Modeling and Simulation of Tool Engagement and Prediction of Process Forces in Milling

Modellierung und Simulation des Werkzeugeingriffes und Berechnung der Prozesskräfte beim Fräsen

Von der Fakultät für Maschinenwesen der Rheinisch-Westfälischen Technischen Hochschule Aachen zur Erlangung des akademischen Grades eines Doktors der Ingenieurwissenschaften genehmigte Dissertation

vorgelegt von
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Gustavo Francisco Cabral

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### Symbols and Abbreviations

**Formelzeichen und Abkürzungen**

#### Capital letters

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<td>Flank face</td>
</tr>
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<td>$A_{\gamma}$</td>
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<td>Rake face</td>
</tr>
<tr>
<td>$A_{\text{chip}}$</td>
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<td>Chip surface area</td>
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<td>[mm$^2$]</td>
<td>Chip surface area below the minimum chip thickness</td>
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<td>[mm$^2$]</td>
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</tr>
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<td>$C$</td>
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<td>Feed cross-sectional direction</td>
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<td>$D$</td>
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<td>Diameter</td>
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<tr>
<td>$D_0$</td>
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<td>$dF_c$</td>
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<td>$F$</td>
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<td>$F_{a,\text{max}}$</td>
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<td>Maximum active force</td>
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<td>$F_c$</td>
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<td>Cutting force</td>
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<tr>
<td>$F_e$</td>
<td>[N]</td>
<td>Effective force</td>
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<td>$F_{en}$</td>
<td>[N]</td>
<td>Effective normal force</td>
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<td>$F_f$</td>
<td>[N]</td>
<td>Feed force</td>
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<td>$F_{f,\text{max}}$</td>
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<td>Maximum feed force</td>
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<td>$F_{fn}$</td>
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<td>$F_{yt}$</td>
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<td>Rake face parallel force</td>
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<tr>
<td>$F_{yn}$</td>
<td>[N]</td>
<td>Rake face normal force</td>
</tr>
<tr>
<td>$G$</td>
<td>[-]</td>
<td>Degree of segmentation</td>
</tr>
<tr>
<td>$IR_{\text{nsp,a}}$</td>
<td>[mm/s]</td>
<td>Impact ratio exit</td>
</tr>
</tbody>
</table>
\( IR_{hsp,e} \) \([\text{mm/s}]\) Impact ratio entrance \\
\( K \) \([-\text{-}]\) Asymmetry value \\
\( L_t \) \([-\text{-}]\) Cutting length \\
\( M_{FCN,TCS} \) \([-\text{-}]\) Rotation matrix \\
\( N \) \([-\text{-}]\) Workpiece surface normal direction \\
\( P_{\text{feed}} \) \([\text{W}]\) Feed power \\
\( P_r \) \([-\text{-}]\) Tool reference plane \\
\( P_{\text{spin},\varphi} \) \([\text{W}]\) Spindle power \\
\( R_{ffe} \) \([-\text{-}]\) Rotation matrix around the feed normal vector \( C \) \\
\( R_{ffe} \) \([-\text{-}]\) Rotation matrix around the feed direction \\
\( R_0 \) \([\text{mm}]\) Tool nominal radius \\
\( S_a \) \([\text{µm}]\) Rounding length on flank \\
\( S_x \) \([\text{µm}]\) Distance of flank separation point to ideal tool tip \\
\( S_t \) \([\text{µm}]\) Rounding length on face \\
\( S_r \) \([\text{µm}]\) Distance of rake separation point to ideal tool tip \\
\( SK_{ax} \) \([-\text{-}]\) Axial skewness ratio \\
\( SK_{rot} \) \([-\text{-}]\) Skewness ratio \\
\( T_m \) \([\text{°C}]\) Melting temperature \\
\( V_{\text{chip}} \) \([\text{mm}^3]\) Chip Volume \\
\( V_{\text{chip,MCT}} \) \([\text{mm}^3]\) Chip Volume below the minimum chip thickness \\
\( W \) \([\text{µm}]\) Chamfer width \\
\( W_{\text{seg}} \) \([\text{µm}]\) Width of segmentation \\
\( Z \) \([-\text{-}]\) Number of teeth \\

**Lowercase letters** \\
\( a_{e,n} \) \([\text{mm}]\) Step over \\
\( a_{p,n} \) \([\text{mm}]\) Depth of cut \\
\( b_{sp} \) \([\text{mm}]\) Uncut chip width \\
\( b_{eff} \) \([\text{mm}]\) Effective uncut chip width \\
\( f_{fxy} \) \([-\text{-}]\) Feed component in the xy-plane \\
\( f_{fz} \) \([-\text{-}]\) Feed component in the z-axis \\
\( f_z \) \([\text{mm}]\) Feed per tooth \\
\( h_{CM} \) \([\text{mm}]\) Height position of the chip center of mass \\
\( h_i \) \([\text{mm}]\) Height of slice \( i \) \\
\( h_{sp,r} \) \([\text{µm}]\) Radial chip thickness \\
\( h_{sp} \) \([\text{µm}]\) Uncut chip thickness \\
\( \bar{h}_{sp} \) \([\text{µm}]\) Average chip thickness \\
\( h_{sp,max} \) \([\text{µm}]\) Maximum uncut chip thickness \\
\( h_{sp,min} \) \([\text{µm}]\) Minimum chip thickness \\
\( h'_{sp,max} \) \([\text{µm}]\) Maximum deformed chip thickness \\
\( h'_{sp,min} \) \([\text{µm}]\) Minimum deformed chip thickness \\
\( i \) \([-\text{-}]\) Tool slice index \\
\( j \) \([-\text{-}]\) Flute index \\
\( k \) \([-\text{-}]\) Total number of slices \\
\( k_{ic} \) \([-\text{-}]\) Cutting force coefficient with \( i = c, cn, p \)
Introduction

\( k_{se} \) [-] Edge force coefficient with \( i = c, cn, p \)
\( l_{sp} \) [mm] Chip length
\( n \) [rpm] Spindle revolutions per minute
\( n_{as} \) [-] Number of angular steps
\( n_{as,i} \) [-] Number of angular steps of the slice \( i \)
\( n_l \) [-] Number of tool slices
\( n_{hsp} \) [-] Total number of chip elements
\( n_{ex} \) [-] Sample size
\( n_s \) [-] Number of simulation steps
\( \tilde{\eta}_k \) [-] Curvature normal vector
\( \tilde{\eta}_{ij} \) [-] Local normal vector to the rake plane
\( \tilde{\eta}_l \) [-] Tool face normal vector
\( p_c \) [-] Intersection point of flank and rake face fitting lines
\( \rho_{int} \) [-] Intersection of wedge angle bisector and edge profile
\( r_h \) [-] Chip thickness deformation ratio
\( r_i \) [mm] Radius of the slice in the current step
\( r_{ji} \) [\( \mu m \)] Cutting edge radius
\( t_{\text{chip,ij}} \) [s] Cutting time
\( v_c \) [m/min] Cutting speed
\( v_{c,\text{local}} \) [m/min] Local cutting speed
\( v_f \) [mm/min] Feed speed
\( x, y, z \) [-] Tool coordinate system
\( z^* \) [-] Confidence interval

**Greek symbols**

\( \alpha \) [\( ^\circ \)] Clearance angle
\( \alpha_n \) [\( ^\circ \)] Normal rake angle
\( \alpha_r \) [\( ^\circ \)] Radial rake angle
\( \beta_f \) [\( ^\circ \)] Lead angle
\( \beta_{fn} \) [\( ^\circ \)] Tilt angle
\( \gamma \) [\( ^\circ \)] Rake angle
\( \gamma_b \) [\( ^\circ \)] Chamfer angle
\( \gamma_r \) [\( ^\circ \)] Radial rake angle
\( \gamma_{ri} \) [\( ^\circ \)] Local orthogonal rake angle
\( \Delta \Gamma \) [-] Tooth angular displacement
\( \delta \) [\( ^\circ \)] Angle between the feed components on the \( XY \) plane
\( \Delta_{h}(\varphi) \) [mm] Tool height
\( \Delta_{h} \) [mm] Slice height
\( \Delta_{\varphi} \) [\( ^\circ \)] Angular step
\( \varepsilon \) [-] Equivalent plastic strain
\( \eta_c \) [\( ^\circ \)] Chip flow angle
\( \theta_n \) [\( ^\circ \)] Supination angle
\( \kappa \) [\( ^\circ \)] Curvature angle of the tool
\( \kappa_i \) [\( ^\circ \)] Curvature angle of slice \( i \)
\( \kappa_r \) \(^{[\circ]} \) Tool cutting edge angle

\( \lambda \) \(^{[\circ]} \) Helix angle

\( \lambda_i \) \(^{[\circ]} \) Helix angle

\( \lambda_i \) \(^{[\circ]} \) Helix angle of slice \( i \)

\( \lambda_s \) \(^{[\circ]} \) Tool cutting edge inclination

\( \xi_x, \xi_y, \xi_z \) \(^{[\circ]} \) Angles between the feed direction and \( x, y, z \) axis

\( \rho \) \(^{[\circ]} \) Apparent friction angle

\( \sigma \) \([-\]\) Standard deviation

\( \tau_s \) \([-\] Shear stress

\( \tau_{ij} \) \([-\] Angular position of the flute \( j \) and slice \( i \)

\( \phi \) \(^{[\circ]} \) Rotation angle

\( \phi_a \) \(^{[\circ]} \) Exit angle

\( \phi_{a,max} \) \(^{[\circ]} \) Maximum values of \( \phi \)

\( \phi_c \) \(^{[\circ]} \) Contact angle

\( \phi_{CM} \) \(^{[\circ]} \) Center of mass angle

\( \phi_e \) \(^{[\circ]} \) Cutting entrance angle

\( \phi_{e,min} \) \(^{[\circ]} \) Minimum values of \( \phi \)

\( \phi_{ve} \) \(^{[\circ]} \) Entrance variation angle

\( \phi_{va} \) \(^{[\circ]} \) Exit variation angle

\( \phi_{hsp,max} \) \(^{[\circ]} \) Angular position of the maximum uncut chip thickness

\( \phi \) \(^{[\circ]} \) Shear angle

\( \phi_n \) \(^{[\circ]} \) Normal shear angle

\( \phi_b \) \(^{[\circ]} \) Apex angle

\( \Psi_f \) \(^{[\circ]} \) Azimuth angle

\( \Omega \) \([\text{rad}]\) Chip flow angle on the tool’s reference plane

**Acronyms**

ANOVA  Analysis of variance

Big-O  Notation for algorithm complexity

CAM  Computer Aided Manufacturing

CSG  Constructive solid geometry

DIN  Deutsches Institut für Normung

FCN  Workpiece surface coordinate system

FEM  Finite Elements Method

GUI  Graphic User Interface

HRC  Rockwell hardness scale

MRE  Arbitrary error margin

MLR  Multiple linear regression

TCS  Tool coordinate system

WCS  Workpiece coordinate system
According to the annual report “World Energy Outlook 2014” of the International Energy Agency (IEA), the global energy demand is set to grow by 30% from 2014 to 2035 [IEA14a]. The correspondent cumulative global investment bill for meeting the energy demand amounts to more than $48 trillion, including $8 trillion investments in energy efficiency of transport, buildings and industry sectors [IEA14b]. This estimation already considers a new policies scenario, in which today’s energy mix is changing towards renewable energy sources and energy consumption growth slows down after 2025, mainly driven by environmental factors. The energy demand growth of above 2% per year over the last two decades should decrease to 1% per year after 2025 [IEA14a]. Advances in technology and efficiency are key factors for slowing down the growth in global energy demand. Efficiency is reducing energy consumption during a time when energy prices have increased significantly across the world. Energy prices increased between 11% and 52% in individual jurisdictions between 2001 and 2011 [IEA13]. These pressures for increasing energy efficiency challenge the industry sector, which is itself responsible for 30% of today’s global final energy consumption [IEA13]. Substantial changes in the design of products and production processes are required to keep pace with the installed energy capacity and prices.

Alongside environmental, political and economic pressures, the production industry also faces challenges resulting from the dynamics of a globalized market. A study from McKinsey & Company [MOHR13] shows how Premium OEMs of the automotive sector respond to the increasing customer demands by creating more and more derivatives and expanding their portfolios. This trend challenges production companies to use advanced product design and materials, which in turn require more flexible and cost-efficient processes. Thus, the improvement of flexible production processes such as machining and additive manufacturing gains more significance in the production scenario. This trend is present in all industries, including the automotive, aerospace and mold and die sectors. Companies are forced to differentiate themselves by achieving more complex forms and precise surfaces on difficult to machine materials.

Machining is since the 19th century one of the most used processes in the production of metallic parts, due to its high flexibility, productivity and precision. According to a study performed by Merchant [MERC98], about 15% of the value of all mechanical components manufactured worldwide is generated through machining, a quota that has since then increased, due to the advent of nano and micro technologies. If the components are not directly produced by machining, so are at least the tools and production machines used in the manufacturing.

The primary machining processes with defined geometry are turning, boring and milling. Because of its large technical and economic importance, a great number of researches has been carried out to optimize machining processes regarding quality, productivity and cost. A study carried out by Armarego et al. in the USA [ARMA96] shows that the cutting tool is properly selected in less than 50% of the cases while the recommended cutting speed is used in only 58% of the cases. Finally, this study also shows that merely 38% of the tools are used up to
their full tool-life capability. Still today such problematic can be found in the industry. This situ-
ation urges the need for developing advanced scientific approaches to improve the perform-
ance of machining processes [CHIN07].

An increasing amount of approaches is used for improving machining performance, such as
the development of advanced cutting tool geometries, materials and coatings, improvement of
the process kinematics, advanced clamping systems, ultrasound and laser assisted machining
and many others. An appropriate planning of milling processes aims to take all these variables
into consideration during the process design. The role of the process planning is thus to enable
productive, reliable and efficient machining processes and at the same time keep the effort for
the establishment of new or modified processes as low as possible [ZABE10].

In this context, modeling and simulation of machining processes present a high potential for
increasing performance by providing the process understanding which is necessary for a ho-
listic optimization. Planning oriented production requires approaches that either significantly
reduce planning effort and/or significantly increase product value. Nevertheless, the planning
of production processes is characterized by unlinked simulations of specific phenomena. As a
consequence, only local optimizations are achieved, as the impact of upstream production
processes is not taken into consideration [BREC11].

Two recent trends are identified in the modeling and optimization of machining processes. The
first is the development of advanced mathematical models for describing and understanding
the cutting process for specific industrial applications, acting at the level of the process plan-
ning. The models are then implemented into computational tools, adding intelligence to Com-
puted Aided Manufacturing software (CAM) and other optimization software. This approach
enables a fast and optimized process design and planning. The second trend in machining is
to add intelligence to the machine tools and CNC by sensoring and process monitoring. Sen-
sors are installed on the machine tool for measuring forces, vibrations, temperature, sound
and position [ALTI12]. Mathematical models that correlate the relationship between the meas-
ured signals and the state of machining are developed and coded into real-time algorithms.

The planning of machining processes, which is the primary goal of the first trend described
above, requires an appropriate selection of the process parameters and machining strategy
for a given tool, material and machine combination. In industrial applications, the manual se-
lection of the process parameters and process design is still the standard. The process planner
normally disposes of guidelines based on standards, catalogs, experimental results from re-
search reports and internal databases of cutting parameters for planning the machining oper-
ations [KLOC08]. These documents usually provide only rough reference values, without
considering the chip geometrical conditions, wear behavior, machine type, and machine
recommended cutting speeds, leading to short tool life and inefficiency [KLOC08].

The machining performance is also limited by the process dynamics, which yields the need for
building up knowledge about the process-related forces. Manufacturing in the future cannot
rely on the manual selection of the process parameters nor the experience of the process
planner, a change driven mainly by the trend of automatization. Therefore, a holistic optimiza-
ation of machining processes can only be achieved by using an effective and efficient combination of heterogeneous simulation models [BREC11] which allows an efficacious utilization of the newest manufacturing equipment. The knowledge about the chip geometrical conditions and the process forces are thus a requirement for enhancing the process performance and reducing the planning time and effort.

In the light of the above, the present work aims to make a meaningful contribution by presenting base models and methodologies for increasing the process understanding and developing an integrated simulation of milling processes. It is envisaged to develop and implement algorithms for calculation of the chip microscopic geometry and prediction of forces in milling, which are suitable for an integrated simulation chain of the milling process. These models can in the future be implemented into an effective and automated system for increasing the process efficiency through knowledge-based algorithms.
Einleitung


Die primären Zerspanverfahren mit definierter Geometrie sind Drehen, Bohren und Fräsen. Aufgrund der großen technischen und wirtschaftlichen Bedeutung dieser Verfahren wurde bereits eine große Anzahl von Untersuchungen durchgeführt, um Zerspanprozesse in Bezug auf
Einleitung

Qualität, Produktivität und Kosten zu optimieren. Eine von Armarego et al in den USA durchgeführte Studie [ARMA96] zeigt, dass in weniger als 50% der Fälle das geeignete Schneidwerkzeug ausgewählt wird, während in nur 58% der Fälle die empfohlenen Schnittgeschwindigkeit verwendet wird. Schließlich zeigt diese Studie auch, dass nur 38% der Werkzeuge bis zu ihrer vollen Werkzeuglebensfähigkeit verwendet werden. Noch heute ist diese Problematik in der Industrie zu beobachten. Diese Situation unterstreicht die Notwendigkeit für die Entwicklung von fortschrittlichen wissenschaftlichen Ansätzen, um die Leistung der Zerspanprozesse zu verbessern [CHIN07].

Eine zunehmende Anzahl von Ansätzen wird bereits zur Verbesserung der Zerspanleistung eingesetzt, wie beispielsweise die Entwicklung von fortgeschrittenen Schneidwerkzeuggeometrien, Materialien und Beschichtungen, sowie die Verbesserung von Prozesskinematik, erweiterten Spannsystemen, ultraschall- und lasergestützter Zerspanung und vielen anderen. Eine angemessene Planung von Fräsprozessen zielt darauf ab, alle oben erwähnte Variablen bei der Prozessgestaltung zu berücksichtigen. Die Rolle der Prozessplanung besteht schließlich darin, produktive, zuverlässige und effiziente Zerspanprozesse zu ermöglichen und gleichzeitig den Aufwand für die Einrichtung neuer oder geänderter Prozesse so gering wie möglich zu halten [ZABE10].


Einleitung


Angesichts der oben aufgeführten Erwägungen zielt die vorliegende Arbeit auf einen sinnvollen wissenschaftlichen Beitrag zur Erhöhung des Prozessverständnisses und zur Entwicklung einer integrierten Simulation von Fräsbearbeitungen durch die Darstellung von Basismodellen und Methoden. Es ist beabsichtigt, Algorithmen für die Berechnung der mikroskopischen Spanungsgeometrie und für die Vorhersage der Kräfte beim Fräsen zu entwickeln und zu implementieren, welche für eine integrierte Simulationskette der Fräszprozesse geeignet sind. Diese Modelle können zukünftig in ein effektives und automatisiertes System für die Erhöhung der Prozesseffizienz durch wissensbasierte Algorithmen implementiert werden.
Process fundamentals and state of the art

The modeling of machining operations has been topic of wide research throughout the 20th and 21st centuries. Empirical models go back to the work of Taylor [TAYL07], who developed the first mathematical expressions for describing the cutting process, such as the relationship between the cutting speed and the tool wear. In more recent years, the previously vastly disseminated experimental and science-based approaches have been supported by computer-aided algorithms, which allow a deeper understanding of the process phenomena and the development of industrial applications. Nevertheless, there is still a lack of fundamental understanding of the basic mechanisms and the interactions of cutting tool and work material [LUTT98], especially for the modeling of the multi-axis milling. The inherited complex kinematics and engagement conditions of these operations hinder the understanding of the phenomena that occur during the chip formation and that ultimately influence the process forces. The process fundamentals and scientific state of the art, on which this dissertation is based, are presented in the following. The focus here lies on simulation of the process forces based on the fundamental of the metal cutting and its industrial application.

2.1 The multi-axis milling process

Multi-axis milling operations are among the manufacturing processes that are most used for the production of free-form surfaces. They are mainly characterized by constant changing engagement conditions along the tool path and the tool axis. An important aspect for the simulation of these operations and calculation of the process forces is the understanding of the most basic interactions between cutting tool and workpiece. The extrapolation of this comprehension to complex engagement situations allows a simulation-based process planning.

2.1.1 Definition of multi-axis milling

Metal cutting, or machining, is defined as the material removal from a workpiece in form of chips through the cutting edges of a tool by mechanical means [DIN8589]. The material removal processes in the machining of metals are distinguished depending on shape, distribution and movement of the cutting edges involved in a process. If the orientation, the number and the shape of the cutting edges are known, the process is referred to as cutting with geometrically defined cutting edges, or simply cutting. If the geometrical features of cutting edges can only be described statistically, the process is called cutting with geometrically undefined cutting edges, or abrasive process. Cutting processes are for example turning, drilling and milling, whereas grinding represents an abrasive process [DIN8589, DIN8580].

The main difference between the various types of cutting processes is their kinematic engagement condition. The process kinematics is given by the tool orientation, the direction of cutting and the direction of feed relatively to the workpiece. Machining with a predominantly circular movement of the tool's cutting edges and a perpendicular or oblique feed direction in relation to the rotational axis of the tool is referred to as milling [DIN8589-3]. Peripheral milling are
milling processes, in which the cutting edges located on the tool perimeter generate the workpiece surface [DIN8589-3]. In this case, the chip formation happens as a consequence of the combination of a rotation movement of the milling tool and a relative movement between the milling tool and the workpiece, which can be linear (3-axis milling) or linear/rotary (5-axis milling). Multi-axis milling is thus defined as milling operations, whereas the milling cutter can be rotated by multiple axes in relation to the workpiece. Figure 2.1 illustrates the classification of the multi-axis milling. The investigated subject of this thesis is the peripheral multi-axis milling process, from this point on referred to as simply multi-axis milling. Since ball-end cutters are the most used for this type of milling operations, the majority of the examples shown in this work will use this type of cutting tool. The concepts described here can nevertheless be extended to different types of peripheral milling tools.

![Figure 2.1: Classification of multi-axis milling, adapted from [DIN8589, DIN8589-3]](image)

2.1.2 Tool geometry

Milling cutters are of different types. They are classified based on various factors, such as constructional features, the purpose of use and direction of helix. The shape, material, and coating depend on the application. Solid body milling cutters are among the most used tools in multi-axis milling operations. Their tool geometry can be characterized by its macroscopic geometry and by the cutting edge geometry. This distinction is important for modeling the process, since some models are not able to take both geometries into consideration [ZABE10].

In the vast majority of practical applications, end mills, and ball-end mills are used. The former are mostly used for roughing operations, while the latter are, due to their small area of contact with the workpiece surface, the main choice for finishing operations. Because of their flexibility and ability to generate high-quality superficies, ball-end mills are also the most used tools for the finishing machining of free-form surfaces [ARNT13].

2.1.2.1 Macroscopic geometry

The macroscopic parameters determine the basic form of the milling tool. They also determine the position of the cutting edge along the tool axis. The tool macro-geometry of solid body tools is given by the standard DIN 11529-1 [DIN11529-1]. Five factors can be used for characterizing the macroscopic geometry of solid body milling tools and the position of the cutting edge: tool
type, radius, length, helix angle and number of cutting edges. This definition is depicted in Figure 2.2.

Each tool type has characteristic parameters. End-mills, for instance, have a constant radius along the tool axis. Ball-end mills, also known as ball nose cutters, in turn, are the combination of a hemispherical and a cylindrical part. Other common tool types are tapered end mill, toroidal mill, and barrel-shaped mill. The tool radius is mostly defined by the form of the machined workpiece. The cutting speed varies linearly with the radius, reason why milling tools with variable radius along the tool axis do not always operate in ideal cutting speeds. The number of cutting edges influences the material removal rate and the dynamic behavior of the tool, since the tooth contact frequency determines the excitation frequency of the system.

The helix angle determines the angular displacement of the cutting edge along the tool axis. It can be constant or variable, as an example of ball-end mills, which have a different helix angle on the tool tip and shank. Many authors consider the helix angle constant in the modeling of ball-end milling operations [ARNT13, ALTI12, URBA09], while others calculate the local helix angle based on the measured parameters of the tool geometry [LAZO00].

### 2.1.2.2 Cutting edge geometry

The cutting edge is defined according to the standard DIN 6581 [DIN6581]. It is the part of the tool which effectively generates the workpiece surface. The parameters that define the cutting edge geometry are shown in Figure 2.3.

The rake face $A_\gamma$ of the tool is the name of the superficies, on top of which the chip flows until it leaves the workpiece. The flank face $A_\alpha$ is the tool’s surface which is adjacent to the most recently machined surface. The wedge angle $\beta$ formed between the rake and flank faces is related to the physical resistance offered by the workpiece on the entrance of the cutting edge. Smaller wedge angles result in less resistance. On the other hand, a cutting edge with bigger wedge angle would be more stable and absorb the heat generated more efficiently [AWIS03].
The clearance angle $\alpha$ is the angle between the flank face and the tangential direction. A compromise solution must be considered in the choice of the value of $\alpha$, since high values lead to heat accumulation in the cutting edge, which increases the tool wear and the probability of its rupture. Values close to zero, however, can affect the integrity of the machined surface and, by pressure welding, increase the wear of the flank face considerably. Moreover, the ploughing forces also increase at small clearance angles.

Another important angle is the rake angle $\gamma$, formed by the rake face and the tool reference plane $P_r$. The rake angle influences the chip formation considerably. As depicted in Figure 2.3, $\gamma$ assumes a positive value. Nevertheless, null and negative values can also be set. In the positive domain, the rake angle tends to low the magnitude of the tangential and feed forces [ALT112]. Too positive rake angles, however, weaken the cutting edge. When in the negative domain, the rake angle brings stability to the process and the chip-breaking becomes more efficient. However, in this case, the force levels and the thermal charges, to which the cutting edge is exposed, are higher. This increases the crater wear tendency on the rake face and, consequently, decreases the lifetime of the tool [KLOC08].

![Figure 2.3 Cutting edge geometry according to [DIN6581]](image)

**Characterization of rounded cutting edges**

While the characterization of the cutting edge macro-geometry is internationally standardized [DIN6581, ISO3002-1], no such standard exists to characterize the cutting edge micro-geometry, which represents the actual shape of the intersection of a tool’s flank and face [WYEN11]. The cutting edge shapes are described in the DIN standard 6582 [DIN6582]. A distinction between a rounded, chamfered or sharp cutting edge reference profiles is presented. The different profiles are shown in Figure 2.4.
A rounded cutting edge is formed by a rounded transition between the tool’s face and the flank. The nominal radius of a rounded cutting edge measured in the cutting edge normal plane is $r_\beta$. The cutting edge rounding radius $r_\beta$ plays an important role in the milling process, especially when its magnitude is on the same order as the uncut chip thickness [ALBR60, WYEN11]. A more precise distinction of the ideal cutting edge profiles is given in [TIKA06], since asymmetries in the profile may occur in the preparation process Figure 2.5.

Extensive research on the characterization and measurement of the cutting edge profile was performed by Wyen [WYEN11]. The author showed that there are several approaches for characterizing cutting edges. The general procedure is first to generate a set of data points representing the edge profile. This is usually performed by tactile or optical means. These data points are then interpolated using fitting algorithms, such as spline interpolation. Finally, a circle is drawn fitting the acquired data. The radius of the circle determines the value of $r_\beta$. Several methods can be used for fitting a circle to a set of points. The easiest is by using three points, which mathematically define a unique circle. Other approaches may incur in different results, since the cutting edge can only be ideally characterized by only one parameter, without considering uniformities and nuances in the shape. Other uncertainties arising from the measurement systems and used fitting algorithms also influence the calculated value of $r_\beta$. The standard procedure for determining the value of the cutting radius $r_\beta$ is shown in Figure 2.6.
Denkena [DENK02] used a two line approach for characterization of the cutting edge, in which two lines are drawn tangentially to the rake and flank faces. The wedge angle bisector is then determined. The points at which the cutting edge begins to separate from the rake and flank face are calculated. Next, parameters for describing the cutting edge micro-geometry are derived. The cutting edge geometry is finally described not by the cutting edge rounding radius $r_E$, but by its asymmetry and other parameters as seen in Figure 2.7.

