

**Modeling of Five-axis Ball-End Milling
Process for Freeform Surfaces**

by

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This is to certify that I have examined this copy of a doctoral thesis by

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TO MY WIFE SAMIRA AND MY DAUGHTER ROSHANAK

ABSTRACT

Five-axis milling of freeform geometries is challenging and costly due to the complexities of the machining operation. Understanding the mechanics of the cutting process helps the process engineers select the best cutting parameters in order to increase the productivity. The main aim of this research thesis is to shed light on the mechanics of five-axis milling operation for complex workpieces such as blades, impellers and blisks in order to increase the efficiency of the process. The contribution of this research is introducing a cutting force model based on a novel cutter-workpiece engagement and a new cutting force coefficient identification approach. Based on the developed cutting force model, the efficiency of the process is improved by scheduling the feedrate, modeling the tool deflection, identifying the tool orientation and modelling the surface generation.

The effect of lead and tilt angles on cutting force, torque, deflection and surface quality in five-axis ball-end milling of flexible parts is thoroughly investigated. Based on that, a new strategy is proposed to select the appropriate tool orientation to avoid suboptimal products. The novelty of the proposed strategy lies in presenting a mechanics-based reference map that suggests the appropriate tool orientation considering above-mentioned parameters. The effectiveness of the approach is demonstrated experimentally.

ÖZET

Serbest yüzey geometrilerinin 5 eksenli frezelenmesi, operasyonun karmaşıklığı sebebiyle zorlayıcı ve maliyeti yüksektir. Kesme işleminin mekaniğinin anlaşılması, üretim verimliliğini arttırmak için gerekli olan en iyi kesme parametrelerini seçme konusunda proses mühendislerine yardımcı olur. Bu araştırma tezinin temel amacı, `blade`, `impellar` ve `blisk` gibi karmaşık geometriye sahip iş parçalarının 5 eksenli frezeleme işlemi mekaniğine ışık tutup bu sürecin verimliliğini arttırmaktır. Bu araştırmanın literatüre katkısı, takım-iş parçası temasını esas alarak yenilikçi bir kesici kuvvet modeli ve yeni bir kesme kuvveti katsayısı belirleme yöntemidir. Kesme işlemi verimliliği, geliştirilen kesme kuvveti modeline dayanarak, ilerleme hızı planlanması, takım eğilmesinin modellenmesi, takım oryantasyonunun tanımlanması ve yüzey oluşumun modellenmesi yapılarak arttırılmıştır.

Eksen ve eğim açılarının, esnek parçaların 5 eksenle yarı küresel uç ile frezeleme işleminde, kesme kuvveti, tork, eğilme ve yüzey kalitesi üzerindeki etkisi incelenmiştir. Buna dayanarak, standart altı ürünlerden kaçınmak için uygun takım oryantasyonu seçmeye yönelik yeni bir strateji önerilmiştir. Önerilen stratejinin yeniliği yukarıdaki parametreleri göz önüne alarak uygun takım oryantasyonu öneren mekanik temelli bir referans haritası sunmasıdır. Bu yaklaşımın etkinliği deneysel olarak gösterilmiştir.

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TABLE OF CONTENTS

TO MY WIFE SAMIRA AND MY DAUGHTER ROSHANAK.....	i
ABSTRACT.....	ii
ÖZET	iii
ACKNOWLEDGEMENTS	iv
TABLE OF CONTENTS	v
LIST OF TABLES.....	ix
LIST OF FIGURES	x
NOMENCLATURE.....	xviii
Chapter 1 INTRODUCTION	21
1.1 Overview	21
1.2 The advantageous of five-axis machining	23
1.3 Research Objectives.....	26
Chapter 2 LITERATURE REVIEW	31
2.1 Overview	31
2.2 Cutter-Workpiece Engagement (CWE) Prediction.....	31
2.3 Identification of Cutting Force Coefficients in five-axis Ball-End Milling	35
2.4 Five-axis Ball-end Milling Force Modeling	37
2.5 Feedrate Scheduling.....	40
2.6 Tool deflection in five-axis ball-end milling	41
2.7 Effects of tool orientation on five-axis ball-end milling.....	43
2.8 Surface generation mechanism in five-axis ball-end milling operation	46
Chapter 3 CUTTER-WORKPIECE ENGAGEMENT (CWE) MODEL	50

3.1 Overview	50
3.2 Overview of the proposed approach	51
3.3 Setup module of CWE model	53
3.4 Parasolid.....	57
3.5 Loop module of CWE model.....	58
3.6 CWE engagement map	61
3.7 Updating the workpiece	68
3.8 Prediction of engagement 2D and 3D maps	70
3.9 Summary	73
Chapter 4 CUTTING FORCE COEFFOCIENTS	74
4.1 Overview	74
4.2 Identification of cutting coefficients from orthogonal to oblique transformation method.....	76
4.3 Experimental setup for identification of orthogonal cutting force coefficients	77
4.4 Identification of cutting coefficients from ball-end milling calibration test.....	85
4.5 Experimental setup for calibration.....	89
4.6 Results and discussion	91
4.7 Summary	98
Chapter 5 MECHANICS OF FIVE-AXIS BALL-END MILLING OPERATION	99
5.1 Overview	99
5.2 Geometry of ball-end mill tool	100
5.3 Geometry of five-axis milling.....	105
5.4 Extracting tool orientation and transformation matrices from CL file	107
5.5 Chip thickness formulation	108
5.6 Experimental validation	111

5.7 Summary	119
Chapter 6 FORCE BASED FEEDRATE SCHEDULING	121
6.1 Overview	121
6.2 Force based feedrate scheduling method	122
6.3 Simulation and experimental result	123
6.4 Force based Feedrate Scheduling considering constraints of feed drive system	131
6.5 Summary	135
Chapter 7 TOOL DEFLECTION IN FIVE-AXIS MILLING OPERATION	137
7.1 Overview	137
7.2 Theory of tool deflection modeling	138
7.3 Simulation and Validation	142
7.4 Summary	149
Chapter 8 ANALYSIS OF TOOL ORIENTATION FOR FIVE-AXIS BALL-END MILLING	150
8.1 Overview	150
8.2 The effects of tool orientation on cutting force and torque	151
8.3 The effect of tool orientation on five-axis ball-end milling of Ti6Al4V	153
8.4 The effect of tool orientation on five-axis ball-end milling of Al 7050	158
8.5 The effect of tool orientation on the surface quality in five-axis ball-end milling of flexible thin parts	165
8.6 Mechanics-based reference maps for tool orientation selection	172
8.7 Validation and discussion	174
8.8 Summary	179

Chapter 9 MODELING OF SURFACE TOPOGRAPHY IN FIVE-AXIS BALL-END MILLING	181
9.1 Overview	181
9.2 Trochoidal motion of the cutter points	182
9.3 Simulation of surface topography	186
9.3.1 Finding machining point cloud	186
9.3.2 Finding the cross section of the machined surface	188
9.3.3 Simulation of the machined surface.....	190
9.4 Results and discussion	191
9.5 Summary	205
BIBLIOGRAPHY	207
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LIST OF TABLES

Table 4-1 – Cutting conditions in orthogonal cutting test.	77
Table 4-2 – Measured parameters for rake angle of 15 [deg.] and cutting speed of [m/min].	80
Table 4-3 – Orthogonal calibration results	83
Table 4-4 – Cutting coefficients for 12[mm] carbide ball-end mill tool form Orthogonal to oblique transformation test.	85
Table 4-5 - Cutting conditions	90
Table 4-6 – Comparison of the RMSD and R-square of two approaches.	97
Table 5-1 – Measured phase angle.....	102
Table 5-2 – Cutting conditions for machining of the blisk.....	112
Table 5-3 – Cutting conditions for machining of the airfoil geometry.....	117
Table 6-1 – Cutting Parameters	126
Table 7-1- Cutting conditions for simulation of tool deflection.....	142
Table 7-2 – Inputs for the FEM analysis of tool deflection.	147
Table 8-1 - Cutting Conditions	154
Table 8-2 - Lead and tilt angle pairs used for experimental validation.	159
Table 8-3 Cutting parameters in ball-end milling of the flexible airfoil parts.....	167
Table 8-4 - Cutting parameters in ball-end milling of the flexible airfoil parts.	170
Table 8-5 - Average surface roughness measured with the WLI.....	175
Table 9-1 - Cutting Conditions	192

LIST OF FIGURES

Figure 1-1 – Blades from Pratt &Whitney JT3D, JT8D, JT9D Engine families [2].	24
Figure 1-2 – Samples of rapid prototyping in electronics, biomedical, jewelry and automotive industry [3]......	26
Figure 1-3 – Flowchart of the research on five-axis ball-milling operation.	30
Figure 2-1 – Z-buffer model of the workpiece [10]......	32
Figure 2-2 – Solid modelling method [15].	33
Figure 3-1 – Flowchart of the CWE model.	52
Figure 3-2 – CAD/CAM model for an impeller geometry used in centrifugal pump.	53
Figure 3-3 – CAD/CAM model of a blisk geometry.	53
Figure 3-4 – Sample toolpath file extracted from NX	54
Figure 3-5 – Sample CL file	55
Figure 3-6 – Reference vectors.	56
Figure 3-7 – Swept volume for one sample toolpath.	60
Figure 3-8 – IPW modelling.	61
Figure 3-9 – Tool-IPW intersection for the centrifugal pump impeller.....	63
Figure 3-10 - Tool-IPW intersection for the blisk geometry.	64
Figure 3-11 – Generated engagement points for the case of centrifugal pump impeller.....	66
Figure 3-12- Engagement points from the view in along with tool axis (for Figure 3-11 case).	66
Figure 3-13 – Definition of start and exit angles.	67
Figure 3-14 – Engagement angles for the sample CL point of the case shown in Figure 3-3.	67
Figure 3-15 – Machined surface for roughing operation of the blisk geometry.	69
Figure 3-16 - IPW for roughing operation of the centrifugal pump impeller up to CL # 436.	69

Figure 3-17 - A sample of engagement output (entrance (En1, En2) and exit (Ex1, Ex2) angles are in degrees).....	70
Figure 3-18 - 2D map of engagement for multiple engagement case.....	71
Figure 3-19- 3D map of engagement projected on the tool in multiple engagement case. .	71
Figure 3-20 – Interface of the developed engagement model.....	72
Figure 4-1 - Engagement in slot cutting ball-end milling.....	75
Figure 4-2 - Illustration of engagement region in a freeform surface ball-end milling.	76
Figure 4-3 – Orthogonal test setup.....	78
Figure 4-4 – Cutting force components in orthogonal cutting.....	79
Figure 4-5 – Measured cutting forces for orthogonal cutting test (Rake Angle of 5 [deg.], Cutting Speed of 170 [m/min], Feedrate of 0.5 [mm/rev]).....	80
Figure 4-6 – Variation of cutting forces by changing the feedrate (Rake Angle of 15 [deg.], Cutting Speed of 15 [m/min]).....	81
Figure 4-7 - Variation of cutting forces by changing the feedrate (Rake Angle of 20 [deg.], Cutting Speed of 170 [m/min]).....	82
Figure 4-8 - Discretized tool geometry and force components for ball-end mill.	84
Figure 4-9 – Experimental Setup for prediction calibration test.....	91
Figure 4-10 - Predicted mechanistic cutting coefficients.	92
Figure 4-11 - Predicted mechanistic edge coefficients.	92
Figure 4-12 - Measured and simulated cutting forces, doc = 0-1 [mm], Up milling, Feed = 1080 [mm/min], half immersion, Spindle Speed = 4500 [rpm].....	94
Figure 4-13 - Measured and simulated cutting forces, doc = 1-2 [mm], Feed = 720 [mm/min], Slot milling, Spindle Speed = 4500 [rpm].	94
Figure 4-14 - Measured and simulated cutting forces, doc = 5-6 [mm], Down milling, Feed = 1080 [mm/min], half immersion, Spindle Speed = 4500 [rpm].	95
Figure 5-1 – Geometry of ball-end mill tool and cutting forces.	101

Figure 5-2 – Experimental setup for helix angle measurement	101
Figure 5-3 – Measured and fitted phase lag.....	103
Figure 5-4 – Cutting flute modeled based on the measured data and Equation (5-2).	104
Figure 5-5 – Comparison between the measured and analytical phase lag.	104
Figure 5-6 - Definition of lead and tilt angles.....	106
Figure 5-7 – CAD/CAM model and toolpathes in five-axis ball-end milling of an impeller geometry.	106
Figure 5-8 – Tool tip position and tool orientation extracted from CL file.	108
Figure 5-9 – Chip thickness model.	109
Figure 5-10 – Experimental setup.....	113
Figure 5-11 – (a) Experimental setup for machining of the impeller geometry, (b) Simulated In-process workpiece (IPW), (c) A sample of simulated 2D engagement map.	114
Figure 5-12 – Simulated and measured cutting forces for the blisk in X direction of FCF.	115
Figure 5-13 - – Simulated and measured cutting forces for the blisk in Y direction of FCF.	115
Figure 5-14 – Simulated and measured cutting forces for the blisk in Z direction of FCF.	116
Figure 5-15 - Experimental setup for machining of an airfoil geometry.	117
Figure 5-16 – Simulated cutting forces for airfoil geometry in X direction.....	118
Figure 5-17 - Simulated cutting forces for airfoil geometry in Y direction.....	118
Figure 5-18 - Simulated cutting forces for airfoil geometry in Z direction.	119
Figure 6-1 - CAM of the experimental workpiece.....	124
Figure 6-2 - five-axis machining center and experimental setup used to validate the feedrate scheduling strategies.	125

Figure 6-3 - Variation of resultant cutting force through toolpath	126
Figure 6-4 Force model validation test for impeller.	127
Figure 6-5 - Scheduled feedrate values for each cutter location.....	128
Figure 6-6 – left: NC code for constant feed, right: modified NC code using the scheduled feedrate.....	129
Figure 6-7 - Measured and simulated cutting forces during constant feedrate and scheduled feedrate cases for the second level of rough milling.....	130
Figure 6-8 – Feedrate Scheduling for the third level of rough milling.	130
Figure 6-9 – Geometry of the setup used for experimental validation, (a) Toolpath #5, (b) Workpiece after finishing operation.	133
Figure 6-10 - Roughing toolpath and kinematic profiles for a sample pass [35], (a) Y-z plane toolpath (path #5), (b) Kinematics profiles (path #5).....	134
Figure 6-11 - Resultant cutting forces and cycle time comparison.	135
Figure 7-1: FEM model of tool deflection.....	139
Figure 7-2: Timoshenko element beam	139
Figure 7-3 - Cutter and workpiece geometry.....	142
Figure 7-4: Modeled engagement for five-axis ball-end milling of airfoil geometry.....	143
Figure 7-5 – Exploded view of the laser displacement sensor fixture.	144
Figure 7-6 – Tool deflection measurement setup.	145
Figure 7-7: Experimental setup for measuring force and tool deflection.	146
Figure 7-8: Simulated and measured cutting forces. (a) Simulated forces in y direction, (b) Simulated forces in x direction, (c) Measured forces in y direction and (d) Measured forces in x direction.	146
Figure 7-9: (a): Deflection in X-direction, (b): Deflection in Y-direction.	148
Figure 7-10: Simulated and measured deflection during the cutting process at the tool tip.	148

Figure 8-1 - Definition of lead and tilt angles.....	151
Figure 8-2 - Tool tip (TT) position with respect to contact point (CC) position.	153
Figure 8-3 - Cutting Forces for Lead = 1.5 [deg] and tilt = 3.7 [deg]	155
Figure 8-4 - Cutting Forces for Lead = 0 [deg] and tilt = 4 [deg]	155
Figure 8-5 - Average resultant cutting force in down milling operation.	156
Figure 8-6 - Average resultant cutting force in up milling operation.	156
Figure 8-7 - Average of cutting force normal to machined surface for down milling.....	157
Figure 8-8 - Average of cutting force normal to machined surface for up milling operation	158
Figure 8-9 – Simulated and measured cutting forces for L5T-5 and L25T0 cases.....	160
Figure 8-10 – (a) normal cutting force and (b) average cutting torque five-axis ball-end milling of Al 7050.....	161
Figure 8-11 – Projection of the spherical part of the tool.	162
Figure 8-12 – Variation of average cutting torque by changing the CC point.	163
Figure 8-13 - Variation of average cutting force in tool axis direction by changing the CC point.	163
Figure 8-14 - Variation of average cutting force perpendicular to tool axis direction by changing the CC point.	164
Figure 8-15 - Variation of average total cutting force by changing the CC point.	164
Figure 8-16 - Tool posture in five-axis ball-end milling of a flexible part.....	166
Figure 8-17 - milling of flexible cantilever workpiece (airfoil blade).....	167
Figure 8-18 - (a) Critical positive tilt angle, (b) Critical negative tilt angle.	169
Figure 8-19 - (a) Lead angle of 25°and Tilt angle of -5°, (b) 3D engagement map for Lead angle of 2°and Tilt angle of 10°.....	171

Figure 8-20 - (a) 2D engagement for Lead angle of 25° and Tilt angle of -5°, (b) 2D engagement for Lead angle of 2° and Tilt angle of 10°, (c) Normal and resultant cutting force measured by a rotary dynamometer.....	172
Figure 8-21 - Mechanics-based tool posture reference maps of part deflection (a) and cutting torque values (b) for the selection of lead-tilt angle combinations in the ball-end milling of the flexible airfoil blade.	174
Figure 8-22 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughed, S-Semifinished, F-Finished) (d) for the cases of A (lead=2°, tilt=10°).	176
Figure 8-23 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughed, S-Semifinished, F-Finished) (d) for the cases of D (lead= -5°, tilt=10°).	177
Figure 8-24 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughed, S-Semifinished, F-Finished) (d) for the cases of H (lead=20°, tilt=10°).	178
Figure 9-1 – Ball-end mill tool geometry and runout definition.	184
Figure 9-2 – Trochoidal motion of cutting flutes for two different height.	185
Figure 9-3 – Trochoidal motion of a point on cutting flute.	185
Figure 9-4 – Schematic view of the intersection between cutting point trajectories and a plane perpendicular to the feed direction.....	187
Figure 9-5 – Point cloud of the intersections between trochoidal trajectories and planes perpendicular to feed vector.....	189
Figure 9-6 – Simulated cross section of the machined surface.....	189
Figure 9-7 – obtained machining cross sections for 25 plane perpendicular to the feed direction (Lead = 30°, Tilt = 0°, Feed = 1 [mm/tooth], Tool Radius = 12 [mm], Runout offset ρ = 0 [mm]).....	190

Figure 9-8 – Simulated machined surface by fitting a surface to the convex hull of intersection points.	191
Figure 9-9– (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0°, Tilt = 0°, Feed = 0.5 [mm/tooth], runout offset $\rho = 0$).	193
Figure 9-10 - (a) Simulated 3D surface topography (b) Measured 2D surface topography using WLI (Lead = 0°, Tilt = 0°, Feed = 0.5 [mm/tooth], runout offset $\rho = 0$).	194
Figure 9-11 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0°, Tilt = 0°, Feed = 0.5 [mm/tooth], Runout offset $\rho = 40$ [μm], Runout angle 90°).	195
Figure 9-12 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 0°, Tilt = 0°, Feed = 0.5 [mm/tooth], $\rho = 20$ [μm]).	196
Figure 9-13 – (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 0$).	197
Figure 9-14 - (a) Simulated 3D surface topography (b) Measured 3D surface topography using WLI (Lead = 0°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 0$).	198
Figure 9-15 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 30°, Tilt = 0°, Feed = 1 [mm/tooth], $\rho = 0$).	199
Figure 9-16 - (a) Simulated 3D surface topography (b) Measured 3D surface topography using WLI (Lead = 30°, Tilt = 0°, Feed = 1 [mm/tooth], $\rho = 0$).	200
Figure 9-17 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 30°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 0$).	201
Figure 9-18 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 30°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 0$).	202
Figure 9-19 - (a) Simulated 2D surface topography (b) Measured surface topography using WLI (Lead = 0°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 40$ [μm], $\lambda = 90^\circ$).	203

Figure 9-20 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 0°, Tilt = 30°, Feed = 1 [mm/tooth], $\rho = 40$ [μm], $\lambda = 90^\circ$).	204
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NOMENCLATURE

$[A]$	Transformation matrix from local coordinate frame to feed coordinate frame
A_c	Chip thickness area
a	Depth of cut
β	Friction Angle
b	Width of cut
ds	Edge length of the cutting flute
dz	Disk thickness
E	Modulus of elasticity
$\bar{F}$	Average cutting force
F_x, F_y, F_z	Cutting Force in X, Y and Z directions
F_r, F_t, F_a	Cutting Force in radial, tangential and zenith directions
F_f	Feed Force
f_t	Feed per tooth
G	shear modulus
h_c	Uncut chip thickness
I	2 nd Moment of area
i_0	Nominal helix angle
$[K]$	Stiffness matrix
K^e	Element stiffness matrix
K_{rc}, K_{tc}, K_{ac}	Cutting Force coefficients in radial, tangential and zenith directions
K_{re}, K_{te}, K_{ae}	Edge Force coefficients in radial, tangential and zenith directions
K_s	Correction factor
l	Lead angle
(l_{crp})	Critical positive lead angle

(l_{crn})	Critical negative lead angle
R	Tool radius
R_a	Average surface roughness (2D)
$\dot{\mathbf{r}}_{\max}$	Axis velocity limit of the feed drive system
$\ddot{\mathbf{r}}_{\max}$	Axis acceleration limit of the feed drive system
$\dddot{\mathbf{r}}_{\max}$	Axis jerk limit of the feed drive system
S_a	Average surface roughness (3D)
sh	Scalloped height
so	Step over
$[T]$	Transformation matrix
TT	Tool tip position
t	Tilt angle
(t_{crn})	Critical negative tilt angle
(t_{crp})	Critical positive tilt angle
V	Cutting Speed
δ	Workpiece deflection
θ	Inclination Angle
θ_{ex}	Engagement exit angle
θ_j	Angular position of the reference flute
θ_p	Pitch angle
θ_{st}	Engagement entrance angle
κ	Zenith angle
ν	Poisson ratio
ψ	Local helix angle

φ	Screw angle
ρ	Runout offset
λ	Runout angle

Chapter 1

INTRODUCTION

1.1 Overview

Being at the beginning of the industrial supply chain, machine tools are significantly important in the competitive global manufacturing marketplace. Well-designed machine tools play a key role in boasting the productivity on the shop floors by reducing the manufacturing cycle time and efficient use of material and energy. According to the “World Machine-Tool Output & Consumption Survey” [1], the global machine tool consumption in 2014 was \$75.3 billion. Based on this report, the machine tool purchasing trends evidently show that the strongest manufacturing countries invest more on the latest machine tool technology. This is due to the fact that machine tools have a hand in manufacturing of every products in a wide range of industries such as biomedical engineering, nanotechnology, aerospace, automotive, home appliances, and so forth.

Despite all the improvements in machine tools technology, there is still room for increasing the efficiency and productivity in all types of metal cutting processes such as turning, milling, grinding, drilling and so forth. The machining cycle time, wasted material and consumed energy will be reduced by selecting optimum cutting parameters. This, in turn, leads to higher productivity and sustainable manufacturing. By comprehensive modeling of the geometry and the mechanics of the machine tools and machining operations, it is possible to understand the complexity of the metal cutting process for numerous types of machine tools and tooling systems. The modeled geometry and mechanics of the machining operations can be integrated in a virtual machining system, which is the best solution to find optimum cutting parameters to have a productive and sustainable metal cutting operation. The main aim of virtual machining system is to

understand and optimize the behavior of manufacturing systems prior to physical production of the final product. In the other word, the goal is to produce the first part correctly based on the demanded features in the shortest time, lowest cost and the most sustainable way. Since the complexity of the products is increasing in the hi-tech industries and daily products such as automotive, electronics and home appliances, the machine tools industries can no longer afford the time-consuming and expensive manufacturing and testing of physical prototypes based on trial and errors.

Most of the current virtual machining systems are geometry-base, wherein the cutting parameters and tool orientation are determined only to avoid the risk of collusion between the tool and the workpiece setup. However, in order have higher productivity and better surface quality, the mechanics of the machining operations needs to be modelled and integrated to the virtual machining system. In order to achieve this goal, comprehensive static and dynamic models are needed to be developed for the machining operations to predict the cutting forces, cutting torque, tool/workpiece vibration, dimensional error and surface quality parameters prior to the real machining operation.

