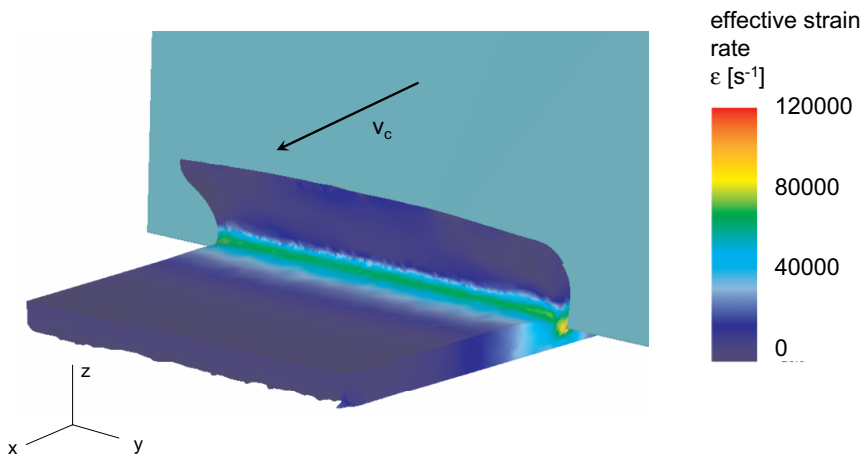


Figure 7-7: Three dimensional elasto-viscoplastic model of an orthogonal cutting process at $v_c = 50 \text{ m/min}$ (effective strain rate distribution).

The number of elements that is required to properly depict the cutting edge radius is

significantly higher than it is for a two dimensional model. Moreover, the mesh has to be fine enough to store the results for the residual strains and stresses so that the mesh density throughout the workpiece should not vary significantly.

The major advantage of a three dimensional model for the orthogonal cutting process lies within the consideration of edge effects and the thermal expansion coefficient.

The cross section highlighted in **Figure 7-8** represents the cross section that is modeled in a two dimensional plane strain model. It can be seen that due to the low observed strains, the plane strain assumption is a valid approximation.

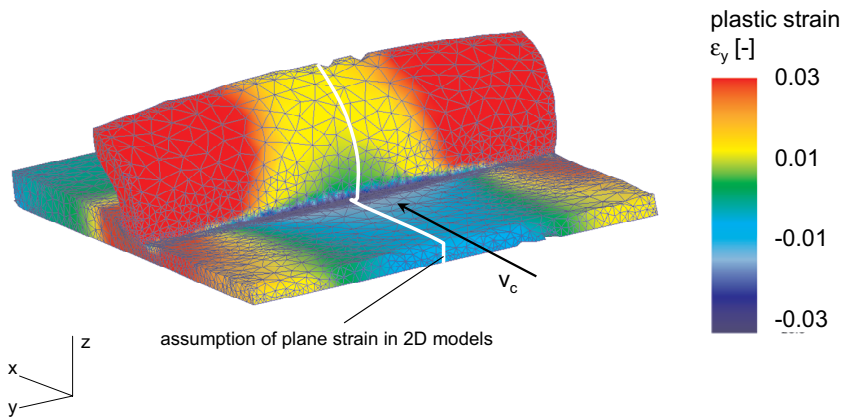


Figure 7-8: Distribution of plastic strain in y-direction; scale adjusted

While three dimensional machining models have shown promising results concerning material flow [FISCHER2005b], the prediction of residual stresses induced by a machining process requires further development on the software side.

8 Conclusions and Outlook

Demand for highly detailed information from machining simulations is increasing. In addition to material flow and process forces, industry is interested in information such as tool wear or the impact of the machining process on the workpiece. One possible impact of the machining process with respect to the workpiece is the induction of residual stresses.

In order to provide more detailed information, the detail level of the models and their input data needs to be increased. In order to model the impact of the machining process on the workpiece rim zone, the model needs to be expanded to include elastic material properties.

For the verification of these models, experimental verification methods are necessary. The degree of simplification in these experimental set ups needs to be compatible to the assumptions or, respectively, simplifications that are made within the respective machining model.

A new experimental machining set up for the determination of post process properties in the workpiece surface layer has been proposed. Due to its kinematics the proposed cutting process ensures that the cutting conditions for each location on the machined surface are known and that the cutting edge passes each point on the workpiece only once. A comparison with results from a standard orthogonal cutting set up confirmed that the newly proposed process can be considered an orthogonal cutting operation.