The research performed by Wyen [WYEN11] indicates that the best method for unique determining the cutting edge rounding is the use of a least square circle fit over an area that is determined iteratively. According to the author, by iteratively determining the area used to fit in the cutting edge radius, one uncertainty driver is eliminated. This method drastically reduces the point sensitivity uncertainty and deviations of a single point. To specify the asymmetry of the rounding, the suggested characterization method is insensitive to point uncertainties and form deviations and can be used irrespective of whether the edge is chamfered or rounded [WYEN11]. The method proposed by Wyen is illustrated in Figure 2.8.
The understanding of the cutting mechanism is crucial for comprehension of the phenomena that influence the process forces. A chip can be understood as a part of the workpiece removed by the cutting tool, as a result of the relative movement between each other. Phenomena of elastic and plastic deformation are present in this interaction. The geometry of the deformed chip differs significantly from the uncut chip geometry, due to high plastic deformations occurring during the material removal. Some models such as Merchant [MERC45] and Oxley [OXLE89] try to describe this formation, but no analytical approach is available [AMOR03]. Figure 2.9 shows the three main deformation zones which characterize the process.
The three main zones are the primary, the secondary and the tertiary shear zone. In the primary zone, the material of the workpiece is submitted to shear stresses caused by the degree of shear strain imposed by the cutting speed of the process. These stresses cause a reorientation of the grain boundaries according to the flow direction and finally transfer of the material, typically in the form of lamellae, from the workpiece to the chip, which leaves the region over the rake face. Unlike the model of the Merchant [MERC45], that describes the primary shear zone as a line and, therefore, precludes the description of the deformation occurred in this region, the model of Oxley predicts an area for the primary shear zone in which the strain and the strain rate are estimated [OXLE89]. In the secondary and tertiary zones, high levels of temperature and stresses are reached as a result of the friction forces, which cause plastic deformation of the material in contact with them.

One of the most used models for describing the stress in the shear zones is the Johnson-Cook model. Since this model takes into account the work hardening of the material and its softening due to the high temperatures achieved, its use is recurrent in machining process simulation by Finite Elements Method (FEM). The Johnson-Cook flow stress model empirically describes the stress of a given state taking into account dynamic and temperature parameters, as in shown in Equation 2.1 [JOHN83]:

\[
\sigma = \left[ A + B \varepsilon^n \right] \left[ 1 + C \ln \dot{\varepsilon} \right] \left[ 1 - T^m \right] \\
\]  

**Equation 2.1**

in which:

- \( \varepsilon \) is the equivalent plastic strain
- \( \dot{\varepsilon} = \frac{\dot{\varepsilon}}{\dot{\varepsilon}_0} \) is the dimensionless plastic strain rate for \( \dot{\varepsilon}_0 = 1.0 \text{ s}^{-1} \)
- \( T = \frac{T - T_0}{T_m - T_0} \) is the homologous temperature, \( T_0 \) = reference temperature

\( T_m \) is the melt temperature of the material A, B, C and \( m \) are constitutive constants that represent the elastic and plastic characteristics of the material.

Depending on the workpiece material and on the process parameters, the form, deformation, curvature and color of the chip may give indications about the quality of the cutting process.
There are four different chip types: flow, laminar, shearing and tearing chips [KLOC08]. These are not always easily identified and can also be coexistent in a given machining operation. Types of chips in dependence on material characteristics [KLOC08].

The kind of chip resultant is, at first, a function of the material characteristics and the process parameters. Flow chips are usually produced when the material of the workpiece has a relatively high level of deformation capability, and the chip formation is not adversely affected by any machine vibrations. Laminar chips are developed in cases where the grain structure of the material is uneven or when vibrations result in fluctuation of the uncut chip thickness and process forces [KNOD04]. They can occur both at high feed rates and high cutting speeds [KLOC08]. Shearing chips are developed when the strain rates at the primary zone cause local structural brittleness in the workpiece material. In this case, a discontinuous formation of lamellae occurs, being these partially welded with each other as a cause of the temperature achieved in the process. Tearing chips are present in the machining of brittle materials. These materials, such as cast iron, stone, and bronze, have a very low plastic regime and often irregularly distributed grains. This behavior leads to the removal of undeformed parts of the workpiece surface. Similar results can also be achieved in the machining of tough materials when small rake angles and low cutting speeds are used [DEGN02, WARN74]. As shown in Figure 2.11, the discontinuous chip formation is composed of two main alternate phases: the stagnation and the elimination phase.

In the stagnation phase, an analogy to fluid mechanics can be applied to explain the stagnation region formed in the cutting edge. The relative movement between tool and workpiece from the tool's referential, in which the workpiece material comes against the cutting tool can be interpreted in the form of streamlines. The part of the material flowing on the streamline concurrent to the cutting edge has no velocity component to flow neither on the rake face nor the flank face, being stuck in the tip of the cutting edge. The effect of this stagnation on the primary shear zone is a continuous increase in the stress and temperature values until the failure of the material, which can be explained by two models [DEGN02].
The thermoplastic shear model is based on the fact that the work hardening of the material competes with its softening due to the high temperatures achieved in the primary shear zone [ASTA96]. When the thermal softening is greater than the work hardening, an unstable condition is present, and a shear edge is formed. On the other hand, the crack propagation model explains the chip segmentation through the great stress rates to which the free surface of the workpiece is exposed [DEGN02]. This creates a crack which is propagated until the cutting edge, forming another chip segment [ISAJ60]. In the elimination phase, the chip segment produced on the previous stagnation phase comes off the workpiece and flows through the rake face of the tool, welded with other chip segments for the formation of the chip.

Two quantities are often used in the measurement of the chip's degree of segmentation: the segmentation G and the width of segmentation $w_{seg}$ (Figure 2.9). The segmentation G is a local quantity defined as a ratio:

$$
G = \frac{h_{sp,max} - h_{sp,min}}{h_{sp,max}}
$$

Equation 2.2

Where $h_{sp,max}$ is the maximum deformed chip thickness and $h_{sp,min}$ is the minimum deformed chip thickness for the chosen point of the calculation.

### 2.1.4 Process kinematics

The kinematics of milling processes can be understood as the tool position, orientation, and motion in relation to the workpiece. The movement of the cutting edge is mainly constituted by the superposition of a translational and a rotational component. The translational component
is given by the feed velocity $v_f$ while the cutting speed determines the rotation of the tool around its axis. The complex kinematics of multi-axis milling processes is a major factor responsible for the difficulty in understanding the phenomena that occur during cutting. Complex chip geometries are formed by the interaction between the milling tool and the workpiece, especially when the tool geometry is also complex. For better understanding the process kinematics, special attention is given to the tool orientation and to the technological parameters which define the tool motion.

2.1.4.1 Tool orientation

The tool orientation in relation to the machined surface can be defined in different manners. Several authors use a combination of two angles for its description: the lead angle $\beta_l$ and the tilt angle $\beta_{fn}$ [OZTU07, URBA09, HOCK96, ZAND95, MARK95, ALTI00]. The concept behind this orientation system is to intuitively guide the machine operator or CAM programmer by choosing how far in the direction of the feed and normal to it the tool is oriented. These angles and the tool and workpiece coordinate systems are illustrated in Figure 2.12.

![Figure 2.12: a) Coordinate systems, b) ball-end mill geometry, c) Lead and tilt angles [OZTU07]](koordinatensysteme, b) Kugelkopfgeometrie, c) Voreil- und Anstellwinkel [OZTU07]

The workpiece surface coordinate system is defined by the feed direction (F), the cross-sectional direction (C) and the surface normal direction (N). The tool coordinate system is given by the tool axis (z) and by the x and y-axis. The center of this coordinate system is the center of the hemisphere for ball-end mills, the tool tip for end mills and can differ for other types of milling tools. Two rotation matrices are individually calculated for a rotation around the feed normal vector C ($R_{fn}$) and around the feed direction ($R_{fl}$). These matrices are then subsequently multiplied for obtaining the global rotation matrix. Although this procedure may seem intuitive for users and developers of CAM systems, it does not describe a unique tool position in space, since matrix multiplication is not a commutative mathematical operation and the models do not necessarily consider the two rotations in the same order. Furthermore, the position of the tool cannot be defined if any of the angles $\beta_l$ or $\beta_{fn}$ is equal to 90°, being this a mathematical singularity of the models. In this way, even the vastly literature research about this topic is not consistently defined, what makes the process of comparing and gaining knowledge difficult. The Figure 2.13 illustrates this problematic.
An innovative model for defining the tool orientation is proposed by Arntz [ARNT13]. In this model, spherical coordinates are used for defining the position of the tool axis, as depicted in Figure 2.14. The supination angle $\theta_N$ (90° - polar angle) is the angle between the surface normal axis N and the tool axis z. It can vary between 0° and 90°. The azimuth angle $\psi_F$ is defined as the angle between the feed axis F and the projection of the tool in the FC plane and can assume values between -180° and 180°. The major advantage of this system is the lack of singularities and the unambiguous definition of the tool orientation in space. Another advantage is the directed relationship between the azimuth angle $\psi_F$ with up and down milling. Negative values of $\psi_F$ will always characterize up milling while positive values are always related to down milling [ARNT13]. For defining the position of the cutting edge, it is necessary to add a rotation around the tool axis. This rotation is given by the angle $\varphi$.

The tool axis is defined by the vector:

$$\vec{v}_{wz} = \begin{pmatrix} F \\ B \\ N \\ 1 \end{pmatrix}$$
By using homogeneous coordinates, the following rotation matrix can be derived from the described rotations [ARNT13]:

\[
\begin{bmatrix}
F & -\sin \psi_F & 0 & 0 \\
\sin \psi_F & \cos \psi_F & 0 & 0 \\
0 & 0 & 0 & 1 \\
0 & 0 & 0 & 1
\end{bmatrix}
\]

\[
B =
\begin{bmatrix}
\cos \theta_N & 0 & \sin \theta_N & 0 \\
0 & 1 & 0 & 0 \\
-\sin \theta_N & 0 & \cos \theta_N & 0 \\
0 & 0 & 1 & 0
\end{bmatrix}
\]

\[
N =
\begin{bmatrix}
\cos \varphi & -\sin \varphi & 0 & 0 \\
\sin \varphi & \cos \varphi & 0 & 0 \\
0 & 0 & 0 & 1 \\
0 & 0 & 0 & 1
\end{bmatrix}
\]

\[U \quad V \quad W \]

Equation 2.3

\[
with c(x) = \cos(x) \text{ and } s(x) = \sin(x)
\]

### 2.1.4.2 Technological parameters

The path generated by the translational movement of the milling tool is referred to as tool path. Although the path can be very complex for the milling of free-form surfaces, it can be described by technological parameters, which are often used by CAM programmers and machine operators in the process layout. The terminology of chip removing, movements and geometry of the chip removing process is given in the DIN standard 6580 [DIN6580] parameters are also commonly found in catalogs of milling tool manufacturers and describe the manufacturer’s recommendations for each type of milling tool, material, and targeted surface combination. They are the most used parameters in the process layout. Figure 2.15 depicts the technological parameters for ball-end milling. The concept illustrated in this figure can be expanded to other peripheral milling operations. Nevertheless, different process strategies, as circular milling [MEIN09], require other parameters for describing the tool movement. A universal parameter set for describing all types of milling strategies cannot be found neither in the academic nor the industrial environment.

![Figure 2.15: Definition of the technological parameters for ball-end milling](image)
The step over \( a_{e,n} \) is the width between two consecutive tool passes. It is strongly correlated to the roughness of the generated surface, since the scallop height on the workpiece is directly correlated with the value of \( a_{e,n} \). The selection of the \( a_{e,n} \) value depends therefore on the quality expected of the machined surface. The depth of cut \( a_{p,n} \) is the tool penetration in the normal direction of the surface.

The feed per tooth rate \( f_z \) is the translational movement of the tool for each tooth pass. It is the most influential technological factor among the process forces. The feed velocity is directly related to the feed rate by the following equation:

\[
v_f = z \cdot f_z \cdot n \tag{2.4}
\]

In which \( n \) stands for spindle revolutions per minute (rpm). The nominal cutting speed \( v_c \) is the tangential speed of the cutting edge. It depends on the tool radius and also on the spindle speed.

\[
v_c = 2\pi \cdot R_0 \cdot n \tag{2.5}
\]

The cutting speed is one of the most relevant factors for the process planning. It affects the chip formation mechanism, forces, tool wear and consequently the workpiece quality. The distinction between local and nominal cutting speed is, therefore, crucial for the process modeling and design. The local cutting speed depends on the local radius of the milling tool, as described by the following expression:

\[
v_{c,\text{local}} = v_c \cdot \frac{r_i}{R_0} \tag{2.6}
\]

### 2.1.5 Cutting forces

Zerspankräfte

In order to clarify the terminology used for describing the mechanical loads in the cutting edge, the fundamentals about the cutting forces are presented in this section. The different models for predicting and simulating the tool load, as well as the several factors that influence the process forces are detailed in section 2.3.

The conventions about the process forces in cutting operations are presented in the DIN standard 6584 [DIN6584].

The cutting force \( F \) can be defined as the total force, applied by the tool on the workpiece, required to overcome [MERC45]:

- The elastic and plastic regimes of the workpiece’s material
- The frictional resistances present in the rake and flank faces.
The milling force can be divided into two components: the active force $F_a$ and the passive force $F_p$, which are contained in the work plane, defined by the vectors $v_c$ and $v_f$, and on its orthogonal direction, respectively.

The active force can be described in three different coordinate systems, as shown in the Figure 2.17.

Current models for modeling the process forces normally use the cutting force $F_c$ and the cutting normal force $F_{CN}$ for describing the active force [ALT12]. The feed coordinate system normally facilitates the characterization of the process forces, since measurement platforms are usually coupled to the workpiece and, therefore, the milling force is reported directly in this coordinate system. For a given angular position $\varphi$, the cutting and feed coordinate systems are related by the following trigonometric transformation:


\[
\begin{bmatrix}
F_C \\
F_{CN}
\end{bmatrix} = \begin{bmatrix}
-\cos \varphi & \sin \varphi \\
\sin \varphi & \cos \varphi
\end{bmatrix} \begin{bmatrix}
F_f \\
F_{fN}
\end{bmatrix}
\]

Equation 2.7

Where \( F_f \) is the feed force and \( F_{fN} \) the feed normal force. The effective coordinate system is used when the milling parameters are such that the cycloidal behavior carried by the teeth cannot be neglected. In this case, through the use of the effective direction angle \( \eta \), the active force is decomposed in \( F_e \) and \( F_{eN} \), which are the effective force and the effective normal force components, respectively.

**Orthogonal and oblique cutting**

Many models are based on the orthogonal cutting to investigate the process forces. This particular type of machining is defined as cutting with the tool cutting edge inclination \( \lambda_s = 0^\circ \) and the tool cutting edge angle \( \kappa_v = 90^\circ \) [KLOC08]. In this case, the passive force \( F_p = 0 \) N, which facilitates the system theoretical analysis [CLAU05]. This type of cutting is not used in practical applications, yet it is ideal for the comprehension of the chip formation mechanism, due to the reduced number of output parameters [CLAU05]. It is also the type of cutting used for acquiring process forces parameters in different models [ALTI96, BUDA07, MERC45]. The cutting is assumed to be uniform along the cutting edge [ALT112]. The chip thickness \( h_{sp} \) and the cutting width \( b_{sp} \) are well defined in this operation, which enables an individual assessment of their influence in the material removal.

The oblique cutting is one enhancement of the orthogonal cutting, in order to incorporate the cutting edge inclination \( \lambda_s \) into the model [ALT112]. The process is contemplated in a three-dimensional way, which depicts a better representation of the reality [CLAU05]. In this case, the passive force \( F_p \) is different from zero. A schematic representation of the orthogonal and oblique cutting is shown in Figure 2.18.

![Figure 2.18: Orthogonal and oblique cutting [ALT112]](image)
2.2 Geometrical parameters

The uncut chip can be characterized by its geometrical parameters, which are defined according to the DIN standard [DIN6580]. A distinction between macroscopic and microscopic contact conditions is essential for the process modeling, since the capability of predicting the resulting parameters differs according to the model and approach used [MEIN09, MINO15].

2.2.1 Macroscopic contact conditions

Macroscopic geometrical parameters are parameters resulting from the translational tool movement, without considering the tool rotation around its axis [MEIN09]. They are the product of the intersection between the tool macro-geometry and the workpiece, without taking into account the helix angle. They can be understood as the material volume removed by one tooth in one revolution of the tool. The resulting volume is characterized by these parameters. The Figure 2.19 illustrates the macroscopic contact conditions for ball-end milling.

The main parameter used for determining the macroscopic contact conditions is the contact angle, which is the tool rotation angle between the cutting edge entrance and exit angles [MINO15, WITT14, MEIN09].

\[ \phi_c = \phi_a - \phi_e \]  \hspace{1cm} \text{Equation 2.8}

Whereby \( \phi_e \) and \( \phi_a \) are the entrance and exit angles, respectively. In most engagement situations, the contact angle varies along the tool axis. The chip volume and surface area and length are also derived from the macroscopic contact conditions.

2.2.2 Uncut chip geometry

The uncut chip geometry derives from the superposition of the rotational and translational components of the tool movement. It is the result of the projection of the chip volume on the cutting edge for a given tool rotational angle. Three basic parameters can be used for describing the uncut chip geometry: thickness \( (h_{sp}) \), width \( (b_{sp}) \) and length \( (l_{sp}) \). A local calculation of these parameters, i.e. a computation for a given point of the cutting edge, forms the basis for the
calculation of further geometrical quantities, such as the chip cross-sectional area $A_{sp}$. A complete definition of all parameters that constitute the uncut chip geometry can be found in [DIN6580].

The chip cross-sectional area $A_{sp}$ is the projection area of the uncut chip on the cutting edge in a given rotation angle. It can be understood as the material surface seen by the cutting edge in a given time during the tool engagement. Its width is defined as the chip width $b_{sp}$ while its thickness is the chip thickness $h_{sp}$ [DIN6580]. The chip length is the cutting length of each cutting edge element during the tool revolution. If locally calculated, this definition of the uncut chip geometry can be universally applied in all types of tools and process strategies.

2.2.2.1 Chip thickness

The uncut chip thickness $h_{sp}$ represents an important quantity for the local cutting force and thus, is a parameter suitable to explain the phenomena of various wear and chip formation mechanisms. It also forms the basis for numerous attempts to model the process forces [STREM63, PIEK56, KIEZ42, ALT12, KLOC08]. As the cutting thickness varies both along the tool rotation and along the tool axis, it is reasonable to define it locally, as a function of the cutting edge position.

Each cutting edge describes a cycloidal motion, derived from the combination of a rotational and a translational movement. The translational movement is given by the tool path, which varies according to the milling strategy while the rotational component is determined by the spindle speed. The tool path can be approximated by linear segments between two consecutive cuts, which enables a simplified calculation of the uncut chip thickness.

The uncut chip thickness is defined as the radial distance between the paths by superposing the paths of two consecutive teeth. It is a function of the angular position of the tooth, once the center of the cycloid and its curvature are not constant along the trajectory. Figure 2.20 illustrates a down milling 3-axis milling process, with two cutting regions named I and II.

![Figure 2.20: Uncut chip thickness and cycloidal trajectory in 3-axis milling](image-url)
As the cutting speed is much greater than the feed velocity \((v_c >> v_f)\) it is possible to approximate the tooth trajectory by a perfectly circular path. With this consideration, Marttelotti [MART45] derived an equation for calculating the uncut chip thickness, which is simply the distance between two circumferences.

\[
h_{sp,i}(\varphi) = \frac{D}{2} + f_z \sin \varphi - \sqrt{\left(\frac{D}{2}\right)^2 - (f_z \cos \varphi)^2}
\]

Equation 2.9

This equation is nevertheless valid only for the region II in the Figure 2.20. In the region I \((\varphi_e < \varphi < \varphi_a)\), the chip thickness can be approximated linearly from zero to \(h_{sp,max}\), resulting in the following expression [MEYE09]:

\[
h_{sp,ll}(\varphi) = h_{sp,max} \cdot \frac{\varphi - \varphi_e}{\varphi_{hsp,max} - \varphi_e}
\]

Equation 2.10

where \(\varphi_e\) is the cutting entrance angle in and \(\varphi_{hsp,max}\) is the angular position of the maximum uncut chip thickness given by:

\[
\varphi_{hsp,max} = 180^\circ - \arctan \left( \frac{D \sin \varphi_c - 2f_z}{D - 2a_{e,n}} \right)
\]

Equation 2.11

In a simplified form, for processes where the feed speed \(v_f\) is much smaller than the cutting speed \(v_c\) and the feed per tooth is much smaller than the tool radius \((f_z << D_0/2)\), the uncut chip thickness can be expressed as following [ALTI12, KÖNI82]:

\[
h_{sp}(\varphi) = f_z \sin \varphi
\]

Equation 2.12

This expression only describes the chip thickness for a 3-axis milling process with end mills. Current models for ball-end milling only approximate the value of \(h_{sp}\) as a projection of the feed direction in the tool surface normal [OZTU07], by calculating the scalar product between the feed and the unitary tool’s edge normal vector \(\hat{n}\), which is a vector formed from the cutting edge towards the tool center point.

\[
h_{sp}(\varphi) = \hat{f} \cdot \hat{n}(\varphi)
\]

Equation 2.13

Whereby the feed vector in the coordinate system FCN is defined as:

\[
\hat{f} = \begin{pmatrix} 0 \\ 0 \\ f_z \end{pmatrix}
\]

(2.2.1)

No universal expression exists in the literature for calculating the chip thickness in multi-axis milling processes with complex engagement situations. The main challenge resides in considering the stock material and the large variety of milling strategies in the calculation. All available approaches assume therefore simplified process kinematics and tool geometries.
2.2.2.2 Chip width and cross-sectional area

The Figure 2.21 presents the uncut chip geometry for simplified 3-axis milling processes using end-mills [MEIN09].

![Figure 2.21: Uncut chip geometry for simplified 3-axis milling with end mills](image)

Applying the definitions presented here, following equations can be derived:

\[ b_{sp}(\phi) = \frac{\Delta h(\phi)}{\cos \lambda} \tag{Equation 2.14} \]

Where \( \Delta h(\phi) \) is the tool height in contact with the workpiece for a given contact angle \( \phi \) and \( \lambda \) is the helix angle.

\[ A_{sp}(\phi) = h_{sp}(\phi) \cdot b_{sp}(\phi) \tag{Equation 2.15} \]

More advanced approaches are needed for calculating the chip width and cross-sectional area for multi-axis milling processes, which are presented in Chapter 4.

2.3 Simulation of milling processes

2.3.1 Process chain in milling

The machining of free-form surfaces is especially associated with high-speed machining as well as simultaneous 5-axis high complex milling operations so that the process is only possible using computer-aided manufacturing (CAM) [STAU05]. In these systems, software is used to assist in all milling operations, aiming to achieve faster and more consistent production process and parts with more precise dimensions.

A simulation-assisted process chain for milling operations is illustrated in Figure 2.22. The typical process chain is constituted of three main steps: product planning, which mostly requires CAD; production planning, where the CAM and simulation software can be employed; and lastly the manufacturing.
In this figure, it is possible to identify the potential of simulation during the process planning. Through the holistic use of model-based simulation systems, potential process weak points can be recognized at an early stage. In this way, time and effort can be spared by avoiding changes in later production phases [ZABE10]. Current CAM-based systems for the programming of milling operations offer the user only the most common technological parameters for designing the process (depth of cut, feed rate, step over, etc.). Current studies show advances in the implementation of advanced algorithms for enabling a parameter based process design [SURM06, STAU05, MINO15, ARNT13, KLIMA13]. By using geometrical parameters instead of only technological parameters, it is possible to design more efficient and productive processes. In this context, the need for models that can determine process parameters for a vast number of process strategies become more evident.

### 2.3.2 Coupled simulation of milling operations

The simulation of milling operations is mainly characterized by isolated simulations of single stages of the process chain. Current researchers point out the need for combining different systems to an integrative simulation tool [BREC09, BREC11, LOHS13, MINO15]. Within the scope of the project “Integrative production technology for high wage countries” of the RWTH Aachen University, the development of a Virtual Manufacturing System (VMS) is proposed, which consists of a platform with various individual simulations that are connected to each other. The milling process is composed of different subsystems, which interact with each other. The process itself consists of the interactions between cutting edge and workpiece. The tool kinematics and therefore the process is however affected by the interaction between the ma-
chine structure, the control unit, fixtures, CAM and post-processor properties. A coupled simulation of milling operations, which takes into consideration all elements that influence the cutting, allows thus the prediction of process outputs that would otherwise not be possible, such as regenerative vibrations and form errors in the manufacturing of complex shapes [BREC11]. Figure 2.23 illustrates the coupled simulation of milling process proposed by Brecher [BREC11]. The model for force simulation of milling processes depicted, which is illustrated in this figure is subject of the present thesis.

![Figure 2.23 Scenario for coupled simulation of milling processes [BREC11]](image)

The main purpose of the force simulation module in this context is the calculation of the process forces that will influence the machine dynamics, which in turn influence the tool kinematics and dynamics. This simulation is performed in a closed loop so that optimizations of the machining and machine parameters can be achieved. A prerequisite for this is a fast calculation capability of the system and each of the modules, given the restriction of simulation times. Simulation cycles of approximately 125 μs are aimed [BREC11]. This requirement limits the possible algorithms used for the simulation of each sub-system. Algorithms that demand high computational effort are therefore ruled out.

### 2.3.3 Prediction of the process forces in milling

Berechnung der Prozesskräfte beim Fräsen

Several attempts have been made in the past to predict the process forces in milling. A close examination of the literature points out different classifications of the large number of existing approaches and models for simulation of various process phenomena. A combination of the classifications proposed by Lutertveld et al. [LUTT98], Zabel [ZABE10] and Clausen [CLAU05] is illustrated in Figure 2.24.
2.3.3.1 Semi-empirical and empirical models

Semi-empirical and empirical models are strongly associated with the work of Taylor [TAYL07]. Taylor was probably the first to define basic guidelines for designing machining processes, in the late 1800s, a time when usual cutting speeds hardly passed 30 m/min and spindle speeds would barely reach 30 rpm [TAYL07]. His guidelines were targeted to help machine operators in their daily practical tasks in the shop floor [LUTT98]. He derived the first equation that relates process parameters with the cutting forces, being the first to identify the influence of the chip cross-sectional area $A_{sp}$ and the cutting force. [TAYL07, WALL08]

$$F_c = k_c \cdot A_{sp}$$

Equation 2.16

Where $k_c$ is a cutting coefficient determined by an exponential function. This relation quickly acquired practical applications, due to its simplicity. Even without knowing how to predict the process forces precisely for a certain application, due to the vast amount of experiments required for determining the value of $k_c$ with sufficient precision, machine operators and process designers adhered to the intuitive concept that the cutting force could vary directly with the chip cross-sectional area. The biggest flaw in these models is that extensive experimentation is needed each time a new cutting variable is added to the process [LUTT98]. That means, a new set of experiments is required when a different combination of materials, tool geometries, machine or cutting parameters, among other variables, is used.

2.3.3.2 Analytical models

Still in the early 1900s, other authors and scientists made significant contributions to the theory of cutting force, alongside with the early development of analytical models. Friedrich formulated an equation for determining the force coefficient $k_c$ [FRIED09] while Kronenberg worked on the concept that it is possible to combine the feed rate, the depth of cut $a_{p,n}$ and the chip cross-sectional area in just one coefficient [KRON27]. Martellotti, Königsberger and Sabberwal
[MART41, KOEN61, SABB61] determined a new equation for analyzing the cutting forces of end milling operations.

\[ F_c = k_c \cdot b_{sp} \cdot h_{sp} \]  \hspace{1cm} \text{With } i = c, \text{cn, p} \quad \text{Equation 2.17}

A milestone in the modeling of the process forces was the work of Kienzle [KIENZ42]. In 1942, Kienzle published the perhaps most famous equation for determining the process forces until modern days, which is an improvement of the works performed by Martellotti, Königsberger, and Sabberwal.

\[ F_i = k_{i11} \cdot b_{sp} \cdot h_{sp}^{(1-m_i)} \]  \hspace{1cm} \text{With } i = c, \text{cn, p} \quad \text{Equation 2.18}

This equation indicates until today the most known relationship between the geometrical parameters of the milling processes and the process forces.

Another important milestone was the work of Merchant [MERC45]. He developed a new science-based approach with the shear plane theory. Merchant assumed that the shear occurs in a thin plane. He used experimental data along with theoretical equations to predict the shear stress values and therefore the force coefficients - for given chip thickness, cutting velocity and tool rake angle. His model is illustrated in Figure 2.25.

![Shear plane model according to Merchant](image)

\[ P_i: \text{ Tool reference plane} \]
\[ P_s: \text{ Shear angle trace} \]
\[ F: \text{ Resultant force} \]
\[ F_s: \text{ Shearing force} \]
\[ F_{\text{sn}}: \text{ Shearing normal force} \]
\[ F_c: \text{ Cutting force} \]
\[ F_{\text{cn}}: \text{ Cutting normal force} \]
\[ F_{rt}: \text{ Rake face parallel force} \]
\[ F_{rn}: \text{ Rake face normal force} \]
\[ \gamma_h: \text{ Normal rake angle} \]
\[ \rho: \text{ Apparent friction angle} \]
\[ \phi: \text{ Shear angle} \]

From which following mathematical expressions for the cutting and normal cutting forces can be derived:

\[ F_c = \frac{\cos(\rho - \gamma_h) \cdot \tau_\phi}{\sin(\phi) \cdot \cos(\phi + \rho + \gamma_h)} \cdot b_{sp} \cdot h_{sp} \]  \hspace{1cm} \text{Equation 2.19}
Using the principle of the minimum energy, the minimum cutting force is found by differentiating Equation 2.19 with respect to $\phi$:

$$\frac{dF_c}{d\phi} = r_n \cdot \cos(\rho - \gamma_n) \cdot \cos(\rho - \gamma_n + 2\phi) \cdot \sin^2(\phi) \cdot \cos^2(\rho - \gamma_n + \phi) \cdot b_{sp} \cdot h_{sp} = 0$$  
Equation 2.21

Which results in:

$$\rho - \gamma_n + 2\phi = \frac{\pi}{2}$$  
Equation 2.22

This equation shows the relation between the normal rake angle, the friction coefficient, and the shear angle. The shear angle increases with the rake angle while a higher friction angle results in smaller shear angles.