Among the different types of machining, five-axis milling is widely being used in the manufacturing of freeform complex parts for the customers in high-tech industries such as aerospace, marine, automotive, power generation, die-mold, biomedical and tool making, where there is a high demand for tight manufacturing tolerances, dimensional integrity and surface quality. The term of “five-axis” refers to the number of degrees of freedom in the machining process. On a five-axis milling center, tool translates across the X, Y and Z directions, as well as rotates around two axis to approach the workpiece. Moving the tool in this way dramatically increases the tool access to almost all features and facets of the workpiece.

1.2 The advantageous of five-axis machining

The major advantage of using five-axis machining is the ability of manufacturing complex geometries in a single setup. This advantage increases the machining productivity in contrast to the other machining operations that needs a series of set ups and fixtures. This fact, in turn, leads to significant reducing the manufacturing time and cost and increasing the productivity in shopping floors. Furthermore, mounting and moving the workpiece several times during the using of multiple setups and fixtures may lead to misalignments and scraping the expensive materials. Figure 1-1 illustrates some complex part geometries from aerospace industry. The demanded tolerances for manufacturing of those kinds of parts are extremely tight and almost impossible to be achieved using other types of machining operations.



Figure 1-1 – Blades from Pratt &Whitney JT3D, JT8D, JT9D Engine families [2].

Moreover, five-axis machining provides the ability of using shorter cutting tools due to the tilting and leading capability of the spindle with respect to the workpiece. Thus, high speed machining becomes possible due to the less vibration in the machining setup. This also reduces the risk of tool fracture and leads to increasing the productivity of the machining operation. Lower vibration of the tool also provides better surface quality and lower dimensional error. This eradicates the needs of extra finishing and super finishing operations after milling operation.

Using five-axis milling operation, it is possible to manufacture extremely complex geometries that otherwise have to be cast. Especially for prototyping, R&D and biomedical purposes, five-axis machining is the fastest and chipset solution. Otherwise, for each single part, a set of die and mold needs to be manufactured, which is costly and time consuming. Figure 1-2 illustrates some example of rapid prototyping in automotive, biomechanics, jewelry, and electronics industry.

Five-axis machining also provides extremely high time saving in drilling holes for the 5-sided workpieces. Since there is no need for moving the part and mount it again on the machine table, higher accuracy and tight tolerance of drilling operations is possible using five-axis machining centers.

Five-axis machining provides large number of machining possibilities regarding to the size and shape of the parts. Overall, the benefits of using five-axis milling can be outlined as the following:

- Machining of complex shapes in a single setup leading to increased productivity
- Better accessibility of tool to all hard to reach facets of the workpiece
- Save in time and money because of less need for design and manufacturing especial fixtures
- Higher accuracy and less dimensional error because the workpiece does not need to be mounted and dismounted multiple times
- Capability to use shorter tools to suppress chatter and avoid tool chipping and tool breakage
- Having superior surface finishes and better part quality

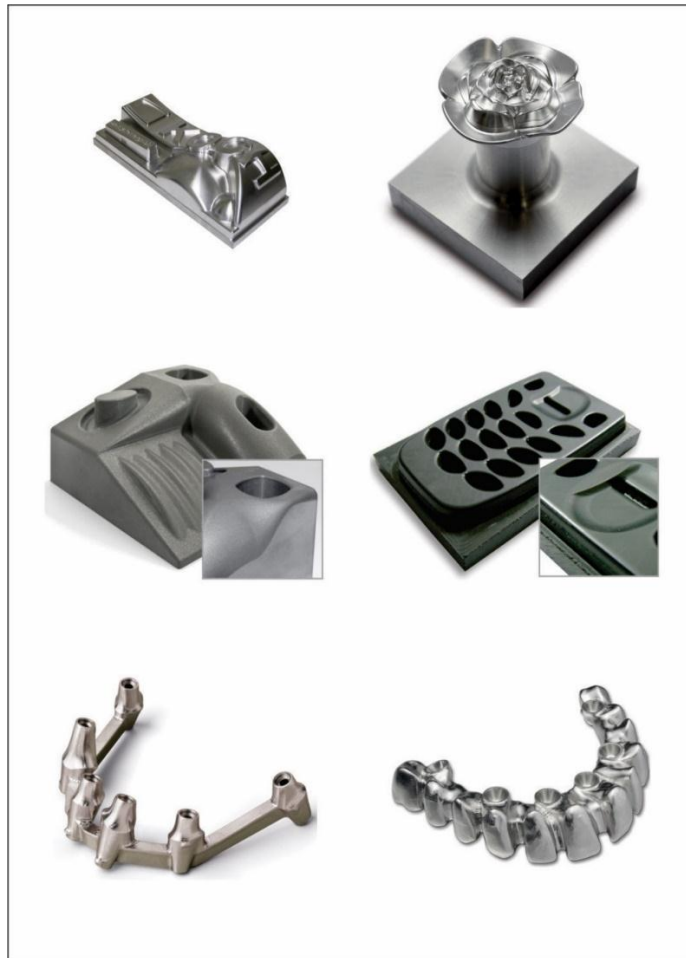


Figure 1-2 – Samples of rapid prototyping in electronics, biomedical, jewelry and automotive industry [3].

1.3 Research Objectives

In order to fully exploit the potential of five-axis milling, comprehensive geometrical and mechanical analysis are necessary. The main aim of using five-axis milling cannot be achieved without modeling the effects of cutting parameters on the process and machining under optimum cutting conditions. There are many factors that affect the cutting performance in five-axis milling, such as cutting forces, dynamic characteristics of the

machine tool and workpiece, stability, tool/workpiece deflection, limitations of the feed drive control of the machine, smoothness of toolpath, etc. Because the parts must be generally machined within tight tolerances in five-axis milling, excessive cutting force and vibration may cause tool failure and jeopardizing the dimensional error and the quality of the machined surface. Any damage to part and tool during the machining of especial parts in high-tech industries means scrapping the whole machining setup which is costly and inefficient. In order to prevent such incidents, the machining process must be virtually simulated to determine appropriate cutting parameters.

Different workpiece materials are used in industries because of the wide-ranging needs of mechanical, thermal, bio compatibility and other properties. Titanium alloys and Inconel are being commonly used in aerospace industries for manufacturing of impellers, blades, and blisks in jet engines because of the capability of keeping high strength and durability in elevated temperatures. Aluminum alloys are preferred by the car manufacturer and aerospace industries to be used in frame structures due to having high strength-to-density ratio. Titanium alloys and hydroxyapatite bio-ceramic are commonly used in biomedical industries due to their lightness, bio compatibility and corrosion resistance. The machining of all above mentioned materials is challenging due to their mechanical properties, thermal conductivity, and high chemical reactivity with many cutting tool materials. Considering the poor machinability of those materials, in order to increase the productivity and machining quality, a comprehensive mechanical simulation of the machining process is needed to predict the best cutting parameters such as feed, spindle speed, tool posture, etc.

Currently, some commercial software packages are capable of five-axis milling simulation in virtual environment [4], [5]. However, these packages are mostly geometrical based that can be used for verification of numerical control (NC) code to detect collision. In the current state of the art of CAM technology, there is a high demand for mechanical models capable of calculating useful quantities such as cutting forces, tool stress/deflection,

chip thickness, tool/workpiece engagement and cutting torque / power. They should also be capable of determining the best cutting parameters to increase the productivity, considering the constraints of the process such as cutting force, cutting torque, tool/workpiece deflection, vibration and surface quality.

In the lack of available comprehensive mechanical model that considers above mentioned factors, cutting parameters must be determined based and the try and error methods which are expensive and time consuming in today's competitive machining technology. Moreover, most of the time there is a trade-off between surface quality, dimensional error, generated cutting force and torque during the machining processes[6]. In order to define the best cutting parameters, the cutting mechanism in five-axis milling must be studied in details and the effects of cutting parameters on the productivity and the quality of the machined part must be determined. Then, the best cutting strategy should be selected based on the mechanical simulation of the process. This method can be integrated as a complementary approach to the current geometry-based CAM technology to fully exploit the potential of five-axis milling operation.

This research aims to fill this gap by providing algorithms for mechanical-based simulation of five-axis ball-end milling operation to investigate the effects of cutting parameters on the performance of the process. Figure 1-3 illustrates the overall flow chart of the research. According to the proposed approach, generated G-code from any commercial CAD/CAM package is analyzed to extract feed-rate, cutter location (CL) and the posture of the cutter at each block of the NC code. In the next step, the cutter workpiece engagement domain is simulated using CL point and tool posture information. Feedrate and engagement map and experimentally measured cutting coefficients are utilized to model the cutting force at each CL point. Afterwards, the proposed algorithms estimate scheduled feed-rate, work piece deflection, tool deflection, force waveforms and surface roughness. According to the virtually simulated results, the G-code is improved and used for

machining in the real five-axis ball-end milling setup. The provided algorithms can be used as a complementary module to the current geometry-based CAM packages to meet with the demands of the industry. The thesis is organized as is shown in Figure 1-3.

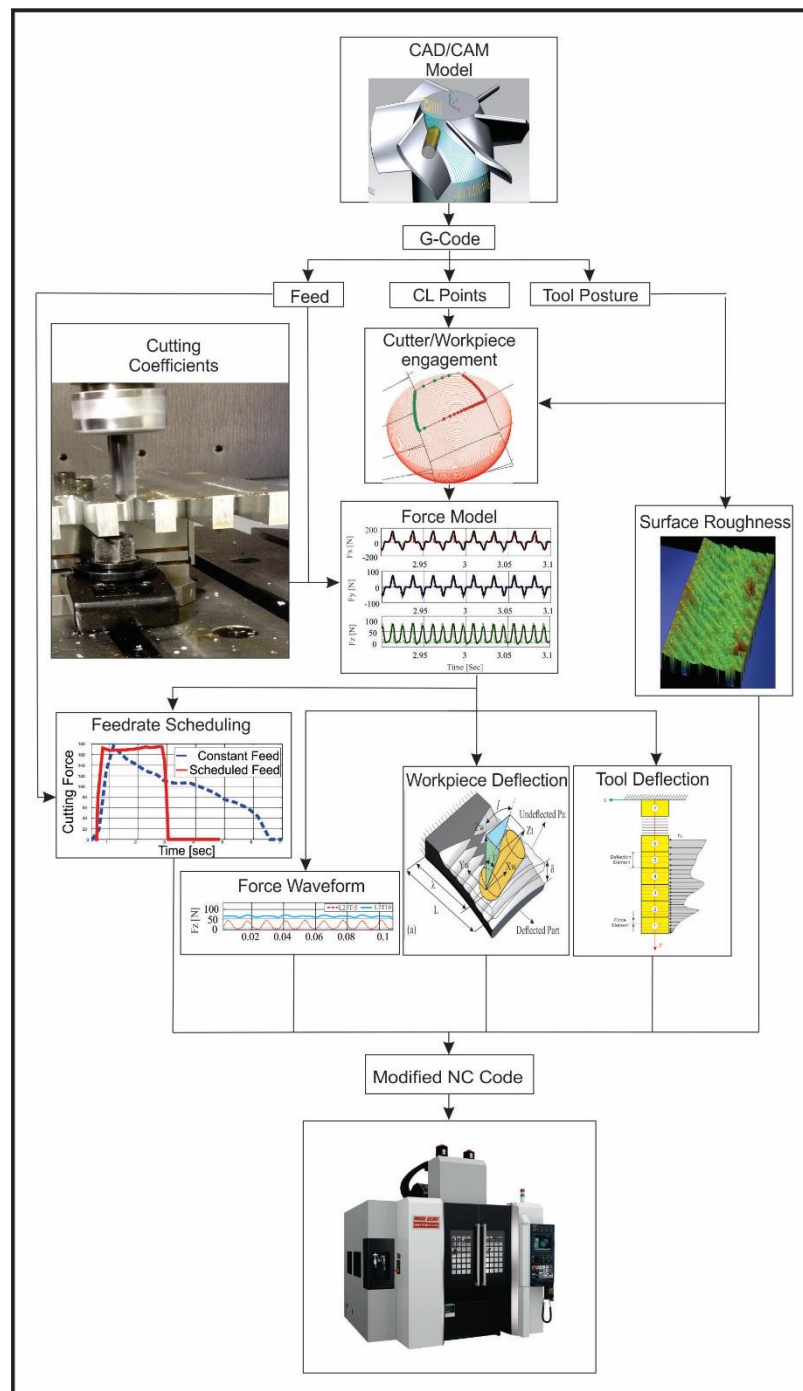


Figure 1-3 – Flowchart of the research on five-axis ball-milling operation.

Chapter 2

LITERATURE REVIEW

2.1 Overview

This thesis aims to virtually simulate the process mechanics of five-axis ball-end milling of freeform surfaces. This simulation work includes determination of the tool/workpiece engagement as a geometry problem. By using the geometric information of tool/workpiece engagement as a boundary condition, cutting force, cutting torque, tool deflection, workpiece deflection and waveforms of cutting force and torque are simulated as a mechanical problem. The simulated data, validated by experimental tests is used to find the best cutting parameters improving the efficiency of the machining operation in terms of time cycling, surface quality, feed drive constraints of the machine, surface quality, workpiece deflection, and tool deflection. In this chapter, a review of previous research works conducted on cutter-workpiece engagement modeling and mechanical simulation of the five-axis milling of the freeform surfaces are provided.

2.2 Cutter-Workpiece Engagement (CWE) Prediction

Accurate prediction of engagement between tool and workpiece (CWE) at any cutter location (CL) point along the toolpath is desired because it is a critical input for force modelling and any other analysis and optimization of milling processes. Compared to the three-axis milling, in the five-axis milling the engagement region cannot be calculated accurately and easily using analytical methods. This is valid, especially for sculptured and freeform surface milling operations. In such cases, solid modeler based simulations and advanced geometry modeling approaches are required. An extensive number of publications exist about engagement models for different milling operations.

The Z-buffer approach is one of the most common methods of CWE calculation. This method was employed by Chappel [7], Van Hook [8] and Jerard et al. [9] for the first time for simulation and verification of Numerically Controlled (NC) machining programs. In this method, the facets of the workpiece are discretized into some points known as Surface Point Set (SPS). The intersection of the swept envelope (SWE) of a given tool with straight lines emanating from surface points, known as Z-buffer elements (ZDVs) of the workpiece, yields segments which are used to find the CWE area. Fussell et al. [10], Roth et al. [11], Rixin et al. [12] and Kim et al. [13],[14] used this approach to calculate entry/exit angles of the cutting flutes of a given tool as a function of height to estimate cutting forces in machining of freeform surfaces. Figure 2-1 is an illustration of the Z-buffer approach for simulation of in-process workpiece surface for ball-end milling operation. This method has the advantages of short simulation times and reduction in the complexity of the intersection calculation. On the other hand, this method does not provide enough accuracy in calculation of cutter-workpiece contact surface, thus, leading to less accurate cutting force estimation especially for small contacts, multiple engagement and finishing cases.

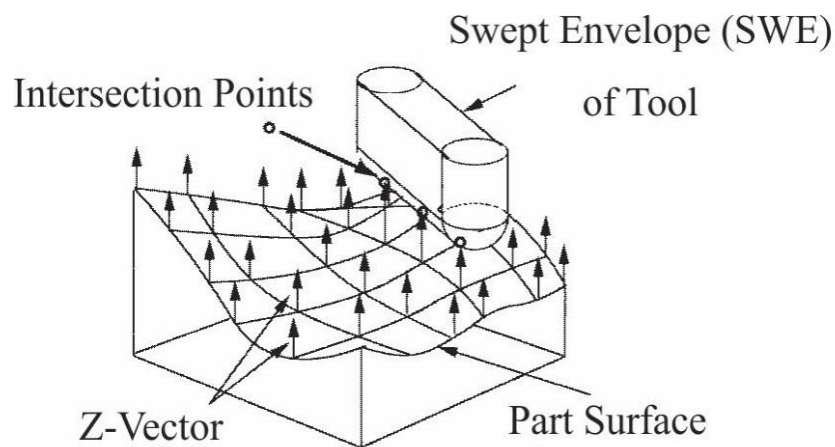


Figure 2-1 – Z-buffer model of the workpiece [10].

In order to increase the accuracy, solid modeling approach can be employed. Despite Z-buffer schemes, which stores discretized format of workpiece facets as a series of Z-vectors, solid modeling methods employ simple entities or features in a child-parent structure. This technique is more accurate than the Z-buffer approach, however, in terms of computation time it is more expensive. Figure 2-2 illustrates the concept of solid modeling method for simulation of in-process workpiece in milling operation.

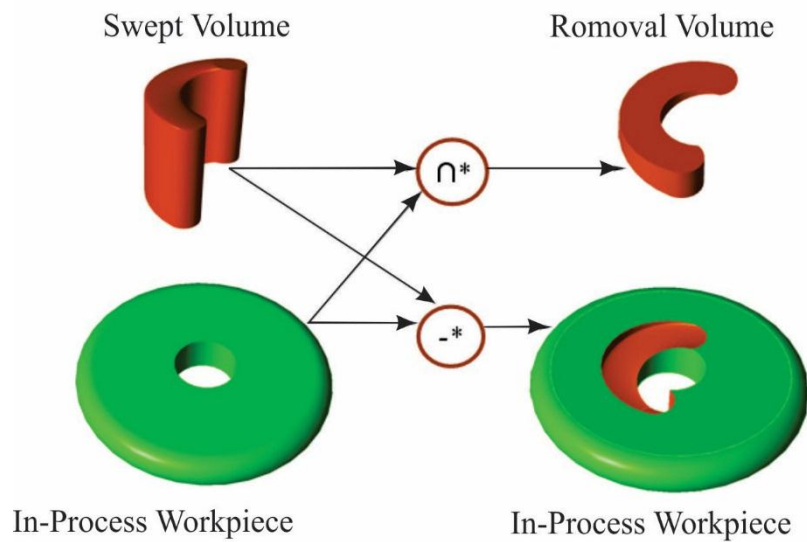


Figure 2-2 – Solid modelling method [15].

Constructive Solid Geometry (CGS) scheme is employed by Spence and El-Mounayri [16], [17], [18] to calculate immersion angles for general part shapes. In this approach the intersection of milling cutter is separately analyzed with primitive blocks and cylinders. Using Boolean operators, individual results are combined to construct the immersion area between the cutter and the workpiece.

Imani [19] used Boundary Representation (B-rep) for freeform surfaces using ACIS which is a 3-D B-rep solid modeler engine to simulate the machining process. In-process

workpiece and immersion angles are calculated using Boolean operations with ACIS in C++ and Scheme. Using this approach, in-cut segment and instantaneous chip thickness can be modelled and utilized in force modeling.

D. Yip-Hoi and X. Huang [20] presented a solid modeling based solution to consider the variation of depth of cut between two consecutive CL point. This research work, extended the concept of features in CAD modelling to support machining process modeling. The cutter-workpiece engagement at each feed step of the end milling was modeled by considering the variation of prismatic stocks. ACIS solid modeler kernel was used in this research.

W. Ferry and D. Yip-Hoi [21] presented a cutter-workpiece engagement prediction model based on the parallel slicing method (PSM) for five-axis flank milling. In this model, semi-discrete solid modeling techniques of the ACIS kernel were used to simulate the swept volume, in-process workpiece and cutter-workpiece intersection. The removal volume was calculated by sweeping the tool along the toolpath. The engagement map was obtained by slicing the swept volume into a number of parallel planes along a given axis, and intersecting of them with the updated workpiece. This model was compared with Z-buffer technique and it was concluded that the results were more accurate.

Lazoglu et al. [22] used another solid modeler kernel known as Parasolid. In this approach immersion angles are identified by utilizing the exact Boolean approach for five-axis ball-end milling process. A mechanistic cutting force model was used to validate the results. This engagement model is capable of determining multiple and low immersion conditions.

Multi-dexel volumes is a discrete representation of solid models that is employed for simulation and analysis of milling and grinding processes. The dexel representation of a solid body consists of a set of line segments inside the solid body. The process also called ray-casting or ray tracing [23], [24]. Muller and Surmann [25] used this idea to simulate the

in-process workpiece in milling operation. Ray-casting and voxel-based geometric representation method is also used by Wou et al. [26] for simulation of cutting forces using a mechanistic model for ball-end milling process.

G-buffer [27], Graf-tree[28] and Gauss map [29], [30] are other special partitioning representation methods that have been used by researchers to simulate the milling operation and identifying the immersion angle between the cutter and the workpiece.

In this thesis, Parasolid kernel, which is a solid modeler engine, is used for the engagement modelling in five-axis ball-end milling operation. The advantage of this approach is using the library of Parasolid in an efficient and robust way to generate accurate engagement map for five-axis milling of freeform and complicated geometries. Since Parasolid is a 3-D solid modeler, it has the advantage of CSG approach that is higher accuracy in predicting the engagement area between the cutter and the workpiece. A parallel slicing method is developed and employed to identify the immersion angles for ball-end mill tool. The proposed method is also capable of modelling multiple engagements that frequently happen in five-axis milling operation. Effectiveness of the proposed strategy is validated with experimental data from five-axis machining center.

2.3 Identification of Cutting Force Coefficients in five-axis Ball-End Milling

The most common model to estimate the cutting forces in the literature is the mechanistic cutting force model in which the cutting force is a linear function of the uncut chip thickness area. Accurate estimation of cutting force coefficients plays a crucial role in accurate prediction of the cutting force and cutting torque. So far, many methods have been developed in the literature aiming to identify the cutting coefficients for ball-end milling operation. The reliability of the force model is highly dependent on the accuracy of the estimated coefficients at each disk.

Budak et al. [31] used an orthogonal database from turning operation to calculate the cutting coefficients for the flat end mill tools. Lee and Altintas [32] extended this work to

come up with cutting constants for the ball-end mill tools. The model proposed by Lee and Altintas is able to predict the cutting forces in roughing processes with acceptable accuracy. However, the machining of freeform surfaces and finishing process are not presented in the paper. Ferry and Altintas [33] used the orthogonal database for titanium Ti_6Al_4V alloy to predict the cutting forces for flank end milling of the impellers.

Ko and Cho [34] proposed a model to estimate the mechanistic cutting coefficients. They introduced instantaneous cutting coefficients for different cutting conditions. They provided an in-depth analysis of the characteristics of these cutting conditions, which can be estimated using a few calibration tests. The size effect is also modeled in this method to increase the accuracy of the identified coefficients.

Lazoglu, Boz and Erdem [22] introduced a cutting force model for five-axis ball-end milling of free from surfaces. They used a calibration method to calculate the cutting constants. This model is used later to optimize the feedrate in ball-end milling of freeform surfaces by Erkorkmaz et al. [35].

Gradisek et al. [36] introduced a general approach to calculate the cutting constants for general end mills using the calibration method. They conducted several milling test with arbitrary radial immersion and constant depth of cut. In this paper, the experimental tests were conducted for constant depth of cut and the contribution of each cutting disk were not measured separately.

Wojciechowski [37] proposed a model for estimation of cutting force coefficients in finishing ball-end milling of inclined surfaces. Run-out, that has dominant effect in finishing operation, has taken into account in this approach. Moreover, the influence of surface inclination, feed per tooth and cutting speed is considered. Using this approach cutting force estimation error is below 16%.

Yao et al. [38] used a chatter free calibration method for determining cutting force coefficients in ball-end milling. In this approach, lead angle of the tool varies to consider

the effect of tool inclination. The non-uniform cutting force coefficients is presented by a dual-cubic polynomial function. The variation of the maximum chip thickness versus the lead angle is established considering tool run-out.

Cao et al. [39] also established a new experimental force coefficient identification approach for ball-end mill finishing considering inclination angle. In this model the immersion angles are modified according to the inclination of the tool. The approach proposed by Gradisek et al. [36] have been employed and modified in this research work.

Grossi et al. [40] investigated the effects of cutting speed on mechanistic cutting coefficients. This model especially is appropriate for high-speed milling. They analyzed a wide range of spindle speeds to show the effect of cutting speed. In order to overcome transducer dynamics issue, dynamometer signal was compensated using an improved Kalman filter method. The run-out is also considered in the proposed approach. Wang et al. [41] considered the effects of cutter vibration and run-out in calculation of chip thickness and identification of cutting force coefficient for five-axis ball-end milling.

In Chapter 4, a new and accurate strategy for estimation of mechanistic cutting coefficients for ball-end milling of freeform surfaces in three- and five-axis operations is presented. Since the cutting coefficients are not constant along the tool axis in the ball part of the cutter, the tool is considered by dividing the ball region into thin disks. The derivations are improved by considering the helix angle and cutting edge length to enhance the accuracy of the estimated cutting coefficients.