Defined cutting edge radii were generated on commercially available tool blanks and experimental process forces as well as temperatures were acquired. Subsequently, post process residual stresses were determined by means of x-ray diffraction.

To investigate the effect of elastic material properties on modeling the orthogonal machining process, a rigid-viscoplastic model using two different flow stress descriptions for Ti6Al4V is developed. A thermo-visco-elastoplastic FE model is proposed and the simulation results are compared to the predictions of the rigid-plastic simulation.

The results show that the inclusion of elastic material properties changes the contact

length between tool and workpiece as well as the chip curvature and the predicted plastic deformation in the workpiece or, respectively, the chip. While the contact length is important for the simulation of tool wear, the induced plastic deformation influences the state of residual stress.

The predictions were compared to the experimental results. Process forces are in reasonable, temperatures in good agreement. The observed trends in residual strains and stresses that are observed with a change in the process parameters are reasonable from a physical point of view. Absolute values, however are not in agreement.

Future models require enhanced material models. The occurring chip mode needs to be a result of the material description rather than of artificially introduced softening, separation or, respectively failure criteria.

With respect to the examined material, new material descriptions are required which take into account both phases of the alloy Ti6Al4V. This involves the determination of mechanical as well as thermo-physical material properties for both phases as a function of temperature. Particularly the interaction of thermal expansion between the two phases is important in order to predict residual stresses more accurately.

While the transition to three dimensional models for the prediction of rim zone properties subsequent to machining is desirable, the three dimensional model introduces new challenges with respect to hardware and software capabilities.

9 Appendix

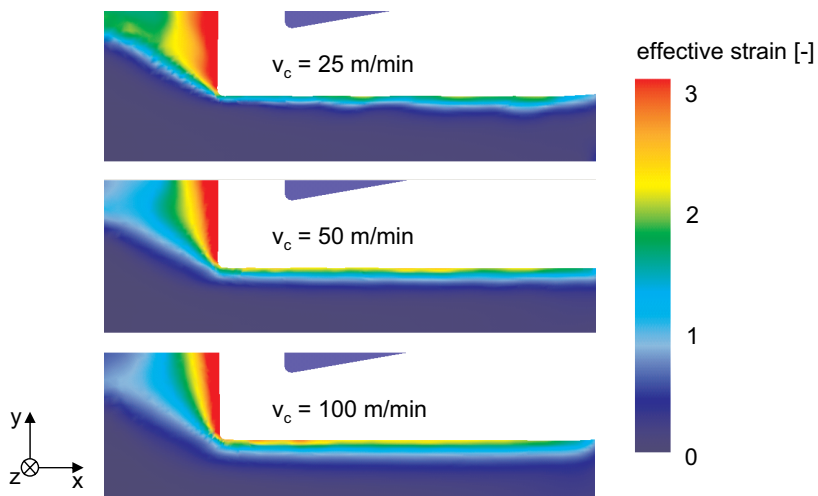


Figure 9-1: Effective strain distribution; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 10 \mu\text{m}$

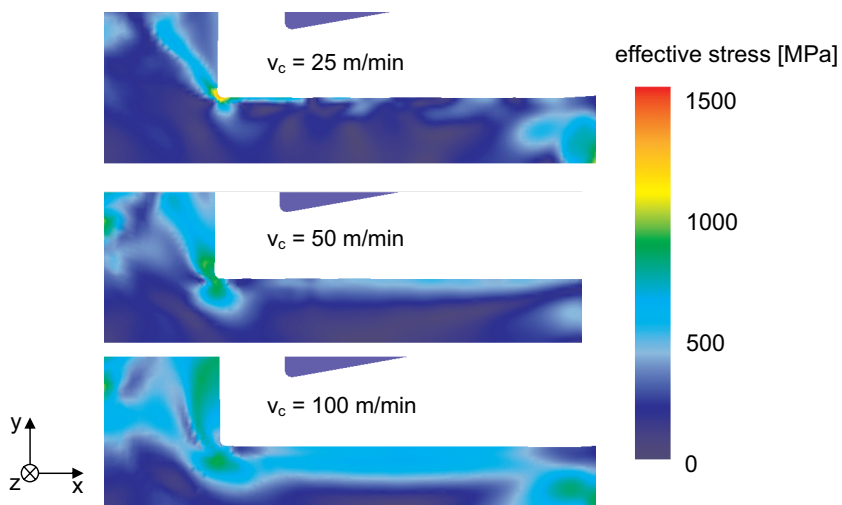


Figure 9-2: Effective stress distribution; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 10 \mu\text{m}$