While Merchant used the principle of minimum energy, Krystof proposed the principle of the maximum shear stress for determining the value of the shear angle [ALTI12]:

$$2\rho - 2\gamma_n + 2\phi = \frac{\pi}{2}$$  
Equation 2.23

This relationship results in smaller values of the shear angle. Several other authors contributed to the calculation of the friction angle $\rho$ and shear angle $\phi$ based on Merchant’s shear plane theory [LEE51, HUCK51, AMOR03, PALM59, ALTI12]. The models result in different relationships between the shear angle and the friction and rake angles.

Mechanistic modeling is a term marked by the name of Altintas. He applied the shear plane theory for the oblique cutting. Altintas [ALTI96] introduced an oblique concept to ball-end milling process, in which the flute was expressed in a parametric form and divided into small oblique cutting edges. The mathematical relationships were developed to relate the geometry of each oblique element to variables obtained from orthogonal cutting experiments. He expanded the model designed by Friedrich in the early 1900s [FRIE09] to the passive and cutting normal forces.

$$dF_i = (k_{i,e} + k_{i,c} \cdot h_{sp}) \cdot db_{sp}$$  
With $i = c, cn, p$  
Equation 2.24

Where $dF_i$ are the differential cutting ($dF_c$), cutting normal ($dF_{cn}$) and passive ($dF_p$) forces. The uncut chip thickness and differential chip width are given by $h_{sp}$ and $db_{sp}$ respectively. The cutting forces are separated into cutting and edge components, which are determined by the force coefficients $k_{el}$ and $k_{cu}$. Lee, Budak and Altintas [BUDA96, LEE95] also derived an experimental method for determining the force coefficients from orthogonal cutting data. They based their model on the orthogonal cutting model from Merchant [MERC44] and the mechanics of oblique cutting presented by Armarego and Brown [ARMA69].
First, a series of orthogonal cutting experiments is performed, and the values of the cutting force $F_c$ and cutting normal force $F_{cn}$ are acquired for different cutting speeds and rake angles. Next, the values of the friction angle and shear stress are obtained by using the well-known orthogonal cutting model equations from Merchant [MERC44, ARMA69].

From the Figure 2.25 it is possible to derive the following expression:

$$r_h = \frac{h_{sp1}}{h_{sp2}} = \frac{\sin\phi}{\cos(\phi - \gamma_n)} = \frac{\sin\phi}{\cos\phi \cos\gamma_n + \sin\phi \sin\gamma_n} = \frac{1}{\cot\phi \cos\gamma_n + \sin\gamma_n} \quad \text{Equation 2.25}$$

Then,

$$\tan \phi = \frac{r_h \cos \gamma_n}{1 - r_h \sin \gamma_n} \quad \text{Equation 2.26}$$

With $r_h$ being the chip thickness deformation ratio. Measuring the deformed chip thickness is very inaccurate, so it is easier to estimate $r_h$ by measuring the deformed chip length. Assuming the chip is continuous and incompressible, the ratio $r_h$ is obtained as following [ARMA69]:

$$r_h = \frac{h_{sp}}{h'_{sp}} = \frac{l_{sp}}{l'_{sp}} \quad \text{Equation 2.27}$$

Whereby $h_{sp}$ and $l_{sp}$ are the uncut chip thickness and length respectively while $h'_{sp}$ and $l'_{sp}$ are the deformed values.

The friction angle $\rho$ can be calculated from Equation 2.19 and Equation 2.20, by dividing both and solving the resulting equation for $\rho$, yielding following expression:

$$\tan \rho = \frac{F_{cn} + F_c \tan \gamma_n}{F_c - F_{cn} \tan \gamma_n} \quad \text{Equation 2.28}$$

The shear stress $\tau_s$ is then calculated by from the resulting forces in the shear plane divided by the sliding area:

$$\tau_s = \frac{(F_c \cos \phi - F_{cn} \sin \phi) \sin \phi}{b_{sp} h_{sp}} \quad \text{Equation 2.29}$$

Finally, the cutting coefficients are derived from the modified mechanics of cutting provided by Armarego and Brown [ARMA69]:

$$k_{cc} = \frac{\tau}{\sin \phi_n} \sin \left( \rho_n - \gamma_n \right) \sin \lambda + \tan \eta_c \sin \rho_n \tan \lambda \quad \text{Equation 2.30}$$

$$k_{nc} = \frac{\tau}{\sin \phi_n \cos \lambda} \quad \text{Equation 2.31}$$
\[ k_{pe} = \frac{\tau}{\sin \phi_n} \cos \left( \rho_n - \gamma \right) \tan \lambda - \tan \eta_c \sin \rho_n / c \]  

Equation 2.32

Where:

\[ c = \sqrt{\cos^2(\phi_n + \beta_n - \gamma_n) + \tan^2 \eta_c \cdot \sin^2 \beta_n} \]

\( \eta_c \) is the chip flow angle in the rake face as described in the Figure 2.26.

![Figure 2.26: Definition of the chip flow angle \( \eta_c \) [ARMA69]](image)

The radial rake angle \( \gamma_r \) is defined as the rake angle of the cutting edge section normal to the tool axis. The normal rake angle is related to the radial rake angle by the following expression [ARMA69].

\[ \tan(\gamma_r) = \tan \gamma_r \cdot \cos(\lambda_i) \]  

Equation 2.33

Where \( \lambda_i \) is the helix angle.

In order to apply the equations for an oblique cutting, the tool cutting edge is divided into infinitesimal elements, and each one is analyzed separately. In this formulation, each small cutting edge is considered as performing an oblique cut, whereas a single part does not influence the others nor the entire chip formation. Therefore, this model considers a flat chip instead of a curved one, which would be more suitable to reality.

The edge coefficients \( k_{oe}, k_{one} \) and \( k_{pe} \) are calculated based on the experimental data, by extrapolating the force data and calculating its value for \( h_{bp} = 0 \).

In this approach, an orthogonal database has to be established for the given tool-workpiece material combination [BUDA96].

In sum, the model from Altintas makes following assumptions (ALTI12):
The shearing takes place in a thin layer called shear plane.

The deformed chip is not curved.

Orthogonal shear angle is equal to the normal shear angle in oblique cutting ($\phi_c = \phi_n$).

The normal rake angle is equal to the radial rake angle in orthogonal cutting ($\alpha_n = \alpha_r$).

The chip flow angle is equal to the oblique angle, which is itself equal to the local helix angle ($\eta_c = \lambda$).

The friction angle ($\rho$) and shear stress ($\tau_s$) are the same in both orthogonal and oblique cutting for a given speed, chip load, and tool-work material pair.

Altintas [1996] also proposed a method for calculating the cutting and edge force coefficients for specific cutting operations by measuring the average forces in milling with cylindrical end mills. In this case, an idealized chip geometry is assumed, and the chip thickness is taken as a function of the contact angle ($h_{op} = \sin(\varphi)$), so that the equations can be integrated along the tool axis and the coefficients can be derived. This approach is nevertheless only suitable for straight cutting operations, since the integral equations cannot be solved for more complex chip geometries, which are not described directly by mathematical expressions. He called this method mechanistic approach.

Although vastly used, the approach from Altintas is strongly criticized, mainly because of the shear plane and straight chip assumptions. Furthermore, the assumptions made are only partially reasonable when scale effects and the cutting edge rounding is taken into consideration.

Lee, Palmer and Oxley used the principles of orthogonal cutting to develop shear zone models [LEE51, PALM59]. They observed that the chip formation occurs in a wider region rather than a thin plane. They used principles of minimum energy, taking the variation of the flow stress properties regarding strain, strain rate, and temperature into consideration [LUTT98]. The models developed also take as input the uncut chip geometry, as well as the cutting speed, tool geometry, and work material properties. Many other attempts of including the influence of further factors on the process forces were developed over the years. They allow for example the consideration of vibrations, material inhomogeneity, tool run-out and many other factors into the force modeling [ALTI12, TLUS81, CABR07, SMIT92].

### 2.3.3.3 Geometrical models

The term geometrical modeling comes from Zabel’s classification of simulation methods [ZABE10]. In the classification used within this work, geometrical models are models which are used for purely defining the macroscopic and microscopic contact conditions specified in section 2.2, without considering the analytical relations which lead to the process forces.

The macroscopic contact conditions can be extracted by using constructive solid geometry, sweep volume, voxel, and dextrel models, among other possibilities [MINO15, ZABE10,
The basic approach is to develop geometrical representations of the work-piece and milling tool and calculate their intersection along the tool path. The entrance angle $\varphi_e$, exit angle $\varphi_a$, and contact angle $\varphi_c$ can be then extracted from each disc element of the cutting tool by evaluation of the generated volume [BREC11]. Since these models only consider the tool macro-geometry, they are not suitable for calculation of the microscopic contact conditions, which are ultimately the parameters required for prediction of the process forces in multi-axis milling operations.

The available models for calculation of the microscopic contact conditions are only applicable under specific assumptions about the previous surface, failing to contemplate more complex engagement situations. Attempts of modeling these conditions for ball-end milling, for instance, assume the previous surface as being flat and use the common technological parameters depth of cut $a_{p,n}$, step over $a_{e,n}$ and feed rate $f_z$, for determining the uncut chip geometry [ARNT13, URBA09, HOCK96, BUDA96]. No residual material or complex tool path are taken into consideration in current approaches, as explained in section 2.2.

### 2.3.3.4 FE-based models

FE-based models are models which use numerical techniques for solving differential equations [ZABE10]. The investigated system is divided into infinitesimal sub-systems so that the targeted equations for these can be derived by using physical principles. Their application to machining gained momentum recently due to the advances in computer engineering and increase of the calculation capabilities [DENK10].

The basic approach is described by Liu [LIU03]:

- **1- Modeling of the geometry**
- **2- Meshing**
- **3- Determination of material properties**
- **4- Definition of boundary conditions and load situation as well as contact conditions and relative movements**
- **5- Derivation of the system equations (equilibrium, flow, etc)**
- **6- Solution of the system of equations**
- **7- Analysis and visualization of the results**

FE-based approaches allow a deep understanding of the process occurring in the chip formation. Nevertheless, they require profound previous knowledge about the material in combination with the tool geometry and surface properties. Furthermore, a proper knowledge of the geometrical conditions in milling is required for setting up the simulations, which are not yet complete.
known for complex engagements conditions. These approaches are for the purpose of this work less interesting and will therefore not be described in detail.

2.3.3.5 Influence of rounded cutting edges on process forces

Taylor was also the first to observe the influence of rounded cutting edges on the process forces. He observed an increase in the forces along the tool life that was not purely related to the tool wear, but to the fact that the cutting edge gets dull and rounded after a certain cutting length [TAYL07].

Masuko [MASU53] and Albrecht [ALBR60] revealed independently of each other the significance of the cutting edge rounding to the metal cutting process, suggesting basically the same model for describing the appearing phenomena related to the magnitude of the cutting edge radius. Albrecht considered the ploughing effect additionally to the shearing. He defined the ploughing force as the pressure on the rounded portion of the cutting edge. This pressure results in a force which is the reaction of the material in front of the cutting edge being pressed into both the chip and material surfaces [WYEN11]. The pressed material into the newly generated surface results in residual compressive stresses, which in turn could lead to form errors due to bending, if the workpiece shape is not sufficiently rigid [ALBR60]. Albrecht presented an adaptation of the force diagrams suggested by Merchant, which is illustrated in Figure 2.27.

Albrecht identified that the amount of ploughed material depends on the size of the cutting edge radius and that the total force increases linearly with the feed rate \( f_z \), once the zone of the cutting edge is fully engaged [ALB60]. The ploughing force, however, does not change with increasing feed and the friction angle \( \rho \) is independent of the uncut chip thickness [ALBR60].

The ploughing force can be then obtained by extrapolating the forces to an uncut chip thickness of \( h_{sp} = 0 \), similarly to the model proposed by Altintas [ALTI96]. The ploughing forces are therefore related to the edge coefficient \( k_{ie} \).

Wyen used a modified Kienzle model for determining the influences of the cutting edge rounding on the machining of Titanium [WYEN11]. He described the linear and exponential coefficients \( k_i \) and \( m_i \) from the Kienzle model (Equation 2.18) as a function of the cutting edge radius \( r_{\beta i} \), rake \( \gamma \) and clearance angle \( \alpha \):

\[
F_i = k_{i1,1} \cdot b_{sp} \cdot h_{sp}^{-1,1} \cdot m_i = k_{i1,1} \cdot \gamma^{n_i} \cdot \alpha^{m_i} \cdot r_{\beta i}^{n_i} \cdot b_{sp} \cdot h_{sp}^{c} \cdot \gamma^{n_i} \cdot \alpha^{m_i} \cdot r_{\beta i}^{n_i} \tag{2.34}
\]

The value of the coefficients \( k_{i1,1}^{n_i} \), \( n_i \), \( n_i \), \( n_i \beta_i \), \( m_i \), \( m_i \beta_i \), \( m_i \) and \( c \) are determined by regression of extensive orthogonal tests results. They are therefore only valid within the tested range. The model error increases for values of chip thickness outside the tested range. His model, however, can only be applied for determining the \( F_c \) and \( F_{cn} \) forces, being not applicable for determining the axial forces in multi-axis milling operations, which require the calculation of the passive force component. For determining these forces, further analytical abstractions are necessary.
Another important effect observed, which is influenced by the cutting edge rounding, is the accumulation of material in front of the cutting edge [ARAM08]. This was observed especially for small values of the uncut chip thickness $h_{sp}$. When the uncut chip thickness is equal to or smaller than a critical value, referred to as minimum chip thickness $h_{sp,min}$, part of the material is just ploughed instead of removed. This material is partially accumulated in front of the cutting edge while another part of it penetrates under the cutting edge. The latter suffers after that an elastic recovery and as a consequence pressures the tool flank. In these conditions, the specific cutting energy goes through a non-linear increase, as the rake angle turns to be negative. This characterizes what is called size-effect [ARAM08].
2.4 Conclusions regarding the simulation of milling processes

Fazit für die Frässimulation

The milling process is responsible for a significant amount of the added value of metallic parts. One limiting factor of this process is the reduced capacity of available methods and tools for supporting the design of effective multi-axis operations of complex shapes, especially when new materials, tools and process kinematics are introduced. Current industrial applications fail to consider the microscopic contact conditions and the resulting forces during the process planning. Therefore, unfavorable contact conditions are often used, leading to higher production costs and longer production cycles.

The solution is the development and integration of science-based models for supporting the process planning. A vast amount of approaches and models for predicting the process forces is available in the literature, all of which depend on the uncut chip geometry. Nevertheless, their applicability to the multi-axis milling is restricted to a few milling strategies and combinations of tool and workpiece material. This is in part due to the lack of understanding of how the cutting edge microscopic geometry affects the process forces. On the other hand, it is also related to the difficulties in the calculation of the uncut chip geometry for complex engagement situations. Even for the most common and disseminated milling tool geometries and engagement situations, the process is still only partially understood.

Current research presents solutions for calculating the macroscopic contact conditions, for complex engagement situations. Available models for calculation of the uncut chip geometry, nevertheless, assume simplified milling tool geometries, workpiece form and tool path. In ball-end milling, for instance, even the position of the cutting edge along the tool axis is not precisely defined, since the helix angle is often assumed as constant. For complex milling tool geometries and process strategies, in which the contact conditions vary along the tool axis and tool path, there is still no possible approach for calculating the uncut chip geometry. Models with universal application are therefore required.
As seen in the previous chapter, the state of the art shows a clear knowledge deficit about how to determine the undeformed chip geometry and process forces in the multi-axis milling of free-form surfaces. The existing models for calculating these parameters only describe special cases, leaving aside important variables such as the residual material, the changing contact conditions along the tool path and the cutting edge microscopic geometry. The result is the inability to correctly evaluate the tool path and process strategy during the process planning. The process performance is thus limited by weak spots. Furthermore, the selection of effective milling tools, process parameters and CAM strategies is also compromised by the lack of understanding about the contact conditions for a specific milling operation and how these affect the process forces. The process design is characterized by a laborious experimentation based on previous experiences, catalogs or research reports. In industrial applications, the process planner normally does not have the time nor the required knowledge and resources for designing an efficient process.

Current approaches for simulation-based planning and evaluation of milling processes rely mostly on macroscopic contact conditions, such as the chip volume and contact angles. These parameters only allow a superficial process understanding and do not provide sufficient knowledge for making full use of the process potential. A deeper process understanding requires the calculation of the microscopic contact conditions. Furthermore, these approaches usually represent isolated applications that reflect the behavior and characteristics of a single subsystem, disregarding the influence of the machine structure and NC controllers on the process. Current research indicates that the solution of the problem is a combination of multi-level simulation approaches. The development and implementation of science-based methods for process design and optimization which are suitable for an integrated simulation scenario are of utmost importance for enabling high-performance milling operations.

Given the above, this dissertation aims at the development and implementation of models for calculating the microscopic chip geometry and determining the process forces for multi-axis milling of free-form surfaces. The basic premise is the availability of macroscopic contact conditions, tool and process-related parameters. The integrability of these models into a coupled simulation scenario is highly regarded. The developed simulation system and methods contribute to a more efficient and reliable process planning and increase the process understanding, paving the way for a parameter-based process design.

This goal can be formulated more clearly through the composition of smaller and more concrete objectives:

- Determination of the influence of the tool microscopic geometry and process parameters on the process forces.
Subject and task

- Development of models and systematic for calculating the microscopic chip geometrical parameters for multi-axis milling with an arbitrary tool geometry and process strategy.
- Development of a simulation system for calculating the modelled parameters.
- Extension and application of the developed models on multi-axis milling operations.
- Verification of the results based on experimental data.

3.2 Definition of scope

Milling processes are influenced by numerous factors. The interactions with each other make the process characterization difficult. For this reason, it is always important to focus on selected factors for enabling a systematic investigation of the targeted phenomena. It is, therefore, crucial to hold all other variables constant. In this manner, it is possible to determine the influence of the isolated factor in the observed phenomenon. The process forces in milling are extremely difficult to determine, as they are not the result of the sum of isolated factors influences. Cross interaction between the influence factors must also be taken into consideration during the analysis, which makes of the experimental investigation a laborious task. These principles of scientific investigation were taken into account in defining the scope of this work.

The factors taken into consideration within this work are described in Figure 3.1. As here illustrated, for the purpose of simplification, some parameters are held constant in the experimental investigation.

The field of modeling of milling operations is vast. There exists a wide variety of models for describing different aspects of the milling process. Only approaches that are suitable for integration in industrial applications in a coupled simulation environment are considered within this work. Approaches that require elaborate setup, as well as long computational times, such as finite element methods, are due to their inherent low integrability ruled out.

The macroscopic contact conditions are assumed as known for a particular milling operation. For simplified cases, such as end and ball-end milling with inclined milling tool, these conditions are also analytically calculated. In this case, the previous geometry of the milling surface is taken as flat. The hot-work steel X38CrMoV5-1 (DIN 1.2343) is used for developing and validating the force model. The wide industrial dissemination of this steel, particularly in the die and mold industry, as well as the available knowledge about it in the literature, sets it as the ideal choice for the proposed examination.
Several important questions in the field of design and optimization of machining processes can be answered by a proper process modeling and understanding. Figure 3.2 shows the major players in the field and their key questions related to this work. A deeper knowledge of the undeformed chip geometry and its influence on process related phenomena can reduce the time for process setup and consequently reduce the production times and time-to-market. It can also reduce production costs by avoiding unfavorable cutting parameters, which can lead to early tool breakage and wear, among other undesirable effects. More efficient cutting tools, clamping systems, and machine tools can be designed with the knowledge of the process forces.

The present work enables this understanding for a vast range of process strategies and tools. The developed models are valid for any process kinematics and tool geometry, given rotational symmetry. The requisite for application of these models is the previous knowledge of the cutting tool geometry, the macroscopic engagement situation and the process related parameters, particularly feed rate, cutting speed and specific force coefficients.
The major outcomes of this work are a methodology and mathematical models for calculation of the microscopic geometrical conditions and process forces in multi-axis milling. The models also take into consideration important aspects of the cutting edge geometry, such as rake and clearance angles as well as the cutting edge rounding, delivering relevant technological and scientific results. The models are implemented into a software system, allowing it is verification in different industrial applications. User and software interfaces provide access to the system through a stand-alone application or other software or systems.
3.4 Procedure and structure of the thesis

Based on the previous problem description and objectives, the following research hypothesis is derived:

»If the macroscopic contact situation as well as the tool geometry, material and process dependent parameters are known, it is possible to calculate the undeformed microscopic chip geometry and to predict the process forces in multi-axis milling operations.«

To verify or falsify this hypothesis, three main research questions need to be answered, which determine the further procedure of this work:

- Research question I: What is the influence of the tool microscopic geometry and the process parameters on the process forces?
- Research question II: How can the undeformed chip geometry be determined for any multi-axis milling operation, given the macroscopic contact conditions and the tool geometry?
- Research question III: How can the geometrical, material and process-related parameters be used for the prediction of the process forces in multi-axis milling?

These questions were investigated and answered against the background of a coupled simulation environment. The proposed solution involves the integration of multi-scale geometrical models and their verification in experimental tests.

The procedure and structure of this thesis are illustrated in Figure 3.3. The state of the art is described in Chapter 2, in which the deficit of current models for the simulation of the process forces in industrial applications is pointed out.

The first research question is investigated in Chapter 4, in which the interdependence between process parameters, microscopic tool geometry, and the milling forces is studied. Analogous tests using a lathe were carried out for deriving the influence of the cutting parameters and cutting edge geometry in the process forces. The influence of the chip geometrical parameters on the process forces was determined. Available models for calculation of the process forces in multi-axis milling were then adapted for an integrated simulation scenario.

A model for calculation of the undeformed chip geometry is described in Chapter 5, dealing with the second and third research questions of this work. It is assumed that the macroscopic contact conditions are calculated using geometrical models. Based on these conditions and on the cutting edge geometry, mathematical models for the calculation of several geometrical and technological parameters which enable a detailed analysis of the contact situation were developed. The relationship between typical process parameters and the chip geometrical conditions was established, building the basis for a parameter-based process design.
The models developed in Chapters 4 and 5 were implemented into a simulation system, which is described in Chapter 6. The main goal of the system is to enable fast access to the developed knowledge and pave the way for the integration of the elaborated models into a coupled simulation environment. The system was implemented using Matlab 2014a and can be exported for integration into other simulations. The user and software interfaces are described.

The proposed models were applied in a selected case study described in Chapter 7, which serves as the basis for investigating the third research question. Experimental tests are performed with varying contact conditions. The parameters for describing the chip geometrical conditions were calculated using the simulation system and correlated with the measured process forces. The applicability of the models to multi-axis milling operations is discussed. Significant findings regarding the model applicability are outlined.

Finally, conclusions about the potential application of the developed models and their further development are made in Chapter 8.

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<td>Modeling and simulation of the process forces in milling.</td>
</tr>
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<td>Chapter 3: Subject and task</td>
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</tr>
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<td>Chapter 4: Influence of the tool microgeometry and the process parameters on the cutting forces</td>
<td>Determination of the influence of the geometrical conditions and process parameters on the process forces. Force model.</td>
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<td>Universal model for calculating the uncut chip geometry, cutting forces and further parameters for multi-axis milling with an arbitrary tool geometry and tool path.</td>
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Figure 3.3: Procedure and structure of the thesis

Forschungskonzept und Aufbau der Arbeit
4 Influence of the tool micro-geometry and the process parameters on the cutting forces

Einfluss der Werkzeugmikrogeometrie und Prozessparameter auf die Zerspankräfte

The state of the art shows that the magnitude of the cutting forces on the machining of a particular material is strongly dependent on the tool micro-geometry and process parameters. The cutting edge microscopic geometry is of particular importance in finishing operations, in which the values of the uncut chip thickness are close to the cutting edge rounding radius. In these conditions, the effective rake angle is mostly negative, resulting in an increase of the ploughing forces and on a different behavior of the forces in comparison with processes with higher values of the uncut chip thickness.

To predict the process forces on peripheral milling operations, it is, therefore, necessary to understand the influence of the cutting edge microscopic geometry and the process parameters on the different force components. This understanding allows the development of a mathematical model which is suitable for posterior application in an integrated simulation environment, enabling the consideration of the process forces still in the early phases of the process planning.

This chapter presents an experimental investigation of the influence of the parameters mentioned above on the cutting forces in finishing milling operations of the hot-work steel X38CrMoV5-1 (DIN 1.2343, AISI H11). The investigation was performed in orthogonal cutting with varying cutting edge geometries and process parameters. The relation between the parameters was empirically evaluated. Based on the sensitivity analysis of the results, a model for prediction of the cutting and cutting normal forces was derived. The developed model was then extended to the oblique cutting by using analytical expressions.

4.1 Experimental procedure

Versuchsmethodik

The research question number one is the subject of investigation of this chapter:

- What is the influence of the tool microscopic geometry and the process parameters on the process forces?

The milling process is characterized by an interrupted cutting, in which the cutting edge suffers a cyclic thermal and mechanical load, whereby the latter is the topic studied in this work. To investigate the mechanical loads and develop a mathematical model that can be extended to multi-axis milling operations, it is necessary to understand the load in each portion of the cutting edge and how this contributes to the total load. In experiments, only the total load can be measured, without the possibility of determining the load distribution along the cutting edge. For this reason, an experimental investigation in free orthogonal cutting tests was conducted, in which it was possible to isolate the effects of each different parameter on the active cutting forces components.


4.1.1 Experimental setup

The orthogonal cutting experiments were carried out on a Hembrug Slantbed-Mikrotum 100 (Figure 4.1). The machine is a dual-axis ultra-precision fully hydrostatic lathe, which is characterized by high static and dynamic rigidity. It was necessary to construct a unique experimental setup to realize the orthogonal cut on this lathe.

The specimens were attached to a specially developed sample holder, which was clamped to the machine chuck. The sample holder was designed with a view to providing a rigid support for the samples, being carefully balanced to avoid vibrations and run-out. At the same time, it allowed a quick replacement of the specimens.

The tool holder was specially machined to achieve a tool cutting edge inclination $\lambda_s = 0^\circ$ and the tool cutting edge angle $\kappa_r = 90^\circ$, conditions necessary for orthogonal cutting. This was then attached to a dynamometer, which in turn was fixed on the machine revolver. The tool was centered with the rotation axis of the lathe and by adjusting the height of the tool with an especially ground metal shim, it was possible to set the rake and clearance angles for constant wedge angles. Through continuous feed of the tool, an orthogonal cut was then realized, and the formed chip was removed uniformly.

Figure 4.1 shows the engagement during the tests. The coordinate system shows the specifications of the force plate and later analyzed forces in their orientation. The sample is rotated and thereby the tool comes into engagement, while the set chip thickness, which could be adjusted by the feed, is removed. The active forces could then be directly extracted from the x- and z-force channels of the used dynamometer.

Figure 4.1: Experimental setup for orthogonal cutting

Versuchsaufbau zum orthogonalen Schnitt
To investigate the targeted phenomena, the process was characterized by the resulting forces. In addition, the generated chips were collected and submitted to a metallographic analysis. Also, the generated workpiece was characterized regarding surface roughness and hardness. In the following, the methodology used in the analysis is described.

**Measurement of the process forces**

Figure 4.2 shows the measurement chain used in the acquisition of the force data. The resulting forces were measured on the tool side with a 3-components dynamometer Kistler 9121. The piezoelectric force transducer was attached to a multichannel charge amplifier Kistler 5070. The amplifier also serves as conditioner and filter. The signal was filtered with a low-pass filter set to 3 kHz. Each force component was then processed by a digital-to-analog converter National Instruments 9125 and acquired on a measurement laptop via a USB module National Instruments USB-9162. The software Diadem 2012 was used in the data acquisition and posterior evaluation. The measured forces were directly converted to the active forces $F_c$ and $F_{cn}$ according to the coordinate systems illustrated in Figure 4.1.

**Chip analysis**

Each test generated multiple chips, which were collected and analyzed. First, the hypothesis of chip breakage was tested. This was performed by calculating the theoretical weight of the chips and measuring the deformed chips with a high precision scale. A comparison between the measurements showed that no chip breakage occurred in the tested parameter range.

The chips were then analyzed with an optical microscope to measure the deformed length, thickness and degree of segmentation, among other aspects. For each experiment, two chips were analyzed.

Furthermore, a metallographic investigation of the chips was performed to gather information about the chip formation mechanisms during the cutting process.