2.4 Five-axis Ball-end Milling Force Modeling

In order to simulate and improve the five-axis ball-end milling operation the first step is the accurate prediction of cutting force at each CL point of the toolpath. In contrast to three-axis milling, having two more degrees of freedom constitute complexity in predicting and measuring the cutting forces. As is explained in Chapter 3, the immersion angles

cannot be achieved directly from analytical approaches, and advanced solid modeling methods are necessary.

Cutting forces in three-axis ball-end milling has been investigated by numerous researchers. Two main approaches are available in the literature to model cutting forces namely, *the mechanistic model* and *the orthogonal to oblique transformation model*.

In *the mechanistic cutting force model*, the cutting constants are estimated through calibration test conducted on a specific pair of tool and workpiece [42], [36], [43] and [44]. There are some pros and cons to this approach. Prediction of cutting constants faster and more accurate is the advantage of this method. On the other hand, the predicted coefficients are just valid for the experimental pairs of tool and workpiece. If the geometry or material of cutter-workpiece pairs changes, the calibration tests needs to be conducted again.

Using *orthogonal to oblique transformation model*, the cutting coefficients can be predicted by means of fundamental cutting parameters such as yield shear stress, average friction coefficient on the rake face and shear angle measured from a set of orthogonal cutting tests at various cutting speeds on turning machine [42], [32], [45].

One of the earliest cutting force model for five-axis ball-end milling was introduced by Fussel et al. [10]. They used an extended Z-buffer model of the workpiece to simulate immersion angles. They used a discrete cutting force model using the pressure and friction constants introduced by Yucesan and Altintas [46]. Lazoglu and Liang [47] modeled the cutting forces for inclined ball-end milling of freeform surfaces in the frequency domain using the convolution integration technique. Ozturk and Budak [48] employed and orthogonal to oblique transformation model to simulate cutting forces in five-axis ball-end milling. Mechanics of five-axis flank milling for machining of jet impellers is investigated by Ferry and Altintas [33], [49]. They developed a cutting force model for tapered, helical ball-end mill tool with variable pitch angle and serrated flutes. The horizontal and vertical

feed is updated at each CL point. Engagement is obtained from a semi- discrete solid modeling techniques using ACIS kernel and parallel slicing method (PSM).

Lazoglu and et al. [22] developed a five-axis mechanistic cutting force model for machining of Al7075 by utilizing Parasolid as a solid modeler kernel to identify immersion angles at each CL point. They introduced appropriate transformation matrixes to simulate and measure cutting forces using rotary dynamometer. Taner et al. [50] introduced a model for force prediction in multi axis ball-end milling considering the tool indentation effect. In this model, the indentation of the tool into the workpiece is modeled using the elastic-plastic deformation theory. Wang et al. [41] predicted cutting forces considering vibration and run-out. They considered run-out effect as an extra feed in machining process and updated chip thickness accordingly. They obtained real-time vibration of the cutter by solving the differential equation of motion using the measured modal parameters. The movement of the cutter due to the vibration is considered in chip thickness calculation. Sun and Guo [51] also numerically simulated cutting forces in five-axis milling process considering the run-out effect. They considered the effect of run-out in swept traces of cutting edge. They used calibration method to estimate the cutting constants. This model considered relative motion between tool and workpiece to model the run-out accurately. The proposed model was validated by conducting experimental tests.

In the Chapter 5 of the current research thesis, the cutting forces are predicted by employing a mechanistic cutting force model. The transformation matrixes, needed for simulation and validation of the cutting forces in the coordinate systems attached to rotary type and table type dynamometers, are calculated using the CL file information extracted from the NC code. A novel approach is proposed to estimate the cutting coefficients dedicatedly for five-axis ball-end milling operation. The effect of multi engagement and partial engagement are considered in the proposed approach.

2.5 Feedrate Scheduling

As demands for parts with complex shapes are increasing, the need for enhancing the efficiency and productivity is also increasing. Although five-axis machining is much more efficient in terms of time cycling, there are still more potentials to increase the productivity by optimization of cutting process. One of those potentials is the optimization of feedrate during the cutting process. The feedrate optimization can be done based on material removal rate, cutting force or constraints of feed drive system.

Several investigations have been performed about feedrate scheduling in three-axis milling operation. Erdim et al. [52] performed feedrate scheduling using material removal rate (MRR) and the force based methods for three-axis milling. They compared these two methods and inferred that the force based method is more applicable and reliable rather than MRR method.

Lee, and Cho [53] developed an intelligent off-line feedrate scheduling for three-axis end milling by dividing original blocks of NC codes into smaller ones with the optimized feedrate values. By applying the optimized feed values, peak values of cutting forces were kept below a constant value.

Sun et al. [54] developed a guide spline-based feedrate scheduling method for machining along curvilinear paths with simultaneous constraints of chord errors and ac/deceleration.

Merdol, and Altintas [55] presented a solid modeler approach to simulate the cutting forces in three-axis machining of dies and molds. Using this model, they introduced a constraint-based optimization scheme for feedrate to maximize the material removal rate (MRR) by calculating acceptable feedrate levels.

Altintas and Erkorkmaz [56] introduced an algorithm for feedrate optimization of spline interpolation in high speed machine tools. This algorithm reduces cycle time and keeps axis velocity, torque and jerk below the constraints of the servo drivers with minimal tracking

error. This approach leads to nonzero acceleration and jerk values at segment connections, which in turn leads to continuous motion of the tool. They also presented a quantic spline interpolation with minimal feed fluctuation which results in a smooth and consistent feedrate profile [57]. The same algorithm was employed by Erkorkmaz et al. considering the cutting force during the toolpath as and complementary constrain [35].

A feed optimization method for five-axis CNC machine tools considering the drive constraints was proposed by Sencer et al. [58]. This algorithm reduces machining time by not violating the feed drive constraints of the system for sculptured surface milling. Cubic B-spline are employed to express toolpath in five-axis milling operation. The time optimal feed values are identified by running the algorithm in a loop to maximize the feed along toolpath without violating the constraints of the system.

Feedrate interpolation considering jerk constraints on five-axis toolpath is also studied by Beudaert et al. [59]. This algorithm gives optimized feedrate which makes best use of the kinematical characteristics of the machine. This algorithm can be applied to linear and NURBS toolpath interpolation in three- and five-axis milling.

Considering the constraints of the feed drive system, an enhanced Force model based Feedrate Scheduling (FFS) technique for five-axis ball-end milling parts with complex freeform surfaces are presented in Chapter 6. In the presented approach, the feedrate is modulated to keep resultant cutting forces below a desired threshold without violating the constraints of the feed drive system. The drive constraints are considered by limiting the velocity, acceleration, and jerk magnitudes commanded to each actuator.

2.6 Tool deflection in five-axis ball-end milling

One of the main sources of unwanted dimensional error in milling operations is the tool deflection stem from the periodic cutting forces. Until now, few tool deflection models have been developed for simulation of dimensional error in a complex machining process like five-axis ball-end milling. Most of the models developed in the literatures consider

three-axis ball-end milling. Besides, cutting force has been considered as a vector that is applied on the tip of the tool rather than take it as distributed load acting on cutting edge.

One of the earliest studies on estimation of tool deflection and machining error in ball-end milling process was conducted by Lim and Menq [60]. They used moment area method to calculate the stiffness of the cutter/tool holder system at each point. In their model the instantaneous cutting force is considered as a vector that is applied on the tip of the tool.

Ikua et al. [61], [62] developed a force model and tool deflection system for ball-end milling of cylindrical parts. They suggested the optimum machining strategy to end up with the highest machining precision. The main drawback of their model is that the formulation for chip thickness and deflection error is just for cylindrical part of the tool.

G.M. Kim, B.H. Kim and C.N. Chu [63] suggested a method to calculate the cutting forces based on an empirical approach and using Z-map methodology to determine cutter contact area. Using estimated cutting forces and considering the tool as a cantilever beam, they came up with estimation of tool deflection and dimensional error. In this model, cutting force is considered as a concentrated load rather than a distributed force.

M. Kaymakci and I. Lazoglu [64] developed a mathematical model of cutting forces integrating with a CAM package to simulate tool contact region. They used the modeled cutting forces as an input for their deflection model to simulate the tool deformation. However, they considered the cutting force as concentrated load acting on cutting edge.

Hiroyasu Iwabe [65] et al. introduced a model of surface generation mechanism based on tool deflection and FEM analysis. They used ANSYS to simulate the tool deflection due to cutting forces; nevertheless, cutting forces are changing complicatedly and updating the cutting forces for each CL point as an input for ANSYS is difficult and time consuming.

Rao and Rao [66] proposed a tool deflection compensation strategy for peripheral milling of curved geometry. In this approach, a mechanistic model is employed to simulate the cutting forces. Tool deflection is modelled by considering the tool as a cantilever beam.

In this research work the cutting force is considered as a concentrated load. Soori et al. [67] used this model to develop a virtual machining approach considering dimensional, geometrical and deflection errors in three-axis CNC milling operations.

Zeroudi and Fontaine [68] developed a model for prediction and compensation of tool deflection error in three-axis ball-end milling of freeform surfaces. FEM analysis and analytical method are employed and compared to simulate the cutter deflection error. A realistic tool geometry is used to reduce the simulation error.

In Chapter 7 of this thesis project, Finite Element Method (FEM) method is employed to simulate the deflection of tool during the cutting process. The proposed model is evaluated by conducting some experiments. In this approach, cutting forces are modelled as a distributed load acting on engaging part of cutting edge to increase the accuracy of the simulation. The deflection code is integrated with force model code that leads to convenience and lower computation time.

2.7 Effects of tool orientation on five-axis ball-end milling

In the current state of the art of CAM technology, tool posture is only determined by geometrical analysis. However, in high-performance five-axis milling, not only the geometry, but also the mechanics of the process is critical. Therefore, a new and comprehensive mechanics-based strategy is proposed for selection of tool postures considering process parameters such as cutting force, torque, part vibration, and surface quality in Chapter 8. Effectiveness of the proposed strategy is validated by conducting experiments on five-axis ball-end milling of flexible freeform structures.

Generally, research on tool orientation in ball-end milling process can be classified in the following categories:

- Gouge free tool orientation identification
- Mechanics-based tool orientation identification

Most of the research on the tool orientation is about gouge free tool orientation identification. In this category, the posture of the tool is calculated based on the geometrical concerns to avoid any interfere between tool setup. So far, numerous approaches have been developed to generate feasible and collision free toolpath for five-axis milling.

Traditionally, the orientation of the tool is set to an angle that ranges from 3° to 10° with respect to the normal of the machining surface during tool motion [69]. This method can increase the material removal rate in contrast to three-axis milling, demonstrably. Nevertheless, a trial and error process is required to select the optimal angle.

Another approach is Principle Axis Method (PAM). This method uses curvature alignment between the tool and machining surface. Tool is tilted toward feed direction so that the minimum effective curvature of the tool covers the maximum effective curvature of the surface at each cutter contact (CC) point [70]. This approach may not be feasible due to the fact that the direction of the minimum principal curvature of the surface and the cutting direction cannot always be the same [71].

Configuration space search method (C-space) is the other method that is developed for gouge free tool path generation. In this method, based on the different machining constraints and global smoothing method, the machining configuration space is sought to find the optimal tool orientation for five-axis machining [72]. Toolpath is considered as a sequence of CC points. The tool orientation is sought in a way that minimizes cusp height while avoiding gouging. Although C-space method is effective to find and represent all the feasible tool orientations, expensive and time consuming computation is needed to reach the optimal solution [71].

Rolling Ball Method (RBM) was developed based on the idea of rolling a ball with various radius along the toolpath on the machining surface and fitting the cutting tool inside the ball [73]. This guarantees that there is just one possible CC point and the tool

will not gouge the machining surface. The only requirements for this method is surface coordinate and surface normal that can be extracted from CAD data. In this approach, the interferences of the tool shank and tool holder with workpiece is not considered.

In mechanics-based tool orientation identification, the posture of tool is calculated based on mechanical parameters such as cutting force, cutting torque, dimensional error and surface quality. This category has not been investigated thoroughly because of the complexity of the five-axis ball-end milling operation. H.K. Tönshoff and J. Hernandez-Vamacho are among the earliest researchers who investigated the mechanical effects of tool orientation in five-axis flat and ball-end milling operations [74]. In this research, the effects of lead and tilt angles on flank wear and surface quality are examined. It is concluded that an increase in tilt angle leads to reduced width of flank wear because of distribution of cutting area along the cutting edge in flat milling operation. It can also be deduced from this research work that higher tilt angle is unfavorable because it results in reduced stiffness of system, triggering chatter vibration. According to this paper, advantageous tilt angles for ball-end milling are between -40° and -10° .

J. Kruth and P. Klewais studied the mechanical effects of tool orientation in five-axis milling using cylindrical tool [75]. In this research work, the best results are achieved when applying a small negative lead angle. By using slightly negative lead angle, the stability of machining was increased and the frequency content of the force signal was reduced. However, the difficult chip removal during machining using negative lead angles may cause unfavorable machining conditions. Whereas, positive lead angle causes higher dimensional error. This phenomenon is due to the fact that the stiffness of the tool and tool holder is reduced significantly with higher positive lead angles.

Lacalle et al. proposed a toolpath selection based on the minimum cutter deflection [76]. In this research, cutting force and tool deflection perpendicular to the tool axis are estimated and measured. Different toolpath are examined and the one which significantly

reduced force during machining is selected. Using this approach, dimensional error in five-axis milling is managed to decrease from 40 to 10 micron. However, this approach demands for an exhausting running of the force model and engagement simulation to consider all possible toolpath to find optimum orientation of cutter.

The effects of tool orientation on residual stress was studied by T. Kalvoda and Y.R. Hwang for high speed ball-end milling of low carbon steel [77]. It was shown in this research that the tool orientation can drastically affect the surface integrity and residual stress. According this study, the tilt angle is beneficial and cause compressive magnitude of residual stress. Positive and negative values of lead and tilt angle have the same effect on the residual stress.

One of the latest investigation about tool orientation in five-axis ball-end milling process is conducted by E. Ozturk et al. [78]. According to this study, changes in tool orientation drastically and non-linearly affects the mechanics and dynamics of the process. A mechanistic cutting force model as well as an analytical engagement approach are employed to simulate the effects of tool orientation on generated cutting forces.

The main aim of Chapter 8 is to study the effects of tool orientation on cutting force, torque, deflection and surface quality in five-axis ball-end milling of flexible cantilever-shaped parts such as turbine blades. The strategy is based on the mechanistic modeling of cutting forces that is developed in preliminary studied[79], [80].

2.8 Surface generation mechanism in five-axis ball-end milling operation

Better quality of the machined surface is highly demanded in all types of milling operations, e.g., peripheral milling, face milling and freeform three-and five-axis ball-end milling operation. There are numerous parameters that affect the surface quality of the machined surface. Static and dynamic tool deflection, runout, tool edge wear, cutting parameters and workpiece deflection are the main source of undesired surface quality in all types of milling operation. There is a high quest in machining industries for virtual models

capable of accurate prediction of the surface topography, surface roughness and dimensional error prior to the real machining. The virtual realization of the machined surface, eradicates the try and error methods to achieve desired surface quality, thus leading to reduce the cost of machining. In order to address this demand, some models for the prediction of surface topography have been developed in the literature by scholars and researchers.

Some researchers have considered the effect of tool vibration in determining the machined surface topography of peripheral milling operation. Montgomery and Altintas [81] introduced a model for surface generation in presence of tool vibration. This model proposes a dynamic approach for determining the uncut chip thickness and cutting forces. The kinematics and trochoidal motion of the tool has been considered in peripheral flat end milling operation to model the final machined surface. Ismail et al. [82] proposed a mechanistic model for surface generation mechanism in peripheral milling considering the effects of cutter vibrations, run out and flank wear. The surface roughness parameters are modeled virtually and validated by conducting experimental tests. The ploughing force effect is also considered in modelling the machined surface. Imani and Layegh [83] introduced a comprehensive simulation for surface texture in peripheral end-milling operation. The trochoidal motion of the cutting lip is modeled at each level of the tool. The dynamics of the process and tool run out are taken into account by updating the tool center position at each position. An algorithm is proposed to simulate the machined surface texture based on the instantaneous trajectory of the cutting flutes. Arizmendi et al. [84] presented a model for surface topography prediction in peripheral milling operation considering the tool vibration. The cutting edge path equations are modelled as a polynomial equations and solved for discrete points along the toolpath. Yong and Liu [85] introduced a surface topography prediction model in peripheral milling with variable pitch end mill tool. Tilting, runout, deflection of the tool and the workpiece is included in the

model. They employed a time domain simulation algorithm developed by Peigne et al. [86] to generate the surface topography. The model is validated by conducting experimental tests.

Some other scholars investigated the surface generation mechanism in free form ball-end milling operation. Lim and Menq [60] predicted the dimensional error and surface texture in ball-end milling operation. In this model, the dimensional error caused by tool deflection is also taken into account. The instantaneous tool deflection is modeled at each cutting position to calculate the horizontal position of the generated points on the cutter. The texture superposition method is employed by Kim and Chu[87] for prediction of surface generation mechanism in three- and multi-axis milling operation. This model is capable of modelling surface generation mechanism for ball-end mill, filleted end mill, and flat end mill. The effects of scallop height, runout, and feed marks are modelled. In this approach the trochoidal motion of the cutter has not considered. Arizmandi et al. [88] introduced a model to predict the surface topography generated by ball-end milling operation. In this model the trajectory of the analytical equations of cutting flutes are derived and solve to find the generated points on the final machined surface. The effect of tool runout and cutting parameters are also considered. The trochoidal motion of the tool is finally intersected by planes perpendicular to feed direction leading to modelling the cross section of the machined surface. Multi-axis milling is not considered in this model. Bouzakis et al [89] determined the chip geometry, cutting force and roughness in free form surface milling with ball-end mill tool. The kinematics of multi-axis ball-end milling is considered to propose a surface topography model for finishing operation. The unreformed chip geometry is modelled considering the initial part geometry, NC code, and tool geometry. In this approach, the trochoidal motion of the cutter has not considered.

Despite all above mentioned research works, still there is no analytical model available in the literature for virtual simulation of surface texture in multi-axis milling operation. An

analytical model for prediction of surface topography and realizing the surface generation mechanism is introduced in Chapter 9 of this thesis. The analytical equations of trochoidal motion of the cutting flutes of the ball-end mill tool are derived considering the inclination of the tool and runout. Some planes are defined perpendicular to the feed vector with the constant distances. The intersection between the trochoidal motion of each cutting point and the defined planes, represents generated points on the machined surface. The bigger number of perpendicular planes leads to better resolution of the simulated machined surface texture. By fitting a surface to the generated points, the topography of the machining surface can be simulated. The model is capable of considering the effects of tool orientation, feed, step over, depth of cut, tool orientation, and runout. The performance of the proposed strategy is validated by conducting experimental tests and measuring the surface texture using WLI (White light interferometry). The novelty of the presented method is considering the effects of tool orientation as lead and tilt angles on the machined surface topography.

Chapter 3

CUTTER-WORKPIECE ENGAGEMENT (CWE) MODEL

3.1 Overview

Accurate prediction of engagement between tool and workpiece at any instant along the tool path is desired because it is a critical input for force modelling and any other analysis and optimization of milling processes. In other words, cutter-workpiece engagement (CWE) map or entry/exit angles of cutting flute as a function of tool height, is considered as boundary condition for prediction of the cutting forces. In the proposed mechanistic cutting force model, the differential cutting forces are integrated over the engagement area to come up with the total cutting forces acting on the tool/workpiece setup.

CWE map can be affected by the dynamic displacement of the tool during the cutting process. However, the dynamic displacement effect on CWE map is minimized in the industrial application by avoiding chatter in five-axis ball-end milling operation. Consequently, if the static and dynamic deflection of the tool is negligible, CWE prediction turns to be a pure geometric problem.

As was mentioned in the literature review chapter, an extensive number of publications exist about the CWE calculation for different milling operations. It is possible to classify these works in two main categories:

- Solid modeler kernels based approaches
- Analytical approaches

Compared to the three-axis milling, the engagement region cannot be calculated accurately and easily using analytical methods in the five-axis milling operation. This is valid, especially for sculptured and freeform surface milling operations. In such cases, solid modeler based simulations are required.

In this research, Parasolid is used for the engagement modelling as a solid modeler kernel which is the main core of many advanced commercial CAD/CAM packages such as SolidWorks, NX (Unigraphics), Abaqus, Ansys, MasterCAM and DelCAM. Using Parasolid, the Tool Swept Volume (TSW), In-Process Workpiece (IPW) and the required Boolean operations can be modelled and executed in a loop under C++ or C# frameworks.

The proposed engagement modeler is introduced under two main modules, namely the setup and the loop modules. The loop module is divided into two sections as “intersection modeler” and “IPW modeler”. The advantage of this approach is using the library of Parasolid in an efficient and robust way to generate accurate engagement map for five-axis milling of freeform and complicated geometries. The proposed method is also capable of modelling multiple engagements that frequently happen in five-axis milling operation. Effectiveness of the proposed strategy is validated with experimental data from five-axis machining center in Chapter 5.

3.2 Overview of the proposed approach

The Parasolid based CWE model can be divided into two main sections as is shown in Figure 3-1. The first part of the model is the setup where the model is initiated and the required input for the further operations are prepared. The second part is the loop which iterates through all the cutter location (CL) points to compute IPW and instantaneous engagement area. NX is used to create tool geometry, blank, toolpath and CL file. CWE calculation code is written using the Parasolid functions on Microsoft Visual Studio C# framework with the assist of Enes Yigit who is a PhD candidate in *Manufacturing and Automation Research Center* (MARC) at Koc University [90]. Matlab is also used for processing the output results as a postprocessor.

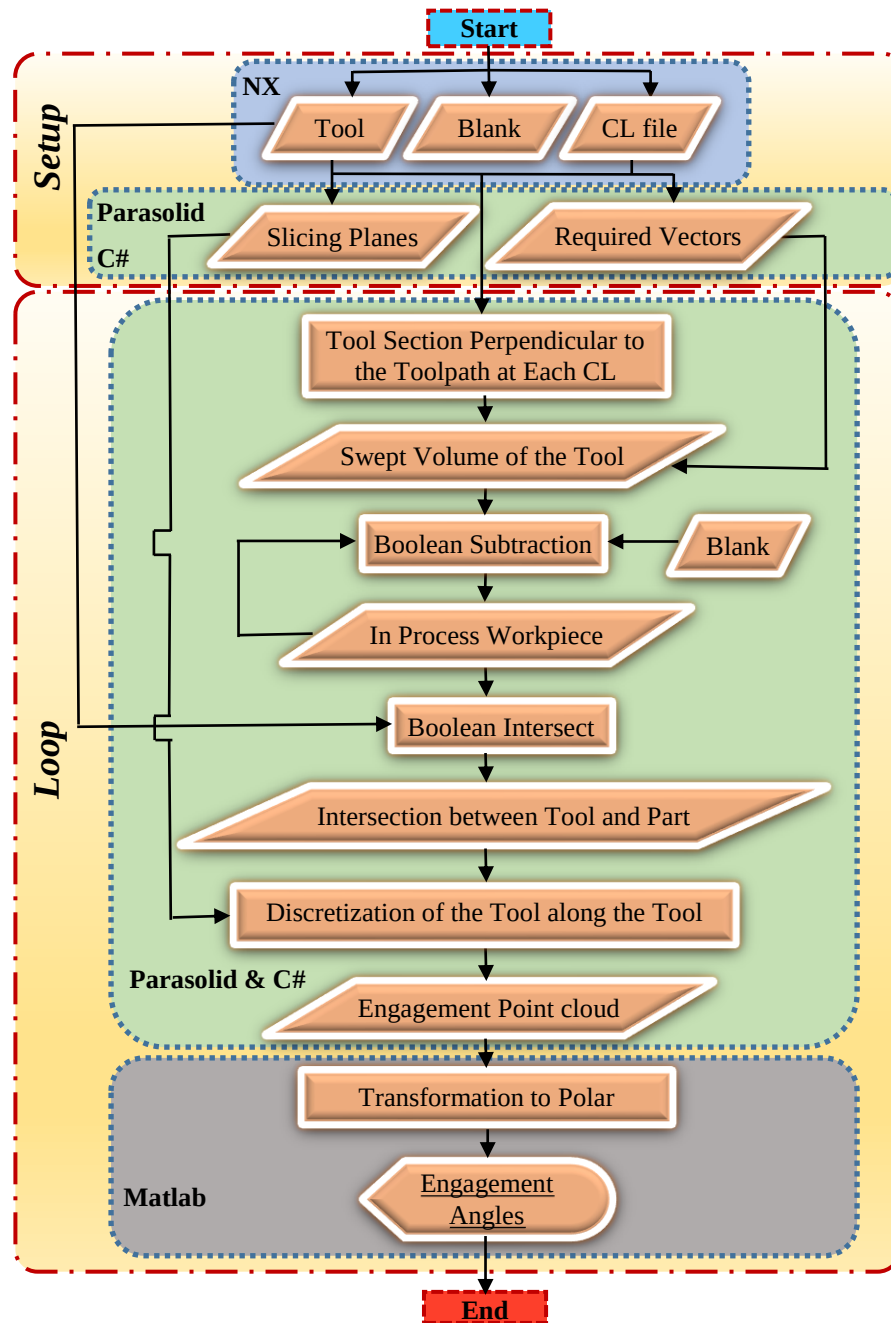


Figure 3-1 – Flowchart of the CWE model.