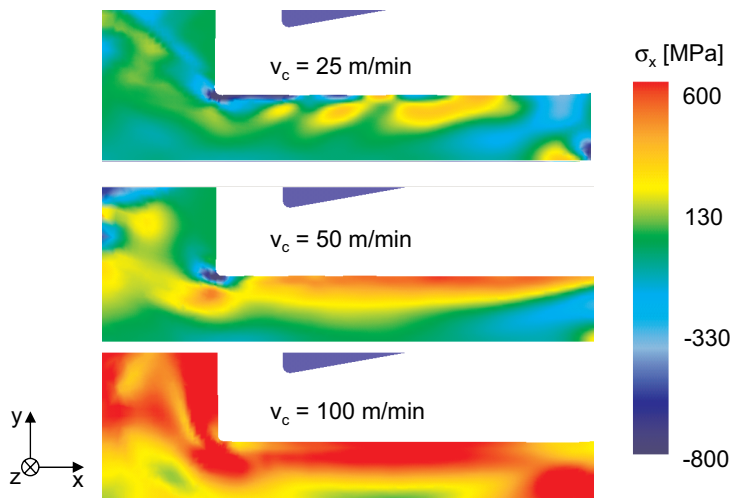


Figure 9-3: Distribution of σ_x ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 10$ μm

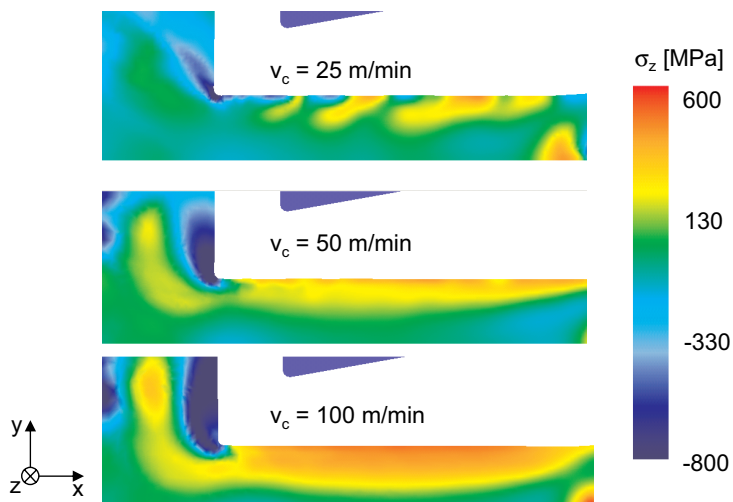


Figure 9-4: Distribution of σ_z ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 10$ μm

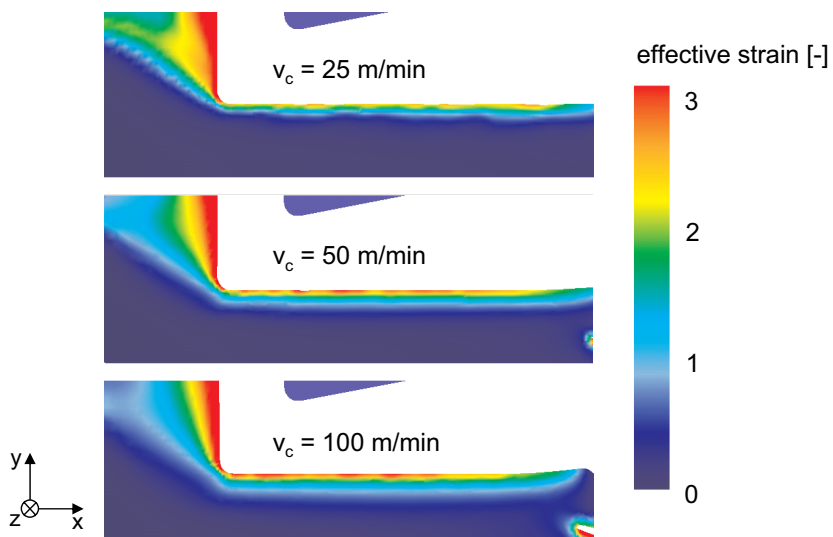


Figure 9-5: Effective strain distribution $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 20 \mu\text{m}$