**Analysis of the workpiece hardness and surface roughness**

In order to collect information to evaluate the amount of ploughing during the cutting process, the surface roughness and hardness of the test specimens were analyzed. An increase in both quantities could give comparative indicators to higher ploughing forces.
The hardness was measured before and after the tests in a micro-hardness testing machine Leco M-400-H, whereby a four-sided diamond pyramid with a test load of 0.1 kg was used. The test load was specially kept very low to measure the hardening at the upper layers of the newly generated surface, which may be only a few micrometers thick. The load is held for 10 seconds. The plastic deformation of the material results in a corresponding impression on the specimen, whose geometry is then measured under a microscope. Based on the average diagonal length it is then possible to evaluate the hardness from table values. For each sample, five points were measured in the longitudinal direction.

The surface roughness was optically measured with a white light interferometer Wyko NT 1100, from the company Veeco. This device enables a high-resolution, non-contact surface measurement. The measurement is performed by using filtered white light, which is reflected on the specimen surface and a reference mirror. The light is then superposed to an interference pattern. The short coherence length of the white light is then used to determine the surface roughness of the measured area.

4.1.2 Characterization of the cutting tools
Charakterisierung der Schneidwerkzeuge

Since the influences of tool wear and coating were not the focus of this investigation, uncoated inserts were used in the tests, which were additionally performed without the use of cutting fluid, reducing the amount of influencing factors on the process and focusing on the targeted parameters. Tool wear can be quite high in this conditions. Therefore, a new insert was used in each experiment. The inserts were also measured before and after each cut, to certify that no wear or breakage of the cutting edge influenced the measured forces. Thus, the effect of isolated parameters could be investigated.

Solid carbide inserts from the company SECO Jabro Tools GmbH type TNMA160404F 410 were used, which were specially modified for the experiments, to achieve the targeted geometry. The cutting edge radii $r_e$ were prepared to 5 $\mu$m, 10 $\mu$m, 20 $\mu$m and 40 $\mu$m, whereby different combinations of rake and clearance angle were used.

The exact determination of the cutting edge radii represents an important and essential factor for the present investigation. The precise cutting edge geometry was optically measured with an infinite focus variation method, defined by the standard ISO 25178 [ISO25178]. This approach is particularly well-suited for measuring the shape of critical surfaces such as angles and radii of cutting edges. In order to perform the measurement, the cutting edges are illuminated with a coaxial white light, which is then reflected. A precise optical system projects the light onto a digital sensor, which evaluates the contrast values between adjacent pixels and determines the position of the measured surface on a plane. The focus is then varied in a defined range, gathering data for each focus plane. The measured part can be afterward reconstructed in a 3D and different sectional views.

The Alicona InfiniteFocus measurement system was used, as illustrated in Figure 4.3. The measurements were performed in three positions of each cutting edge of all inserts used.
A deviation between the values provided by the manufacturer was found. The measured values were therefore considered in the modeling.

### 4.1.3 Material selection and manufacture of the test specimens

The material chosen for the present investigation was the hot work tool steel X38CrMoV5-1 (DIN1.2343, H11). The choice of this material was motivated by its high industrial relevance and the vast amount of scientific information available, which serves for validating the newly developed models.

The AISI H11 has a low carbon content, a high chromium content, and a good toughness, offering a high corrosion resistance as well. It also has an excellent machinability, reason why this material is used as die material in many hot working processes. It is applied in mandrels, dies and containers for metal tube and rod extrusion, tools and dies for the manufacture of hollow bodies, die casting equipment, forming dies, inserts and also in many other press working industries.

The specimens were produced from a single block supplied by the company Böhler, specified as W300 ISOBLOC hardened at HRC 52, standard condition used in the die and mold industry. The standard shape designed for the samples was 20x20x1 mm. The chip width was, therefore, constant equals to 1 mm, what simplifies the evaluation of the process forces, whereas only the variation of the chip thickness can be taken into account. To achieve the targeted dimensions, the specimens were firstly machined by electrical discharge leaving 0.2 mm stock material, which was then ground with the purpose of reaching a high form tolerance. The process used in the manufacture of the test specimens is illustrated in Figure 4.4.

The developed experiment setup was first tested to establish the repeatability and reliability of the results, as well as the error margin in a chosen confidence interval. The method described by Montgomery was used [MONT01].
First, 30 repetitions were performed with a single parameter set, also known as a treatment in statistics. The objective was to analyze the dispersion of the measured forces and to define the margin error and number of repetitions for the tests. The results were then statistically evaluated.

### 4.1.4 Design of experiments and procedure

The number of experiments is then given by the following expression [MONT01]:

\[
 n_{\text{ex}} = \left( \frac{z^* \cdot \sigma}{\text{MRE}} \right)^2 \tag{4.1}
\]

Where \( n_{\text{ex}} \) is the sample size, \( z^* \) is confidence interval, \( \sigma \) is the standard deviation of the test and MRE is an arbitrary error margin. The results from the first tests are summarized in Table 4.1:

<table>
<thead>
<tr>
<th>( F_{\text{cn}} )</th>
<th>Confidence interval</th>
<th>( F_c )</th>
<th>Confidence interval</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \text{Error margin (N)} )</td>
<td>( 95% )</td>
<td>( 90% )</td>
<td>( 85% )</td>
</tr>
<tr>
<td>1</td>
<td>39</td>
<td>27</td>
<td>21</td>
</tr>
<tr>
<td>2</td>
<td>10</td>
<td>7</td>
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<td>5</td>
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<td>1</td>
</tr>
<tr>
<td>6</td>
<td>2</td>
<td>1</td>
<td>1</td>
</tr>
</tbody>
</table>

Table 4.1: Number of repetitions and confidence interval

As illustrated in the table above, by admitting an error margin of 2 N for the value of \( F_c \) and 5 N for the value of \( F_{\text{cn}} \), it is possible to evaluate the results with a confidence level of 95 % by using two repetitions for each parameter set. The experimental investigation was then designed to assess the influence of the cutting edge geometry and process parameters on the process forces. The rake angle, clearance angle, cutting edge radius, cutting speed and uncut chip thickness were varied in different intervals illustrated in Table 4.2.
Over 300 experiments were used in the final evaluation. For each repetition, a new cutting edge of the insert was used, which was characterized as already described in this chapter. The chip thickness was determined by adjusting the feed. To avoid any uncertainty and variation on the chip thickness, which could result from the kinematics of the process and geometry of the specimens, at least 20 cuts in each repetition were performed, assuring that, after a certain point, the cut was carried out with the targeted chip thickness. The process forces were measured with the calibrated dynamometer, and the generated chips were collected for later analysis.

4.2 Influence of the process parameters and tool geometry on the process forces

The influence of the cutting edge radius, rake, and clearance angles on the active forces was investigated at different cutting speeds. The main results are presented here.

4.2.1 Influence of the tool geometry on the cutting forces

Cutting edge radius

Figure 4.5 shows the active forces in orthogonal cutting for different cutting edge radii $r_\ell$. The forces are standardized to a cutting width of $b_{sp} = 1$ mm and are depicted for rake and clearance angles equal to zero and a cutting speed $v_c = 140$ m/min. As illustrated, an increase of the cutting edge radius led to higher values of both force components. The cutting normal force $F_{cn}$, nevertheless, is significantly more affected by the variation of the cutting edge radius than the cutting force component $F_c$. The same effect was observed for other tested angle combinations: $\gamma = -6^\circ, \alpha = 6^\circ; \gamma = 5^\circ, \alpha = 11^\circ; \gamma = 10^\circ, \alpha = 6^\circ; \gamma = 10^\circ, \alpha = 11^\circ; \gamma = 0^\circ, \alpha = 11^\circ$.

As described in Chapter 2, according to Altintas [ALT12], the cutting and cutting normal forces can be divided into two different components, namely edge and cutting forces, the edge forces are strongly influenced by the cutting edge radius, as the figure above suggests. This effect can be observed by the vertical shift in the curves.
Influence of the tool micro-geometry and the process parameters on the cutting forces

Figure 4.5: Cutting and cutting normal forces in orthogonal cutting tests for $\gamma = 0^\circ$ and $\alpha = 0^\circ$

Schnitt- und Schnittnormalkraft beim Orthogonalschnitt mit $\gamma = 0^\circ$ and $\alpha = 0^\circ$

It can also be observed that the inclination of the curves is not significantly affected by the variation of $r_p$. The curves are mainly shifted towards higher values. This shift is higher for the cutting normal forces than for the cutting forces. A gap between the curves for $r_p = 30 \mu m$ and $r_p = 60 \mu m$ suggests the existence of different phenomena occurring in the cutting process for these two values. This indicates that the increase of the process forces is therefore not linear with the value of the cutting edge radius. This non-linearity was also observed in other studies [WITT14, ARNT13, ALTM01, XU96, ALTI12, ALBR60].

The observed increase of the process forces is related to the influence of the cutting edge radius on the effective rake angle. As the cutting edge radius grows, the effective rake angle becomes negative, given that the uncut chip thickness does not surpass the value of $r_p$. In this conditions, higher deformation rates and temperatures are present in the chip formation zones, leading to work hardening effects [WYEN11].

It was observed that the type and form of the generated chips vary with the cutting edge geometry, cutting speed and chip thickness. This variation is mainly perceptive for small values of the uncut chip thickness. For uncut chip thickness equal to or higher than 20 $\mu m$, continuous curly chips were observed for all parameters tested, as shown in Figure 4.6. Surprisingly, for extreme low ratios $h_{sp}/r_p$, as for example $h_{sp} = 5 \mu m$ and $r_p = 60 \mu m$, the generated chips were completely flat. Also, the color changed from dark brown to blue, indicating higher temperatures during the chip formation. Measurements of the deformed chips revealed a lower ratio between the deformed and undeformed chip lengths. Further analysis is necessary to clarify this phenomenon.
The method described by Albrecht [ALBR60] was used to determine the ploughing forces. In this method, the force values are extrapolated to an uncut chip thickness $h_{sp} = 0$. The resulting force corresponds to the value of the ploughing forces, which is, for instance, the point in which the curves depicted in Figure 4.5 touch the abscissa. This method is also used by Altintas [ALTI12], who denominated this as components edge forces. The ploughing forces are represented in Figure 4.7. A linear fitting was applied. The resulting equations, as well as the R-square values, are also depicted in this figure. It is here evident that the cutting edge has a higher influence on the cutting normal forces than on the cutting forces. This indicates that the direction of the ploughing forces also changes with increasing cutting edge radius, as also observed by Wyen [WYEN11].

As here indicated, a perfectly sharp cutting edge also has a ploughing force component, which can be obtained from the curves intersect. This effect is related to elastic spring back of the material.

The average coefficient of friction can also be determined by the values and direction of the ploughing forces. For that, Albrecht [ALBR60] proposes to subtract the ploughing forces from the total forces. The average friction angle is then given by the following expression:

$$\tan \rho = \frac{\sin \gamma_n \cdot F_{ch,c} + \cos \gamma_n \cdot F_{ch,cn}}{\cos \gamma_n \cdot F_{ch,c} - \sin \gamma_n \cdot F_{ch,cn}}$$

Equation 4.2
Influence of the tool micro-geometry and the process parameters on the cutting forces

With:

\[ F_A = F_{pl} + F_{ch} = F_c + F_{cn} \]  
Equation 4.3

\[ F_c = F_{plc} + F_{chc} \]  
Equation 4.4

\[ F_{cn} = F_{plcn} + F_{chcn} \]  
Equation 4.5

Whereby the cutting normal forces are equivalent to the feed forces in orthogonal cutting.

Figure 4.7: Ploughing forces components

Figure 4.8 illustrates the behavior of the specific cutting forces for different values of \( r_\beta \). The specific forces can also be interpreted as the cutting parcel of the forces, which is related to the force coefficients \( k_{ic} \), as described in Chapter 2. As here depicted, the specific forces have a nonlinear decay with the chip thickness. The decay is more evident on the cutting normal forces, where a gap between the curves for \( r_\beta = 30 \) and \( r_\beta = 60 \) can be observed. The nonlinear decrease of the magnitude of the specific forces is related to the variation of the effective rake angle, which becomes more negative with the increase of the tool radius. The effective rake angle can be calculated by the following expression [KOCH96]:

\[ \gamma_{eff} = \arcsin \left( \frac{h_{sp}}{r_\beta} \right) \]  
Equation 4.6

The behavior of the curves also indicates a convergence for higher values of the uncut chip thickness. The scale effect is expected to have a lower impact on the forces after a certain threshold for the chip thickness is reached.
Rake and clearance angles

The influence of the rake and clearance angles on the process forces was also investigated. Figure 4.9 and Figure 4.10 show the measurement results for the active forces with varying clearance and rake angles, whereas all other parameters remain constant.

From the experimental results, it is possible to observe that an increase in both angles results in a reduction of the active forces. The decline of the forces with the clearance angle is attributed to the higher friction zone at the transition between the cutting edge and the flank. Spring back from the ploughed material may also contribute to the increase of the forces, especially to the cutting normal forces. A higher rake angle, on the other side, provides a better chip flow condition [KLOC08]. This influence is for the tested case, nevertheless, relatively small, since the effective rake angle in the chip formation zone is different from the orthogonal rake angle. In the tested parameters above, whereas the cutting edge radius is $r_e = 20\, \text{m}$, the chip thickness is consistently smaller than the cutting edge radius, resulting in negative effective rake angles. The decrease of the cutting normal forces with the rake angle, on the other side, contradicts other results found in the literature. Armarego [ARMA93] derived an equation for the cutting normal force in function of the rake angle and concluded that this force component should increase with increasing $\gamma$. He assumed, however, that the cutting edge was perfectly sharp. The observed decrease of the cutting normal forces may be linked to a better positioning of the cutting edge or even to asymmetries in its geometry.

Although the results indicate a variation of the process forces with the rake and clearance angles, no substantial change is observed on the slope of the curves. The intersection of the curves with the abscissa changes with different rake and clearance angles, meaning that these...
angles influence mostly the ploughing forces rather than the chip forming forces in the investigated parameter range.

This result expands the ploughing theory from Albrecht [ALBR60]. According to Albrecht, the rake and clearance angles do not impact the ploughing forces, once the cutting edge is fully in engaged. In the tested case, only a portion of the cutting edge is engaged. Therefore, it can be concluded that, for conditions in which the chip thickness is smaller than the cutting edge radius, the rake and clearance angle impact only the ploughing forces.
4.2.2 Influence of the uncut chip thickness and cutting speed on the cutting forces

Einfluss der Spanungsdicke und Schnittgeschwindigkeit auf die Zerspankraft

Cutting speed

The effective cutting speed of most tools is variable along the tool axis. This variation is mostly attributed to the change in the size of the local radius, although the compound kinematics and changing feed direction can have a small impact on the local cutting speed. To build a force model which can be applicable to a higher range of parameters and tool geometries, it is, therefore, necessary to determine the influence of the cutting speed on the process forces, so that the model can be adaptable to the changing conditions along the tool axis.

Figure 4.11 shows the experimental results of the specific active forces in orthogonal cutting with varying cutting speed. As it can be observed, the forces do not vary linearly with the cutting speed. This observation is in agreement with other results found in the literature [KLOC08]. By increasing the cutting speed, at lower values, the forces suffer a little decay, followed by an increase, which is attributed to blue hardening. After a certain point, the higher temperatures generated at higher cutting speeds induce thermal softening of the material, the reason why the specific cutting forces after a certain cutting speed decrease linearly.

It can also be observed that the region in which the decrease and subsequent increase occur varies with the value of the chip thickness. The curves depicted in Figure 4.11 for an uncut chip thickness \( h_{sp} = 5 \mu m \) and \( h_{sp} = 10 \mu m \) behave similarly according to the described pattern.

![Orthogonal cutting setup](image)

**Figure 4.11:** Influence of the cutting speed on the specific forces

Einfluss der Schnittgeschwindigkeit auf die spezifischen Zerspankräfte

**Orthogonal cutting setup**

<table>
<thead>
<tr>
<th>Tool:</th>
<th>Process:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rake angle ( \gamma = 0^\circ )</td>
<td>Uncut chip thickness ( h_{sp} = 5 - 20 \mu m )</td>
</tr>
<tr>
<td>Clearance angle ( \alpha = 0^\circ )</td>
<td>Chip width ( b_{sp} = 1 \text{ mm} )</td>
</tr>
<tr>
<td>Cutting edge radius ( r_b = 20 \mu m )</td>
<td>Cutting speed ( v_c = 140 \text{ m/min} )</td>
</tr>
</tbody>
</table>
Influence of the tool micro-geometry and the process parameters on the cutting forces

The curve for $h_\text{sp} = 20 \, \mu m$, on the other side, behaves linearly with the cutting speed. This indicates that the threshold for the linear behavior of the forces might be higher for smaller values of chip thickness. It was also noticed that the cutting speed has an impact on the chip form. Mostly segmented chips were observed at all tested cutting speeds, whereas the chip formed at $v_c = 70 \, m/min$ presented indications of the formation of chips with build-up edge.

The formation of build-up edge is associated with ductile materials, such as the hot work steel X38CrMoV5-1, used in the experiments presented in this thesis. It is the adhesion of layers of high compacted work material on the tool which undertake the function of the cutting edge [KLOC08]. As a result, the machined material suffers higher deformation. Successive layers of the work material are then added to the build-up edge. When the accumulated material increases more than a certain size, it gets unstable, part of it breaks and flows with the chip. A portion of the build-up edge may also slip under the flank and damage the workpiece, causing wear on the flank surface. Since the formation of the build-up edge is associated with higher deformations, more energy is needed in the cutting process, which therefore leads to higher forces.

Figure 4.12 shows the impact of the build-up edge on the generated chips at $v_c = 70 \, m/min$. From the metallographic cross-sections, it is possible to visualize the inhomogeneity of the deformation. More compacted layers reflect better light, the reason why the build-up edge can be identified by the light layers welded to the continuous chip below.

Current researchers also associate the amount of ploughing with lower cutting speeds, which becomes even more evident at cutting speeds lower than 35 m/min [XU96, ALTI2012]. This effect was also observed in this investigation, as illustrated in Figure 4.13.
The edge force components of the cutting and cutting normal forces were significantly higher at lower cutting speeds. This effect was observed throughout all cutting edge geometries used in the experiments. The slight increase of the ploughing cutting normal force for $v_c = 225$ m/min in comparison to $v_c = 140$ m/min suggests that the blue hardening effect is more predominant in the cutting normal direction than in the cutting direction, indicating an asymmetry on the ploughing forces behavior. An evaluation of the machined specimens also revealed that the surface roughness was higher at lower cutting speeds. The average roughness $R_a$ measured optically dropped from 0.2 μm, for $v_c = 70$ m/min, to 0.12 μm, for $v_c = 140$ m/min. At the same time, an increase in the microhardness was observed. The specimens machined at lower cutting speeds had a final hardness of 51 HRC in contrast with 53 HRC for the specimens machined at $v_c = 140$ m/min. All these factors present evidence of higher ploughing forces at lower cutting speeds.

**Uncut chip thickness**

The effect of the uncut chip thickness on the process forces was investigated in the range between 5 μm and 20 μm. The choice of the lowest value of $h_{sp}$ was limited by the experimental setup. The Hembrug Mikrotum 100 has a positioning accuracy of 1 μm, which, added to the setup uncertainties, could result in a higher deviation. A use of low values of $h_{sp}$ could lead to inconsistent and inconclusive results. A large number of repetitions with $h_{sp} = 5$ μm were performed. The results were thoroughly consistent, establishing the repeatability of this value of the uncut chip thickness.

The tested range represents the maximum chip thickness of most finishing operations, whereas the average chip thickness lies normally bellow 5 μm. Scale effects below $h_{sp} = 5$ μm were therefore not investigated.

As indicated in Figure 4.5, the cutting and cutting normal forces in the tested range vary linearly with the chip thickness for a constant cutting edge radius in the tested parameter range. These results are in agreement with the vast majority of the research available in the literature.
Influence of the tool micro-geometry and the process parameters on the cutting forces

Figure 4.14: Variation of the specific forces with the ratio \( h_{sp}/r \)

With a varying cutting edge radius, nevertheless, it was possible to identify scale effects at low chip thicknesses. The ratio \( h_{sp}/r \) is, therefore, an important quantity for determining the process forces. Figure 4.14 shows the variation of the specific forces with the ratio \( h_{sp}/r \). It is depicted an exponential fit for points collected with different cutting edge radii, including the fit equations and the square error.

An important finding can be extracted from this representation. The behavior of the process forces changes at lower ratios \( h_{sp}/r \), as also suggested by other studies [ALTM01, ARNT13, QUIT13, XU96]. Xu suggested the existence of a threshold for a cutting predominantly affected by ploughing, which corresponds to approximately 30% of the cutting edge radius \( r \) [Xu96]. An uncut chip thickness below this value would then lead to a nonlinear increase of the ploughing forces.

This relationship between the cutting edge radius and the chip thickness is therefore of utmost importance in the modeling of the process forces.

4.3 Model for calculation of the process forces

To summarize the influences of the investigated parameters on the process forces, a multiple linear regression (MLR) was carried out. Cross-influential parameters were included in the regression, based on the analysis of the influences described above. The analysis was conducted with the objective of identifying which parameters influence the intercept and which influenced the slope of a linear regression of the measured forces as a function of the uncut chip thickness. From Figure 4.5, for example, it is possible to deduce that the intercept of the cutting forces as a function of the chip thickness varies with the square of the cutting edge radius \( (F \sim \eta^2) \), for \( h_{sp} = 0 \). Similarly, Figure 4.14 suggests a variation of the slope of the linear
Influence of the tool micro-geometry and the process parameters on the cutting forces

regression inversely proportional to the cutting edge radius \((F \sim h_{sp}/r_{f})\). By performing this analysis on all other investigated parameters, the following expression for the determination of the cutting and cutting normal forces was derived:

\[
F_i = \left( a_{i0} + a_{ir} \cdot r_{f} + a_{ir^2} \cdot r_{f}^2 + a_{iv} \cdot v + a_{iv^2} \cdot v^2 + + a_{ia} \cdot a + a_{iah} \cdot h \cdot \frac{a_{ih}}{r_{f}} \cdot h_{sp} \right) \cdot b_{sp}
\]

(Equation 4.7)

With \(i = c, cn\)

An analysis of variance (ANOVA) was performed to identify the significance of each factor on the dependent variables, which are in this case the force components. In this analysis, the null hypothesis is that the regression model proposed does not explain the observations, whereas the alternative hypothesis is its opposite [MONT01]. The objective of the analysis is then to verify if the null hypothesis can be rejected based on a statistical evaluation of the collected data. The measured data was then fitted to the proposed model. The standard deviation and p-values were then calculated. The p-values are a measure of how good the data correlates the null hypothesis. Low p-values mean that the null hypothesis can be rejected while high p-values mean that the null hypothesis is likely true. The results for the first regression are described in Table 4.3.

<table>
<thead>
<tr>
<th></th>
<th>Cutting force (F_c)</th>
<th></th>
<th>Cutting normal force (F_{cn})</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>(R^2: 0.982)</td>
<td></td>
<td>(R^2: 0.853)</td>
</tr>
<tr>
<td></td>
<td>Std. deviation: 0.96 N</td>
<td></td>
<td>Std. deviation: 17.17 N</td>
</tr>
<tr>
<td>(a_0)</td>
<td>1.0502 x 10^2</td>
<td>(a_0)</td>
<td>9.3267 x 10^1</td>
</tr>
<tr>
<td></td>
<td>3.4905 x 10^3</td>
<td>(a_r)</td>
<td>-1.4094 x 10^1</td>
</tr>
<tr>
<td>(a_r)</td>
<td>1.3237 x 10^1</td>
<td>(a_r)</td>
<td>4.3645 x 10^1</td>
</tr>
<tr>
<td>(a_{r^2})</td>
<td>1.8789 x 10^3</td>
<td>(a_{r^2})</td>
<td>6.2568 x 10^3</td>
</tr>
<tr>
<td>(a_v)</td>
<td>3.3782 x 10^2</td>
<td>(a_v)</td>
<td>-4.3863 x 10^1</td>
</tr>
<tr>
<td>(a_{v^2})</td>
<td>8.5513 x 10^5</td>
<td>(a_{v^2})</td>
<td>8.3833 x 10^4</td>
</tr>
<tr>
<td>(a_h)</td>
<td>2.3966 x 10^6</td>
<td>(a_h)</td>
<td>2.0052 x 10^6</td>
</tr>
<tr>
<td>(a_{hv})</td>
<td>4.8594 x 10^2</td>
<td>(a_{hv})</td>
<td>7.2789 x 10^3</td>
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<tr>
<td>(a_{hr})</td>
<td>-2.5837 x 10^2</td>
<td>(a_{hr})</td>
<td>-7.3333 x 10^3</td>
</tr>
<tr>
<td>(a_r)</td>
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<td>(a_r)</td>
<td>-7.0623 x 10^1</td>
</tr>
<tr>
<td>(a_{uv})</td>
<td>-3.9566 x 10^1</td>
<td>(a_{uv})</td>
<td>-4.7849 x 10^7</td>
</tr>
</tbody>
</table>

Table 4.3: ANOVA for the first regression model of \(F_c\) and \(F_{cn}\)

ANOVA für das erste Regressionsmodell für \(F_c\) und \(F_{cn}\)

By adopting a significance level of \(\alpha = 5\%\), the p-values were used to discard the factors that are statistically not relevant to the regression, i.e. that confirm the null hypothesis. These
values are marked in the table above. All factors with a p-value higher than 5% indicate that the null hypothesis cannot be discarded.

A second regression was then realized, in which the factors that confirm the null hypothesis were removed. The newly calculated model was reevaluated, and the coefficients which had a p-value higher than 5% were once again excluded. This process was repeated twice for the cutting normal forces until only factors within the adopted confidence levels remained. Table 4.4 shows the final results of the regression models for the cutting and cutting normal forces.

Table 4.4: ANOVA for the final regression model of $F_c$ and $F_{cn}$

<table>
<thead>
<tr>
<th></th>
<th>Value</th>
<th>Standard deviation</th>
<th>P value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$F_c$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$a_0$</td>
<td>1.0502 x 10^2</td>
<td>3.4905 x 10^0</td>
<td>1.48 x 10^{-69}</td>
</tr>
<tr>
<td>$a_r$</td>
<td>-3.3171 x 10^{-1}</td>
<td>1.3237 x 10^{-1}</td>
<td>1.32 x 10^{-2}</td>
</tr>
<tr>
<td>$a_{r_2}$</td>
<td>1.3218 x 10^{-2}</td>
<td>1.8789 x 10^{-3}</td>
<td>4.81 x 10^{-11}</td>
</tr>
<tr>
<td>$a_V$</td>
<td>-6.4649 x 10^{-1}</td>
<td>3.3782 x 10^{-2}</td>
<td>4.65 x 10^{-44}</td>
</tr>
<tr>
<td>$a_{V_2}$</td>
<td>1.2498 x 10^{-2}</td>
<td>8.5513 x 10^{-5}</td>
<td>9.60 x 10^{-32}</td>
</tr>
<tr>
<td>$a_h$</td>
<td>2.3966 x 10^0</td>
<td>1.6181 x 10^{-1}</td>
<td>2.72 x 10^{-32}</td>
</tr>
<tr>
<td>$a_{hr}$</td>
<td>4.8594 x 10^{-2}</td>
<td>8.8239 x 10^{-4}</td>
<td>1.35 x 10^{-7}</td>
</tr>
<tr>
<td>$a_{hr_2}$</td>
<td>-2.5837 x 10^0</td>
<td>8.7869 x 10^{-1}</td>
<td>3.74 x 10^{-3}</td>
</tr>
<tr>
<td>$a_r$</td>
<td>-2.1326 x 10^0</td>
<td>1.1183 x 10^{-1}</td>
<td>6.98 x 10^{-44}</td>
</tr>
<tr>
<td>$a_{r_3}$</td>
<td>-3.9566 x 10^{-1}</td>
<td>7.8576 x 10^{-2}</td>
<td>1.22 x 10^{-6}</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th></th>
<th>Value</th>
<th>Standard deviation</th>
<th>P value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$F_{cn}$</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>$a_0$</td>
<td>9.0167 x 10^1</td>
<td>1.0607 x 10^1</td>
<td>6.85 x 10^{-15}</td>
</tr>
<tr>
<td>$a_r$</td>
<td>3.4063 x 10^{-2}</td>
<td>1.6414 x 10^{-3}</td>
<td>9.70 x 10^{-50}</td>
</tr>
<tr>
<td>$a_V$</td>
<td>-4.0827 x 10^{-1}</td>
<td>1.0410 x 10^{-1}</td>
<td>1.24 x 10^{-4}</td>
</tr>
<tr>
<td>$a_{V_2}$</td>
<td>7.5031 x 10^{-4}</td>
<td>2.5167 x 10^{-4}</td>
<td>3.26 x 10^{-3}</td>
</tr>
<tr>
<td>$a_h$</td>
<td>1.9695 x 10^0</td>
<td>5.3717 x 10^{-1}</td>
<td>3.23 x 10^{-4}</td>
</tr>
<tr>
<td>$a_{hr}$</td>
<td>7.1542 x 10^{-3}</td>
<td>3.0287 x 10^{-3}</td>
<td>1.92 x 10^{-2}</td>
</tr>
<tr>
<td>$a_{hr_2}$</td>
<td>-6.5359 x 10^{-2}</td>
<td>1.7982 x 10^{-2}</td>
<td>3.62 x 10^{-4}</td>
</tr>
</tbody>
</table>

As it can be observed, the coefficient of determination, R-square, is higher for the cutting force, indicating that the prediction is more accurate for this force component than for the cutting normal force. The relatively higher standard deviation for $F_{cn}$ is attributed to the experiments conducted with $r_p = 60$ μm, which suggests the occurrence of different phenomena in the chip formation zone. An analysis of the individual deviations shows an average percentage deviation of 4.86 % for $F_c$ and 14.83 % for $F_{cn}$. This value is considered safe, given the complexity of the observed phenomena in the range of parameters investigated, especially the high variation of the cutting edge geometry examined and the scale effects related to low ratios $h_{sp}/r_p$.