3.3 Setup module of CWE model

In the setup section of the CWE model, the necessary inputs are prepared in terms of solid blank, tool geometry, CL file and toolpath. NX (Unigraphics) is used to create the required solid models and toolpaths. Any other CAD/CAM package can be used in this stage to extract the toolpath and solid models. Figure 3-2 and Figure 3-3 show the CAD/CAM model of two complex freeform parts using NX.

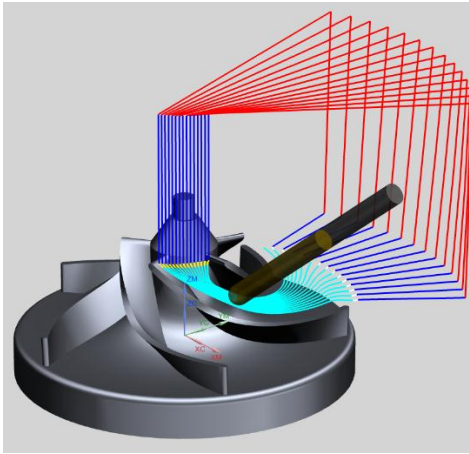


Figure 3-2 – CAD/CAM model for an impeller geometry used in centrifugal pump.

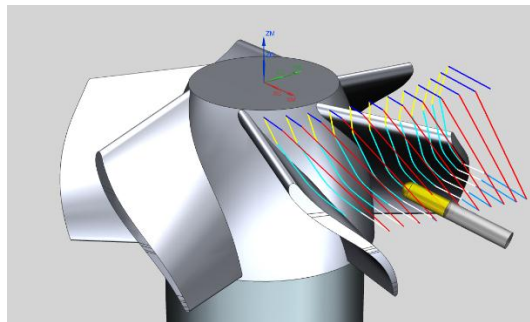


Figure 3-3 – CAD/CAM model of a blisk geometry.

After generation of CAD/CAM model, the toolpath is extracted from NX as a text file. Figure 3-4 is representing a sample toolpath file. Toolpath file contains information about tool position, tool orientation, the color of the tool path, tool geometry, spindle speed, feedrate and other graphical information. The motion commands start with “GOTO” and “RAPID” words. The numbers provide the tool tip coordinates and the orientation of the tool axis.

Using a process information parser module, the toolpath file is converted to a standard CL file. In the standard format of CL file, the tool tip coordinates and the orientation of the tool axis in terms of direct cosine are mentioned at each line for each CL point. The parser module searches through the toolpath file and finds the tool tip position coordinates and the tool axis direct cosines. Then, sort them line by line to create the CL file as an input for the loop section of the CWE model in Figure 3-1. Figure 3-5 presents a generated standard CL file using the parser module.

```

TOOL PATH/MULTI_BLADE_ROUGH, TOOL, BALL_MILL
TLDATA/MILL, 3.0000, 1.5000, 75.0000, 0.0000, 0.0000
MSYS/0.0000, 0.0000, 0.0000, 1.0000000, 0.0000000, 0.0000000, 0.0000000, 1.0000000, 0.0000000
$$ centerline data
PAINT/PATH
PAINT/SPEED, 10
PAINT/COLOR, 186
RAPID
GOTO/7.2513, -5.9440, 36.6650, 0.8603264, 0.0708219, 0.5047997
PAINT/COLOR, 211
RAPID
GOTO/7.2513, -5.9440, 14.8453
PAINT/COLOR, 6
FEDRAT/MMPM, 250.0000
GOTO/7.2429, -5.7838, 14.4881
GOTO/7.3313, -5.6002, 14.1537
PAINT/COLOR, 31
GOTO/7.3336, -5.5969, 14.1483, 0.8602888, 0.0709083, 0.5048517
GOTO/7.4493, -5.4262, 13.8740, 0.8582099, 0.0752899, 0.5077471
GOTO/7.5621, -5.2524, 13.5993, 0.8559543, 0.0796591, 0.5108783

```

Figure 3-4 – Sample toolpath file extracted from NX

X	Y	Z	i	j	k
-13.073	-1.1359	5.9359	-0.31791	0.42236	0.84885
-4.8077	-1.1359	-2.7642	-0.31791	0.42236	0.84885
-4.3462	-1.0311	-2.3294	-0.30944	0.42236	0.85197
-3.8632	-0.9458	-1.9375	-0.30093	0.42236	0.85501
-3.3627	-0.8794	-1.5886	-0.29241	0.42236	0.85797
-2.8442	-0.831	-1.2804	-0.28384	0.42236	0.86084
-2.3113	-0.8001	-1.0134	-0.27526	0.42236	0.86362
-1.7633	-0.7864	-0.7864	-0.26663	0.42236	0.86632
-0.6451	-0.7856	-0.4038	-0.24932	0.42236	0.87147
0.481	-0.7856	-0.0452	-0.23191	0.42236	0.87626

Figure 3-5 – Sample CL file

In order to determine the engagement angles in the loop section of the CWE model, tool needs to be discretized as thin disks perpendicular to the tool axis. This operation is performed using slicing planes in C# environment based on the disk numbers determined by the user in the setup section. Since curvature of the tool changes more rapidly at the tool tip, the heights of the planes, which are used for the slicing operation, are generated exponentially to have more slices at the tip of the tool. The higher number of slices connotes higher accuracy, but it also requires longer calculation time.

There are several vectors, which are used multiple times in the model and are required for different operations such as creating the tool geometry, generating sweep path or displaying relevant directions. These vectors are the sweep direction, sweep reference direction, feed direction, cross feed direction and tool axis direction. The vectors are calculated initially in the setup section at each CL point to avoid additional computations. The vectors for a sample toolpath at CL #n are given in Figure 3-5. Sweep direction is the sweep path between consecutive CL points. This vector is perpendicular to the cross section of tool at each CL point starting from the center of section and is needed for “PK_BODY_make_swept_body_2” function of Parasolid to create the swept solid body. Sweep reference vector is perpendicular to tool axis and sweep direction at each CL point and is required for cross section calculation in the CWE code. Tool axis vector is calculated at each CL point by knowing the position and orientation of the tool from CL file. Feed

direction vector is a unit vector collinear with the feed direction of the tool at the tool-tip. Cross-feed vector is a vector perpendicular to the tool axis and feed vector. This vector is used as the reference direction for predicting of the engagement angles.

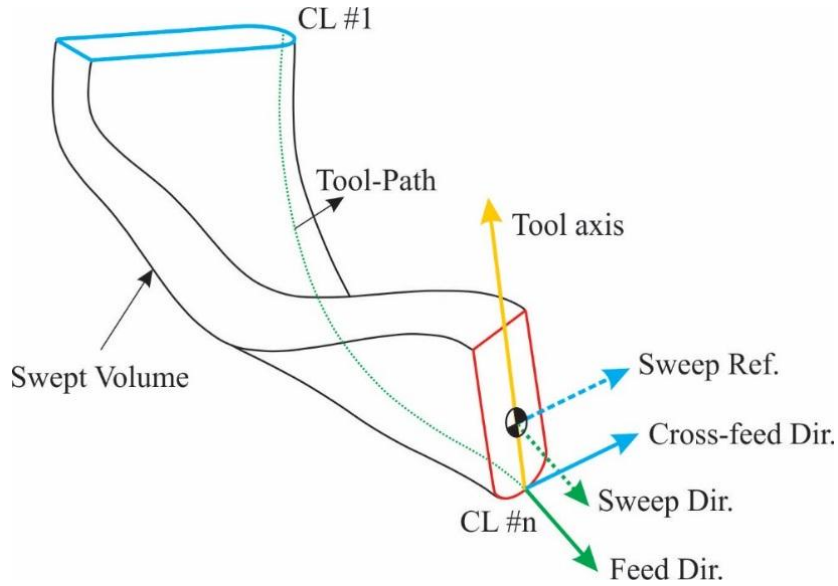


Figure 3-6 – Reference vectors.

Before the loop is initiated, the last step is to create the tool in Parasolid. The geometry of the ball-end mill is modelled in Parasolid by creating and uniting the spherical part and the cylindrical part of the tool. “Create solid sphere” function of Parasolid requires radius, location, tool axis and cross feed direction as reference direction. Since each CL point represents the position of the tooltip, they are shifted as much as the radius of the tool in the normal direction (tool axis direction) to find the center of the spherical part of the tool. The tool location coordinate can be found using the following equations;

$$X_{t_i} = X_i + r * I_i \quad (3-1)$$

$$Y_{t_i} = Y_i + r * J_i \quad (3-2)$$

$$Z_{t_i} = Z_i + r * K_i \quad (3-3)$$

Above, X_i , Y_i and Z_i are the coordinates of the current CL point. I_i , J_i and K_i are the cosine directions of the tool axis that can be extracted from the CL file. Next, the cylinder is created, starting at the center of the sphere and extruding towards the tool axis reference direction. The cylinder requires the same parameters as the sphere as well as height. The two solid bodies are united to form a single tool body using “PK_BODY_boolean_2” function in Parasolid.

3.4 Parasolid

Parasolid is the world’s leading 3D solid modeler kernel, providing fundamental functionalities that enable rapid and robust solid modeling tools for geometric modeling and mechanical analysis of complex products in industries. Parasolid is based on high-precision boundary-representation (B-rep) technology. Developed in 1988 by ShapeData, Parasolid is now owned by Siemens PLM Software (formerly UGS Corp.). Parasolid is the main core of Solidworks, Siemens-NX, and Solidedge which are the most well-known CAD systems based on Parasolid’s 3D kernel [91].

As a solid modeler kernel, Parasolid is capable of feature modeling, advances surface modeling, thickening and hollowing, sheet modeling, Boolean operations and advanced surface and volumetric calculations. Parasolid also provides further tools for direct model editing, including tapering, arraying, offsetting, projection, geometry replacement and removal of feature details with automated regeneration of surrounding data. Wide range of representation techniques and rendering supports such as hidden-line, wireframe and drafting, tessellation and model data inquiries are also available in Parasolid. Parasols file

extension is `x_t` which is commonly used in available CAD/CAM/CAE systems to communicate with different packages. Another format for exporting data in Parasolid is `x_b` which is in binary format so it is independent of the machine and impervious to binary-to-text conversion error.

Parasolid also offers the developer unprecedented accuracy. The parasols default session precision is $1.0e-8$ in a world size of $1.0e3$. This gives an accuracy ratio of $1.0e11$, which is an order of magnitude more accurate than that of any other kernel modeler. The Parasolid API is C-callable, so that it can be integrated into any C or C++ application code. A binding for C# is also available, allowing to call the Parasolid API from C# code [92].

3.5 Loop module of CWE model

The loop module forming the main part of the CWE model, is initialized after the setup section. Two main objectives of the loop module are as the following:

- In-Process Workpiece (IPW) modeling
- Determination of tool-IPW intersection at each CL point

The sequence of loop module is given in Figure 3-1. This module of the CWE code is called loop because it needs to be iterated for each CL point to update IPW for determination of tool-workpiece intersections.

The first step is finding the cross section of the tool perpendicular to the toolpath at each CL point. This operation is handled by knowing the geometry, position and orientation of the tool in Parasolid. Then the sweep path is constructed based on the information of CL file as an input to the setup section. The swept volume of the tool can be modeled using “`PK_BODY_make_swept_body_2`” in Parasolid. By generating the tool cross sections as sweep profile and taking toolpath as sweeping path, the swept volume can be modelled using “`PK_BODY_make_swept_body_2`” in Parasolid. Figure 3-7 illustrates the process for a sample toolpath for machining of the impeller shown in Figure 3-2.

At any given cutter location point CL_i , the algorithm first generates a vector between CL_i and CL_{i+1} facing towards the feed direction. This vector is used as the path for the sweeping operation. At the next step the projection of the tool is taken perpendicular to the current sweep path (which is also the feed direction). This projection of the tool gives us the effective cross section which removes material from the workpiece during the real machining operation. Then, the projection is swept across the sweep path.

In this model, the swept volume of the tool is generated by sweeping the effective cross section of the tool. The effective cross section is basically the projection of the tool on a plane which is perpendicular to the feed direction. Unfortunately, the current version of Parasolid kernel has not proven to perform well for taking the projection of solid bodies at certain projection angles. Hence, different types of tools require different approaches. In this research work, ball-end mill tool is used for the simulation and the experiment. For the ball-end mill, if it is known that only the spherical part of the tool is in contact with the workpiece, the projection of the tool can be modelled as a simple circle. If the cylindrical part of the tool is in contact, a different approach is required. In this case projection of the tool has to be modelled as a simple circle united with a rectangle on top of it.

Using the generated line representing the sweep path and the projection of the tool, the swept volume is generated using Parasolid features between CL_i and CL_{i+1} . However, this volume does not fully represent the volume that the tool would cover in reality. To tackle this problem, the total volume, which the tool sweeps is modeled in three steps. First the tool body is created at the current CL point. Next, the swept volume is generated as was mentioned before. Finally, the tool body is generated again at next CL point.

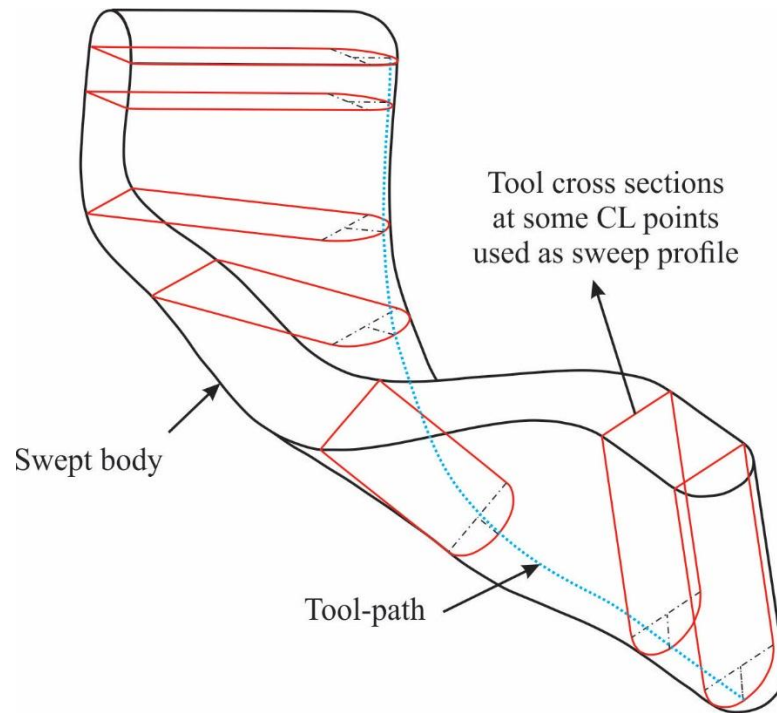


Figure 3-7 – Swept volume for one sample toolpath.

Boolean functions of Parasolid are used to subtract the swept volume of the tool from the workpiece at each CL point. This operation should be iterated for each CL point to model the IPW. The importance of this part is due to the fact that workpiece should be updated to be used as in-process workpiece so that the engagement can be completed successfully for the remaining CL points in the loop. Figure 3-8 illustrates the IPW modeling approach.

In order to compute the engagement at a given CL point, the tool body is intersected with slicing planes which results in rings at each height increment. These rings are then intersected with the IPW. The intersection results in several engagement curves. The beginning and the end coordinates of these curves give the engagement points. Ultimately, engagement points are used to calculate the entrance and exit angles with respect to the cross feed direction.

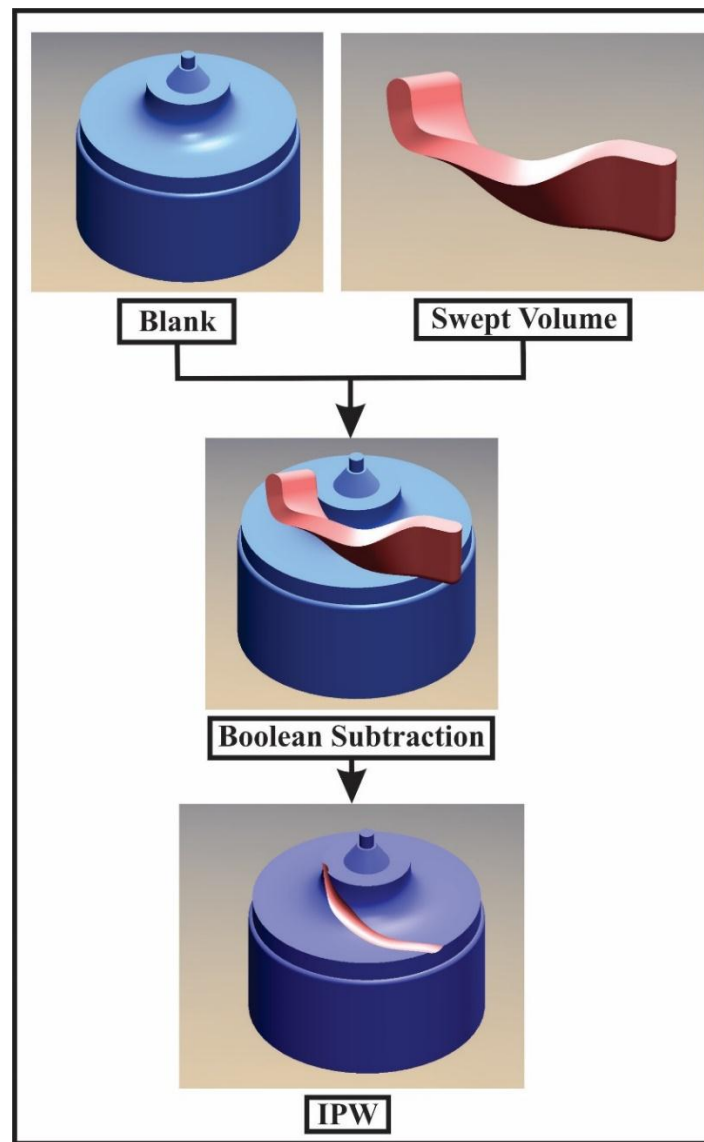


Figure 3-8 – IPW modelling.

3.6 CWE engagement map

The engagement computation forms the main part of the model. It also consumes most of the computational resources. Tool body is sliced into discs as is shown in Figure 3-9

and Figure 3-10. Next, desired number of planes are created perpendicular to the tool axis at each plane height. These plane heights are calculated as covered in setup module. In order to create the plane, Parasolid requires location, normal direction and reference direction. The normal direction and reference direction are the same as of the tool. The location is calculated using the equations below where h represents the disc heights and n is the disc number. X_i, Y_i, Z_i, I_i, J_i and K_i are the same as is mentioned in Equation (3-1), (3-2) and (3-3).

$$P_{x_i} = X_i + h_n * I_i \quad (3-4)$$

$$P_{y_i} = Y_i + h_n * J_i \quad (3-5)$$

$$P_{z_i} = Z_i + h_n * K_i \quad (3-6)$$

These planes are perpendicular to the tool axis direction and start from the tip of the tool extending until the end of the tool with very small height increments. The planes will be used in combination with the tool body for creating the tool discs. In the following step, the planes and the tool body are intersected using the “intersect surface” function of Parasolid. Each intersection operation will result in a disc lying on the corresponding plane, which was created previously at the beginning of the loop.

The discs are then intersected with the copy of the IPW. The IPW is duplicated to preserve the original IPW to be used in the following iterations of the loop. The intersection results in an arc for single engagement or multiple arcs for multiple engagement case. Depending on the engagement condition at the current CL point, the engagement model will output a single or multiple engagement curve. The model is capable of detecting the frequently occurring multiple engagement conditions during freeform five-axis milling.

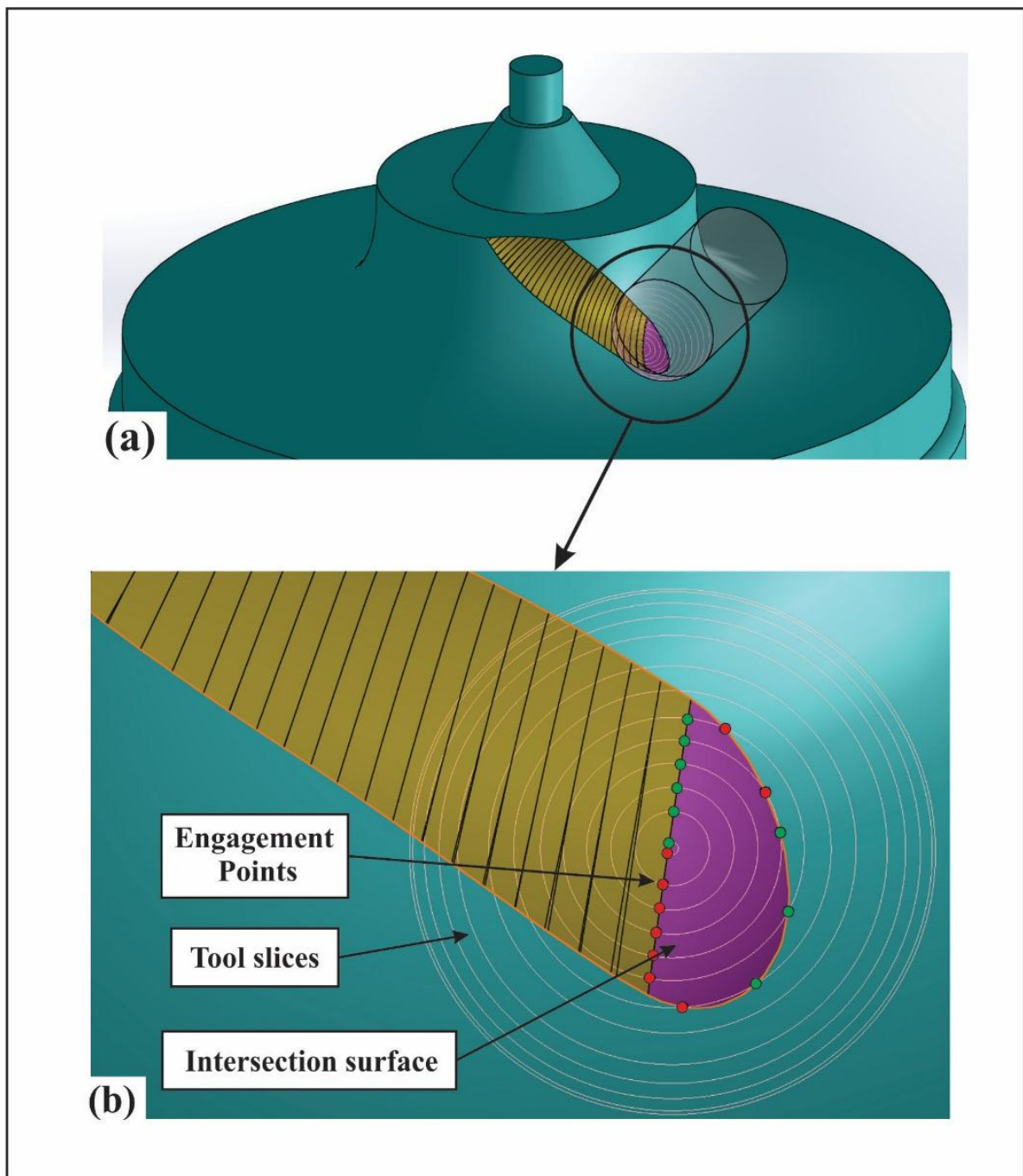


Figure 3-9 – Tool-IPW intersection for the centrifugal pump impeller.