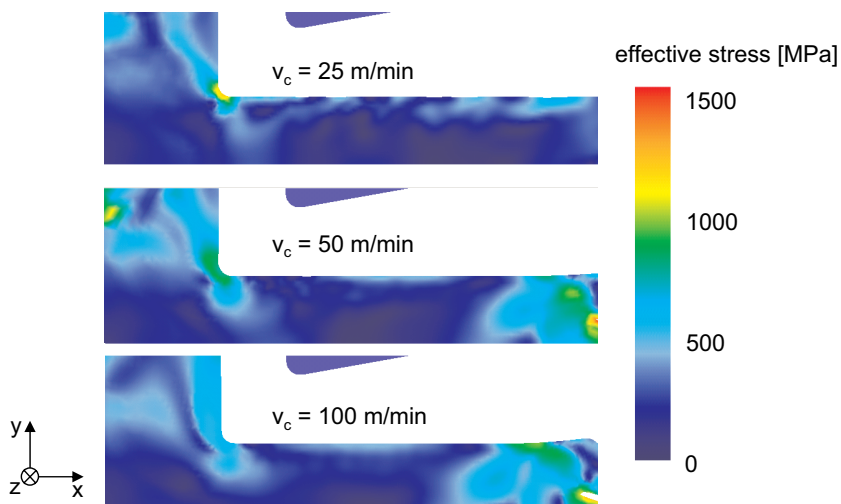


Figure 9-6: Effective stress distribution $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 20 \mu\text{m}$

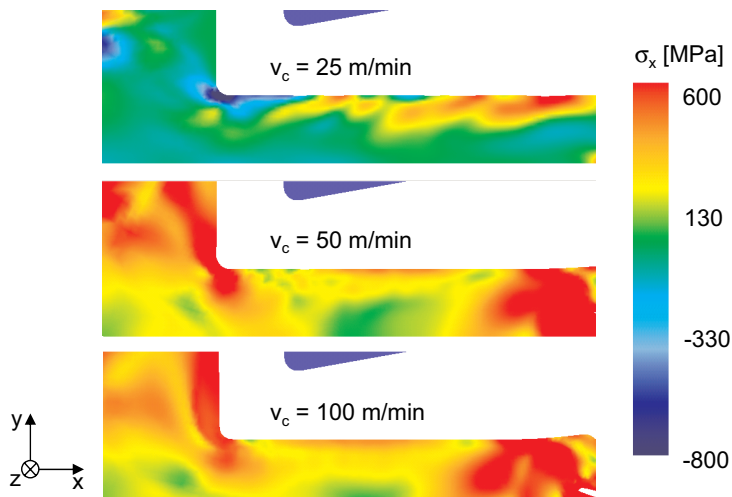


Figure 9-7: Distribution of σ_x ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 20$ μm

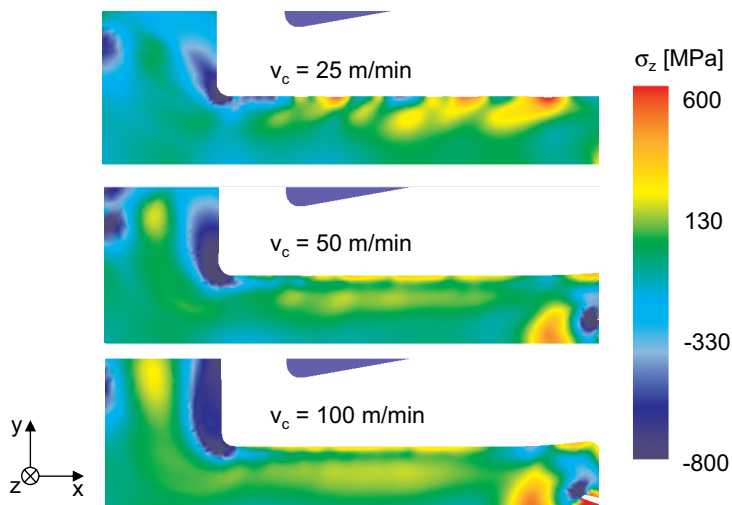


Figure 9-8: Distribution of σ_z ; $v_c = 25$ m/min, 50 m/min, 100 m/min, $r_\beta = 20$ μm

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11 Resumé

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