The observed influences detailed in this chapter can also be observed by analyzing the regression coefficients in the force equation. In Figure 4.5, for example, it was noted that only the intercept of the curves for the cutting normal force is influenced by the cutting edge radius, i.e. that the curves are shifted vertically with a variation of $r_p$, and that this influence is not linear. A measure of this variation is expressed in the coefficient $a_{r_2}$ resulting from the regression for
Influence of the tool micro-geometry and the process parameters on the cutting forces

\( F_{cn} \), which is associated with the square of the radius, expressing, therefore, this nonlinear relationship. The same is valid for all other influences observed.

Therefore, the derived expression for the active forces reflects the influence of the cutting edge geometry and process parameters on the cutting forces for orthogonal cutting of the hot working steel X38CrMoV5-1 within the range of parameters tested.

**Extension to oblique cutting**

The force model experimentally derived in this chapter describes the active forces within the investigated range of parameters with a high confidence level for the orthogonal cutting. To predict all three components of the cutting forces in oblique cutting, nevertheless, it is necessary to make an analytical abstraction.

The correspondence between orthogonal cutting and oblique cutting is described by Budak and Altintas [BUDA96], in which the authors used the oblique cutting equations from Armarego [ARMA69], Albrecht [ALBR60] and Merchant [MERC44] to derive cutting force coefficients from orthogonal cutting data (see section 2.3.3.2):

\[
k_{c,c} = \frac{\tau}{\sin \phi_n} \left( \cos (\rho - \gamma_c) \tan \lambda + \tan \eta_c \sin \rho \tan \lambda \right) \quad \text{Equation 2.29}
\]

\[
k_{cn,c} = \frac{\tau}{\sin \phi_n \cos \lambda} \sin (\rho - \gamma_c) \quad \text{Equation 2.30}
\]

\[
k_{p,c} = \frac{\tau}{\sin \phi_n} \left( \cos (\rho - \gamma_c) \tan \lambda - \tan \eta_c \sin \rho \tan \lambda \right) \quad \text{Equation 2.31}
\]

In which:

\[
c = \sqrt{\cos^2 (\phi_n + \beta - \gamma_n) + \tan^2 \eta_c \sin^2 \beta_n}
\]

The cutting coefficient for the passive force \( k_{p,c} \) can be obtained independently from the friction angle and shear stress, by dividing the Equation 2.31 by the Equation 2.30:

\[
k_{p,c} = k_{cn,c} \left( \frac{\cot (\rho - \gamma_n) \tan \lambda - \tan \eta_c \sin \rho_n}{\sin (\rho - \gamma_n)} \right) \cos \lambda \quad \text{Equation 4.8}
\]

The chip flow angle \( \eta_c \) can be assumed as equal to the local oblique angle \( \lambda \) by adopting Stabler’s chip flow rule [STAB64] an assumption also made by many other authors [LEE95, SEET97, ARMA69, BUDA96, YOUN87, OXLE89]. Thus, the following expression results for the calculation of the cutting coefficient of the passive force:

\[
k_{p,c} = k_{cn,c} \left( \frac{\cot (\rho - \gamma_n) - \sin \rho_n}{\sin (\rho - \gamma_n)} \right) \sin \lambda \quad \text{Equation 4.9}
\]
Influence of the tool micro-geometry and the process parameters on the cutting forces

In which the friction angle is calculated according to the Albrecht’s approach [ALBR60], which is an extension of the Merchant’s shear plane model with the purpose of considering the ploughing and chip generating forces in the calculation:

\[
\tan \rho = \frac{\sin \gamma_n \cdot F_{ch,c} + \cos \gamma_n \cdot F_{ch,nc}}{\cos \gamma_n \cdot F_{ch,c} - \sin \gamma_n \cdot F_{ch,nc}} \\
\text{Equation 4.2}
\]

\[
\tan \rho_n = \tan \rho \cdot \cos \eta_c = \tan \rho \cdot \cos \lambda \\
\text{Equation 4.10}
\]

Moreover, the normal rake angle is calculated according to the Armarego’s model for oblique cutting:

\[
\tan \gamma_n = \tan \gamma_r \cdot \cos \lambda \\
\text{Equation 4.11}
\]

The edge coefficient of the passive force can be estimated as described by Armarego and Deshpande [ARMA93], whereas other authors assume this coefficient being equal to zero [BUDA96].

\[
k_{p,e} = k_{c,c} \cdot \sin \lambda \\
\text{Equation 4.12}
\]

For the active forces, the edge coefficient corresponds to the extrapolation of the force curves to \( h_{sp} = 0 \). By using the regression model derived in this investigation, the edge coefficients can be described by the following expression:

\[
k_{i,e} = a_{i0} + a_{i1} \cdot r_{\beta} + a_{i2} \cdot r_{\beta}^2 + a_{i3} \cdot v_c + a_{i4} \cdot v_c^2 + a_{i5} \cdot \alpha + a_{i6} \cdot \gamma \\
\text{Equation 4.13}
\]

With \( i = c, \, cn \)

Finally, the correspondent forces in oblique cutting can be derived from the orthogonal cutting model proposed as follows:

\[
F_c = \left( a_{c0} + a_{c1} \cdot r_{\beta} + a_{c2} \cdot r_{\beta}^2 + a_{cv} \cdot v_c + a_{cv2} \cdot v_c^2 + a_{cu} \cdot \alpha + a_{cyr} \cdot \gamma_n + \left( a_{ch} + a_{cvh} \cdot v_c + a_{crr} \right) \cdot h_{sp} \right) \cdot b_{sp,eff} \cdot \cos \lambda
\]

\[
F_{cn} = \left( a_{cn0} + a_{cn1} \cdot r_{\beta} + a_{cn2} \cdot r_{\beta}^2 + a_{cavn} \cdot v_c + a_{cavn2} \cdot v_c^2 + a_{cavn} \cdot \alpha + a_{cnyr} \cdot \gamma_n + \left( a_{ch} + a_{cavnvh} \cdot v_c + a_{cavnrr} \right) \cdot h_{sp} \right) \cdot b_{sp,eff}
\]

\[
F_p = \left( a_{ch} + a_{cvh} \cdot v_c + \frac{a_{crr}}{r_{\beta}} \right) \sin \lambda \cdot \cos \lambda + \left( a_{cnh} + a_{cavnvh} \cdot v_c + \frac{a_{cavnrr}}{r_{\beta}} \right) \left( \cot \left( \rho_n - \gamma_n \right) - \sin \rho_n \right) \sin \lambda \cdot h_{sp} \right) \cdot b_{sp,eff}
\]

(Equation 4.14)

Whereas the local helix angle \( \lambda \) and the effective cutting width \( b_{sp,eff} \) for an arbitrary tool geometry is described in Chapter 5.
4.4 Conclusions

Fazit

The relationship between the cutting forces and the cutting edge geometry, the uncut chip thickness and the cutting speed was the subject of investigation in this chapter. The objective was to derive a mathematical expression that describes the process forces for the machining of the hot-work steel X38CrMoV5-1 within the range of the parameters tested.

Results from orthogonal cutting tests suggest a strong impact of the cutting edge radius on the active forces. The cutting normal force, which in orthogonal cutting correspond to the feed force, is more influenced by the value of $r_b$ than the cutting component. The ploughing forces, determined by the approach of Albrecht [ALBR60], can significantly contribute to the process forces, constituting a considerable portion of the total forces.

A nonlinear behavior of the process forces in relation to the cutting edge and the cutting speed was observed. A ploughing predominant process occurred at low values of the ratio $h_{sp}/r_b$ and the formation of build-up edge was noted at $v_c = 70$ m/min and a cutting thickness of $h_{sp} = 5 - 10$ $\mu$m. This effect is attributed to the high ductility of the material studied and to the high negative rake angle resulted from the low ratio $h_{ap}/r_b$.

An empirical equation for the active forces was formulated based on a statistical regression of the experimental data. This equation allows the prediction of the process forces as a function of the cutting edge geometry, cutting speed and chip thickness and width, thus answering the second research question. As usual in empirical models, the expression is valid only within the range of parameters tested. Nevertheless, the methodology presented can be applied to other combinations of materials, process parameters and tool geometries.

The empirical model for orthogonal cutting was extended to the oblique cutting by using well-known relationships available in the literature. The active forces were transformed only by considering the inclination angle $\lambda$, while the passive forces required a higher analytical abstraction level to be determined. The developed expression will be later integrated into a simulation system for calculating the geometrical conditions and process forces in peripheral milling operations, which is described in detail in Chapter 5 and Chapter 6 of this work.
5 Geometrical conditions in milling
Geometrische Bedingungen beim Fräsen

Many key figures for characterizing the milling process are derived from the geometry of the uncut chip. This geometry for processes with a simple kinematics and simple tool geometry is relatively well described in the literature. Nevertheless, the determination of the uncut chip geometry for complex process kinematics and milling tool geometries remains a challenge.

The main challenge resides in determining the geometrical conditions for the milling of a free-form surface with a large variety of milling tools and process strategies. This is the case of the milling of turbine blades or die and molds, for example. Most of the models found in the literature assume a flat surface of the machining part, due to the complexity and difficulties in the determination of the residual material. This approximation is not suitable in most practical situations and can lead to an inappropriate process design.

In this chapter, a universal model is presented for calculation of the uncut chip geometry. This approach enables the calculation of the uncut chip geometry for any tool geometry and process parameters, assuming that the macroscopic contact conditions are known. Several parameters were derived, and the influence of the process parameters on the geometrical conditions was investigated. A force model was then coupled to the geometrical model and adapted. In this way the second and third research questions were approached:

- How can the undeformed chip geometry be determined for any multi-axis milling operation, given the macroscopic contact conditions and the tool geometry?
- How can the geometrical, material and process related parameters be used for the prediction of the process forces in multi-axis milling?

5.1 Universal model for calculation of the uncut chip geometry
Universelles Modell zur Berechnung der Spanungsgeometrie

5.1.1 Model concept
Modellvorstellung

A universal model for calculation of the uncut chip geometry and further geometrical parameters was developed in this work. This model was designed to be adaptable to all kind of milling tools and engagement situations. Its basic premise was the previous knowledge about the macroscopic contact conditions, i.e. the exit and entrance angles for each tool disk along the tool path. This is supported by current research, which shows evidence of geometrical models, most based on CSG, or dixel and voxel discretization of the workpiece that can be used for calculating the macroscopic chip geometry [ZABE10, MINO15, SURM06, STAU05]. Several advantages are associated with this approach. First, fewer assumptions need to be made about the previous workpiece geometry, increasing model flexibility and number of possible applications. Second, it allows the implementation into a CAM-based environment. Finally, by using such multi-level simulation approach, the overall performance of the resulting simulation system is optimized. Figure 5.1 illustrates this concept.
The simulation is modular build according to this idea. The milling strategy is first evaluated based on the macroscopic contact conditions, performed by the macro module. Weak spots are identified based on the contact angle. Regions with higher or rapidly changing contact angles generally incur in higher forces and worst process output. A detailed evaluation is then performed by using the module for calculation of the microscopic contact conditions. This allows a detailed investigation of the uncut chip geometry and other geometrical parameters for selected regions of the tool path. At last, a force model is coupled with the other models, which enables the prediction of the process forces. A holistic process evaluation and parameter-based process design are then possible.

5.1.1.1 Simulation flow

The simulation procedure is depicted in Figure 5.2, in which the force and micro modules correspond to the scope of this work. A simulation step corresponds to a position along the tool path. The macro simulation data required for the calculation of the uncut chip geometry is given for each point of the tool path and position along the tool axis. These are the entrance and exit angles $\varphi_w$ and $\varphi_a$. Additional information about the tool geometry is also supplied, which consists of the local slice radius, position, and axial vector. Based on this data, a geometrical model which calculates the uncut chip geometry and several other parameters that can be used in the process planning was developed. A force model was integrated into the simulation, allowing the prediction of the process forces based on the uncut chip geometry and force coefficient data.

The implementation of the presented models into a software system, along with the input and output of each simulation module, is described in detail in Chapter 7.
5.1.2 Milling tool geometry

The milling tool was discretized into thin layers, hereby denominated as tool “slices”. Each slice was considered as a curved surface with distributed oblique cutting edges along its perimeter. Each slice has, in turn, a determined radius and helix angle, as illustrated in Figure 5.3. The cutting edge microscopic geometry can be additionally specified, consisting of the cutting edge rounding radius, rake, and clearance angles.

This approach is particularly useful for modeling the process by using numerical methods. The higher the tool discretization, the more accurate is the simulation, and the longer is the computation time. Since the computation time of macro simulation methods increases significantly with the number of slices, the tool is usually only roughly discretized. This compromises the accuracy of the uncut chip geometry. For achieving more accurate results, the input data is therefore interpolated. A spline interpolation is used for generating the input to the simulation of the uncut geometry. From the macro simulation data, the tool radius, height, local helix angle and curvature angle are interpolated, as well as the correspondent macroscopic contact conditions entrance $\varphi_e$ and exit angles $\varphi_a$.

Excellent results for the simulation of finishing operations were achieved with an axial discretization step of 0.02 mm and an angular step of 0.05 °. In these conditions just a small fraction of the tool is in contact with the workpiece, resulting in a small chip geometry.
Curvature angle $\kappa$

The curvature angle $\kappa$ is calculated as depicted in Figure 5.3:

$$
\Delta r_i = r_{i+1} - r_i \\
\Delta h_i = h_{i+1} - h_i
$$

Equation 5.1

Where $r_i$ and $h_i$ are respectively the radius and height of the tool slice $i$.

Which results in:

$$
\kappa_i = \arctan \left( \frac{\Delta h_i}{\Delta r_i} \right), \text{ with } k_i = 90^\circ \text{ for } \Delta r_i = 0
$$

Equation 5.2

Most milling tools present only positive curvature angles, but negative angles of $\kappa$ are also considered in this definition. For ball-end mills, the curvature angle is also defined as immersion angle [KLOC11], since the position of the cutting edge in the hemispherical part of the cutter can be determined by the value of $\kappa$. In this case, the curvature angle can be calculated by the following expression:

$$
\kappa_j = \arcsin \left( \frac{r_j}{R_0} \right)
$$

Equation 5.3
The calculation of the curvature angle of the milling tool is of utmost importance for the calculation of the uncut chip geometry because the undeformed chip thickness is defined in the direction of the curvature radius. For more accurate results, the values were calculated by spline interpolation.

**Helix angle**

The helix angle is a feature of the milling tool defined by the kinematics of the grinding process used in the tool manufacture. Most tools have helical flutes, since the helix provides a better chip flow on the rake face. Straight flutes lead to a higher impact on the cutting edge, since the whole tooth impacts the material at the same time, leading to higher forces and consequently to higher tool deflection and vibrations.

The helix angle is defined according to the Figure 5.3. It can be constant or variable. Most models for predicting of the process forces for oblique cutting require the helix angle in the calculation, the reason why a precise definition of the local helix angle is important [ARMA69, BUDA96, ALTI12]. Figure 5.4 illustrates this variation [KLOC11].

![Figure 5.4: Variation of the helix angle for ball-end mills](Variation des Drallwinkels für Kugelkopffräser)

For end-mills, the helix angle is generally constant. For ball-end mills, nevertheless, it is always variable, due to the grinding process used in the manufacture of the tools. Lazoglu [LAZO00] derived an expression for calculating the local helix angle for ball-end mills in function of the tool and the curvature radius of the cutting edge. It is nevertheless necessary to experimentally determine some specific geometries of the cutter, in order to calculate the cutting edge curvature.

**5.1.3 Definition of the tool coordinate system**

Due to the complex kinematics of multi-axis milling operations, the formulation of a model based on the workpiece surface coordinate system (FCN) can be difficult. The tool coordinate system (TCS) allows, therefore, a simpler description of the cutting edge kinematics. In this system, the z-axis is collinear with the tool axis, and thus the xy-plane is parallel to the slices
surface. In this way, the calculation of the position of the cutting edge as well as of the feed components in each axis was possible.

Figure 5.5 shows one slice with the coordinate system FCN and TCS, which is moving with a speed $v_f$ in the $\vec{f}$ direction. The F-axis is parallel to the feed direction and points toward the feed vector $\vec{f}$; N is the surface normal axis and C is the cross-feed axis, normal both to both $\vec{f}$ and $\vec{N}$.

The tool axial vector $\vec{z}$ was assumed as input from the macro simulation system. It can also be calculated by using the rotation matrix described in Equation 2.3, given that the respective values of the supination and azimuth angles, $\theta_N$ and $\psi_F$ respectively, are known. The transformation matrix from FCN to TCS can be calculated as follows.

$$
\begin{align*}
\vec{e}_F &= (1,0,0)_{FCN} , \vec{e}_C = (0,1,0)_{FCN} , \vec{e}_N = (0,0,1)_{FCN} \\
\vec{e}_F &= (1,0,0)_{FCN} , \vec{e}_C = (0,1,0)_{FCN} , \vec{e}_N = (0,0,1)_{FCN} \\
\text{Equation 5.4}
\end{align*}
$$

Are the unit vectors of the FCN.

The tool axial vector written in the WCS is, therefore:

$$
\vec{z} = z_F \vec{e}_F + z_C \vec{e}_C + z_N \vec{e}_N \\
\text{Equation 5.5}
$$

$\vec{x}$ is orthogonal to $\vec{z}$ and is in the NF plane, thus:

$$
\begin{align*}
\langle \vec{x}, \vec{z} \rangle &= 0 \\
x_C &= 0 \\
\left\| \vec{x} \right\| &= 1 \\
x &= \begin{pmatrix} z_N \\ \sqrt{z_F^2 + z_N^2} \\ 0 \\ -z_F \\ \sqrt{z_F^2 + z_N^2} \end{pmatrix}_{FCN} \\
\text{Equation 5.6}
\end{align*}
$$

Whereby the vector in the FCN can be written as:

$$
\vec{x} = x_F \vec{e}_F + x_C \vec{e}_C + x_N \vec{e}_N
$$

Resulting in the following transformation matrix:
The TCS and WCS systems are thus related through the following expression:

\[
\begin{pmatrix}
  x \\
  y \\
  z
\end{pmatrix} = M_{FCN,TCS} \begin{pmatrix}
  F \\
  C \\
  N
\end{pmatrix}
\]

Equation 5.8

5.1.4 Calculation of the chip geometry

Berechnung der Spanungsgeometrie

The uncut chip geometry is depicted in Figure 5.6. The example shown here uses a ball end-mill, but can be generalized for any kind of milling tool geometry. The chip is discretized in the direction of the tool axis and rotational angle.

Each small volume element is described with three main dimensions: the uncut chip thickness \( h_{sp} \), calculated in the direction of the curvature \( \eta_c \); the uncut chip width, which is the projection of each element on the cutting edge, measured in the axial direction; and finally the chip length, which is the element’s projection on the cutting edge measured in the direction of the rotation angle \( \varphi \). It was assumed that the chip geometry does not change during one tool revolution so that the chip geometry of all flutes is the same. Thus, the calculations are performed for just one flute and shifted along the rotation angle for obtaining the values for the other flutes.

The angular position of the cutting edge of the slice \( i \) for each flute \( j \) can be described by the following expression:
\[
\tau_y(\varphi) = \varphi + \Delta \Gamma \cdot (j - 1) - \sum_{i=1}^{j} \frac{\tan(\lambda_i) \cdot (h_{i+1} - h_i)}{r_i}
\]

Equation 5.9

with \( j \in \mathbb{N} \) and \( 1 \leq j \leq n_z \), \( n_z \) being the number of flutes

Where \( \Delta \Gamma \) is the angle between two consecutive cutters:

\[
\Delta \Gamma = \frac{2\pi}{n_z}
\]

Equation 5.10

Thus, three iterations are necessary for determining the whole contact region, in which the uncut chip geometry is calculated:

\( 0 \leq \varphi \leq 2\pi \), equivalent to one tool revolution

\( i \in \mathbb{N} \) and \( 1 \leq i \leq n_i \), \( i \) is the slice index and \( n_i \) is the number of slices

\( j \in \mathbb{N} \) and \( 1 \leq j \leq n_z \), \( j \) is the tooth index and \( n_z \) is the number of flutes

Given the macroscopic contact conditions entrance angles \( \varphi_{e,i} \) and exit angles \( \varphi_{a,i} \), the chip dimensions is calculated if the following condition is satisfied:

\[
\varphi_{e,i} \leq \tau_{ij} \leq \varphi_{a,i}
\]

Equation 5.12

5.1.4.1 Calculation of the uncut chip thickness

Berechnung der Spanungsdicke

The first step for calculating the uncut chip thickness is to calculate the feed components in the TCS. As a result of the free movement of the tool in space, the feed vector \( \vec{f} \) can be decomposed in three axial displacements, namely \( f_x, f_y \) and \( f_z \). Since the feed has the same directions as the F-axis according to the FCN definition, using the rotation matrix \( M_{FCN,TCS} \), the values of \( \vec{f} \) in the TCS can be calculated as follows:

\[
\vec{f} = M_{FCN,TCS} \cdot \vec{f}_{FCN} \Rightarrow \vec{f} = f_z \begin{pmatrix}
\frac{z_N}{\sqrt{z_F^2 + z_N^2}} \\
-\frac{z_F z_C}{\sqrt{z_F^2 + z_N^2}} \\
\frac{z_F}{z_N}
\end{pmatrix}_{TCS}
\]

Equation 5.13

\[
\vec{f} = f_z \begin{pmatrix}
\cos \xi_x \\
\cos \xi_y \\
\cos \xi_z
\end{pmatrix}_{TCS}, \quad \text{where} \quad \begin{cases}
\cos \xi_x = \frac{z_N}{\sqrt{z_F^2 + z_N^2}} \\
\cos \xi_y = -\frac{z_F z_C}{\sqrt{z_F^2 + z_N^2}} \\
\cos \xi_z = \frac{z_F}{z_N}
\end{cases}
\]

Equation 5.14
Where $\xi_x$, $\xi_y$ and $\xi_z$ are the angles between the feed direction and the $x$-, $y$- and $z$-axis, respectively. The $f_x$ and $f_y$ feed components cause the tool disk to move along the $x$ and $y$ axes, respectively. However, the displacement in the tool axial axis $f_z$ means that the radius from the slice of the previous cut at one particular position might be different than from the current slice in the calculation iteration as illustrated in Figure 5.7. This occurs when $\xi_z \neq \pi/2$, i.e., when $\psi_f \neq \pm \pi/2$ and $\theta_N \neq 0$.

The movement of two consecutive circular cuts along the $xy$ plane is illustrated in Figure 5.8. Hereby the radial chip thickness $h_{sp,r}$ is depicted. The actual chip thickness cannot be represented in a plane, since it is defined in the normal direction of the milling tool surface, i.e., it is related to the curvature angle of the cutter. This illustration is the base for deriving an equation for the uncut chip thickness.

From this representation, the feed projection in the slice plane $f_{xz}$ and the angle $\delta_l$ were calculated:
Geometrical conditions in milling

\[ f_{xy} = \sqrt{f_x^2 + f_y^2} \quad \text{Equation 5.15} \]

\[ \delta = \arctan \left( \frac{f_y}{f_x} \right) \quad \text{Equation 5.16} \]

Due to the tool axis angular motion in milling, caused by the changing of the tool orientation, the feed value varies along the tool axis. The feed values \( f_x \) and \( f_y \) can be thus calculated as follows:

\[
\begin{align*}
  f_x &= f_{x,0} + h_i \cdot \Delta \xi_x \\
  f_y &= f_{y,0} + h_i \cdot \Delta \xi_x 
\end{align*} \quad \text{Equation 5.17}
\]

By applying the law of cosines in the triangle \( \triangle A\hat{O}B \) (Figure 5.8):

\[
r^2 = f_{xy}^2 + (r_i - h_{ap,r}(\phi))^2 - 2f_{xy}(r_i - h_{ap,r}(\phi))\cos \left( \frac{\pi}{2} + \phi + \delta \right) 
\quad \text{Equation 5.18}
\]

Which results in:

\[
h_{ap,r}(\phi) = r_i + f_{xy} \sin(\phi + \delta) - \sqrt{r_i^2 - f_{xy}^2 \cos^2(\phi + \delta)} 
\quad \text{Equation 5.19}
\]

Which finally leads to an expression for the calculation of the uncut chip thickness \( h_{ap} \):

\[
h_{ap}(\phi) = \left[ r_i + f_{xy} \sin(\phi + \delta) - \sqrt{r_i^2 - f_{xy}^2 \cos^2(\phi + \delta)} \right] \sin(\kappa) 
\quad \text{Equation 5.20}
\]

Where \( r_i \) is the radius of the slice which satisfies the condition \( h_k = h_i - f_{iz} \). It is important to stress that all three feed components are present in this expression. The \( f_x \) and \( f_y \) components are within the term \( f_{xy} \) while the \( f_{iz} \) component is considered in the changing radius. The curvature angle is also considered in this expression, since the uncut chip thickness is calculated projected in the direction of the curvature radius of the tool.

**Variation angle**

Due to the workpiece previous geometry, the equation for the uncut chip thickness formulated in Equation 5.20 is not applicable in the whole interval \( \phi \in [\phi_e, \phi_a] \). As illustrated in Figure 5.9, depending on the values of \( \phi_e \) and \( \phi_a \), the chip thickness has an approximately linear growth in the entrance and a nearly linear decrease in the exit region. The angles at which the linear increase and decrease occur are denominated entrance and exit variation angles, respectively, indicating that the expression for calculation of the uncut chip thickness changes in intervals defined by these angles. An accurate estimation of these angles is imperative, especially for small chip geometries, which are often present in multi-axis milling operations. Without the consideration of these angles, the uncut chip thickness is always overestimated by a factor of up to two in comparison with a verification performed using analytical models. This would lead to an inaccurate estimation of the process forces.
To estimate the values of the variation angles $\phi_{v,e}$ and $\phi_{v,a}$, an assumption about the previous workpiece geometry in these tiny regions had to be made. A reasonable assumption is to consider the previous workpiece geometry in the intervals $\phi_e < \phi < \phi_{v,e}$ and $\phi_{v,a} < \phi < \phi_a$ parallel to the direction of the feed component $f_{xy}$.

This assumption comes from the premise that the workpiece geometry does not vary abruptly between two consecutive cuts. The values of $\phi_{v,e}$ and $\phi_{v,a}$ can be thus estimated by using the entrance and exit angles of the previous cut. The feed components in each direction were used to determine the position of $\phi_e$ and $\phi_a$ in the previous cut in relation to the current $\phi_e$ and $\phi_a$ values. Since only the height of the current slice is needed in this calculation, the values of $\phi_{v,e}$ and $\phi_{v,a}$ lie after the displacement between two slices, requiring an interpolation for a correct evaluation. In the proposed model, the values of $\phi_{v,e}$ and $\phi_{v,a}$ were obtained by cubic hermitian interpolation, avoiding overshoots and oscillations in the values of the resulting chip thickness.

Under consideration of the variation angles, the chip thickness was calculated as follows:

$$
\begin{align*}
  h_{sp}(\varphi) &= \left( r_i + f_{xy} \sin(\varphi + \delta) - \sqrt{r_i^2 - f_{xy}^2 \cos^2(\varphi + \delta)} \right) \sin(\kappa), & \text{if } \varphi_e < \varphi < \varphi_{v,e} \\
  h_{sp}(\varphi) &= \frac{\varphi - \varphi_e}{\varphi_{v,e} - \varphi_e} h_{sp}(\varphi_{v,e}) & \text{if } \varphi_e < \varphi < \varphi_{v,a} \\
  h_{sp}(\varphi) &= \frac{\varphi - \varphi_a}{\varphi_{v,a} - \varphi_a} h_{sp}(\varphi_{v,a}) & \text{if } \varphi_{v,a} < \varphi < \varphi_a
\end{align*}
$$

Equation 5.21
5.1.4.2 Calculation of the uncut chip width, length, and cross-sectional area

*Geometrical conditions in milling*

**Chip width**

According to the definition in Figure 5.6, the uncut chip width is the arc length of the flute in contact with the chip for a given rotation angle $\varphi$. The width of each cutting edge element is calculated. If the angular position of the flute is outside the range of chip formation for a given value of $\varphi$, i.e., $\tau_{ij} > \varphi_a$ or $\tau_{ij} < \varphi_e$, the value of the chip width is null. The expression for each flute is therefore given as follows:

\[
\begin{align*}
    b_{sp,ij}^w &= \frac{\Delta s_i}{\cos(\lambda_i)}, & \text{if } & \tau_{ij}(\varphi) \in [\varphi_a, \varphi_e] \\
    b_{sp,ij}^w &= 0, & \text{if } & \tau_{ij}(\varphi) \notin [\varphi_a, \varphi_e]
\end{align*}
\]

Equation 5.22

Where $\Delta s_i$ is the interpolated arc length of the tool curvature:

\[
\Delta s = \sqrt{\Delta r_i^2 + \Delta h_i^2}
\]

Equation 5.23

The total chip width in each flute is then the sum of the width of all elements:

\[
b_{sp,ij}^w = \sum_{i=0}^{n_i} b_{sp,ij}^w
\]

Equation 5.24

Where $n_i$ is the total number of slices in contact with the workpiece at a given rotation angle $\varphi$.