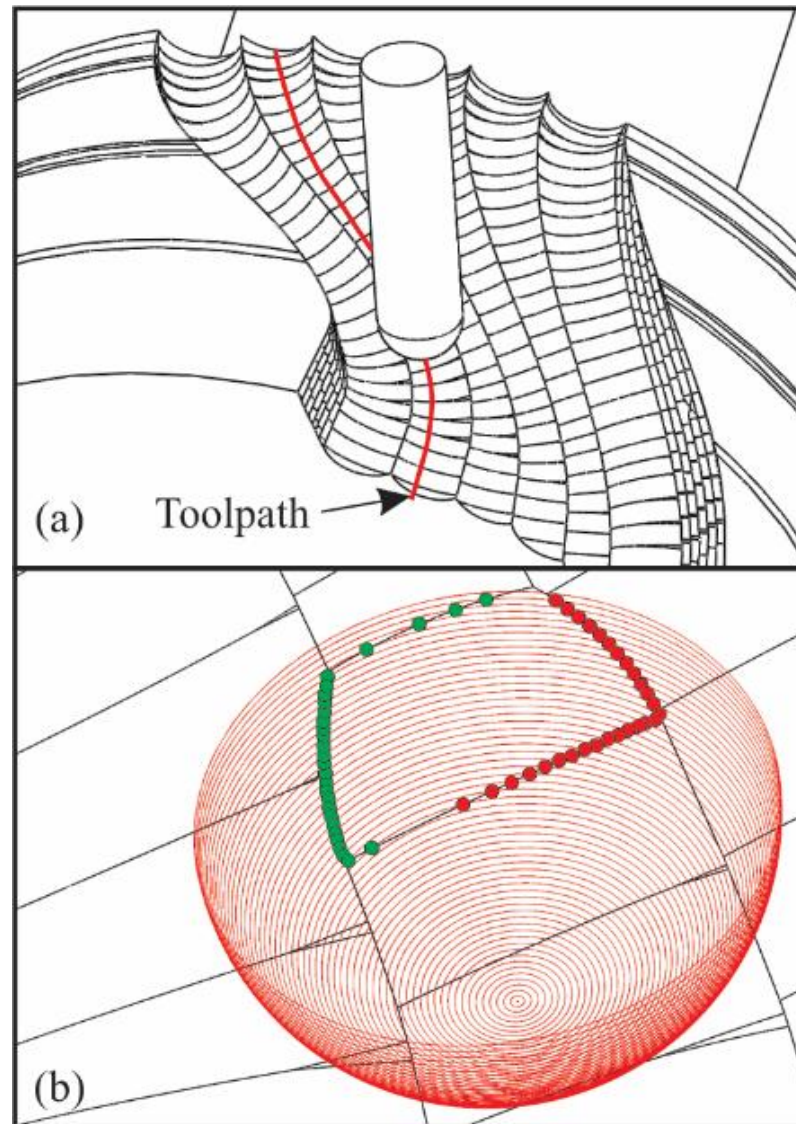


Figure 3-10 - Tool-IPW intersection for the blisk geometry.

From each arc the coordinates of the start and the end points, called engagement points, are extracted and stored in a text file. Figure 3-9(a) and Figure 3-10(a) represents the output of model for a sample CL point of the impeller geometries already shown in Figure 3-2 and

Figure 3-3. For the sake of simplicity, the tool body is not shown in Figure 3-9(b) and Figure 3-10 (b). In these figures, the green and the red points respectively present entrance and exit points. These points are used in angle calculation in the next step.

The entrance and exit angles are calculated by utilizing appropriate transformation to view them in the tool axis direction. This allows for engagement angles to be computed with respect to the cross feed direction. The angle between the vector pointing to the current engagement point from the center of the tool and the cross feed direction vector is calculated. Figure 3-11 illustrates generated engagement points for CL # 33 of the case of centrifugal pump impeller already shown in Figure 3-2. Feed and cross feed direction is updated at each CL point to calculate the engagement point based on them.

In order to ease the process of engagement angle calculation, 3D map of the engagement points shown in Figure 3-11 is transformed using an appropriate transformation matrix to find the projected 2D engagement map on a plane perpendicular to the tool axis. Figure 3-12 illustrated 2D map of the engagement for the CL # 33. In this figure green and red points respectively presents entry and exit points for each disk of the tool.

Figure 3-13 gives the definition of the entry and exit angles for a simple disk containing a multiple engagement. The reference direction for angle calculation is cross-feed direction. The proposed CWE code in C# is able to detect the multiple engagement cases and sort them based on the entry and the exit angle. The calculated entry and exit angles for each disk are saved as engagement map, to be later used as a boundary condition for the force model or other pertinent applications.

Figure 3-14 illustrates the engagement rings, engagement points, definition of entry and exit angles for CL #2183 of the blisk geometry. Entry and exit angles are shown in green and red point in the figure, respectively.

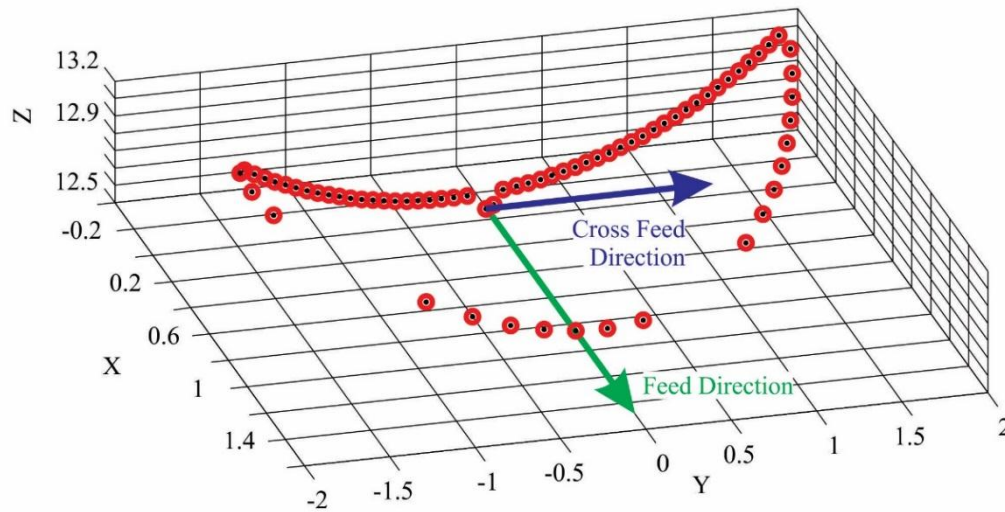


Figure 3-11 – Generated engagement points for the case of centrifugal pump impeller.

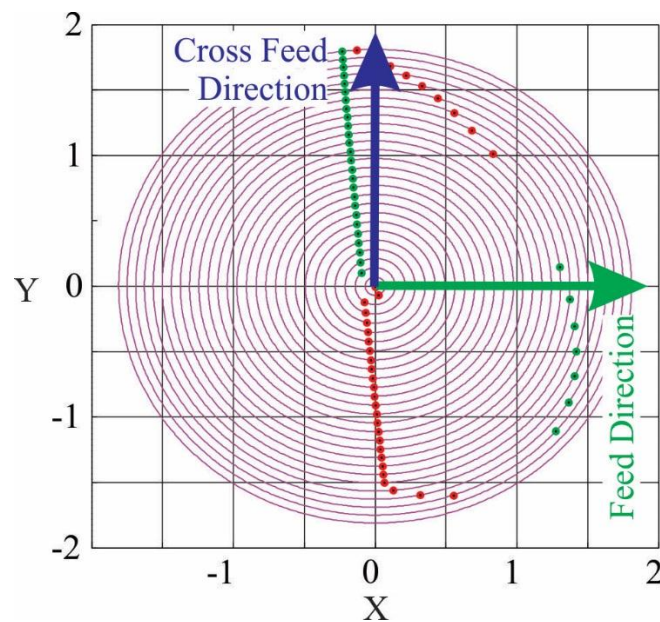


Figure 3-12- Engagement points from the view in along with tool axis (for Figure 3-11 case).

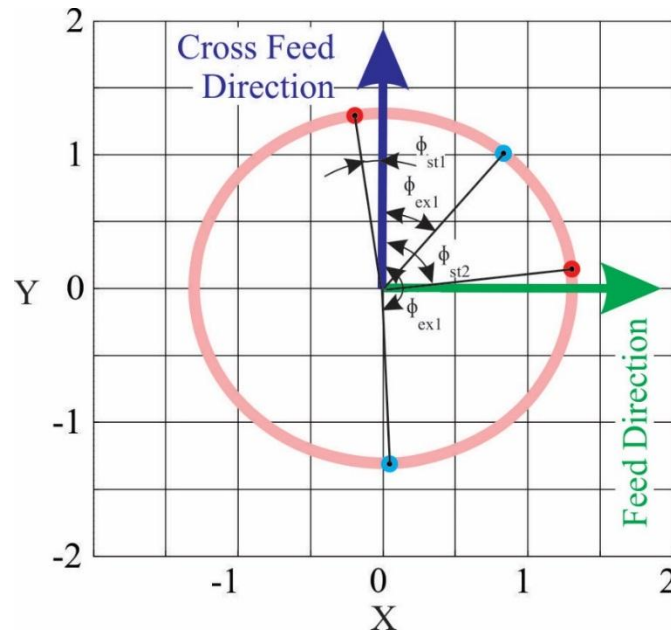


Figure 3-13 – Definition of start and exit angles.

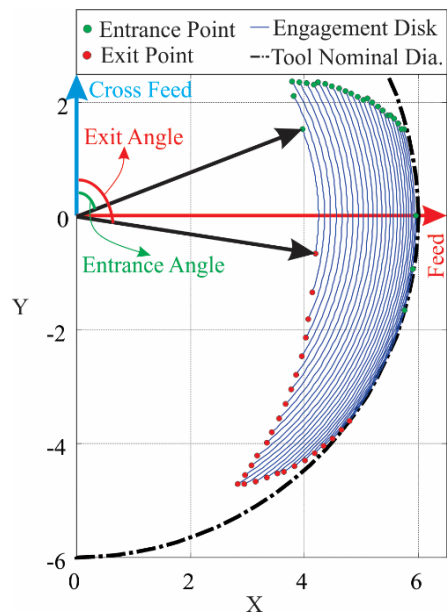


Figure 3-14 – Engagement angles for the sample CL point of the case shown in Figure 3-3.

3.7 Updating the workpiece

In order to finalize updating the workpiece, the previously generated swept bodies need to be subtracted from the IPW. To accomplish this, the Boolean operation subtract of Parasolid is used. First the tool body at the current CL point and the swept volume is subtracted from the IPW. Next, the CL point counter for the loop is incremented by one and the tool body at this CL point is subtracted. As a result the IPW is updated and becomes ready for engagement computation of the remaining CL points. Finally, the tool body is created to be used again in the next loop iteration.

Updating the workpiece is important from two perspectives:

- To model the IPW for the CWE calculation
- To simulate the machining process

By updating the workpiece as IPW at each CL point, it is possible to compute engagement map by intersecting the tool and the updated IPW. Moreover, for demonstration and collision checking purposes the workpiece should be updated at each CL point. Figure 3-15 and Figure 3-16 illustrate the machined surface geometry and the IPW for two different geometry. As is shown in Figure 3-15 the CWE model is able to simulate the final geometry of the machined surface which is necessary for virtual simulation and inspection of the machined surface before real manufacturing.

Figure 3-16, however, illustrates the instantaneous machined workpiece or IPW (In-Process Workpiece) for CL #436 of the centrifugal pump impeller. Modeling the IPW is essential to compute the 3D engagement map at each CL point.

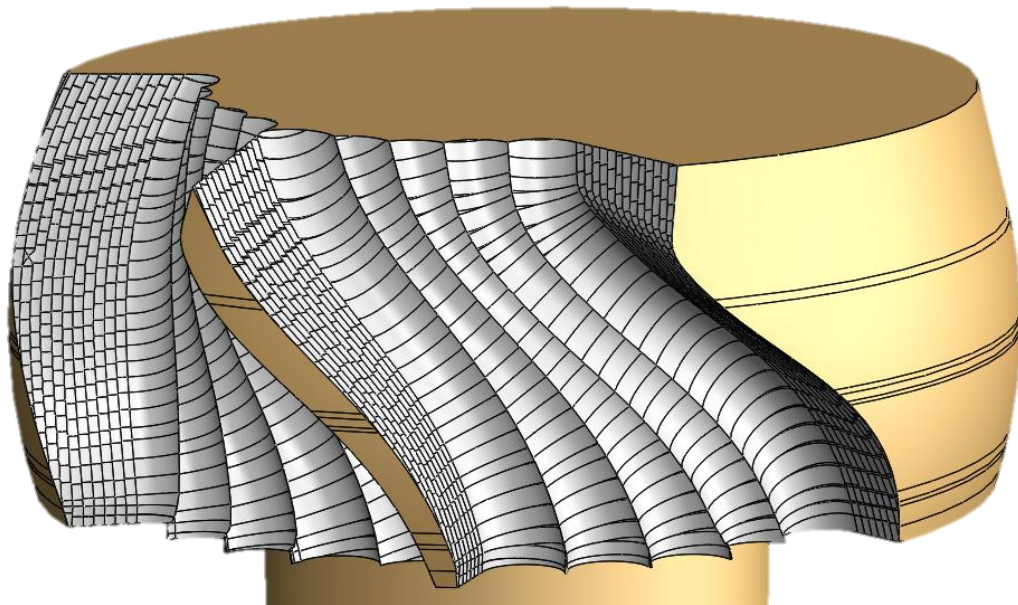


Figure 3-15 – Machined surface for roughing operation of the blisk geometry.

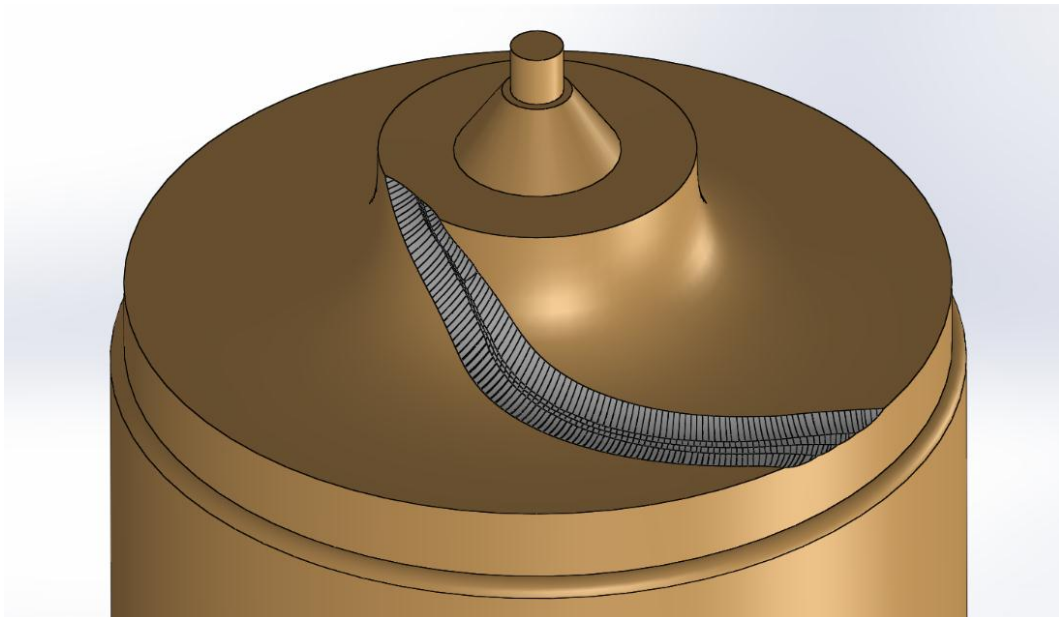


Figure 3-16 - IPW for roughing operation of the centrifugal pump impeller up to CL # 436.

3.8 Prediction of engagement 2D and 3D maps

Following the completion of the engagement model, the engagement map file containing the corresponding engagement angles for each slice of the tool will be generated as well. An example of engagement file containing multiple engagements for CL number 3825 of the blisk geometry is illustrated in Figure 3-17. The leftmost column in Figure 3-17 depicts the row number in the output engagement file. Columns two to seven represent the CL number, the disc number, and the entrance and exit angles in degrees for each engagement curve, respectively.

The sample engagement output given in Figure 3-18 shows a case of multiple engagement where the entry and exit angles for curve 1 and curve 2 are denoted, respectively as En1, Ex1, En2 and Ex2. Figure 3-18 and Figure 3-19 shows 2D and 3D map of the engagement projected on the tool surface. In both of the figures, the red curves represent the second set of engagement that frequently happens during five-axis milling of freeform surfaces.

	CL	Disc	En1	Ex1	En2	Ex2
141138	3825	17	33	88	116	148
141139	3825	18	30	78	126	150
141140	3825	19	28	72	132	152
141141	3825	20	26	67	137	154
141142	3825	21	25	63	141	155
141143	3825	22	23	60	145	157
141144	3825	23	22	57	148	158
141145	3825	24	21	54	150	159

Figure 3-17 - A sample of engagement output (entrance (En1, En2) and exit (Ex1, Ex2) angles are in degrees).

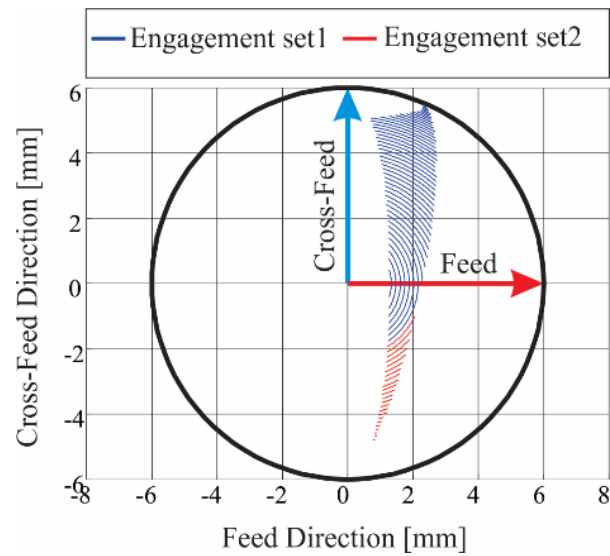


Figure 3-18 - 2D map of engagement for multiple engagement case.

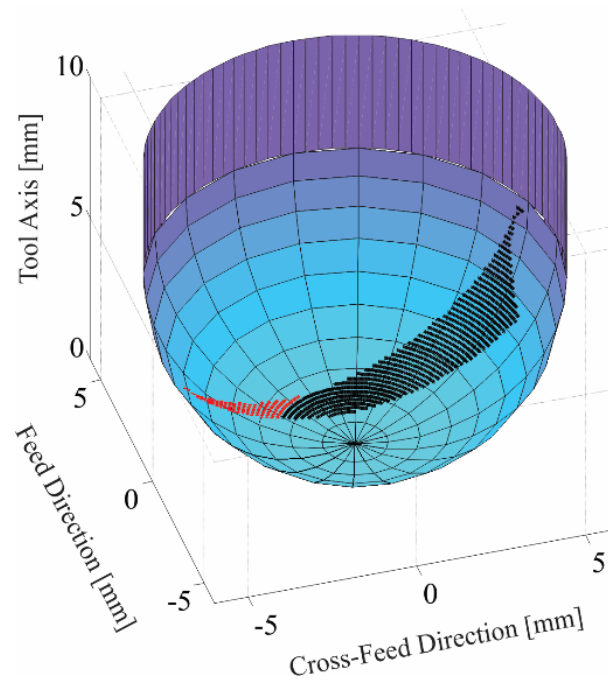


Figure 3-19- 3D map of engagement projected on the tool in multiple engagement case.

The interface of the developed engagement model is shown in Figure 3-20. The interface gives the input parameters such as tool diameter, tool height, Number of disks, blank workpiece and CL file. The engagement angles will be generated in text format after successful computation process. The speed of computation depends on the number of discs and number of CL points. Higher number of discs leads to higher accuracy and slower computation time.

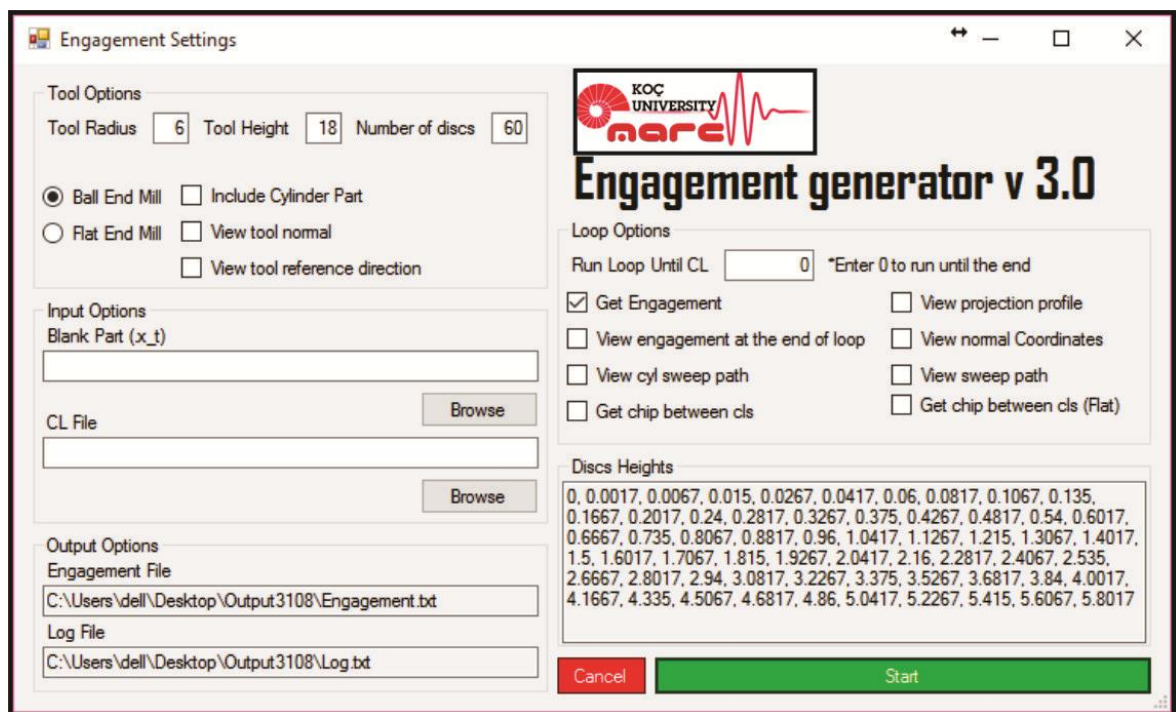


Figure 3-20 – Interface of the developed engagement model

At the end, the engagement angles are stored in a text file to be used in five-axis mechanistic cutting force model as a boundary condition. The accuracy of the predicted cutting forces is highly depended on the accuracy of the modelled engagement angles. Any

change due to the static and dynamic tool deflection can affect the engagement domain. However, in the normal cutting conditions the deflection of the tool is negligible.

3.9 Summary

A new approach of CWE calculation for five-axis ball-end milling operation is presented. The model is capable of providing 2D and 3D engagement maps along toolpath trajectory for each CL point. These maps are needed for accurate prediction of instantaneous cutting forces at each cutter position.

The proposed algorithm is a solid modeler approach based on the Parasolid kernel in C# framework. The swept volume of the tool is calculated between consecutive CL points using the sweep command in Parasolid. The toolpath is used as the sweeping path, which is extracted from NX CAM environment. The cross-section of the tool is used as the sweep profile at each CL point. Using the Boolean operations in the Parasolid, the IPW is calculated by subtracting the swept volume from the in-process workpiece. By intersecting the tool with the IPW at each CL point the CWE map is achieved. The tool is sliced along the Z axis and the engagement angle for each disk slice is calculated using developed postprocessors. The cutting force experiments in the following chapters prove the accuracy of the proposed CWE model.

Chapter 4

CUTTING FORCE COEFFOCIENTS

4.1 Overview

The most common model to estimate the cutting force in literature is mechanistic model in which the cutting force is a linear function of uncut chip thickness area [42], [93]. As is further explained in Chapter 5, the mechanistic cutting force model can be represented using the following formula:

$$F_i = K_{ic}A_c + K_{ie} \quad (4-1)$$

Above, F_i represents F_t, F_r and F_a that are forces in tangential, radial and axial directions, respectively. K_{ic} and K_{ie} stand for $K_{tc}, K_{rc}, K_{ac}, K_{te}, K_{re}$ and K_{ae} which are the specific cutting coefficients and the specific edge coefficients in tangential, radial and zenith directions, respectively. Due to the change of local diameter and local helix angle of the spherical part of the tool, cutting coefficients are not constant along the cutting edge. If the spherical part of the tool is divided into a finite number of thin disks, each one would have different cutting and edge coefficients. The reliability of the force model is highly dependent on the accuracy of the estimated coefficients at each disk. The available methods for calculation of cutting coefficients in literature can be classified as the following:

- Analytical identification of cutting coefficients using experimental database acquired from the orthogonal Turing test.
- Identification of cutting coefficients using calibration experiments conducted on milling operation.

Each of the above mentioned methods has some advantages and disadvantages. The advantage of the analytical method using orthogonal database is that the cutting

coefficients can be predicted before the manufacturing of the tool. Therefore, the number of tests in order to find the cutting constants decrease significantly. Nevertheless, when the geometry of the cutter is not known or too complex, this model cannot be employed. Moreover, there are some simplifying assumptions in the transformation of the orthogonal to oblique cutting mechanics. These assumptions lead to less accuracy, particularly in the tip of ball-end mill tool. In reference [36] a general approach to calculate the cutting constants for general end-mills using the calibration method is introduced. In this approach, several milling tests with arbitrary radial immersion and constant depth of cut are conducted. Since the experimental tests were conducted for constant depth of cut and, the contribution of each cutting disk were not measured separately. Figure 4-1 represents the experimental setup that is used to measure the cutting forces in reference [36]. The drawback of this method is that the estimated cutting coefficients are not accurate enough when the projected engagement area on the tool is small. Therefore, as is shown in Figure 4-2, if the tool is partially engaged with the workpiece, the calculated cutting constants would not be accurate enough.

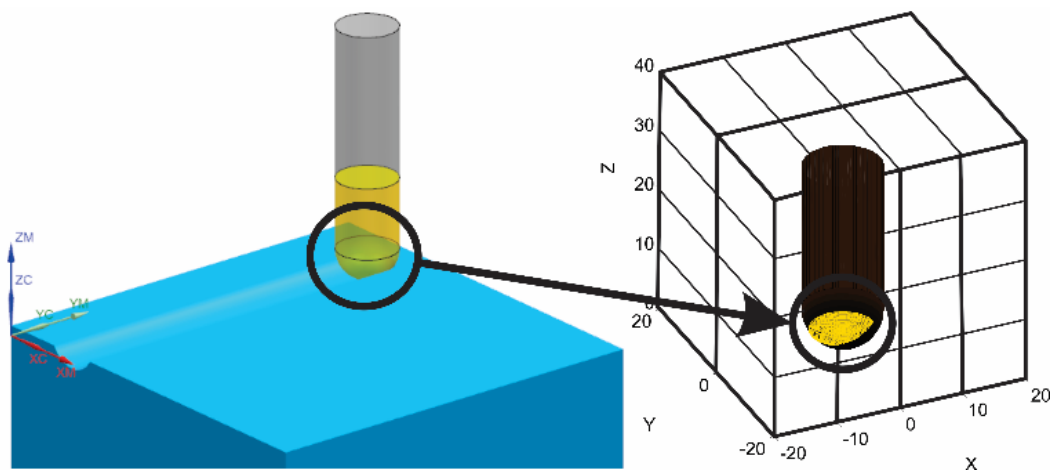


Figure 4-1 - Engagement in slot cutting ball-end milling.

In this thesis, the tool is divided into several cutting disks along the cutter axis. The contribution of each disk in resultant cutting force is measured by designing an experimental setup to cut the workpiece while just that certain disk is engaged with the workpiece. It is shown experimentally that the estimated cutting forces using this approach are more accurate.

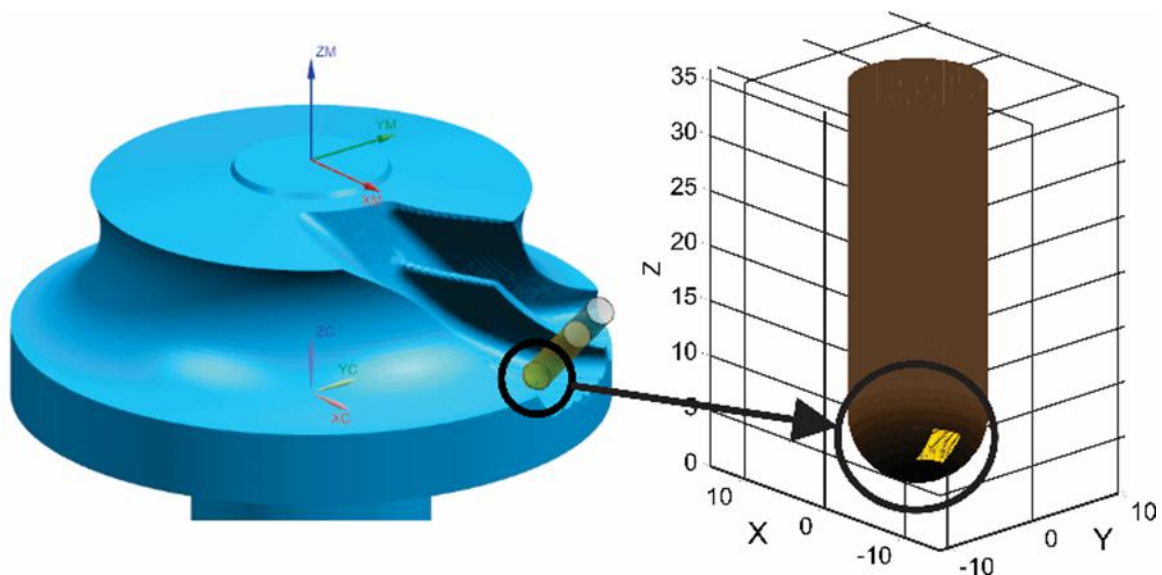


Figure 4-2 - Illustration of engagement region in a freeform surface ball-end milling operation.

4.2 Identification of cutting coefficients from orthogonal to oblique transformation method

The advantage of this method is that the cutting coefficients in general oblique operation can be determined from a database acquired from simple orthogonal cutting test performed on turning machine. Due to this fact, this method is known as “orthogonal to

oblique transformation method”. Using this approach, the cutting coefficient for milling, boring, drilling and turning operations can be identified easily.

In order to establish the database, cutting forces during the orthogonal turning of a tubular workpiece for different feedrate must be collected. The orthogonal experiment must be performed with different rake angles to cover the rake angle of the oblique tool, for which the cutting forces are to be predicted. In order to consider the effect of cutting speed, the orthogonal test should also be conducted for a wide range of cutting speeds, thus increasing the number of experiments. The theory of the approach is explained in details in [42] and is not repeated here.

4.3 Experimental setup for identification of orthogonal cutting force coefficients

All the orthogonal cutting tests are performed on a tubular Al7050 workpiece with the diameter of 60 [mm]. The wall thickness of the grooves manufactured on the workpiece is 2 [mm] which is considered as width of cut. The inclination angle is set to be zero to have an orthogonal cutting condition. Cutting forces are measured using a table type dynamometer, which is shown in Figure 4-3. The chips during the machining are collected to measure the chip thickness ratio. The components of cutting forces are illustrated in Figure 4-4. The range of cutting parameters used for measuring the average cutting forces is tabulated in Table 4-1.

Table 4-1 – Cutting conditions in orthogonal cutting test.

Feedrate [mm/rev]	0.05 – 0.1 – 0.15 – 0.2
Rake Angle [degree]	0 – 5 – 10 – 15 – 20
Cutting Speed [m/min]	15 – 40 – 80 – 170
Width of Cut [mm]	2

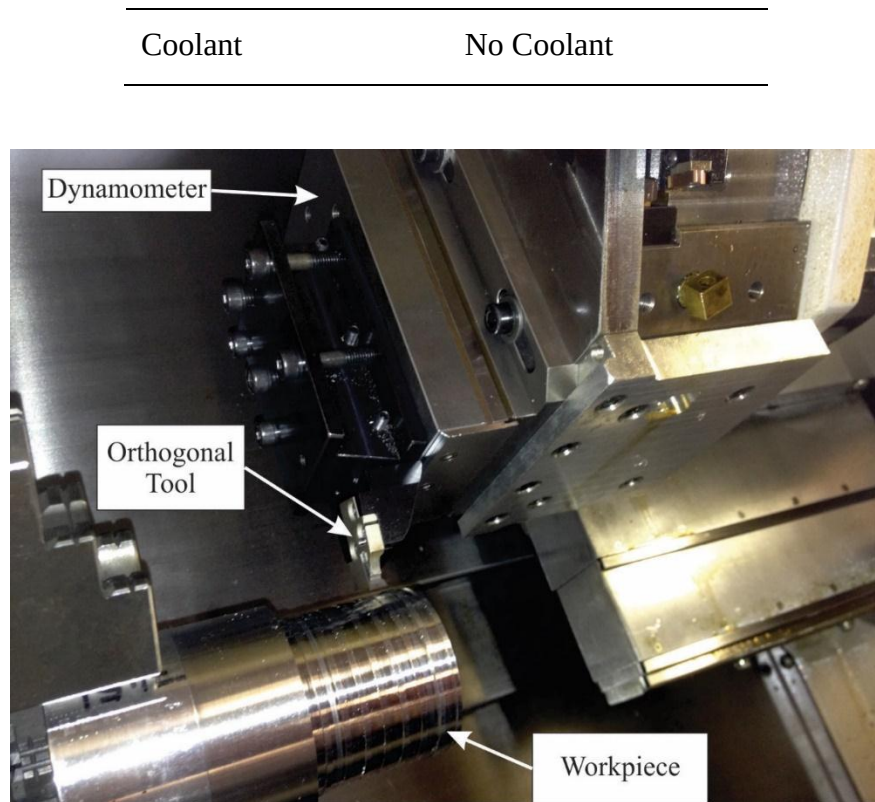


Figure 4-3 – Orthogonal test setup.

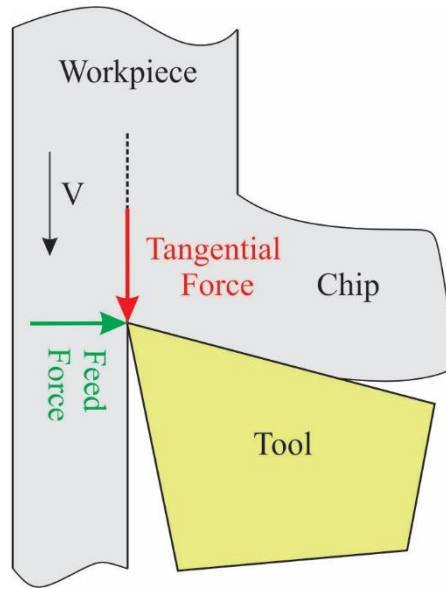


Figure 4-4 – Cutting force components in orthogonal cutting.

A sample of measured cutting forces is depicted in Figure 4-5. Since the operation is orthogonal cutting, the cutting force component in radial direction must be zero, as is shown in this Figure 4-5. The cutting force should be constant during the cutting, but due to the dynamics of the process some small oscillations in the force measurement can be seen. A typical measurement collection for a rake angle of 15 degrees and cutting speed of 90 [mm/min] is shown in Table 4-2.

Figure 4-6 and Figure 4-7 represent the orthogonal test results. A line is fitted through the average values of the measured cutting forces. The relation between the tangential cutting force (F_t) and the feed cutting force (F_f) with the uncut chip thickness (h_c) and width of cut (b) can be expressed as the following:

$$F_t = F_{tc} + F_{te} = K_{tc}bh_c + K_{te}b$$

$$F_f = F_{fc} + F_{fe} = K_{fc}bh_c + K_{fe}b$$
(4-2)

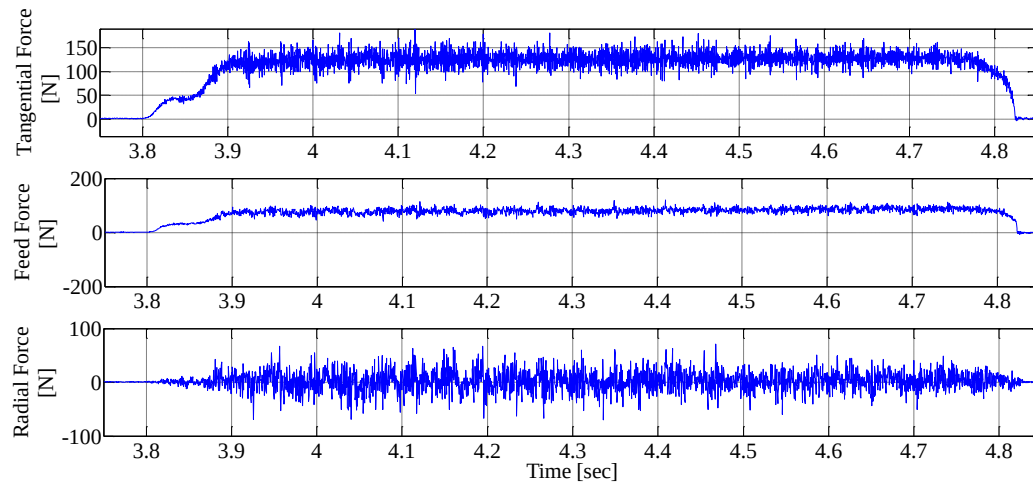


Figure 4-5 – Measured cutting forces for orthogonal cutting test (Rake Angle of 5 [deg.], Cutting Speed of 170 [m/min], Feedrate of 0.5 [mm/rev]).

Table 4-2 – Measured parameters for rake angle of 15 [deg.] and cutting speed of 80 [m/min].

Feed [mm/rev]	Average Chip thickness [mm]	Chip Thickness ratio	Average Tangential force [N]	Average Feed force [N]
0.05	0.090	0.558	125.885	79.967
0.1	0.173	0.578	202.558	93.095
0.15	0.286	0.524	290.148	129.687
0.2	0.349	0.572	363.616	146.909

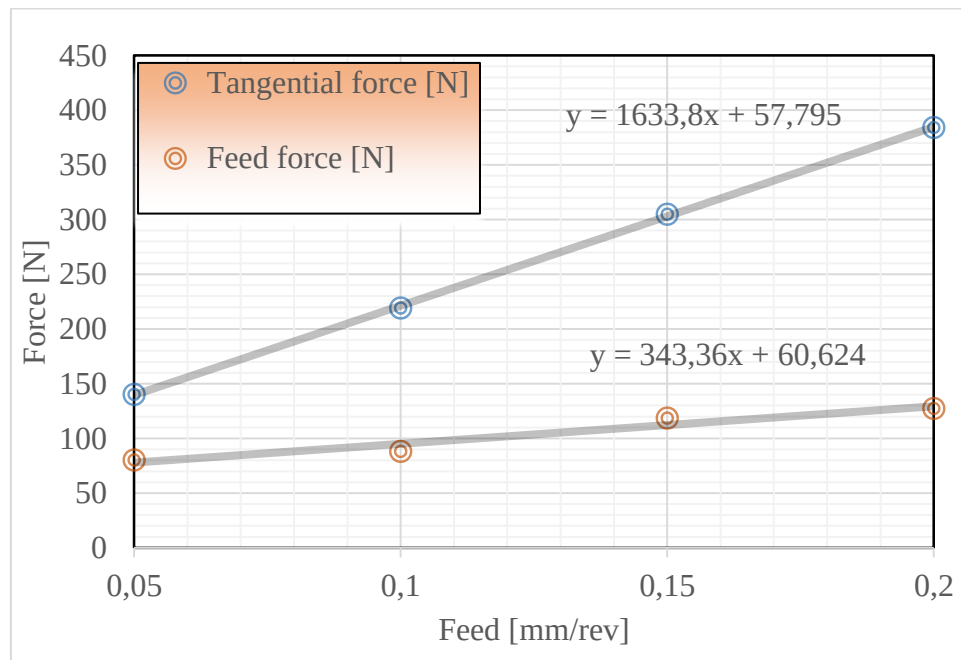


Figure 4-6 – Variation of cutting forces by changing the feedrate (Rake Angle of 15 [deg.], Cutting Speed of 15 [m/min]).

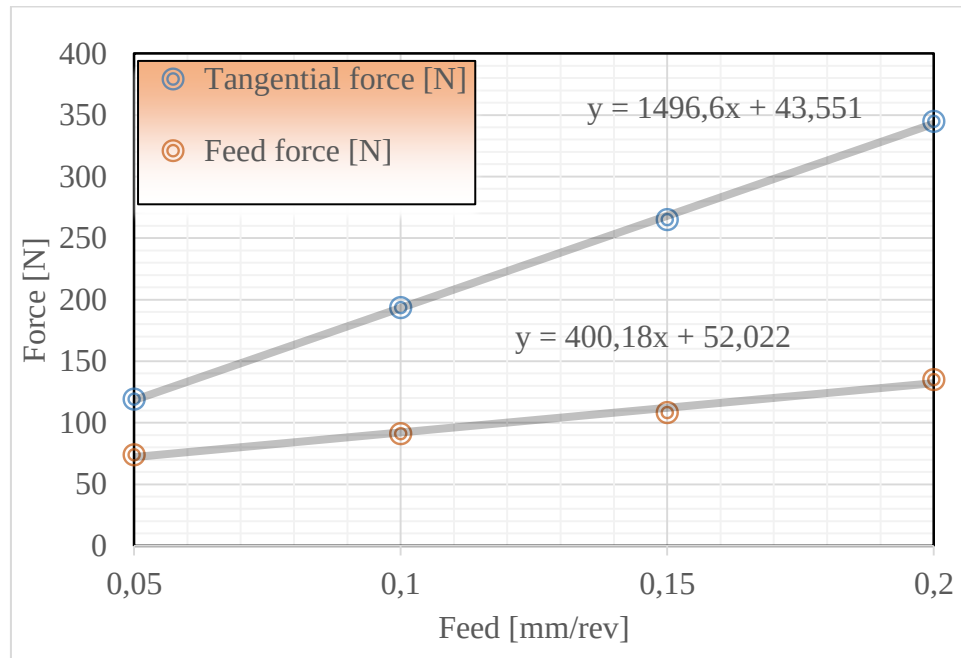


Figure 4-7 - Variation of cutting forces by changing the feedrate (Rake Angle of 20 [deg.], Cutting Speed of 170 [m/min]).

Cutting and edge coefficient in tangential and feed directions can be predicted from Equation (4-2) and Figure 4-6 and Figure 4-7. The details about calculation of the cutting coefficient and other cutting parameters such as shear stress, friction angle and shear angle are explained in [42] and are not repeated here. The results of orthogonal cutting test forms a database which are tabulated in Table 4-3. This database is used to identify the cutting coefficients in oblique cutting operations such as ball-end milling.

Table 4-3 – Orthogonal calibration results

Rake Angle [deg]	Speed [m/min]	Shear Stress [Mpa]	Friction Angle [deg]	Shear Angle [deg]
20	15	285.1	28.7	30.0
	40	301.6	29.4	31.2
	80	316.7	34.1	33.0
	170	309.2	35.0	35.8
5	15	305.9	28.5	23.4
	40	308.0	29.3	27.8
	80	307.9	30.1	28.2
	170	308.6	25.1	30.7
0	15	303.7	23.6	19.7
	40	301.9	29.0	26.3
	80	319.9	28.8	25.3
	170	306.4	21.4	28.3

In reference [31], the theory of prediction of milling force coefficient from orthogonal cutting database is thoroughly explained. In order to avoid repetition the equations are not mentioned in this thesis. Based on the measured orthogonal cutting data, the cutting force coefficients for ball-end mill tool are calculated and shown in Table 4-4. Due to the change in helix angle along the cutting flute, the inclination angle is not constant for ball-end mill

tool, thus leading to different cutting coefficient for ball part of the tool. Table 4-4 represents the cutting coefficients for a 12 [mm] carbide tool with 30 degree nominal helix angle. Ball-end mill geometry, elemental cutting forces and disk numbers are illustrated in Figure 4-8.

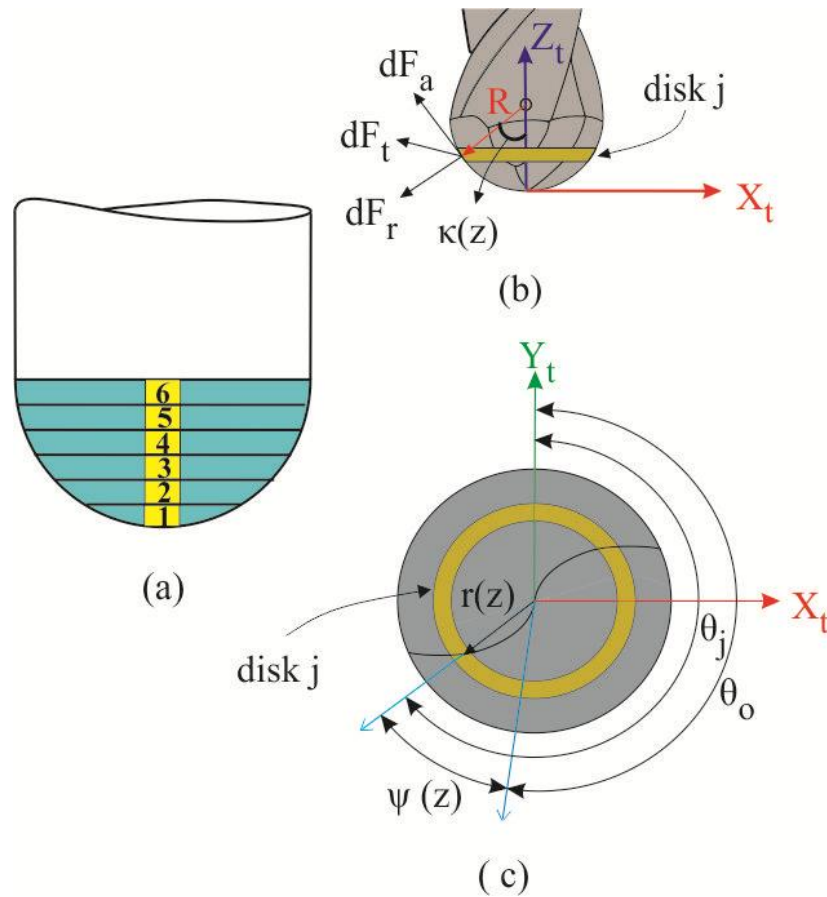


Figure 4-8 - Discretized tool geometry and force components for ball-end mill.

Table 4-4 – Cutting coefficients for 12[mm] carbide ball-end mill tool predicted from Orthogonal to oblique transformation database.

height [mm]	K_{tc} [Mpa]	K_{rc} [Mpa]	K_{ac} [Mpa]	K_{te} [N/mm]	K_{re} [N/mm]	K_{ae} [N/mm]
0-1	1111	117	601	25	22	0
1-2	1131	195	579	25	22	0
2-3	952	267	346	25	22	0
3-4	958	300	342	25	22	0
4-5	962	320	340	25	22	0
5-6	964	330	339	25	22	0

4.4 Identification of cutting coefficients from ball-end milling calibration test

The geometry of ball-end mill tool is briefly represented in Figure 4-8. Since the local geometric parameters such as radius, immersion angle and helix angle change along the tool axis, the cutting coefficients in ball-end mill tool are not constant. In order to predict the cutting coefficients in the spherical region of the tool, this part is divided into 6 disks with the thickness of 1 mm. The disks must be selected as thin as possible to have almost constant geometric parameters.