**Chip length**

The uncut chip length is calculated similarly to the chip width. In contrast with the chip width, however, the length is calculated along the arc described by each cutting edge element during the tool rotation, representing the friction length of each element during the cutting process. In this way, the incremental chip length of each element of the cutting edge at a given rotation angle $\varphi$ is provided by:

\[
\begin{align*}
    l_{sp,ij}^l &= r_i \cdot \Delta \varphi, & \text{if } & \tau_{ij}(\varphi) \in [\varphi_a, \varphi_e] \\
    l_{sp,ij}^l &= 0, & \text{if } & \tau_{ij}(\varphi) \notin [\varphi_a, \varphi_e]
\end{align*}
\]

Equation 5.25

Which results in the following expression for the chip length of each tool slice:

\[
l_{sp,ij}^l = \sum_{\varphi=\varphi_a}^{\varphi_e} l_{sp,ij}^l
\]

Equation 5.26

with: $\varphi = \varphi_{ei} + n_i \cdot \Delta \varphi$, $n_i \in \mathbb{N}$ and $1 \leq n_i \leq n_{as,i}$

whereby $n_{as,i}$ is the number of angular steps of the slice $i$: $n_{as,i} = \frac{\varphi_{ei} - \varphi_{ei}}{\Delta \varphi}$
**Chip cross-sectional area**

The chip cross-sectional area for each element is given by the product of the chip thickness and width:

\[ A_{\text{sp,} \phi_e} = h_{\text{sp,} \phi_e} \cdot b_{\text{sp,} \phi_e} \]  
Equation 5.27

For a given angular position \( \phi \), the cross-section in one flute is thus given by the sum of the individual elements:

\[ A_{\text{sp,} \phi} = \sum_{i=1}^{n_i} A_{\text{sp,} \phi_e} \]  
Equation 5.28

### 5.1.5 Model limitations and accuracy

**Modellgrenzen und Genauigkeit**

#### 5.1.5.1 Algorithm complexity

Algorithmuskomplexität

With the increase of the discretization, the model accuracy increases as well. This also leads to an increase of the simulation times and memory usage. A Big-O [VRAJ14] evaluation of the developed algorithm yields the following algorithm complexity:

\[ O(n_s n_l^2 n_{\text{as}}) \]  
Equation 5.29

Where \( n_s \) is the number of simulation steps, \( n_l \) is the number of tool slices or layers and \( n_{\text{as}} \) is the number of angular steps. It is here, therefore, indicated that the number of layers – or slices – has the most impact on the algorithm complexity. By doubling the number of layers, the simulation times and memory usage increase by a factor of four, while the complexity increases linearly with the number of simulation steps and angular steps. The number of flutes does not influence the model complexity. This occurs because of the assumption that the chip geometry remains unaltered during one tool revolution. The geometry calculated for one flute is transferred to the other flutes by an angular shift.

#### 5.1.5.2 Model assumptions

Modellannahmen

The fewer the number of assumptions in the model development, the higher it’s applicability. It was attempted to reduce the number of assumptions in the model development. Following assumptions were still considered:

- The chip is continuous: that means, there is no “gap” in the chip formation. Only one value of \( \phi_e \) and one value of \( \phi_a \) are considered in the calculation.

- The chip geometry remains unaltered during one tool revolution. The calculation is performed for one flute. The chip geometry for the other flutes results from an angular shift of the calculated values.
Both the tool and workpiece are considered as being ideally rigid. Vibration, run-out, and deflection of the tool or workpiece are not taken into account in the model. These variables must be considered in the previous simulations steps, during the determination of the entrance and exit angles.

The cutting speed \(v_c\) is much greater than the feed speed \(v_f\) (\(v_c >> v_f\)). From this assumption, a circular path of the cutting is used in the modeling, instead of a cycloidal path.

The previous workpiece surface in the cutting edge entrance and exit is parallel to the feed components \(f_{xy}\). This assumption is necessary for calculating the linear growth in the chip thickness between the entrance and exit angles and the respective variation angles.

The milling tool have rotational symmetry, i.e. the radius for one slice is constant for all values of the rotation angle \(\varphi\).

The cut is performed only in the flute periphery, i.e. the model is valid only for peripheral milling

**Model validation**

Extensive tests were performed for validating the model. The smaller the contact conditions, the most difficult it is to determine the uncut chip geometry precisely. Most of the simulations were performed with ball-end mills. Since no particular assumption about the tool geometry is made in the slice model, the results can be then extended to other types of tool geometry and contact conditions.

A set of parameters used for finishing milling operations was used for validation, which is considered the worst case in the calculation. The macro contact conditions that serve as input for the calculation were obtained by an analytical method. The method described by Klocke et al. [KLOC11] was used, in which the chip boundaries curves are analytically determined, what will ultimately determine the values of the entrance and exit angles of each slice. The results were also compared to a CSG model of the chip for the tested parameters, which was built in the software Siemens NX. The deviation between the analytical model and the universal model proposed here were then evaluated. The deviation for the values of the maximum chip width and maximum chip length is less than 1% for the whole set of parameters tested. The highest deviation for the values of \(h_{ap\text{-max}}\) is 11% while the highest deviation for \(A_{ap\text{-max}}\) reaches 17%.

The results are shown in Figure 5.10. These values are considered safe, given the flexibility of the system and the little number of assumptions made. The accuracy of the system is significantly higher if the value of the process parameters \(a_{e,n}\), \(a_{p,n}\) and \(f_z\) increase.

The highest deviations occur in regions with variation angles close to the entrance or exit angles, which is attributed to numerical errors. The removed volume in these areas is nevertheless very small, so that the total deviation remain within the values mentioned above.
Figure 5.10: Validation of the geometrical model

Validierung des Geometriemodells
5.2 Integration with the force model

The geometrical model presented here allows an easy integration with several models for force prediction in milling, since the basic parameters for force calculation, which are the uncut chip thickness and width, are available. The linear model developed in Chapter 4 was implemented and used for identifying the influence of the geometrical parameters on the process forces. First, the force is calculated for each chip element for a given rotation angle $\varphi$. A requisite for that is the input of the force parameters for the correspondent force model used. With this data, the force distribution on the flute as well as the pressure on the cutting edge surface can be calculated.

The cutting, cutting normal and passive forces $F_c$, $F_{cn}$ and $F_p$ are calculated in the CC,CN,P coordinate system illustrated in Figure 5.11. The CC-axis is the vector in the direction of the cutting speed $v_c$, while the CN-axis is given by the local curvature angle $\kappa$, pointing towards the curvature radius. The P-axis is, therefore, the cross product between the $\hat{c}$ and $\hat{n}$ versors.

The individual forces in each element of the cutting edge are calculated in the cutting, cutting normal and passive direction by using the model described in Chapter 4.

\[
\begin{align*}
F_{c,ij\varphi} & = (k_{ce,i} + k_{cc,i} \cdot h_{sp,ij\varphi})b_{sp,ij\varphi} \\
F_{cn,ij\varphi} & = (k_{cne,i} + k_{cnc,i} \cdot h_{sp,ij\varphi})b_{sp,ij\varphi} \\
F_{p,ij\varphi} & = (k_{pe,i} + k_{pc,i} \cdot h_{sp,ij\varphi})b_{sp,ij\varphi}
\end{align*}
\]

Equation 5.30

The local helix angle $\lambda$ is here embedded into the coefficients, as a result from Equation 4.12. The forces are then converted to the TCS, in order to calculate the total load on the cutting edge.

\[
\begin{align*}
F_{x,ij\varphi} & = F_{c,ij\varphi} \cdot \cos(\varphi) + F_{cn,ij\varphi} \cdot \sin(\kappa_i) \cdot \sin(\varphi) + F_{p,ij\varphi} \cdot \cos(\kappa_i) \cdot \sin(\varphi) \\
F_{y,ij\varphi} & = -F_{c,ij\varphi} \cdot \sin(\varphi) + F_{cn,ij\varphi} \cdot \sin(\kappa_i) \cdot \cos(\varphi) + F_{p,ij\varphi} \cdot \cos(\kappa_i) \cdot \cos(\varphi) \\
F_{z,ij\varphi} & = -F_{cn,ij\varphi} \cdot \cos(\kappa_i) + F_{p,ij\varphi} \cdot \sin(\kappa_i)
\end{align*}
\]

Equation 5.31

Figure 5.11: Discrete calculation of the cutting forces

Diskrete Berechnung der Prozesskräfte
The resulting forces acting in each flute are then the sum of the discrete force vectors for each slice. The process forces are thus the sum of the resulting forces vectors in each flute for a given rotation angle $\varphi$.

$$
\begin{align*}
F_{x,j\varphi} &= \sum_{i=0}^{n_i} F_{x,i,j\varphi} \\
F_{y,j\varphi} &= \sum_{i=0}^{n_i} F_{y,i,j\varphi} \\
F_{z,j\varphi} &= \sum_{i=0}^{n_i} F_{z,i,j\varphi}
\end{align*}
\Rightarrow
\begin{align*}
F_{x,\varphi} &= \sum_{j=1}^{n} \sum_{i=0}^{n_i} F_{x,i,j\varphi} \\
F_{y,\varphi} &= \sum_{j=1}^{n} \sum_{i=0}^{n_i} F_{y,i,j\varphi} \\
F_{z,\varphi} &= \sum_{j=1}^{n} \sum_{i=0}^{n_i} F_{z,i,j\varphi}
\end{align*}
$$

Equation 5.32

### 5.3 Parameter-based process design

Kennwertbasierte Prozessauslegung

Many parameters can be used for determining the process boundaries and capabilities, due to their correlation with different phenomena that occur in the cutting. For example, the chip length relates directly to the chipping time, which has a strong correlation with the cutting temperatures and therefore with tool wear and form error, due to dilatations of the tool and workpiece. It is, therefore, difficult to draw a direct relation between the commonly used technological parameters and the desired features in the process and in the workpiece, such as shorter process times, lower surface roughness and higher form accuracy.

The geometrical conditions, on the other side, are closely related to the process phenomena, the reason why many models use these parameters for describing several different aspects of the process. Therefore, it is a much more advanced approach to use the process parameters in the process design. Figure 5.12 illustrates this concept.

![Figure 5.12: Parameter-based process design](image-url)
5.3.1 Calculated parameters

Several parameters were defined and calculated based on the model proposed in this work. The available data for these parameters is the uncut chip geometry and the process forces for each discrete element of the cutting edge during one tool revolution. Some relevant parameters are listed here. The parameters which yield a single value for each simulation step, as for example the chip volume or total cutting time, are denominated path parameters, since they can be used for evaluating the tool path.

Specific cutting force

The specific cutting force is defined as the pressure applied to the face of the cutting edge. It is the quotient between the normal force to the rake plane and the chip cross-sectional area. The calculation is performed locally assuming a perfectly sharp cutting edge. The Figure 5.13 shows a schematic illustration of the forces components used in the modeling.

From the Figure 5.13, the following expression can be derived:

\[
P_{\gamma} = \frac{\left(\langle F_{c,j}, \vec{n}\rangle_{\gamma} + \langle F_{cn,j}, \vec{n}\rangle_{\gamma} + \langle F_{cp,j}, \vec{n}\rangle_{\gamma}\right) \cos(\gamma_{ni})}{A_{sp,j}} \tag{5.33}
\]

Where \( \vec{n} \) is the local normal vector to the rake plane, determined by the rake and helix angles \( \gamma_{ni} \) and \( \lambda_{i} \), respectively.
The specific cutting force is then given by the following expression:

\[
P_{ij} = \frac{(-F_{c,ij} \cdot \sin(\gamma_i) \cdot \cos(\phi_i) - F_{c,ij} \cdot \cos(\gamma_i) \cdot \cos(\phi_i) - F_{p,ij} \cdot \sin(\phi_i)) \cdot \cos(\phi_i))}{A_{ij,ij}}
\]

Equation 5.35

The total and average pressures were calculated weighted by the values of the chip cross-sectional area of each element.

**Cutting time**

The cutting time is defined as the contact time of each element of the cutting edge with the material within one revolution. It is related to the chip length, which can also be understood as the friction length.

\[
t_{chip,ij} = \frac{(\phi_{e,ij} - \phi_{e,ij}) \cdot R_0}{v} \cdot \frac{\pi}{60} \cdot \frac{180}{1000}
\]

Equation 5.36

While the total cutting time is the total time in which the entire cutting edge of one flute stays in contact with the workpiece in one tool revolution:

\[
t_{chip,ij} = \frac{(\phi_{a,ij} - \phi_{e,ij}) \cdot R_0}{v} \cdot \frac{\pi}{60} \cdot \frac{180}{1000}
\]

Equation 5.37

Where \( \phi_{a,ij} \) and \( \phi_{e,ij} \) are respectively the maximum and minimum values of \( \varphi \) that satisfy the conditions shown in the Equation 5.22.

**Chip surface area**

The chip surface area is the total swept surface of the chip by the cutting edge. It is, therefore, necessary to take into account in the calculation the projection of the chip on the cutting edge, which is influenced by the helix angle. In the discrete model proposed, the chip surface can be calculated by the following sum.

\[
A_{chip} = \sum_{i=1}^{n} \sum_{\varphi=\varphi_{e,i}}^{\varphi_{e,i}} b_{sp,i} \cdot t_{sp,i} \quad \text{with} \quad \varphi = \varphi_{ei} + \Delta \varphi, \quad n_1 \in N \quad \text{and} \quad 1 \leq n_1 \leq n_{as,i}
\]

Equation 5.38

Note that the influence of the local helix angle is already taken into account in the calculation of the chip width \( b_{sp,i} \).

**Chip volume**

The chip volume is the swept volume by the cutting edge. Its value is therefore given as follows:
In this formulation, the chip is projected on the cutting edge surface. As a result, the value of the volume estimated is slightly higher than the real chip volume.

**Chip surface area and volume below the minimum chip thickness**

The chip area below the minimum chip thickness $A_{\text{chip,MCT}}$ is defined as the fraction of the whole chip surface in which the chip thickness is smaller than a pre-defined value, referred to as minimum chip thickness $h_{\text{sp,min}}$. The volume below the minimum chip thickness is analogously defined. These parameters are related to the amount of ploughing in the cutting process and were defined here as a ratio of the total area.

$$A_{\text{chip,MCT}} = \frac{\sum_{i=1}^{n} \sum_{\varphi=\varphi_{s}}^{\varphi_{e}} b_{\text{sp},i,p} \cdot l_{\text{sp},i,p}}{A_{\text{chip}}}, \text{ where } b_{\text{sp},i,p} = 0 \text{ if } h_{\text{sp},i,p} < h_{\text{sp,min}}$$

Equation 5.40

$$V_{\text{chip,MCT}} = \frac{\sum_{i=1}^{n} \sum_{\varphi=\varphi_{s}}^{\varphi_{e}} h_{\text{sp},i,p} \cdot b_{\text{sp},i,p} \cdot l_{\text{sp},i,p}}{V_{\text{chip}}}, \text{ where } h_{\text{sp},i,p} = 0 \text{ if } h_{\text{sp},i,p} < h_{\text{sp,min}}$$

Equation 5.41

The value of $h_{\text{sp,min}}$ is defined experimentally for a particular combination of material, cutting edge geometry and process parameters. Several studies report a direct correlation between the sharpness of the cutting edge and the minimum chip thickness [ARAM08, IKAW91, WEUL01]. Other studies suggest that the value of the minimum chip thickness also depends on the workpiece material properties, such as hardness, microstructure, and phase [ARNT13, KIM94, WEUL01, VOGL04]. Weule et al. [WEUL] estimated the ratio $h_{\text{sp,min}}/r_p$ to be 0.293.

**Standard deviation of the chip thickness**

The variation of the uncut chip thickness along the tool rotation is related to regenerative forces in milling. One way of measuring its variation is by calculating the standard deviation of the average chip thickness along the rotation angle $\varphi$. An unbiased estimator is used:

$$\sigma_{h_{\varphi}} = \sqrt{\frac{1}{n_{\text{as}}-1} \sum_{\varphi=\varphi_{s}}^{\varphi_{e}} (\bar{h}_{\text{sp},\varphi} - \bar{h}_{\text{sp}})^2}$$

Equation 5.42

Where $n_{\text{as}}$ is the number of angular steps in the contact region, $\bar{h}_{\text{sp}}$ is the average chip thickness in a simulation step and $\bar{h}_{\text{sp},\varphi}$ is the average chip thickness for a given rotation angle.

$$n_{\text{as}} = \frac{\varphi_{\text{e},\text{max}} - \varphi_{\text{e},\text{min}}}{\Delta \varphi}$$

Equation 5.43
Geometrical conditions in milling

\[ \bar{h}_{sp,\varphi} = \frac{1}{n_{sp}} \sum_{i=1}^{n_n} h_{sp,i,\varphi} \quad \text{with } \varphi = \varphi_{ei} + n_i \cdot \Delta \varphi, \quad n_i \in \mathbb{N} \text{ and } 1 \leq n_i \leq n_{as,i} \]  
Equation 5.44

\[ \bar{h}_{sp} = \frac{1}{n_{hsp}} \sum_{i=1}^{n_n} \sum_{\varphi=\varphi_{ei}}^{\varphi_{ei}+n_i \cdot \Delta \varphi} h_{sp,i,\varphi} \quad \text{with } \varphi = \varphi_{ei} + n_i \cdot \Delta \varphi, \quad n_i \in \mathbb{N} \text{ and } 1 \leq n_i \leq n_{as,i} \]  
Equation 5.45

With \( n_{sp} \) being the total number of slices in contact in a given rotation angle \( \varphi \) and \( n_{hsp} \) the total number of chip elements. It is important to point out that every tool layer has different values of entrance and exit angles, which must be considered in the determination of the average values of the chip thickness.

\[ n_{hsp} = \sum_{i=1}^{n_n} \sum_{\varphi=\varphi_{ei}}^{\varphi_{ei}+n_i \cdot \Delta \varphi} 1 \quad \text{with } \varphi = \varphi_{ei} + n_i \cdot \Delta \varphi, \quad n_i \in \mathbb{N} \text{ and } 1 \leq n_i \leq n_{as,i} \]  
Equation 5.46

A lower standard deviation of the uncut chip thickness correlates with a uniform distribution of the process forces along the tool rotation.

**Skewness ratio**

The skewness ratio is a measure of the chip asymmetry. It is here distinguished between axial and rotational asymmetry. The asymmetry is measured in terms of volume. Figure 5.14 shows the projection of the uncut chip as a function of the rotation angle and slice height. The colors represent the uncut chip thickness.

![Figure 5.14: Definition of the skewness ratio](image)

**Figure 5.14:** Definition of the skewness ratio

\[ SK_{rot} = \frac{\varphi_{hsp,max} - \varphi_{CM}}{\varphi_{a,max} - \varphi_{e,min}} \]  
Equation 5.47

In which the center of mass angle \( \varphi_{CM} \) is the value of the rotation angle \( \varphi \) at which half of the chip’s volume is removed and \( \varphi_{hsp,max} \) is the \( \varphi \) coordinate of the maximum chip thickness. \( SK_{rot} \)
assumes values between -1 and 1. In this definition, a down-milling process has a negative value of skewness, while the values of SKrot are positive for up-milling. The amplitude of the angular skewness indicates how close to the beginning or end of the cut, the maximum specific cutting force occurs.

The axial skewness is similarly defined:

$$ SK_{ax} = \frac{h_{asp, max} - h_{CM}}{h_{max} - h_{min}} $$  \hspace{1cm} \text{Equation 5.48}  

In which the center of mass height $h_{CM}$ is the height position of the tool layer at which half of the chip’s volume is removed. The axial asymmetry indicates that the load on the cutting edge is not uniformly distributed, as well as the mechanical loads. This results in a not uniformly wear of the cutting edge.

The form of the chip itself is not evaluated, but its projection on the cutting edge for a specific tool orientation and helix angle.

**Impact ratio**

Müller [MUEL82] was the first to define the time gradient of the chip cross-sectional area during the cutting edge entrance as a measure of the mechanical loads on the cutting edge. Arntz expanded this concept for 5-axis ball-end milling and also used these parameters as a measure of the asymmetry of the chip volume [ARNT13].

The definition of the impact ratio from Arntz [ARNT13] applied to the universal model presented here results in the following expressions:

$$ IR_{asp,e} = \frac{h_{asp, max} \cdot v_c \cdot 1000 / 60}{R_0 (\phi_{Asp, max} - \phi_{e, min}) \cdot \pi / 180} \left[ \frac{mm}{s} \right] $$  \hspace{1cm} \text{Equation 5.49}  

$$ IR_{asp,a} = \frac{h_{asp, max} \cdot v_c \cdot 1000 / 60}{R_0 (\phi_{a, max} - \phi_{Asp, max}) \cdot \pi / 180} \left[ \frac{mm}{s} \right] $$  \hspace{1cm} \text{Equation 5.50}  

Where $h_{asp, max}$ is the global maximum chip thickness and $\phi_{Asp, max}$ is the angular position of the maximum chip cross-sectional area.

**Aspect ratio**

The aspect ratio is a good measure for describing the form of the chip projected on the flute. It is here defined as the ratio between axial and radial lengths of the chip.

$$ AR_{chip} = \frac{b_{asp, max}}{l_{asp, max}} $$  \hspace{1cm} \text{Equation 5.51}  

This definition was first proposed by Arntz [ARNT13], who denominated it global compactness ratio.
An elongated chip has a more regularly distributed volume along the tool axis, resulting in a more uniform distribution of the load and consequently less concentrated wear on one part of the flute.

Figure 5.15 shows a comparison of the aspect ratio from a barrel-shaped tool and a ball-end mill for similar tool orientation and chip volume. Although the removed volume is the same, its shape is very distinct. The chip formed by the barrel-tool is more distributed along the cutting edge, i.e. its aspect ratio is higher.

Arntz [ARNT13] also proposes the concept of a local aspect ratio, for describing the shape of the chip cross-sectional area. The local aspect ratio is especially useful for evaluating the form of the chip during the cutting process. In the discrete model proposed here, the aspect ratio of the cross-sectional area can be calculated by the following expression:

\[ AR_{asp,p} = \frac{h_{sp,max,p}}{b_{sp,p}} \]

Equation 5.52

Previous studies present evidence that these factors play an important role in the process design, especially in the machining of materials with multiple phases [ARNT13]. A low aspect ratio is related to smaller values of the chip thickness, which in turn correlate with the ploughing and other unfavorable effects.

**Cutting power**

The spindle and feed power can also be determined based on the process forces. Since the cutting force is already defined collinear, i.e. parallel, with the cutting speed, no conversion is necessary for the calculation of the spindle power:
Geometrical conditions in milling

\[ P_{\text{sp}_i\text{t}_j\text{p}} = \sum_{j=1}^{n} \sum_{i=1}^{m} F_{c,i,j,p} \cdot v_c \cdot \frac{f_i}{60 \cdot R_0} [W], \text{ in which } v_c \text{ is given in m/min and } F_{c,i,j,p} \text{ in N} \quad \text{Equation 5.53} \]

The feed power, however, results from the feed force, which requires information about the feed direction to be determined. The feed force can be obtained by using the rotation matrix presented in Equation 5.7:

\[
\begin{pmatrix}
F_{x,p} \\
F_{y,p} \\
F_{z,p}
\end{pmatrix} = M^{-1}_{\text{FCN},\text{TCS}} \cdot \begin{pmatrix}
F_{x,p} \\
F_{y,p} \\
F_{z,p}
\end{pmatrix} = M^{-1}_{\text{FCN},\text{TCS}} \cdot \left( \sum_{j=1}^{n_s} \sum_{i=1}^{n_f} F_{c,i,j,p} \cdot \cos(\varphi) + F_{c,n,i,j,p} \cdot \sin(\varphi) \right) - \sum_{j=1}^{n_s} \sum_{i=1}^{n_f} F_{c,n,i,j,p} \cdot \sin(\varphi) + F_{c,n,i,j,p} \cdot \cos(\varphi) + \sum_{j=1}^{n_s} \sum_{i=1}^{n_f} F_{p,j,p}
\right)
\quad \text{Equation 5.54}
\]

Where the inverse of the \( M_{\text{FCN},\text{TCS}} \) matrix is given by:

\[
M^{-1}_{\text{FCN},\text{TCS}} = \begin{bmatrix}
Z_N & -Z_C Z_F & Z_F \\
\frac{Z_F}{Z_F + Z_N} & \frac{-Z_C Z_F}{Z_F + Z_N} & \frac{Z_F}{Z_F + Z_N} \\
\frac{-Z_F}{Z_F + Z_N} & \frac{Z_C Z_F}{Z_F + Z_N} & \frac{Z_N}{Z_F + Z_N}
\end{bmatrix}
\quad \text{Equation 5.55}
\]

Finally, the feed power can be calculated by the following expression:

\[ P_{\text{feed}} = F_{r,p} \cdot \frac{v_f}{60 \cdot 1000} [W], \text{ in which } v_f \text{ is given in mm/min and in } F_{r,p} \text{ N} \quad \text{Equation 5.56} \]

**Maximum passive, active and feed forces**

The reference system CC, CN, P shows to be suitable for calculating the forces acting on each element of the cutting edge, since the force coefficients can be extracted from orthogonal cutting tests, as shown in Chapter 4. It is also appropriate for deriving many coefficients that are used in the process evaluation, such as the specific pressure on the tool’s face and the spindle power. Nevertheless, it is difficult to evaluate the forces in real milling processes based only on this reference system. Most dynamometers used for measuring force and torque are coupled to the workpiece, and the conversion of the \( F_c \) and \( F_{cn} \) forces to the workpiece coordinate system is very inaccurate. A conversion of the forces to the feed direction allows a direct evaluation of the simulated forces with the measured data. Furthermore, the passive and active forces can also be used for a systematic investigation of real milling processes, if the tool orientation is known, allowing a conversion of the measured forces into these components. By using the conventions shown in this chapter, following expressions can be employed for evaluating the maximum passive, active and feed forces:
In which \( F_{x,\psi} \), \( F_{y,\psi} \), \( F_{z,\psi} \) are given by Equation 5.32, while \( F_{t,\psi} \) is expressed in Equation 5.54.

5.4 Influence of the technological parameters on the geometrical conditions and process forces in multi-axis milling

Einfluss der technologischen Parameter auf die geometrischen Bedingungen und Prozesskräfte beim mehrachsigen Fräsen

A thorough understanding of how the technological parameters influence the process forces and the geometrical conditions is fundamental for designing an efficient process. The developed model allows this investigation through a range simulation, in which all parameters are held constant, and only one targeted parameter is varied.

Since ball-end mills are the most used tools in finishing operations, the investigation presented here is conducted by using this type of cutter. Typical values of the process parameters feed rate, depth of cut, step over, cutting speed and also tool geometry for finishing operations were chosen. The removed chips in this type of operations are quite small and represent a challenge for the modeling. A very fine discretization was required for achieving smooth and accurate results. The process forces were derived for the hot work steel DIN 1.2343 (AISI H11), hardened at HRC 52. Figure 5.16 summarizes the parameters used in the simulation.

![Simulation parameters](image-url)
To perform the simulation with the model presented in this work, it is necessary first to determine the macroscopic contact conditions for the selected parameter set. This was conducted in part with an analytical model presented by Klocke et al. [KLOC11]. The macroscopic conditions were calculated analytically, and a special module generated the required input for the microsimulation. The force model presented in Chapter 4 was also integrated into the simulation system. The force coefficients were therefore given as a function of the local variables, i.e., they were different for each element of the cutting edge, mainly due to the changing in the local cutting speed.

In the following, the influence of the typical process parameters on the microscopic contact conditions and process forces is evaluated for flank milling operations.

### 5.4.1 Feed rate ($f_z$)

**Zahnvorschub ($f_z$)**

The feed rate is perhaps the most studied parameter in the milling process. Its relationship to the process forces and machine power is well-known since the first studies of Taylor [TAYL07]. Figure 5.17 shows the influence of the feed rate on the uncut geometry for ball-end milling.