The differential cutting forces in tangential, radial and zenith directions for disk j can be modelled using the following equations:

$$\begin{cases} dF_{t,j}(\theta_j, \kappa(z)) = K_{tc} h_c(\theta_j, \kappa(z)) db(z) + K_{te} dS(z) \\ dF_{r,j}(\theta_j, \kappa(z)) = K_{rc} h_c(\theta_j, \kappa(z)) db(z) + K_{re} dS(z) \\ dF_{a,j}(\theta_j, \kappa(z)) = K_{ac} h_c(\theta_j, \kappa(z)) db(z) + K_{ae} dS(z) \end{cases} \quad (4-3)$$

Above, θ_j is the current angular position of cutting lip in disk j and $\kappa(z)$, $db(z)$ and $dS(z)$ are zenith angle, chip width and edge length for disk j . These parameters can be calculated using the following equations:

$$\kappa(z) = \sin^{-1} \sqrt{1 - E(z)^2},$$

$$ds(z) = dz \sqrt{\tan^2 i_0 (1 - E(z)^2) + \frac{1}{1 - E(z)^2}}, \quad (4-4)$$

$$E(z) = \frac{R-z}{R} \quad \text{and} \quad db = \frac{dz}{\sin \kappa(z)}$$

Above, dz is the axial integration element, i_0 is nominal helix angle and R is tool radius. In Equation (4-3) h is the uncut chip thickness and can be estimated using equation (4-5). In this equation, f_t is feed per tooth.

$$h_c(\theta_j, \kappa(z)) = f_t \sin(\theta_j) \sin(\kappa(z)) \quad (4-5)$$

Forces in X, Y and Z directions can be found by transforming the predicted forces using the following transformation equation:

$$\begin{aligned}
& \begin{bmatrix} dF_{x,j}(\theta_j, \kappa(z)) \\ dF_{y,j}(\theta_j, \kappa(z)) \\ dF_{z,j}(\theta_j, \kappa(z)) \end{bmatrix} \\
&= \begin{bmatrix} -\sin(\kappa(z)) \times \sin(\theta_j) & -\cos(\theta_j) & -\cos(\kappa(z)) \times \sin(\theta_j) \\ -\sin(\kappa(z)) \times \cos(\theta_j) & \sin(\theta_j) & -\cos(\kappa(z)) \times \cos(\theta_j) \\ \cos(\kappa(z)) & 0 & -\sin(\kappa(z)) \end{bmatrix} \times \quad (4-6) \\
& \begin{bmatrix} dF_{r,j}(\theta_j, \kappa(z)) \\ dF_{t,j}(\theta_j, \kappa(z)) \\ dF_{a,j}(\theta_j, \kappa(z)) \end{bmatrix}
\end{aligned}$$

The cutting forces in X, Y and Z directions generated by each cutting element at each angular position can be predicted using the following equation:

$$F_{xyz,j}(\theta_j) = \int_{z_1}^{z_2} dF_{xyz,j}(\theta_j, \kappa(z)) dz \quad (4-7)$$

The average cutting force for known engagement domain can be determined as the following:

$$\bar{F}_{xyz} = \frac{1}{\theta_p} \int_{\theta_{st}}^{\theta_{ex}} \int_{z_1}^{z_2} dF_{xyz}(\theta, z) d\theta \quad (4-8)$$

In the above equation, θ_p is the pitch angle of the tool. θ_{st} and θ_{ex} are the start and exit angles of engagement domain. Cutting force in each direction can be calculated by implementing Equation (4-3), (4-4), (4-5) and (4-6) into equation (4-7). For instance, cutting force in X direction can be written as follows:

$$\begin{aligned}
F_x(\theta_j) = & \int_{z_1}^{z_2} [-K_{re} \sin(\kappa(z)) \sin(\theta_j) ds(z) - K_{rc} f_t \sin(\kappa(z)) \sin^2 \theta_j dz] \\
& + \int_{z_1}^{z_2} [-K_{te} \cos(\theta_j) ds(z) - K_{tc} f_t \frac{\sin(2\theta_j)}{2} dz] \\
& + \int_{z_1}^{z_2} [-K_{ae} \cos(\kappa(z)) \sin(\theta_j) ds(z) - K_{ac} f_t \cos(\kappa(z)) \sin^2 \theta_j dz]
\end{aligned} \tag{4-9}$$

By calculating the integral in Equation (4-9) in X, Y and Z directions, differential cutting forces can be summarized in Equation (4-10).

$$\begin{aligned}
\begin{bmatrix} dF_x(\theta_j) \\ dF_y(\theta_j) \\ dF_z(\theta_j) \end{bmatrix} &= \frac{f_t}{2} \times \begin{bmatrix} -2K_{rc} \sin^2 \theta_j & -K_{tc} \sin(2\theta_j) & -2K_{ac} \sin^2 \theta_j \\ -K_{rc} \sin(2\theta_j) & 2K_{tc} \sin^2 \theta_j & -K_{ac} \sin(2\theta_j) \\ -2K_{ac} \sin \theta_j & 0 & 2K_{rc} \sin \theta_j \end{bmatrix} \begin{bmatrix} A_1 \\ A_2 \\ A_3 \end{bmatrix} \\
&+ \begin{bmatrix} -K_{re} \sin \theta_j & -K_{te} \cos(\theta_j) & -K_{ae} \sin \theta_j \\ -K_{re} \cos \theta_j & K_{te} \sin \theta_j & -K_{ae} \cos \theta_j \\ -K_{ae} & 0 & K_{re} \end{bmatrix} \begin{bmatrix} B_1 \\ B_2 \\ B_3 \end{bmatrix}
\end{aligned} \tag{4-10}$$

Above, A and B are some constants that can be found as follow:

$$\left\{ \begin{array}{l} A_1 = \int_{z_1}^{z_2} \sin(\kappa(z)) dz, A_2 = \int_{z_1}^{z_2} dz \\ A_3 = \int_{z_1}^{z_2} \cos(\kappa(z)) ds(z), B_1 = \int_{z_1}^{z_2} \sin(\kappa(z)) ds(z) \\ B_2 = \int_{z_1}^{z_2} ds(z), B_3 = \int_{z_1}^{z_2} \cos(\kappa(z)) ds(z) \end{array} \right. \tag{4-11}$$

The average cutting forces can be calculated by implementing Equation (4-11) into Equation (4-8). Equation (4-10) presents average cutting forces in X, Y and Z directions that can be also measured using a dynamometer in the experimental setup.

$$\begin{bmatrix} \bar{F}_{xc} \\ \bar{F}_{yc} \\ \bar{F}_{zc} \end{bmatrix} = \frac{f_t}{\theta_p} \begin{bmatrix} (C_2 - C_1)A_1 & C_3A_2 & (C_2 - C_1)A_3 \\ C_3A_1 & (C_1 - C_2)A_2 & C_3A_3 \\ -C_3A_5 & 0 & C_5A_1 \end{bmatrix} \begin{bmatrix} K_{rc} \\ K_{tc} \\ K_{ac} \end{bmatrix} \quad (4-12)$$

$$\begin{bmatrix} \bar{F}_{xe} \\ \bar{F}_{ye} \\ \bar{F}_{ze} \end{bmatrix} = \frac{1}{\theta_p} \begin{bmatrix} C_5B_1 & -C_4B_2 & C_5B_3 \\ -C_4B_1 & -C_5B_2 & -C_4B_3 \\ 2C_1B_3 & 0 & -2C_1B_1 \end{bmatrix} \begin{bmatrix} K_{re} \\ K_{te} \\ K_{ae} \end{bmatrix} \quad (4-13)$$

Above, $\bar{F}_{xyzc}$ and $\bar{F}_{xyze}$ are the average cutting and edge forces. C are some constants that can be calculated as follow:

$$\begin{cases} C_1 = \left[\frac{\theta}{2} \right]_{\theta_{st}}^{\theta_{ex}}, C_2 = \left[\frac{\sin(2\theta)}{4} \right]_{\theta_{st}}^{\theta_{ex}}, C_3 = \left[\frac{\cos(2\theta)}{4} \right]_{\theta_{st}}^{\theta_{ex}}, \\ C_4 = [\sin(\theta)]_{\theta_{st}}^{\theta_{ex}}, C_5 = [\cos(\theta)]_{\theta_{st}}^{\theta_{ex}} \end{cases} \quad (4-14)$$

The average forces in the left side of the Equation (4-13) and (4-14) must be measured experimentally. Each of the Equation (4-13) and (4-14) is a system of three equations and three unknowns. By solving this system of equations, cutting coefficients can be predicted as semi experimentally.

4.5 Experimental setup for calibration

All of the experimental tests were conducted on Aerospace grade Aluminum 7050. A five-axis milling center equipped with a table type dynamometer was used to perform the experiments. A high performance two fluted carbide ball-end mill from Sandvik (grade

H10f) was used. The other tool specification and cutting parameters are mentioned in Table 4-5.

Table 4-5 - Cutting conditions

Number of flutes	2
Nominal helix angle	30°
Tool diameter	12 mm
Spindle Speed	4500 rpm
Overhang length	60 mm
Coolant	Air

In order to measure the contribution of each disk element in cutting force, only that disk should be in contact with the workpiece. Figure 4-9 demonstrates the configuration of experimental setup that was used to measure the average edge and cutting forces. The thickness of the disk elements in the spherical part of the tool was set as 1 mm. Consequently, the thickness of workpiece in cutting regions was 1 mm. In order to prevent the unwanted vibrations and chatter phenomenon, some shoulders were created using slot cutting.

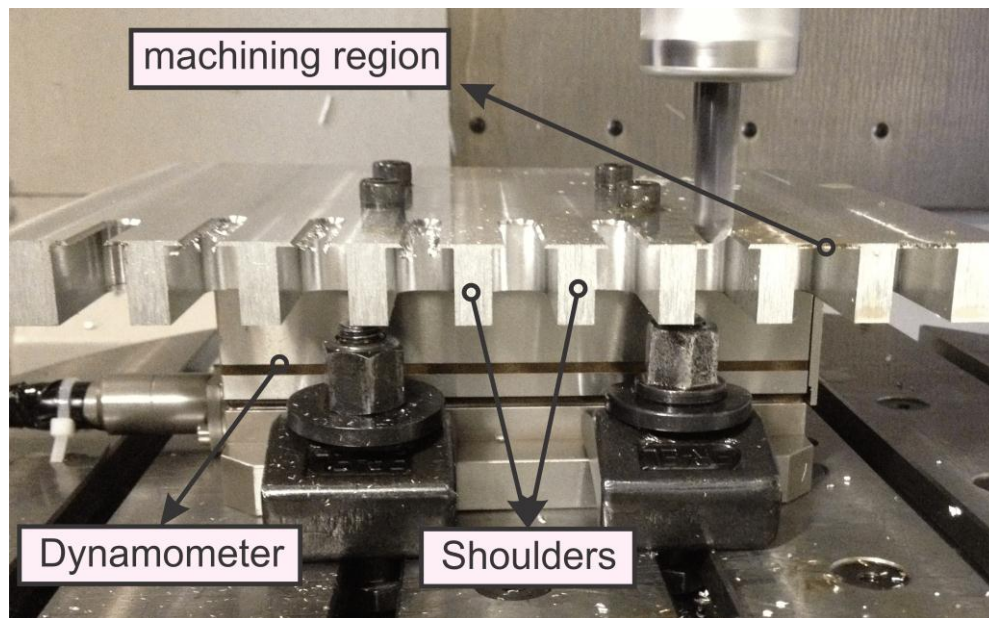


Figure 4-9 – Experimental Setup for prediction calibration test.

Cutting forces were measured using a table type dynamometer with sampling rate of 10000 Hz. The average of the measured cutting forces in X, Y and Z directions were used in Equation (4-12) and (4-13) to come up with the radial, tangential and zenith cutting coefficients. In order to find the edge and cutting forces, a series of slot cutting with different feedrate were performed. Cutting and edge portion of milling forces correspond respectively to the slope and intercept of the line that relate measured average forces and feedrate.

4.6 Results and discussion

Cutting coefficients are predicted by the common mechanistic method available in the literature, proposed calibration method and orthogonal to the oblique transformation model. The predicted cutting coefficients are demonstrated and compared in Figure 4-10 and Figure 4-11.

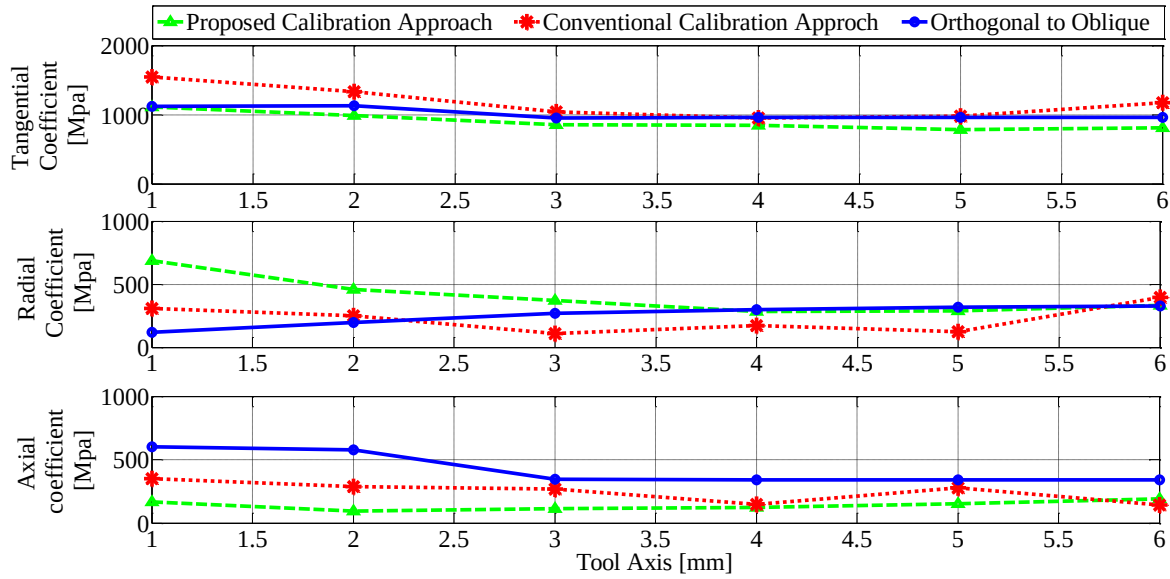


Figure 4-10 - Predicted mechanistic cutting coefficients.

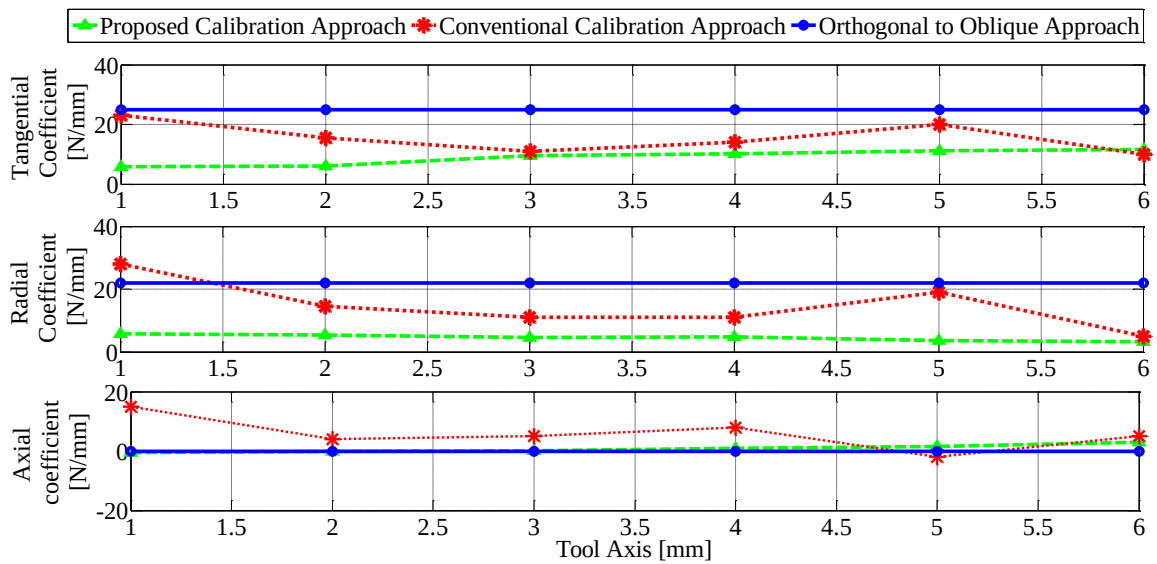


Figure 4-11 - Predicted mechanistic edge coefficients.

In the common approaches [94][95], the contribution of each cutting disk is not measured directly. However, in the proposed approach, cutting forces generated by each cutting disk is measured while only that disk is in contact with the workpiece. According to Figure 4-10, the cutting coefficients at the tip of the tool are higher. The estimated cutting coefficients in tangential and zenith direction are generally lower than the ones predicted by common method. However, radial cutting constant is higher. Since the magnitude of the edge cutting coefficients are quite lower in contrast to cutting coefficient, their effect on the total forces is lower. According to Figure 4-11 predicted mechanistic edge coefficients are almost always lower than the ones estimated using common approach.

In order to validate the proposed model, more than 50 tests for different feedrate and cutting conditions were performed. In order to minimize the uncertainty, each test was performed three times. The feedrate varied from 0.027 [mm/tooth] to 0.12 [mm/tooth], the axial depth of cut varied from 1 [mm] to 6 [mm]. The tests were conducted without any coolant and just air was used for cleaning the chips. The forces were measured with a three component Kistler table type dynamometer. The sampling rate of signal acquisition was set as 10000 Hz. The spindle speed was kept constant as 4500 [rpm]. FFT analysis was performed to make sure that no excessive vibration and chatter phenomenon occurred during the measurement. A lowpass Butterworth filter was used to filter the noises during the measurement. Three of those tests for up milling, down milling and slot cutting are presented in Figure 4-12, Figure 4-13 and Figure 4-14.

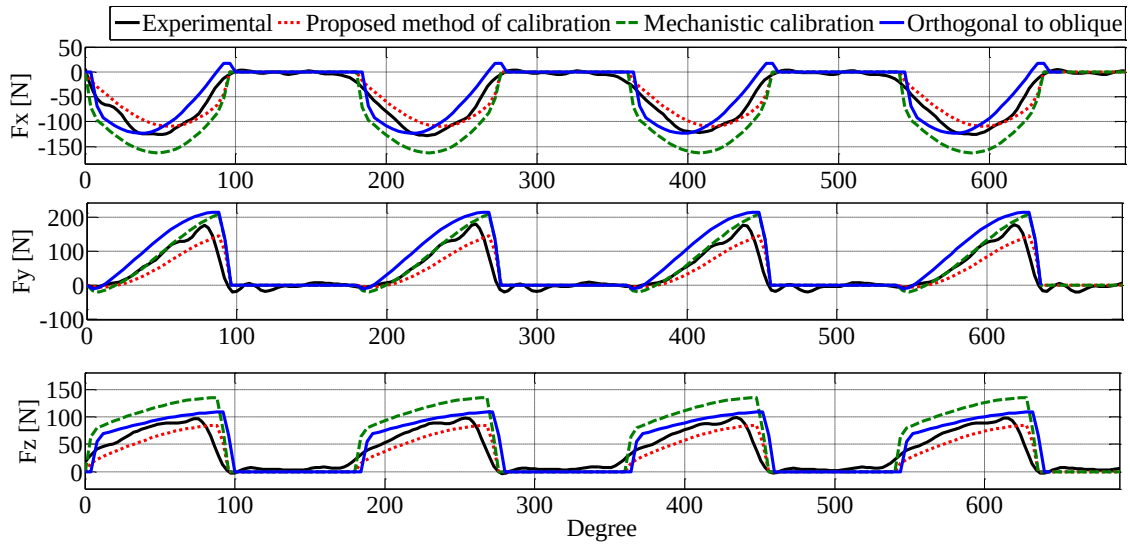


Figure 4-12 - Measured and simulated cutting forces, depth of cut of 0-1 [mm], Up milling, Feed = 1080 [mm/min], half immersion, Spindle Speed = 4500 [rpm].

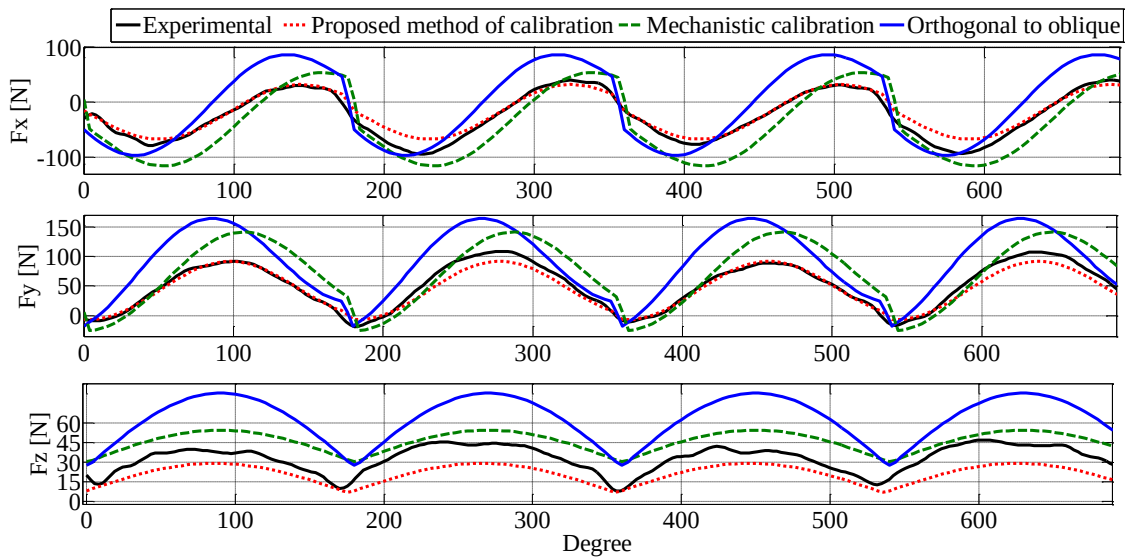


Figure 4-13 - Measured and simulated cutting forces, depth of cut of 1-2 [mm], Feed = 720 [mm/min], Slot milling, Spindle Speed = 4500 [rpm].

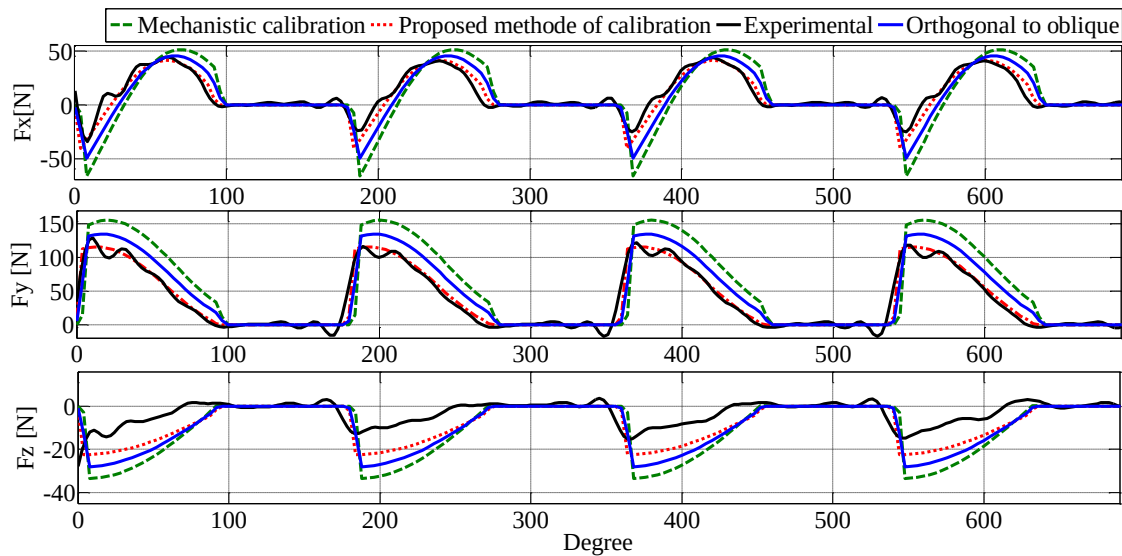


Figure 4-14 - Measured and simulated cutting forces, depth of cut of 5-6 [mm], Down milling, Feed = 1080 [mm/min], half immersion, Spindle Speed = 4500 [rpm].

In the up milling case in Figure 4-12, estimated cutting forces are more accurate in direction of X and Z using the proposed method. In the case of slot cutting in Figure 4-13, the proposed method can estimate the cutting forces in X and Y direction with a better accuracy. But both of the methods are not exact to predict the force in Z direction. The proposed method underestimates the force in the Z direction and the other one overestimates the force in Z direction. In down milling case that is represented in Figure 4-14, the new method predicts cutting forces with better accuracy in all three directions.

One of the most challenging issues in predicting the cutting forces in milling operations is estimation of cutting forces in the Z direction. As is presented in the above figures, the advantage of the proposed approach is the better accuracy in simulation of cutting forces in Z direction. Besides, the estimated cutting forces in both X and Y directions are considerably more accurate rather than the conventional mechanistic model. The main reasons for the higher error in Z direction are indentation, ploughing and rubbing of the

cutting edge that has a bigger effect on Z component of cutting forces [50]. In order to compensate for the error in estimated force in Z direction, the indentation effect should be considered by doing elastic-plasticity analysis that is explained in reference [50]. The proposed mechanistic model can be used in machining of complicated freeform surfaces in 3- and five-axis ball-end milling for reliable prediction of cutting forces.

In order to determine the accuracy of the proposed model, the root mean square deviation (RMSD) and R-square of the simulated cutting forces are calculated and tabulated in Table 4-6 for three cases which are demonstrated in previous figures. As it can be inferred from this table, the normalized RMSD values of the simulated cutting forces using the proposed model are considerably smaller than the common approach and orthogonal to oblique transformation method. The R-square value of the proposed approach is close to 1 thus meaning better accuracy of the proposed approach. In some cases the R-square values are negative due to the estimation error of the models. In those cases, R-square cannot be interpreted as the square of a correlation. Such situations indicate that a constant term should be added to the predicted values.