---

**Simulation setup**

<table>
<thead>
<tr>
<th>Tool:</th>
<th>Process:</th>
<th>Settings:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Geometry: Ball-end mill</td>
<td>Depth of cut $a_{hp} = 0.1$ mm</td>
<td>Slice height $\Delta h = 0.02$ mm</td>
</tr>
<tr>
<td>Diameter: $D_0 = 6$ mm</td>
<td>Feed rate $f_z = 0.01 - 0.05$ mm</td>
<td>Angular step $\Delta \varphi = 0.05^\circ$</td>
</tr>
<tr>
<td>Nr. of flutes: $z = 2$</td>
<td>Supination ang. $\theta_{hp} = 60^\circ$</td>
<td></td>
</tr>
<tr>
<td>Drallwinkel: $\lambda = 10^\circ$</td>
<td>Azimuth $\psi_{bc} = 90^\circ$</td>
<td></td>
</tr>
</tbody>
</table>

---

*Figure 5.17: Influence of the feed rate on the uncut chip geometry*

Einfluss des Zahnvorschubs auf die Spanungsgeometrie
The parameter set chosen for this evaluation corresponds to an up milling process, which can be determined by the value of $\psi_F$ between 0 and 180°. As depicted here, the feed rate has a high impact on the chip thickness and cross-sectional area. Nevertheless, it has almost no effect on the chip width and friction length. The resulting skewness ratio lies between 0.35 and 0.40, with a little decay resulting from the increasing feed rate. The basic form and volume of the uncut chip for ball-end milling, as described by Arntz [ARNT13], is determined only by the feed rate, depth of cut and step over. No residual material or drastic change in the macroscopic chip geometry is considered in this observation.

Figure 5.18 shows the influence of the feed rate on some selected parameters. It is observable that the chip volume increases linearly with the feed rate, while the portion of the area and volume, in which the chip thickness is below the $h_{sp,\text{min}}$, decreases. This explains the known size effect described by Aramcharoen [ARAM08], which observed a percentage decrease in the ploughing forces with the increase of the feed rate.
Furthermore, the local aspect ratio increases with the feed rate, resulting from the increase of the chip thickness in comparison with the chip width. This has a high impact on the milling of materials with carbides and multi-phases. A more compact chip can accommodate better the material grains and its distribution, which can hinder the cutting process.

As demonstrated in the previous chapter, the process forces are influenced mainly by two geometrical factors: the chip thickness \( h_{sp} \) and the chip width \( b_{sp} \), which constitute the chip cross-sectional area. The cutting component, which is related to \( k_{ic} \), depends on both quantities while the edge forces depend only on the width of the chip. The chosen combination of process parameters, material, and microscopic tool geometry suggests a predominant influence of the edge forces rather than the cutting forces. For this reason, the process forces behave similarly to the chip width, as depicted in Figure 5.19.

The model developed in Chapter 4 was used in the simulation, considering both the local helix angle and the local cutting speed in the determination of the force coefficients. For the simulated parameters, the feed rate has only little influence on the process forces. The resulting average specific pressure on the cutting edge reduces therefore with the feed rate. An important figure for describing this effect is the ratio \( k_{ic}/k_{ie} \). The bigger this ratio, the most influential is the chip thickness and consequently the feed rate on the process forces. As indicated in Figure 5.19, for the investigated cutting conditions, this ratio is approximately 58 for the cutting forces, 25 for the cutting normal and 3.5 for the passive forces, varying accordingly to the local cutting speed. It is important to note that the values of \( h_{sp} \) and \( b_{sp} \) are given in millimeters in this estimation. Since no significant variation of the process forces is observed, only a little variation on the spindle and feed power is expected, as shown in Figure 5.18.
Figure 5.20: Influence of the feed rate on the process forces with a high k\textsubscript{ic}/k\textsubscript{ie} ratio

This scenario changes considerably when the cutting conditions, material, and tools used result in a higher ratio k\textsubscript{ic}/k\textsubscript{ie}, as observed in roughing operations [ALTI12]. Figure 5.20 illustrates the process forces for a ratio k\textsubscript{ic}/k\textsubscript{ie} approximately 40 times higher than the previous one. In this case, the influence of the feed rate on the forces is noticeable, especially on the cutting and cutting normal forces. The process forces increase significantly with the feed rate. A correspondent increase in the cutting pressure is also observed in the simulation.

5.4.2 Depth of cut (a\textsubscript{p,n})

The depth of cut is one of the most influential factors on the uncut chip geometry and process forces, alongside the feed rate and the step over. Higher depths of cut are related to regenerative vibrations and consequently to rough surfaces. Stable and unstable processes, for instance, can be determined according to the depth of cut [CABR07, ALTI12]. While vibrations are the limiting factor for the depth of cut in roughing, in finishing operations, the value of a\textsubscript{p,n} it is typically chosen in order to achieve the targeted surface quality, keeping the shape of the workpiece inside the required tolerances and avoiding extreme tool wear. The scallop height and theoretical surface roughness, which depend only on geometrical parameters, also increase with the depth of cut. The depth of cut also determines the amount of cutting edges in contact with the workpiece at the same time. In general, the more flutes removing material simultaneously, the more stable is the process, and the higher is the material removal rate.

Figure 5.21 illustrates the influence of the depth of cut on the basic uncut chip geometry for ball-end milling.
With increasing depth of cut, lower regions of the tool get in contact with the workpiece. The result is a longer cutting time and a higher engagement angle $\varphi_c$, since the local radius is smaller closer to the tool tip and a higher portion of the tool circumference lies in the engagement region. This effect can be observed in the increasing chip length. An associated disadvantage is the possible unfavorable cutting speed, which decreases along the tool axis in the direction of the tool tip. In contrast to the feed rate, the depth of cut influences both the chip thickness and the chip width, an effect which is added on the resulting cross-sectional area.

All components of the process forces are significantly affected by the depth of cut, as indicated in the simulation results illustrated in Figure 5.22. This effect derives from the increase of the uncut chip thickness and width, whereas the chip width has the highest impact. The cutting coefficients used in the simulation result of the experimental investigation described in Chapter 4.

Tool deflection is often one limiting factor for selecting the depth of cut, especially in finishing operations, in which the surface quality and the final form of the workpiece are the targets. The resulting increase in the cutting normal forces directly influence the tool deflection, what could lead to form errors, tool breakage and damage to the workpiece surface. Many studies have been performed to determine the optimal values of the depth of cut based on the deflection of the tool [DESA12]. An investigation of the milling tool as a cantilever beam would derive maximum values for the cutting normal forces, which in turn could be used in the process design. The simulated values are useful for determining rough values of the cutting forces, which serve as input for FEM or analytical simulations of the tool deflection.
Figure 5.22: Variation of the process forces with the depth of cut

Variation der Prozesskräfte in Abhängigkeit der Schnitttiefe

Figure 5.23 shows the influence of the depth of cut on selected parameters. It is evident that the chip volume and surface area increase proportionally to the depth of cut. The local aspect ratio increases slightly, whereas a shift in the direction of the rotation angle can be observed. This suggests the formation of more compact chips towards the end of the cut, typical condition of up milling operations.

The chip volume and area below the minimum chip thickness decrease with the value of \(a_{p,n}\), indicating a reduction of the relative amount of ploughing in the process. A higher influence of the cutting components on the process forces at higher depths of cut, in comparison with the edge components, is expected. The maximum spindle power also increases significantly. This is related to the increase of the cutting forces, since the spindle power can be estimated based only on this component.

Although the process forces increase with the depth of cut, the simulation shows a decline of the pressure on the cutting edge. This indicates that load normal to the tool face increase at a lower rate than the surface on which it is applied. The drop rate is, however, lower in comparison with the reduction of the pressure associated with the increase of the feed rate, which is much more significant.

The axial skewness ratio for simulated parameters lies between -0.20 and -0.17, while the rotational skewness ratio slightly increases from 0.35 to 0.38. This indicates that the asymmetry of the chip remains virtually unaltered by the depth of cut.
5.4.3 Step over ($a_{e,n}$)

The step over, as defined in Figure 2.15, has a similar effect on the core form of the chip as the depth of cut. The removed volume and chip area increase linearly with the value of $a_{e,n}$, which results in an increase in the material removal rate. High values of the step over are thus mostly used in roughing operations, in which the focus lies on high material removal rates. The surface roughness, however, also increases with the step over. Small values of $a_{e,n}$ are therefore necessary for finishing operations. In fact, equal values of $a_{e,n}$ and $a_{p,n}$ are often chosen in finishing operations, due to the fact that the generated scallops are more symmetrical. The value of the step over also depends on the final form of the machined workpiece and on the milling strategy used. A high feed strategy, for instance, uses a light value of $a_{e,n}$ and a high feed rate, targeting a compromise between the material removal rate and the load on the cutting edge. The effect of the step over on the uncut geometry is depicted in Figure 5.24.
As illustrated, a significant increase of the chip thickness is observed. This effect results from the fact that a greater portion of the machined material lies in front of the tool, in the feed direction. The normal force of the cutting edge in the engagement zone has a higher component in the F-axis, resulting in higher values of the engagement angle $\phi$, which in turn results in higher values of the chip thickness (Equation 5.20). The chip length also increases with the value of $a_{e,n}$, while portions of the tool closer to the tool shank get in the engagement region. An increase of the average local cutting speed is therefore expected.

The maximum uncut chip width, however, is only slightly affected by the value of $a_{e,n}$ for the chosen tool orientation. A different behavior of the uncut chip width may, however, occur for other values of $\theta_N$ and $\psi_F$. For other types of tools, as end mills, for instance, the step over also affects the maximum chip thickness and length rather than the width of cut.

Since the process forces for small uncut thicknesses is, as already mentioned, most influenced by the chip width instead of the chip thickness, the process forces behave accordingly with the chip width, as shown in the top right portion of the Figure 5.24. The results for the force simulation are shown in Figure 5.25. The maximum forces are only slightly affected by the step over while the duration of the forces increases considerably. An increase of the total work and impulse is therefore expected. The total work derives from the integration of the forces in this figure over the friction length, which is related to the rotation angle $\phi$ by the local radius $r_i$. The impulse, which is here related to the variation of the linear momentum of the chip, is likewise calculated by integrating the forces over time, which is also proportional to $\phi$. It can give a measure of the escaping velocity of the deformed chip.
Geometrical conditions in milling

Figure 5.25: Impact of the step over on the process forces

Auswirkung der Zeilenbreite auf die Prozesskraft

Figure 5.26 shows the variation of selected process parameters with the step over. Both rotational and axial skewness ratios slightly increase with the value of $a_{e,n}$. The chip is, therefore, more asymmetrical. This increase reflects the shift in the angular position, at which the cutting edge reaches the maximum chip thickness.

The local aspect ratio is extremely small for most milling operations, since the values of $h_{sp}$ are much lower than $b_{sp}$. In the considered parameter set, the value of $AR_{asp, \varphi}$ increases linearly with the step over. As already mentioned, this has a positive effect on chip formation, reducing the ploughing forces.

The reduction of the ploughing forces is also related to the portion of the chip area and volume under the minimum chip thickness, also depicted in Figure 5.26. Both area and volume below the minimum chip thickness decrease with the step over. The value of the $A_{sp,MCT}$ drops from 18%, for $a_{e,n} = 0.05 \text{ mm}$, to 10.5%, for $a_{e,n} = 0.15 \text{ mm}$, while the volume decreases from 3.1% to 1.2% in the same interval. For lower values of the feed rate, as for instance $f_z < 0.01 \text{ mm}$ in the investigated parameter set, both area and volume below the minimum chip thickness reach 100%, the reason why extremely low feed rates should be avoided.

Since the forces are not significantly affected by the step over for the conditions tested, the cutting pressure on the rake face decreases, inversely proportional to the increase of the chip cross-sectional area, as depicted in Figure 5.26. No substantial variation on the entrance and exit impact ratios is observed. The maximum feed and spindle power increase marginally with the step over, whereas the feed power is much lower than the spindle power. A higher increase of the power is expected for higher ratios $k_{ic}/k_{ie}$.
5.4.4 Helix angle ($\lambda$)

The helix angle has no influence on the macroscopic form and volume of the chip. Yet, it changes the projection of the chip on the cutting edge, influencing the way the chip is formed, as well as the process forces. Figure 5.27 illustrates the variation of the uncut chip geometry with the helix angle.

As the removed volume is the same, the value of the maximum chip thickness remains unaltered. The time instant, at which $h_{\text{sp, max}}$ is reached, however, shifts. The cutting time increases, what can be deduced from the increase of the contact angle represented as the distance between the maximum and minimum engagement angle $\varphi$ at the diagrams in the figure. The chip width in function of the rotation angle decreases at higher helix angles for the selected parameters. This effect is counterintuitive and can be explained by the illustration on the bottom of Figure 5.27.
Figure 5.27: Variation of the uncut chip geometry with the helix angle

This illustration represents the projection of the chip on the cutting edge as a function of the height and rotation angle, already considering the helix angle. The colors represent the chip thickness in each position of the cutting edge. The chip width $b_{sp}$ for each $\varphi$ is the difference between the maximum and minimum height, whereas the chip length for each height is the difference between the highest and lowest $\varphi$. It is here clear that the chip width decays and shifts with increasing helix. From volume conservation, the chip cross-sectional area increases, so that the sum of the volumes of each chip element remains constant.

Another counterintuitive impact of the helix angle on the uncut chip geometry is represented in Figure 5.28. The uncut chip geometry projected on the cutting edge for the same process with identical parameters is hereby depicted. The side of the workpiece on the C-axis and the values of the azimuth $\psi_F$, however, are mirrored so that a down milling and an up milling can be represented. The chip volume is the same in this situation. The uncut geometry is perfectly mirrored if the helix angle is not considered. When considering the helix angle in the simulation, the projection of the volume is different for the down milling and up milling process. The helix angle has a higher impact on the down milling process in comparison with up milling.
The chip length of a down-milling process with a helical tool is longer than the chip length of an equivalent up-milling process. The chip width, however, is smaller. The contrary is expected for tools with opposite helix. The tool considered in the modeling rotates clockwise, as already explained in the modeling. This effect has a high impact on the process forces and should be considered during the choice of the milling tool and process strategy.

All parameters that only depend on the basic chip geometry, such as maximum chip thickness, volume and area below $h_{sp,min}$, chip surface area and volume and average values of the chip geometrical parameters remain unaltered by the helix angle.

Figure 5.29 illustrates the variation of the process forces with the helix angle. The helix angle considered in the simulation varies from 0° to 40°. The total force in function of the rotation angle increases only slightly with the value of $\lambda$. The passive force, on the other side, increases considerably at higher helix angles. In the considered model, the values of $k_{pe}$ and $k_{pc}$ are proportional to $\sin(\lambda)$, the reason why the theoretical passive forces are zero in straight tools and increase with the helix. This effect also ratifies other practical and experimental results [ALT112]. A major impact can be observed on the feed force, which decreases with increasing helix angle. Less feed power is, therefore, necessary for helical tools. Consequently, the tool deflection in the feed direction is also reduced. High values of $\lambda$ can, therefore, lead to a better form accuracy.
Another parameter that is strongly influenced by the helix angle is the aspect ratio. The maximum aspect ratio \( \frac{h_{sp,max}}{b_{sp,max}} \) drops by almost ten times from 0.038 to 0.004 in the considered parameter field, a factor that should be considered in the process design according to the workpiece material.

5.4.5 Tool radius \( (R_0) \)

To analyze the influence of the tool nominal radius \( R_0 \) on the uncut chip geometry and process forces, it was necessary to keep all other variables constant, including the cutting speed. To achieve that, the spindle speed was adjusted for each value of \( R_0 \). The variation of the uncut chip geometry with the tool radius is shown in Figure 5.30.

The tool radius varied from 1 mm to 9 mm in the tested case. As a result, less than 0.9 % variation of the chip volume was observed. Therefore, for this analysis, it was possible to assume that the volume remains constant.

By increasing the tool radius, the maximum chip thickness and the contact angle \( \varphi_c \) decrease significantly. This fact is related to the shift of the material towards the side of the tool, where lower values of \( \varphi \) occur, which results in lower values of \( h_{sp,max} \). On the other hand, a major increase of the maximum chip width was observed, arising from the growth in the number of tool slices that come in contact with the workpiece. This effect may seem counterintuitive at the first glance, but it becomes obvious by extrapolating the value of the radius. The value of \( b_{sp} \) tends to infinity with increasing tool radius, since the curvature of the tool tends to zero.
From the increasing value of $b_{sp}$ and volume conservation, it was possible to deduce the increase of the uncut chip length $l_{sp}$. Although the contact angle increases with the tool radius, the friction length reduces. The cross-sectional area also decreases substantially with the growth of the tool radius.

From this geometrical condition, the resulting local aspect ratio $h_{sp}/b_{sp}$ dropped considerably, as illustrated in Figure 5.31. As the value of the uncut chip thickness drops, the portion of the chip below the defined minimum chip thickness decreased. For this analysis, the cutting edge rounding radius was taken as $5 \times 10^{-3}$ mm and the minimum chip thickness as 30% of this value. At a tool radius of 9 mm, almost 100% percent of the chip volume and area lied below the minimum chip thickness.

The cutting forces are also illustrated in Figure 5.31. All forces behaved in the same manner, as a detailed analysis of the results suggests. For this reason, only the total forces are illustrated here. For analyzing the process forces, it is appropriate to plot the load over time rather than over the rotation angle, since different radius have different angular speeds to keep the cutting speed constant. The substantial increase in the process forces is the result of a balance between the increase of the chip width and decrease of the uncut chip thickness. As already discussed, the analyzed process is much more influenced by $b_{sp}$, the reason why an increase on the process forces is expected, although the uncut chip thickness decreases.

This effect is additionally counterbalanced by a slight increase in the local cutting speed ratio $v_{c,hsp,max}/v_{c}$, which rises from 0.67 to 0.79. As investigated in Chapter 4, the process forces decrease with an increasing value of $v_{c}$. 

![Simulation setup](image)
For tool, material and process strategies combinations with higher ratios $k_{ic}/k_{ie}$, the opposite effect on the forces is expected, as observed by Kölling [KÖLL86]. The process forces would then increase with the tool radius.

Since the process forces increase and the chip cross-sectional area reduces at higher tool radius, an increase of the cutting pressure on the rake plane is expected, as observed in Figure 5.31.

The tool radius also influences other parameters, which is illustrated in Table 5.1.

### 5.4.6 Cutting speed ($v_c$)

The cutting speed is another important process parameter. The quality of the milling process is strongly dependent on the right choice of the cutting speed for the given material and tool combination. Other factors such as coating, cutting fluid, spindle speed, vibrations, and targeted material removal rate are also considered in the choice of the cutting speed. Vast studies also relate the value of $v_c$ with tool wear, being this and chemical reactions due to the temperature increase in the chip formation zone two important limiting factors of the cutting speed.

The tool life is also strongly influenced by the cutting speed, due to its influence on the friction between the cutting edge and the workpiece material [BIEK91]. Several studies have shown that the tool wear increases exponentially with the value of $v_c$ [TAYL07, GOME01, BIEK91].
From the purely geometrical point of view, the cutting speed has no influence on the uncut chip geometry. It nevertheless has a strong impact on the process forces and on some of the calculated parameters, such as the impact ratio, cutting time and all other parameters which take the forces as input for the calculation.

The influence of the cutting speed on the process forces is illustrated in Figure 5.32. As the in Chapter 4 derived force model indicates, the cutting and cutting normal forces decrease with the rise of the cutting speed. This drop is related to the temperature gradient in the chip formation zone, which facilitates the cutting process. More heat is generated as a result of the increasing deformation rate and friction. This observation is also supported by extensive research [ALTI12, KÖNI82, ARMA69, OXLE89, KRON27, MERC45].

The passive forces, however, increase slightly with the cutting speed. This effect is related to the method of calculating the cutting coefficients used and is explained in detail in Chapter 7. The passive forces are assumed proportional to the cutting coefficient of the cutting force, \( k_{cc} \), which as demonstrated in Chapter 4, increases proportionally with the cutting speed.

Although the cutting forces decay with increasing cutting speed, an elevation on the maximum spindle power was observed, as illustrated in Figure 5.33. This indicates that the process forces reduce in a lower rate as the increase of the cutting speed, since the spindle power is the product of both quantities. An increase of the impact ratios is also observed, what is expected from the definition of these ratios (Equation 5.49 and Equation 5.50), assuming the uncut chip geometry remains unaltered.
A reduction of the pressure on the cutting edge follows the force decay. This estimation is a natural result of the vector decomposition used in the calculation of the forces normal to the rake plane, which considers the forces in the CC,CN,P coordinate system, the rake angle and the helix angle. Since the decay on the cutting and cutting normal forces is higher than the increase of the passive forces, the resulting force normal to the rake plane is lower and the pressure consequently lower.

### 5.4.7 Summary over the influences

In order to provide quick access to the results achieved with the simulation, Table 5.1 summarizes the influences of the technological parameters on the calculated parameters and process forces.

The arrows indicate the direction of the influence considering an increase in the technological parameters. An orange dash represents relations in which the influence is considered only marginal for the selected parameter set. For example, an increase in the feed rate leads to a significant increase of the maximum chip thickness, to a decrease of the area below the minimum chip thickness and only to a little variation of the chip width.

The results were generated considering flank ball-end milling operations with small contact conditions. Nevertheless, with appropriate considerations, the results can be extended to other process strategies and tool geometries.
### Table 5.1: Influence of the increase of the technological parameters on the calculated quantities

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*Table 5.1: Influence of the increase of the technological parameters on the calculated quantities*

*Einfluss der Erhöhung der Technologieparameter auf die berechneten Kennwerte*
5.5 Conclusions about the geometrical conditions in milling

This chapter presents a universal model for calculation of the uncut chip geometry for an arbitrary tool and process strategy, answering thus the second research question. The model requires the knowledge of the macroscopic engagement situation, the tool geometry, and the material-related coefficients in order to calculate the microscopic contact conditions and derive the process forces. It enables the calculation of important parameters, which build the fundamentals for evaluation of the engagement situation along the tool path.

A comparison with analytical models indicated a high accuracy of the results, given a fine discretization of the tool. The complexity and the correspondent computation time and memory usage, nevertheless, increased quadratically with the number of slices considered in the simulation.

A force model was integrated into the geometrical model, allowing the calculation of the process forces in different reference systems, thus answering the third research question. A combination of the forces and the geometrical parameters allowed the derivation of several useful parameters for designing and evaluating the milling process.

The geometrical model is valid for all types of rotational symmetrical tools. The force model, on the other side, is valid only in the investigated range presented in Chapter 4. All terms related to the cutting edge radius in Equation 4.7 need to be disregarded if considering a perfect sharp tool.

The influence of the technological parameters on the process forces and microscopic contact conditions was evaluated for flank ball-end milling, i.e. with a tool orientation closer to the cross-feed vector. The developed force model depicts the milling process for small contact conditions, in which the cutting edge rounding radius plays an important role, being most applicable for finishing operations. The results are therefore valid especially for the chosen test case, but can be extended to other process strategies and tool geometries, given the appropriate considerations.

The analysis of the process forces behavior pointed out that the ratio $k_w/k_t$ determines whether the process forces are influenced mostly by the uncut chip width or thickness. Lower ratios indicate a width predominant influence, whereas higher rates are affected by both quantities. For lower values of $k_w/k_t$, the process parameters which influence only the uncut chip thickness have therefore a lower influence on the process forces.

In the following chapters, the integration of the developed models into a simulation system is presented, and the results are verified based on experimental data.
6 Development of the simulation system

All the algorithms described in Chapters 4 and 5 were implemented and tested in a software system. The implementations described here enable a comprehensive understanding of the form in which the algorithms can be used and how the different interfaces can be designed. Based on the principles used in the design and implementation of the simulation system, it is possible to derive insights for an industrial application of the developed algorithms and methods in a coupled simulation environment.

6.1 Concept of the software tool

The software tool was conceived and implemented taking into consideration an integrated simulation environment. Therefore, the primarily targeted users are other simulation systems and modules. A stand-alone version of the software tool was also implemented, which allows the system operation and testing without the need for coupling with other simulations.

One of the most important factors for ensuring the software compatibility and therefore possible integration into other systems is a modular design. Modularity allows a reduction of the overall system complexity through decentralized software architecture. It also facilitates the reusability and increases the performance of the system. A good performance regarding computation time and memory usage was also envisaged during the implementation and supported by the low algorithmic complexity developed for calculating the chip properties and process forces. The system follows an own standard, as there is no widely used standard in the field of simulation of machining processes [ZABE10].

For increasing the system usability and contributing to solving the dichotomy of planning orientation by reducing the planning effort, it is essential to provide quick access to the results. For this reason, a separate module was developed for visualizing and analyzing the simulation data, which enables a visual evaluation of the engagement conditions and exports the targeted results.

The implementation was performed in MATLAB 2014a [GEKE10], which provides an excellent environment for testing and developing analytical algorithms and performing numerical calculations with large amounts of data. The stand-alone version runs on any machine that is compatible with MATLAB Compiler Runtime v8.3, allowing the system usage in personal computers with low hardware performance.

6.2 Software structure and modules

The system was divided into six individual modules with a high cohesion and a graphic user interface (GUI Module) which are depicted in Figure 6.1. The GUI module serves as a controller to the other modules, in order to unify all the system into a software, using a bus-modular design.
Development of the simulation system

Figure 6.1: Modular description of the software system

GUI Module
Intuitive graphical user interface for operating the standalone version of the system. Handles user input, including error management. Controls the program flow by calling the other modules.

Geometry
Calculates the undeformed chip geometry and derives further parameters, such as impact factor and surface area under the minimum cutting thickness.

Path Parameters
Calculates global parameters for the whole path, enabling a comprehensive analysis of the milling strategy.

Import & Export
Handles importing data from other simulations, such as macro contact conditions, allowing use of the system without the GUI. Exports simulation results.

Analysis
Module for analysis and visualization of the simulation results. Generate all the charts required for a fast evaluation of the contact conditions and process forces.

Tool
Defines the tool geometry through either analytical calculation or numerical interpolation of the input data.

Force
Determines the process forces for given engagement conditions. Enables the use of different force models (Kienzles, Altintas, etc.).

O(n_s n_l n_{as})

Legend:
- \(n_s\): number of steps
- \(n_l\): number of layers
- \(n_{as}\): number of angular steps
6.2.1 Module for calculation of the geometrical conditions

The module for calculation of the geometrical conditions includes three different models: ball-end mill, end mill, and custom tool geometry, which are described in detail in Chapter 5. The input data differ for each of the models. The output is, however, the same. The ball-end mill and end-mill models are purely analytical and require tool geometry, process and discretization parameters as input. The custom tool geometry model, on the other hand, is the most appropriate for a coupled simulation and requires additionally the macro engagement conditions. The information flow in this module is described in Figure 6.2.

6.2.2 Module for analysis of the process forces

The model used for calculating the process forces is described in Chapter 4, whereas its integration with the geometrical model is described in Chapter 5. This module has a high performance, due to the low complexity of the algorithms used. It requires the chip micro-geometry, the force coefficients, and the geometry, as well as the position and the orientation of the cutting edge in each step for calculating the process forces. The algorithms used for calculating the process forces are selected by the user, which allows a comparison of the accuracy of different models, such as Kienzle and Altintas. The regression model presented in Chapter 4 is also integrated into the force module. Figure 6.3 describes the information flow in this module.

6.2.3 Tool geometry module

The tool geometry module determines the position of the cutting edge taking into consideration the desired tool geometry and helix angle. It calculates the tool geometry analytically for end
and ball end-mill. For a custom tool geometry, it interpolates the input slices for increasing the simulation accuracy. The input and output variables of this module are described in Figure 6.4.

![Figure 6.4: Information flow in the tool geometry module](image)

**6.2.4 Additional modules**

Four additional modules are used in the simulation. A detailed documentation of these modules can be found in Appendix A.

**GUI**
The graphical user interface enables the stand-alone operation of the software. It is the only module that is coupled to all other modules, functioning as a bus controller and managing all callbacks. The input errors are also administered in this module. A log file with the input error is written in the destination path.

**Import / Export**
The import and export modules are used for importing user input and export the simulation results as text files and graphics.

**Analysis**
A separate module enables the evaluation of the simulation results. This module requires only the exported text files for operating. The targeted results are plotted, and the chip geometry can be reconstructed into a 3D diagram based on the position and size of the individual chip elements.

**Path parameters**
This module summarizes the matrix and vector output for each step, yielding only one value for each parameter, enabling the evaluation of the engagement conditions along the tool path, which is essential for identifying weak spots. The force progression along the tool rotation in a particular step is for example summed up as maximum and average forces. The same is executed for the other calculated parameters.

**6.2.5 Software and user interface**

The system disposes of a graphical user interface, which was used for generating the results in this work. The individual modules can also be compiled into a library for integration in other systems. Matlab offers several options for integration in other systems, such as Excel Add-in,
Hadoop, C/C++, Java, .NET etc [IGLE06]. MEX- and p-files can also be used for a black box integration. More details about the interface of the individual modules can be found in Appendix A.

The user interface of the simulation system is described in Figure 6.5. Tabs can be used for inputting the strategy, tool, process and analysis parameters. The interface also allows importing data from the simulation of the macro contact conditions. An explaining active image is displayed with further details about each input field. The calculated data are exported as .csv files.

An additional user interface was developed for analyzing the simulation results (Figure 6.6). This interface reads the exported .csv files and calculates all the parameters described in Chapter 5. The parameters are then plotted and exported as images for a fast visualization and assessment of the engagement situation.
6.3 Evaluation of the software tool

For further development and implementation of the developed software in other simulation scenarios, a qualitative assessment of the software modules were performed. Not only users of the stand-alone version of the software were taken into consideration in the system assessment, but also further developers who would implement the individual modules into a different system. The software interface plays, therefore, a more important role in the evaluation than the graphical user interface. Four factors were considered in the assessment:

- Performance depends on the computational effort to perform a given simulation. A high performance indicates short computation times. It is highly related to the algorithmic complexity.

- Testability is the degree to which each module allows testing for a given test scenario. If the testability is high, then finding and managing errors by testing is easier.

- Reusability is the capacity of code reuse inside the software and integrating into other simulation systems. It is highly linked to the software cohesion.

- Understandability is a measure of how comprehensible a software is for users. It involves aspects of navigation, procedures, terminology and application structure. The code documentation is also essential for the understandability.

The performance of the individual modules was evaluated using the Big-O notation [VRAJ14]. The algorithmic complexity of each module is described in Figure 6.1. The Big-O notation uses a function for describing the algorithm’s worst case performance by evaluating its asymptotic behavior. The function is written within parentheses after a capital letter “O”. For example, \( O(N^{2}) \) implies a calculation's runtime (or memory utilization) increase as the square of the number of inputs \( N \). By doubling \( N \), the expected runtime and memory usage are 4 times the initial value.

Three variables play a major role in the system performance: the number of steps \((n_s)\), the number of tool layers \((n_l)\) and the number of angular steps \((n_{as})\), whereas the number of tool layers has the most influence on the system performance. The module for calculation of the geometrical conditions has the highest complexity and therefore requires more memory and longer computation time than the others.

Table 6.1 shows the results of the evaluation. The module with the best overall assessment is the tool module. It is used for calculating and interpolating the tool geometry, calculating local quantities such as local helix angle, curvature, and radius. The geometry module, on the other side, had a lower performance due to its high complexity. The calculation times for one point in the tool path ranges from a few seconds to a few minutes, depending on the number of slices used. More than 90% of the simulation time is spent on this module. A lower tool discretization can reduce the simulation time. The accuracy is however also reduced. A compromise must be made to balance the accuracy and simulation times.
6.4 Conclusions on the development of the simulation system

Fazit zur Entwicklung des Simulationssystems

A simulation system was developed to test and provide quick access to the developed models, which was used to generate all the results presented in this work. The system was built with a view to integrating into other simulations. A major challenge is the definition of the data interface, since no standard exists in the field of analytical process simulation.

A high potential for integration with systems for macroscopic simulation of the engagement conditions can be pointed out. This integration would allow the evaluation of the microscopic contact conditions on selected regions of the tool path. By choosing appropriate thresholds based on technological observations, it is possible to avoid unfavorable engagement situations and to accomplish a parameter-based process design.

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Table 6.1  
Assessment of the individual modules
Wertung der individuellen Modulen

<table>
<thead>
<tr>
<th>Module</th>
<th>Performance</th>
<th>Testability</th>
<th>Reusability</th>
<th>Understandability</th>
</tr>
</thead>
<tbody>
<tr>
<td>GUI Module</td>
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<td>Geometry</td>
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<tr>
<td>Analysis</td>
<td></td>
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</tbody>
</table>

Legend:
- Poor
- Very good
- not applicable
7 Model verification and case study
Modellverifizierung und Fallstudie

A model for prediction of the process forces was developed in Chapter 4. Through analytical abstractions, this model was coupled to a geometrical representation of the uncut chip geometry for an arbitrary tool and process strategy, which was presented in Chapter 5. Chapter 6 presented the development of a simulation system by the integration and implementation of these models into a modular system. A test case was designed to assess to which extent the developed system can be used to evaluate the process, which is presented in this chapter.

The test case deals with finishing operations with ball-end mills. Experiments were performed with different tool orientations and the process forces were measured. The forces were compared to simulation results, and the geometrical parameters were used for describing the observed phenomena. The objective was to test the model with complex contact conditions, stressing, therefore, the models capabilities.

7.1 Experimental setup
Versuchsaufbau

Milling experiments were conducted on a Heller model MC25 with two-fluted 6 mm diameter ball-end mills. The machine disposes of a horizontal spindle with a HSK63 interface, which has a maximum speed of 16,000 rpm and a maximum power of 30 kW. Figure 7.1 illustrates the experimental setup used.

Figure 7.1: Experimental setup
Experimeteller Aufbau

The material used in the experiments was extracted from the same block used in the orthogonal cutting experiments described in Chapter 4: X38CrMoV5-1 hardened at HRC52. Also, the geometry of the ball-end mills used corresponds to the geometry employed in the analogous investigation. The tool had a cutting edge radius $r_E = 5 \, \mu m$, a rake angle $\gamma = 6^\circ$ and a clearance angle $\alpha = 6^\circ$, which lies within the investigated range in the orthogonal cutting experiments. The material position in relation to the C-axis was C- for all tests. Therefore, for the condition C+, the results needed to be mirrored.
The experiments were conducted with a constant step over a\(_{e,n} = 0.1\) mm and depth of cut a\(_{p,n} = 0.1\) mm. Three different values of feed rate were used: f\(_z = 0.01\) mm, 0.03 mm and 0.05 mm. The resulting maximum chip thicknesses lied within the range studied in Chapter 4 for most tool orientations used. The objective was to keep the macroscopic form of the chip constant for each feed rate and investigate the influence of the orientation on the resulting forces. In this way, only the influence of the microscopic chip geometry on the process forces and other geometrical parameters was evaluated, stressing the importance of considering this conditions during the process design. It was then possible to verify if the geometrical model proposed sufficiently describes the process forces in complex engagement situations. The tool orientation model is described in Figure 2.14. The supination angle \(\theta_N\) varied within the range 15 – 75° while the azimuth angle \(\psi_F\) lied between 20° and 340°. The spindle speed was held constant at 7000 rpm, which resulted in a nominal cutting speed of 132 m/min. The local cutting speed, nevertheless, varied according to the orientation used.

In each experiment, a straight path of the tool was generated, and the forces were measured with a Kistler dynamometer type 9257A, which was coupled to the workpiece fixture. The azimuth angle was held fixed while the supination angle varied in 5° steps. This was accomplished by the kinematics of the machine, as depicted in Figure 7.1. The A-axis determined the azimuth angle, which is defined as the projection of the tool axis on the FC-plane. Since the spindle is horizontal, to vary the supination angle it was necessary to rotate only the B-axis of the machine, which moves the workpiece in an axis orthogonal to the tool axis and also orthogonal to its projection on the FC plane. Thus, it was possible to precisely define the tool orientation regarding the surface reference system FCN.

7.2 Comparison between the simulated and experimental forces

Since the forces were measured on the workpiece and the machined surface was parallel to the XY-plane of the Kistler dynamometer used, a conversion to the FCN coordinate system was directly accomplished. Also, the feed force could be directly extracted from the measured forces, corresponding to the Y-axis of the force measurement platform. A transformation of the forces to the tool coordinate system, TCS, was, however, necessary. This transformation was accomplished by using the inverse of the rotation matrix presented in Equation 2.3, in which the values of the azimuth \(\psi_F\) and supination angles \(\theta_N\) correspond to the orientation angles used in the experiments.

The engagement situation studied is associated with low cutting forces, which are usually between 5 and 65 N for the studied material, tool and process parameters used. In this range, vibrations and the dynamics of the machine presented to be a significant disturbance effect. For this reason, it is very inaccurate to synchronize the simulated forces in the TCS-system with the measurements, since the determination of the point in which the cut begins in the measured force signal is very imprecise. Thus, only the magnitude of average and maximum forces was used in the evaluation. For the process design, nevertheless, these are the most relevant parameters.
Figure 7.2 shows the volume and area of the chip in the tested range. As illustrated, the magnitude of these quantities is nearly constant for all investigated parameters, demonstrating that the basic macroscopic form of the chip does not vary within the investigated parameter range. A light deviation can be observed for small values of the supination and azimuth angles. This deviation is intrinsic to the mathematical model used for describing the tool geometry. The chip in this orientation range is formed close to the tool tip, a region where the curvature angle $\kappa$ is extremely low. Since the volume is calculated by using a discrete model, numerical errors may influence the results. These are more significant when the value of $\kappa$ is closer to zero, thus leading to a different volume in comparison with other engagement situations.

The maximum forces were simulated and experimentally determined in three different components: feed direction, axial direction and on tool cross-sectional plane, here referred to as XY-plane, since it corresponds of the XY-axis of the TCS. The force coefficients were determined by using the force model presented in Chapter 4.

An important aspect of the simulation was the determination of the passive edge force coefficient from the orthogonal cutting data, i.e. the passive force parcel when the uncut chip thickness tends to zero. The value of this coefficient is very small, and no consensus exists in the literature as how it is determined from orthogonal cutting data. The value of $k_{p,e}$ was then assumed as proportional to the cutting coefficient of the cutting force and the helix angles. This is an adaptation of the model proposed by Armarego and Deshpande [ARMA93].

$$k_{p,e} = A_{pe} \cdot k_{c,c} \cdot \sin \lambda$$  \hspace{1cm} Equation 7.1

The value of the constant $A_{pe}$ was then determined empirically from the collected data as 0.05 for the conditions tested. This value was held constant in all simulations performed in the studied test case. Figure 7.3 shows a comparison between the experimental and simulated feed force for a feed rate $f_z = 0.05$ mm.
Model verification and case study

In this figure, the forces are plotted as a function of the supination ($\theta_N$) and azimuth ($\psi_F$) angles. A higher supination angle corresponds to a higher position of the chip in relation to the tool tip, thus resulting in a higher cutting speed, while the azimuth determines the position of the tool axis in relation to the feed direction and thus changes the orientation of the resulting forces in the cutting edge in relation to the surface coordinate system. This relationship is not necessarily valid for other tool geometries rather than ball-end mills and must be evaluated in each simulated case.

From this figure, it is possible to state that the developed model presents a high predictability of the behavior of the feed force. The feed force is greater for $\psi_F = 90^\circ$ and $\psi_F = 270^\circ$ and present a light decay with increasing $\theta_N$. The high amplitude of the feed force at this values of azimuth can be explained by the resulting uncut chip geometry. The average uncut chip cross-sectional area and local cutting speed are presented in Figure 7.4.

A correspondence between the chip cross-sectional area, shown in Figure 7.4, and the feed forces can be then established. Although the average chip cross-sectional area is higher at $\psi_F = 270^\circ$, a higher value of the feed force is observed at $\psi_F = 270^\circ$. This effect is explained by the low cutting speed ratio resulting from this tool orientation, as also depicted in Figure 7.4.
The model developed in Chapter 4 considers the local cutting speed in the determination of the force coefficients. A low cutting speed corresponds to higher cutting coefficients for the same chip geometry, thus leading to higher forces. This effect is related to the thermal softening that is associated with the higher temperatures at the chip formation zone, which are characteristic of higher cutting speeds and the associated high deformation rates [KLOC08].

Both the experimental and simulated feed forces are lowest at \( \psi_F = 225/20^\circ \) and \( \theta_N = 75^\circ \). The small peak at \( \psi_F = 180^\circ \) is related to the orientation of the cutting normal and passive forces in this condition. The engagement condition for this tool orientation results in a chip close to the X-axis of the TCS, which is in this case aligned with the feed vector, thus yielding a higher component in the feed direction, which in turn results in a higher value of the expected feed force.

The simulated forces were consistently higher than the experimental measurements. The average error in the predictions was 15%. This error is associated with the projection error of a curved volume on a plane. As described in Chapter 5, the chip is considered as a rectangular block which is cut by an angled cutting edge, with an inclination equal to the helix angle \( \lambda \) and a curvature equal to the angle \( \kappa \). This abstraction has an intrinsic overestimation error and the resulting forces are therefore overestimated. The bigger the tool radius and the closer to the tool shank is the chip, the smaller is this error.

Similar behavior is observed on the axial forces, presented in Figure 7.5. The influence of the cutting speed is even more evident in this force component, what can be seen from the steep decrease of the force magnitudes with increasing local cutting speed. The uncut chip geometry is also depicted in this figure. The images of the uncut chip geometry are represented as a projection of the chip on the cutting edge and are all on the same scale. The vertical length represents the variation of the curvature angle along the chip while the horizontal length shows the rotation angle. The colors represent the uncut chip thickness in each chip element.

**Figure 7.5:** Maximum axial forces (\( f_z = 0.05 \) mm): simulated (left) and experimental (right)

Maximale axiale Kräfte (\( f_z = 0.05 \) mm): simuliert (links) und experimentell (rechts)
The cutting edge can be interpreted as a vertical line that sweeps the chip from lower to higher values of the rotation angle, i.e. from left to right in the images. It is here clear to see that the cutting process is equivalent to down milling for \( \psi_f = 270^\circ \) (hsp goes from high to low) and up milling for \( \psi_f = 90^\circ \) (hsp goes from low to high).

Both conditions, however, result in approximately equal maximum force values in the cross-sectional plane, as depicted in Figure 7.6. It is also noticeable how the projection of the chip is bigger for lower values of \( \theta_h \). This results from the smaller spherical cap which is used in the material removal in this condition. Since the chip macroscopic form remains unaltered, the closer the chip is to the tool tip, the higher is the portion it englobes in the rotational direction of the tool. As a result, the cutting time increases for this engagement situations, as depicted in Figure 7.7.

The cutting time here illustrated represents the contact time of the cutting edge in one tool revolution. A higher contact time may be positive, since in high helical tool, the superposition of cutting edges on the work material increases with it. The cutting time may, however, suggest a stronger tool wear, since the friction path of the cutting edge is longer. Other factors as the local cutting speed and the pressure on the cutting edge, nevertheless, also need to be considered in this assessment.
Figure 7.8 shows two other important parameters for the process design, the maximum pressure on the rake face and the maximum spindle power. As illustrated, the azimuth angle has no significant effect on the maximum average pressure on the cutting edge. The supination angle, on the other side, has a high impact on it. The conditions with a low supination angle have clearly the disadvantage of providing a high pressure on the cutting edge. The chip cross-sectional area doesn’t present a high variation for different values of $\theta_n$, so that the pressure difference is attributed to the elevation of the process forces.

The maximum spindle power is related to the cutting force and follows, therefore, the behavior of the chip cross-sectional area, as described in Chapter 5. The lowest spindle power is achieved at an azimuth angle $\psi_F = 135^\circ$ and a supination angle $\theta_n = 15^\circ$. The low power at this orientation is a result of a balance between the higher forces and the smaller local cutting speed.

Another important aspect to be analyzed is the model behavior with different feed rates. As discussed in chapter 4, the designed model is linear with respect to the uncut chip thickness. Therefore, a variation of the uncut chip thickness, while keeping all other variables constant, will result in one linear behavior of the simulated forces. That is observed in the linear shift of the feed forces in Figure 7.9.

A comparison between the simulated and experimental forces with the feed rate $f_z = 0.05$ mm (Figure 7.3), $f_z = 0.03$ mm and $f_z = 0.01$ mm (Figure 7.9) reveals the model’s capabilities. The overestimation of the proposed model increases slightly from 15% to 20% from $f_z = 0.05$ mm to $f_z = 0.03$ mm, what is attributed to scale effects.

The behavior of the forces is very well predicted for a feed rate of 0.03 mm per tooth and higher. For smaller values, nevertheless, the impact of scale effects on the measured forces starts to appear. The geometrical model, however, is still precise in this range, as demonstrated in Chapter 5. The regression model for determining the force coefficients, on the other side, is not able to depict the process in this range of uncut chip thicknesses. The average chip thickness for a feed rate $f_z = 0.05$ mm is $h_{sp,avg} = 2.145 \, \mu m$, while for is $f_z = 0.01$ mm is $h_{sp,avg} = 0.467 \, \mu m$. The model was built based on uncut chip thickness values between 5 – 20 $\mu m$. It can be therefore concluded that a certain level of extrapolation of the values still yields accurate results.
Figure 7.9: Variation of the simulated and experimental forces with the feed rate

Considering a minimum uncut chip thickness of $h_{sp,min} = 0.3r_p$, as proposed by Xu [XU96], a geometrical analysis of the simulated chip geometry results in different values of the chip volume under $h_{sp,min}$ for the different values of feed rate tested. 13% of the chip volume lies under the minimum uncut chip thickness for $f_z = 0.05$ mm, while these values are 31% and 100% for $f_z = 0.03$ mm and $f_z = 0.01$ mm, respectively.

The scale effects associated with small uncut chip thicknesses can also be observed in a detailed analysis of the generated chips. Figure 7.10 shows scanning electron microscope images of the collected chips for the three values of feed rate used in the tests. For this investigation, the tool orientation was held constant, so only the influence of the feed rate on the chip formation could be determined.

Segmented chips were observed in all tested range, as depicted in the close-up images on the bottom of Figure 7.10. The form of the generated chips, however, differs drastically according to the feed rate. Although the chip formed at $f_z = 0.05$ mm has approximately five times the volume of the chip formed at $f_z = 0.01$ mm, its deformed size is much more compact than the latter and resembles the form of the simulated uncut chip geometry. The degree of deformation increases drastically with a decrease in the feed rate. It can also be observed that the chips formed with lower values of feed rate have gaps in the center and especially on the edges, indicating that part of the material might not have been removed. Further analysis would be required to determine the exact value of the uncut chip thickness for the used combination of tool, material, and process parameters.
7.3 Conclusions on the model verification

A test case was designed to stress the capabilities of the developed geometrical and force models. Finishing operations with ball-end mills were performed with different cutting tool orientations, keeping the macroscopic chip geometry constant during the process. Thus, only the influence of the microscopic uncut chip geometry on the cutting forces was investigated. The process forces were measured and compared with simulation results performed with the system presented in Chapter 6.

The developed models demonstrated to have a high predicting capability of the behavior of the different force components. The simulated forces were, however, consistently higher than the experimental results. This difference is attributed to the intrinsic distortion related to the projection of the chip’s curved surface on a plane. The deviation is more prominent in situations where the cut is performed closer to the tool's tip. The average overestimation errors oscillated in a range between 15% and 20% in the tested conditions.

An analysis of the generated chips suggests the existence of a minimum uncut chip thickness, as also indicated in other studies [XU96, QUIT13, ARNT13, WYEN11]. Also, the degree of deformation for small values of feed rate was considerably higher, as observed in scanning
electron microscope images. The simulations results start slightly to diverge from the measurements at low values of feed rate, indicating the presence of scale effects.

As also investigated by Arntz [ARNT13], the tool orientation has a high impact on the uncut chip thickness. This impact was also observed both on the simulated and measured forces, as well as on the calculated parameters. Based on different technological and economic criteria, optimum ranges for the technological parameters can be derived.
8 Summary and outlook
Zusammenfassung und Ausblick

Background
The current global conjecture urges the need for increasing the efficiency of production processes. Machining operations, as being responsible for a great deal of the added value of all mechanical components, are thus under the constant need for optimization. Most machining operations fail in achieving the process potential due to a lack of basic understanding about the process fundamentals still during the process planning. The development and integration of science-based models into the early phases of the process design is therefore of great importance.

Milling operations of free-form shapes with complex milling tool geometries and process strategies are among the machining processes with the highest untapped potential. Studies have shown that, in most cases, unfavorable conditions are used due to the lack of fundamental knowledge about the contact conditions between milling tool and work material. Current approaches for process design and optimization are based mostly on the macroscopic contact conditions. The microscopic contact conditions, however, are ultimately responsible for the process output. An optimization of these conditions would enable the avoidance of unfavorable contact situations and thus contribute to a significant increase in the overall process performance.

Objectives and approach
Against this background, the present work aimed the development of an advanced geometrical model for calculation of the microscopic contact conditions and prediction the process forces in multi-axis milling. In order to achieve this goal, it was necessary to investigate and understand the factors that influence the process forces and to use analytical abstractions to calculate the uncut chip geometry and derive the cutting forces.

The process was investigated with a focus on finishing operations, in which the small contact conditions are even harder to determine, due to the higher level of complexity associated with the process. In this type of operation, the cutting edge geometry plays an important role, since the amplitude of the removed uncut chip thickness often lies in the same order of magnitude of the cutting edge radius. A model which allows the prediction of the process forces with consideration of the cutting edge geometry and process parameters was then proposed.

A geometrical model was built based on the premise that the macroscopic contact conditions are known, which is supported by current research. Based on this conditions, a universal model for calculation of the uncut chip geometry for an arbitrary tool geometry and process strategy was developed. The geometrical model was coupled to the force model and adapted to the multi-axis milling kinematics, allowing thus the prediction of the process forces in different coordinate systems.

To verify the proposed models, a simulation system was developed and implemented, which was used for simulating a test case which was in turn verified against experimental data.
**Core findings**

The core findings of this work can be summarized as follows:

- Experimental investigations in orthogonal cutting allowed the development of a regression model, which enhanced and extended the predictability of the force coefficients for different tool geometries and process parameters. The force coefficients could be then determined as a function of the cutting speed, the uncut chip thickness and width, the cutting edge radius and the rake and clearance angles.

- It was shown how analytical abstractions can be used to convert the forces determined in orthogonal cutting to the oblique cutting.

- Experimental data indicated that the ratio between the cutting and edge force coefficients $k_c/k_e$ determines whether the process forces follow the behavior of the uncut chip thickness or width. The influence of the uncut chip thickness on the process forces for conditions in which the chip thickness lies in the same order of magnitude of the cutting edge radius was proven to be secondary.

- Based on the macroscopic contact conditions, it is possible to determine the uncut chip geometry for an arbitrary tool and process strategy. A universal model for achieving this was presented.

- An integration of the developed geometrical model with a force model allowed the prediction of the process forces in multi-axis milling operations with a high level of accuracy. Errors associated with the intrinsic deformation related to the projection of a curved volume in a plane may cause an overestimation of the simulated forces. The higher the level of curvature of the uncut chip, the higher are the overestimation errors.

- Several technological parameters were derived from a combination of the geometrical conditions and calculated process forces, which can be used in the process design.

- Scale effects were evident for low ratios of $h_{sp}/r_i$. To consider these effects in the force model, orthogonal cutting experiments need to be performed in the evaluated range. The proposed force model is therefore highly dependent on the input data and is valid only in its validation region.

Finally, the above-mentioned core findings were used to validate the investigated research hypothesis:

> If the macroscopic contact situation as well as the tool geometry, material and process dependent parameters are known, it is possible to calculate the undeformed microscopic chip geometry and to predict the process forces in multi-axis milling operations.

The undeformed microscopic chip geometry can be determined for an arbitrary tool geometry and process strategy. The accuracy of the calculated geometry is nevertheless highly dependent on the discretization used. The process forces can be calculated for the valid range of the input data supplied to the model.
Applicability and outlook

The models developed for the determination of the geometrical conditions and forces in multi-axis milling operations pave the way for a parameter-based process design. The calculated parameters allow a deeper understanding of the process and are therefore useful for process planners and designers of CAM software.

To enhance the models’ applicability, they need to be integrated into other simulation scenarios. The generated data serves thus as input for other simulations, such as a machine simulation or a system for optimization of the tool path. Special attention is given to integration with models for calculation of the macroscopic contact conditions in milling. In the context of a coupled simulation scenario, the need for standard interfaces arises.

Further experimental investigations could be performed to correlate the calculated parameters with observed physical phenomena. This would allow the determination of thresholds that can be used for designing the milling of a specific material or also for the development of optimized milling tool geometries and process parameters.

The need for extending available machining databases becomes more relevant for a better exploitation of the developed models. This could be accomplished by standardized methodologies for process design, a subject of current research. The basic uncut chip geometry for different complex engagement situations is a requirement for building up these databases and can be determined by using the analytical abstractions presented in this work.

Finally, the developed models could be extended for considering other factors, such as vibrations, material inhomogeneity and tool deflections, what would increase the process understanding.
Zusammenfassung und Ausblick

**Hintergrund**


**Ziele und Ansatz**
Vor diesem Hintergrund verfolgte die vorliegende Arbeit das Ziel, ein fortgeschrittenes geometrisches Modell zur Berechnung der mikroskopischen Kontaktbedingungen und zur Vorhersage der Prozesskräfte bei Mehrachsfräsen zu entwickeln. Um dieses Ziel zu erreichen war es notwendig, die Faktoren, welche die Prozesskräfte beeinflussen, zu untersuchen, sowie analytische Abstraktionen zur Berechnung der Spanungsgeometrie und der Schnittkräfte zu verwenden.


Basierend auf der Prämisse, dass die makroskopischen Kontaktbedingungen bekannt sind, wurde ein geometrisches Modell entwickelt. Auf der Grundlage dieser Bedingungen wurde ein universelles Modell für die Berechnung der Spanungsgeometrie für beliebige Werkzeuggeometrien und Prozessstrategien ausgearbeitet. Das geometrische Modell wurde an das Kraftmodell gekoppelt und auf die Kinematik des MehrachsfräSENS angepasst, so dass die Vorhersage der Prozesskräfte in unterschiedlichen Koordinatensystemen ermöglicht wird.
Um die vorgeschlagenen Modelle zu verifizieren, wurde ein Simulationssystem entwickelt und implementiert, welches für die Simulation eines Testfalls verwendet wurde, der wiederum gegen Versuchsdaten verifiziert wurde.

**Kernergebnisse**

Die Kernergebnisse dieser Arbeit lassen sich wie folgt zusammenfassen:


- Es wurde gezeigt, wie analytische Abstraktionen verwendet werden können, um die Kräfte von Orthogonal- zu Schrägschnitt zu konvertieren.

- Experimentelle Daten zeigten, dass das Verhältnis zwischen der Scheid- und der Edgekoeffizienten kic/kie bestimmt, ob die Prozesskräfte dem Verhalten der Spanungsdicke oder -breite entsprechen. Der Einfluss der Spanungsdicke auf die Prozesskräfte bei Bedingungen, bei denen die Spanungsdicke in der gleichen Größenordnung wie der Schneidkantenradius liegt, wurde als sekundär nachgewiesen.

- Auf der Grundlage der makroskopischen Kontaktbedingungen ist es möglich, die Spanungsgeometrie für eine beliebige Werkzeug- und Prozessstrategie zu bestimmen. Ein universelles Modell für die Erreichung dieses Ziels wurde vorgestellt.


- Mehrere technologische Parameter wurden aus einer Kombination aus den geometrischen Bedingungen und den berechneten Prozesskräften abgeleitet, welche in dem Verfahrensdesign verwendet werden können.


Schließlich wurden die oben genannten Kernaussagen verwendet, um die untersuchte Forschungshypothese zu prüfen:
Sind die makroskopischen Kontaktbedingungen sowie die Werkzeuggeometrie und die material- und prozessabhängige Parameter bekannt, ist es möglich, die Spanungsgeometrie zu berechnen und die Prozesskräfte beim Mehrachsfräsen vorherzusagen.


**Anwendbarkeit und Ausblick**


Um die Anwendbarkeit der Modelle zu verbessern, müssen sie in andere Simulationsszenarien integriert werden. Die erzeugten Daten dienen somit als Eingabe für weitere Simulationen, beispielsweise für eine Maschinensimulation oder für ein System zur Optimierung der Werkzeugbahn. Besondere Aufmerksamkeit wird auf die Integration mit Modellen für die Berechnung der makroskopischen Kontaktbedingungen beim Fräsen gerichtet. Im Kontext eines gekoppelten Simulationsszenarios wird die Notwendigkeit für Standardschnittstellen deutlich.

Weitere experimentelle Untersuchungen könnten durchgeführt werden, um die berechneten Parameter mit beobachteten physikalischen Phänomenen zu korrelieren. Dies würde die Bestimmung von Grenzwerten ermöglichen, die für die Gestaltung der Fräsprozesse von spezifischen Materialien oder auch für die Entwicklung optimierter Fräswerkzeuggeometrien und Prozessparameter verwendet werden könnten.

Der Bedarf nach Erweiterung der verfügbaren Zerspandatenbanken erweist sich als sehr wichtig für eine bessere Verwertung der entwickelten Modelle. Dies könnte durch eine standardisierte Methodologie zur Prozessauslegung erreicht werden, was Gegenstand aktueller Forschung darstellt. Die grundlegende Spanungsgeometrie für verschiedene komplexe Kontaktbedingungen ist eine Voraussetzung für den Aufbau dieser Datenbanken und kann mit Hilfe der analytischen Abstraktionen, welche in dieser Arbeit dargestellt wurden, bestimmt werden.

Schließlich könnten die entwickelten Modelle für die Berücksichtigung anderer Faktoren wie Vibrationen, Materialinhomogenität und Werkzeugdurchbiegungen erweitert werden, was zu einer Erhöhung des Prozessverständnisses beitragen würde.
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[DIN6582] Norm DIN 6582 (1988-02). Begriffe der Zerspantechnik; Ergänzende Be griffe am Werkzeug, am Schneidkeil und an der Schneide, German Institute for Standardization, Berlin


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Figure A.1: UML Diagram of the simulation system

UML-Diagramm des Simulationssystems
Figure A.2: User diagram of the simulation system

Benutzerdiagramm des Simulatinssystems
Figure A.3: Force module

Figure A.4: Tool module
Figure A.5: Geometry and path modules

Geometrie- und Werkzeugbahnmodul