Table 4-6 – Comparison of the RMSD and R-square of two approaches.

Cutting Condition	Force	Normalized RMSD (Common calibration method)	Normalized RMSD (Proposed calibration method)	Normalized RMSD (Orthogonal to Oblique)	R-square (Common calibration method)	R-square (Proposed calibration method)	R-square (Orthogonal to oblique)
doc = 0-1 [mm] Up milling Feed = 1080[mm/min] half immersion	Fx	0.195	0.124	0.166	0.564	0.921	0.646
	Fy	0.102	0.112	0.186	0.895	0.845	0.596
	Fz	0.234	0.175	0.199	0.226	0.831	0.767
doc = 1-2 [mm] Feed = 720 [mm/min] Slot milling	Fx	0.174	0.117	0.210	0.525	0.932	0.199
	Fy	0.189	0.096	0.247	0.322	0.939	0.166
	Fz	0.549	0.592	0.668	-0.917	-0.880	-1.484
doc = 5-6 [mm] Down milling Feed = 1080 [mm/min] half immersion	Fx	0.137	0.064	0.095	0.653	0.911	0.795
	Fy	0.220	0.082	0.168	0.399	0.954	0.775
	Fz	0.3557	0.3177	0.3397	-4.2247	-0.8683	-2.1395

4.7 Summary

In this chapter, a new mechanistic identification method to predict the cutting coefficients for ball-end milling operation was presented. Despite the common methods, in the proposed approach the mathematical equations and measured cutting forces are specifically implemented for each cutting disk along the tool axis.

The advantage of this model is the better accuracy in predicting the cutting forces, especially for five-axis ball-end milling in which the engagement is mostly partial. The exactness of this approach is shown by comparing it with the other approaches. Since the modeling of phenomena such as tool indentation and ploughing is quite difficult, implementing of the proposed approach can be fast and reliable solution due to implicit consideration of these effects.

Finally, the validation of the model is examined using several experimental tests in different cutting conditions. The presented model can be used in simulation of cutting forces for machining of complex freeform surfaces in three- and five-axis ball-end milling with acceptable accuracy.

Chapter 5

MECHANICS OF FIVE-AXIS BALL-END MILLING OPERATION

5.1 Overview

Milling operation is one of the most fundamental metal cutting operations, which is extensively being used in manufacturing. Milling involves feeding in multi-directions to a rotating multi-edged tool into or across the workpiece. Unlike turning, which is a continuous cutting operation, milling is an interrupted machining operation, where the cutting edges enter and exit the engagement domain. Due to this fact, the cutting forces in milling operation are harmonic and depending on the dynamic characteristics of the system can affect the performance and efficiency of the machining operation.

Considering the geometry of the tool, coating, workpiece material and machine kinematics, quite diverse type of milling operation exists. All types of the milling operation provide good quality of the machined surface with good accuracy and repeatable dimensions. This operation can be used for heavy metal cutting as a roughing operation to remove as much material as possible. Moreover, because of the flexibility and wide range of tool geometry, this operation is used for finishing and ultra-finishing operations with quiet tight dimensional tolerances.

If the cutting edge of the milling tool is rounded to form a spherical nose, ball-end-mill tool will result. This type of the milling tool is efficiently capable of producing blended and curved workpieces with complex freeform features, supporting drilling, contouring, trochoidal and other conventional milling operations. Using ball-end mill tools, gouging, over cutting and under cutting can be avoided due to the curvature of the tool by not penetrating the back or front of the tool into the workpiece. All of these characteristics, makes the ball nose mills ideal for machining of 3-dimensional contour shapes, for example, in die and molds and five-axis freeform surface machining.

In this thesis research, the mechanics of five-axis ball-end milling operation is investigated based on a mechanistic cutting force model. Using the modelled engagement domain and mechanistic cutting force coefficients as boundary conditions, cutting forces are predicted by estimating the instantaneous chip thickness. The geometry of the cutting flute is precisely modelled by scanning the tool using laser sensor, and based on that the helix angle and phase angle of the tool are predicted. In order to validate the model, the necessary transformation matrixes are calculated to transform the predicted cutting forces to the measurement coordinate system.

5.2 Geometry of ball-end mill tool

Despite flat end mill tools, the helix angle, rake angle and cutting coefficients of ball-end mill tools are not constant across the cutting flute due the geometry of the cutter in the spherical region. Figure 5-1 is an illustration of the cutting flute and the cutting forces applied on the tool. The instantaneous angular position of the tool is denoted by θ , and the phase difference of each point on the cutting flute, such as point P, is illustrated by ψ . In contrast flat end mill tools, ψ is not constant in the ball nose section of the tool and varies from zero at the tool tip to i_0 at the cylindrical part of the tool. Consequently, identifying the local helix angle and the phase angle at point P is necessary for prediction of the geometry and related mechanical analysis.

Local phase angle ψ can be predicted analytically and experimentally. The general analytical formulation of local lag angle can be written as the following [32]:

$$\psi = \frac{z \tan(i_0)}{R} \quad (5-1)$$

Above, R is the tool nominal radius and i_0 is nominal helix angle.

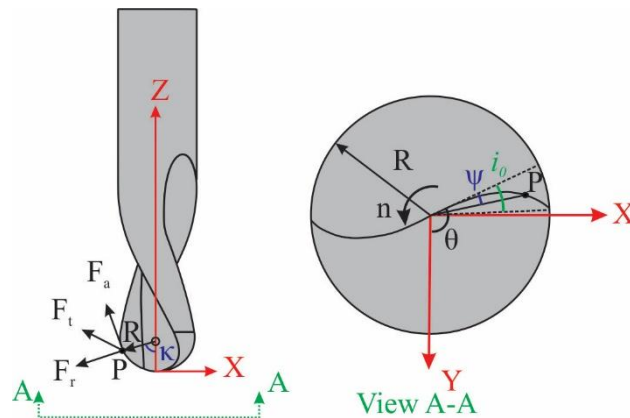


Figure 5-1 – Geometry of ball-end mill tool and cutting forces.

A special experimental setup is designed to measure the helix angle empirically. This setup consists of two displacement laser sensors attached to the table of the machine oriented in X and Y directions. One laser was aimed at the tip of the tool and the other one measured the phase angle at different height while the spindle was rotating with 50 rpm speed. Figure 5-2 illustrates the experimental setup used for measuring the local helix angle and phase angle.

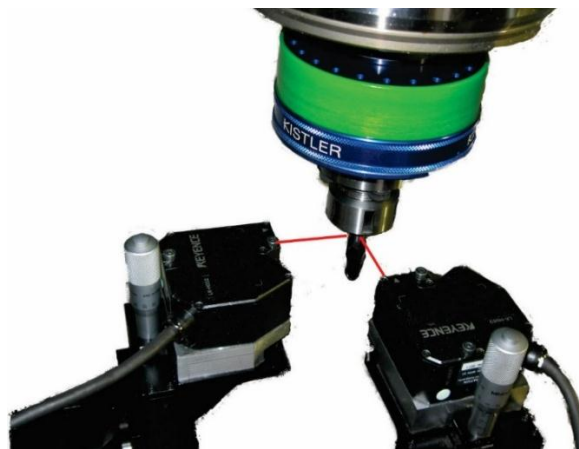


Figure 5-2 – Experimental setup for helix angle measurement

The measured phase angle at different height level is tabulated in Table 4-5. Figure 5-3 illustrates measured phase lag angle ψ for each height level and a three degree polynomial which is fitted to the measured data. According the polynomial curve fitting, the phase delay angle as a function of Z can be written as the following in degree:

$$\psi(z) = 0.070z^3 - 0.790z^2 + 6.486z + 0.059 \quad (5-2)$$

Table 5-1 – Measured phase angle

Height from the tip [mm]	time delay [sec.]	phase delay ψ [deg.]
0.0	0.255	0.000
0.5	0.266	2.640
1.0	0.276	5.040
1.5	0.287	7.680
2.0	0.297	10.080
2.5	0.307	12.480
3.0	0.314	14.160
3.5	0.325	16.800
4.0	0.332	18.480
4.5	0.339	20.160
5.0	0.347	22.080
5.5	0.353	23.520
6.0	0.359	24.960

The cutting flute and local helix angle are modelled based on Equation (5-2) and the geometric information was later used in the cutting force model. Having an accurate prediction of the phase lag and local helix angle is critical in prediction of the cutting forces. Figure 5-4 illustrates the modelled cutting flute based on the measured data using the experimental setup shown in Figure 5-2.

Comparison between the analytically predicted phase lag angle and the experimentally measured phase lag angle is illustrated in Figure 5-5. The analytical points in this figure are predicted from the classical Equation (5-1). As it can be inferred from Figure 5-5 the analytical formula always overestimates the local phase lag angle. The blue line in this figure is a three degree polynomial fitted to the red points as the experimental data points.

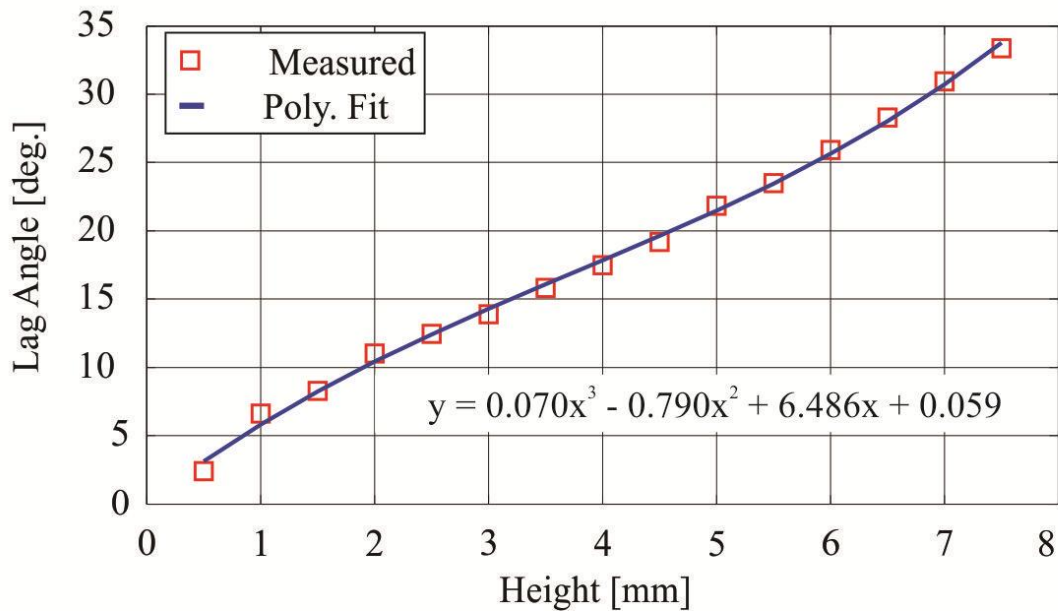


Figure 5-3 – Measured and fitted phase lag.

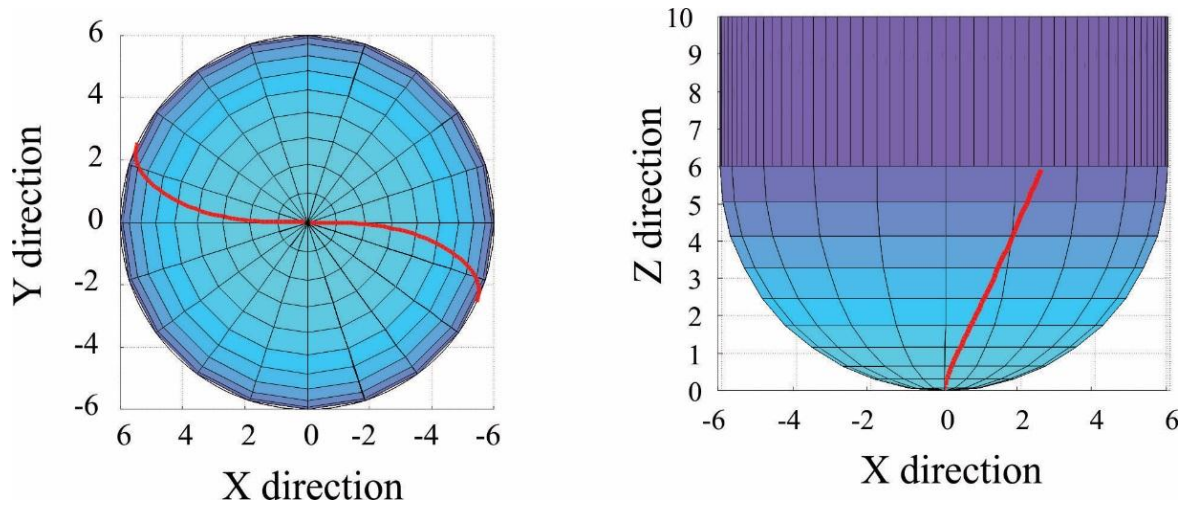


Figure 5-4 – Cutting flute modeled based on the measured data and Equation (5-2).

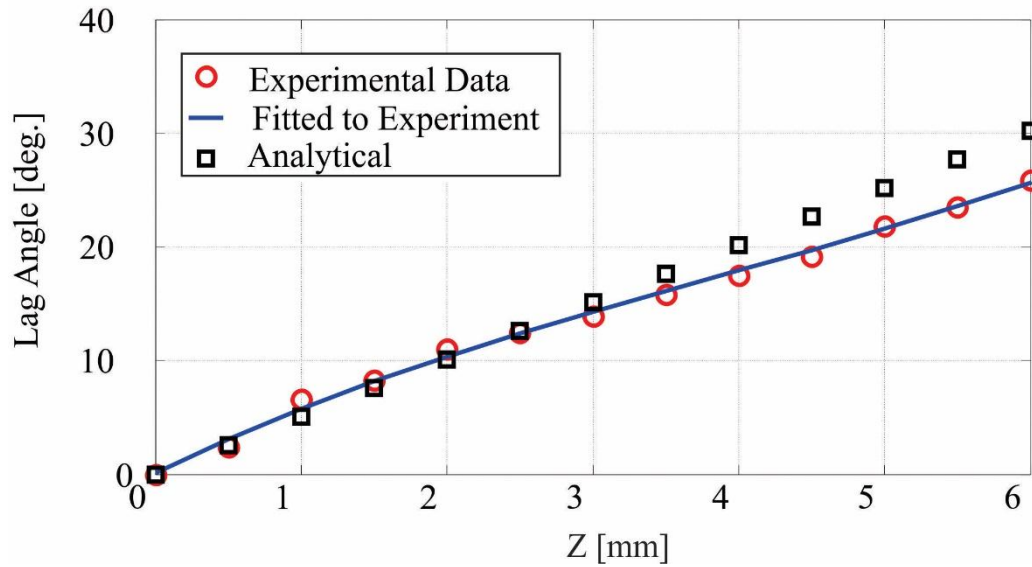


Figure 5-5 – Comparison between the measured and analytical phase lag.

5.3 Geometry of five-axis milling

In contrast to three-axis milling that can be defined by three translational movements, five-axis milling contains two further rotational movements as well. Therefore, tool axis vector in five axis milling is not constant. In three-axis machining, tool coordinate frame (TCF) and workpiece coordinate frame (WCF) are coincident. However, in five-axis milling because of two more rotational axis, a transformation matrix has to be defined to relate TCF and WCF. This transformation matrix is needed for simulation of the cutting forces and comparing the results with the measured cutting forces in rotary or table type dynamo meter coordinate frame.

Two rotational movements of five-axis milling can be represented by lead and tilt angles. Tool orientation, lead angle and tilt angle are illustrated in Figure 5-6. As it can be seen in this figure, lead angle is defined as the angle between the projection vector of the tool axis on feed plane and the normal vector to the machined surface. Lead angle is denoted as l in Figure 5-6. Tilt angle, which is indicated as t in the Figure 5-6, is the angle between the tool axis and the feed plane. The advantage of using lead and tilt angles is avoiding the tip of the tool from contact, due to the zero cutting speed at the tool tip.

Figure 5-7 is an illustration of the conventional coordinate frames in five-axis milling. $X_w Y_w Z_w$ represents workpiece coordinate frame (WFC) which is a fixed coordinated attached to the workpiece. This coordinate frame can be coincident with the machine coordinate frame which is used as reference coordinate frame for G-code generation. The cutting forces are measured in this coordinate frame using the table type dynamometer. Feed coordinate frame (FCF) is shown by $X_f Y_f Z_f$ in Figure 5-7. According to this coordinate frame, Z_f is collinear with the tool axis and the feed vector is located in $X_f Z_f$ plane. Feed coordinate frame is the reference coordinate frame for engagement domain calculation and defining the entrance and exit angles, as it was explained in Chapter 3. Tool coordinate frame (TCF) which is presented as $X_t Y_t Z_t$ is coincident with rotary coordinate

dynamometer (RCD). The Z_t of the TCF, which is rotating as the tool rotates, is collinear with FCF.

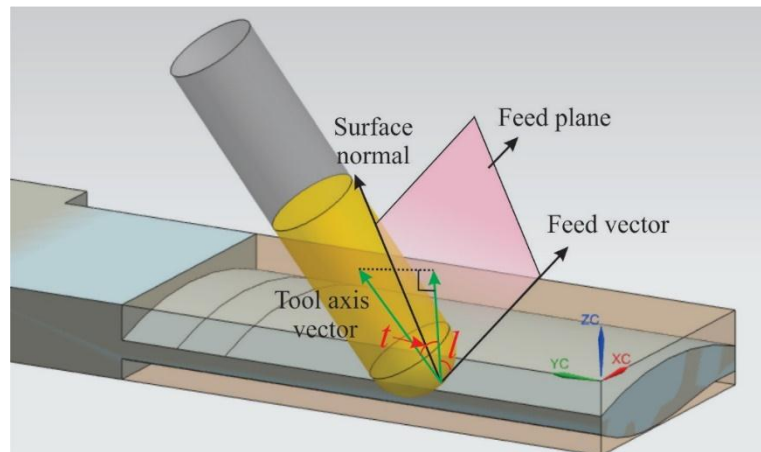


Figure 5-6 - Definition of lead and tilt angles.

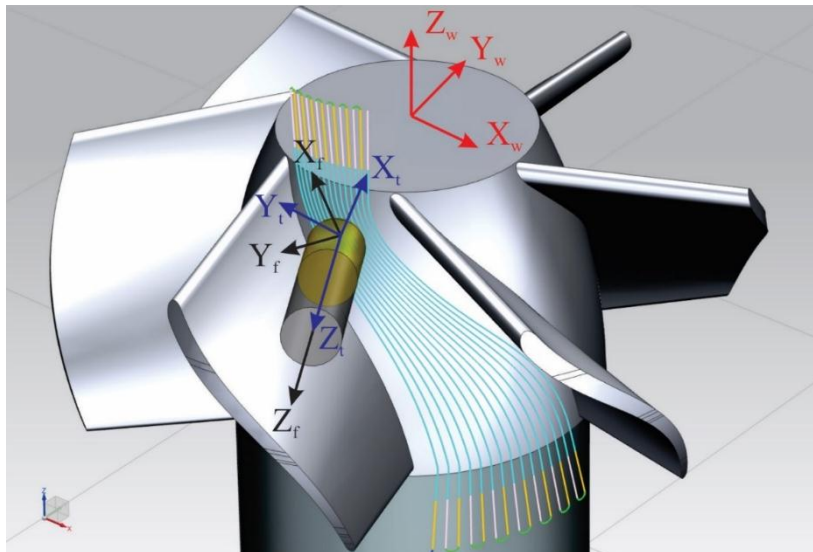


Figure 5-7 – CAD/CAM model and toolpaths in five-axis ball-end milling of an impeller geometry.

5.4 Extracting tool orientation and transformation matrices from CL file

As introduced in Chapter 3, CL file is a text file containing the x , y and z coordinates of the tool tip and direction cosines of the tool axis at each cutter location, which can be exported from any CAM package. A sample CL file is shown in Figure 3-5. One can easily calculate lead and tilt angles from the following equations:

$$l = \text{atan2}(i, \sqrt{j^2 + k^2}) \quad (5-3)$$

$$t = \text{atan2}(-j, k) \quad (5-4)$$

Figure 5-8 depicts the simulated tool tip position, tool path and tool orientation extracted from CAM module of NX for one tool path of the model shown in Figure 5-7. In this figure the red lines represent the cutter orientation and the blue line shows the position and path of the cutter. Because of using a rotary type of dynamometer in validation tests, we need a transformation matrix to transfer cutting simulated forces from FCF to TCF. This transformation matrix can be found using calculated lead and tilt angles from CL file in Equation (5-3) and Equation (5-4) as the following:

$$\begin{bmatrix} F_{xt} \\ F_{yt} \\ F_{zt} \end{bmatrix} = [T]^{-1} \begin{bmatrix} F_{xf} \\ F_{tf} \\ F_{zf} \end{bmatrix} \quad (5-5)$$

$$[T] = \begin{bmatrix} \cos(l) & 0 & \sin(l) \\ \sin(t)\sin(l) & \cos(t) & -\sin(t)\cos(l) \\ -\cos(t)\sin(l) & \sin(t) & \cos(t)\cos(l) \end{bmatrix}$$

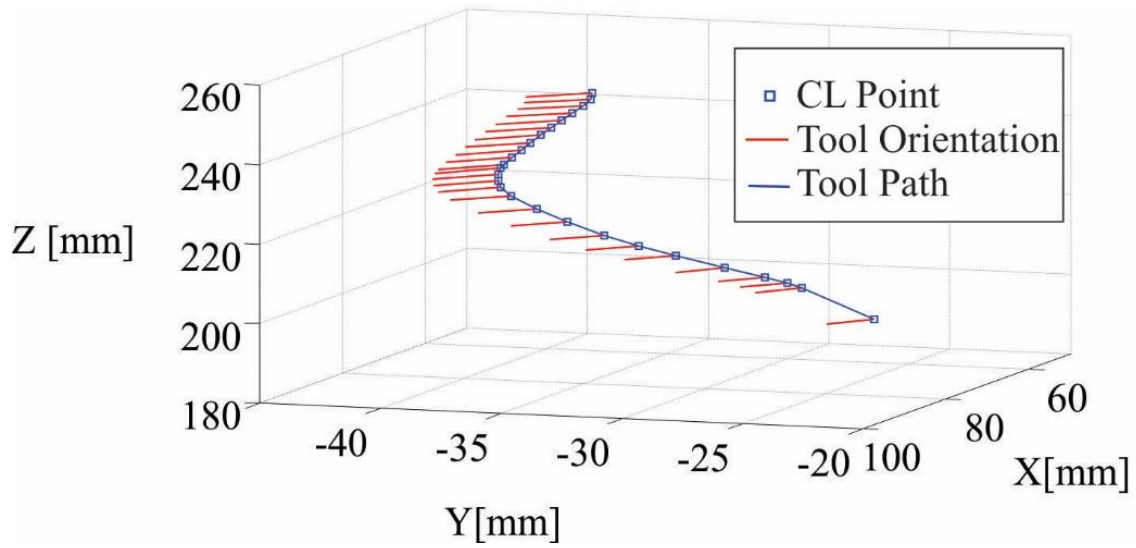


Figure 5-8 – Tool tip position and tool orientation extracted from CL file.

5.5 Chip thickness formulation

In conventional three-axis ball-end milling operation the instantaneous chip thickness can be calculated from Martellotti equation. Figure 5-9 (a) illustrates the instantaneous chip thickness for flute j at angular position of $\theta_j(z)$, where Z is measured from the tip of the tool along tool axis. The chip thickness in $X_f Y_f$ plane can be calculated using the Martellotti equation as the following:

$$h_{cj} = f_x \sin(\theta_j(z)) \quad (5-6)$$

In this equation, f_x is horizontal feed per tooth in $X_f Y_f$ plane and $\theta_j(z)$ is the angular position of j^{th} cutting flute.

In five-axis milling, feed vector has a component in $X_f Z_f$ plane, as well. This component of feed is illustrated as vertical feed per tooth f_z in Figure 5-9 (b). Considering vertical feed, the general instantaneous chip thickness formula can be written as the following:

$$h_{cj} = f_x \sin(\theta_j(z)) \sin(\kappa_j(z)) - \xi f_z \cos(\kappa_j(z)) \quad (5-7)$$

$$\begin{cases} \xi = -1, & f_z < 0; \\ \xi = +1, & f_z > 0; \end{cases}$$

Above, f_z is vertical feed per tooth, and $\kappa_j(z)$ is the zenith angle. ξ is direction factor and if the vertical feed is downward, its magnitude is -1 and if it is upward, its magnitude is +1.

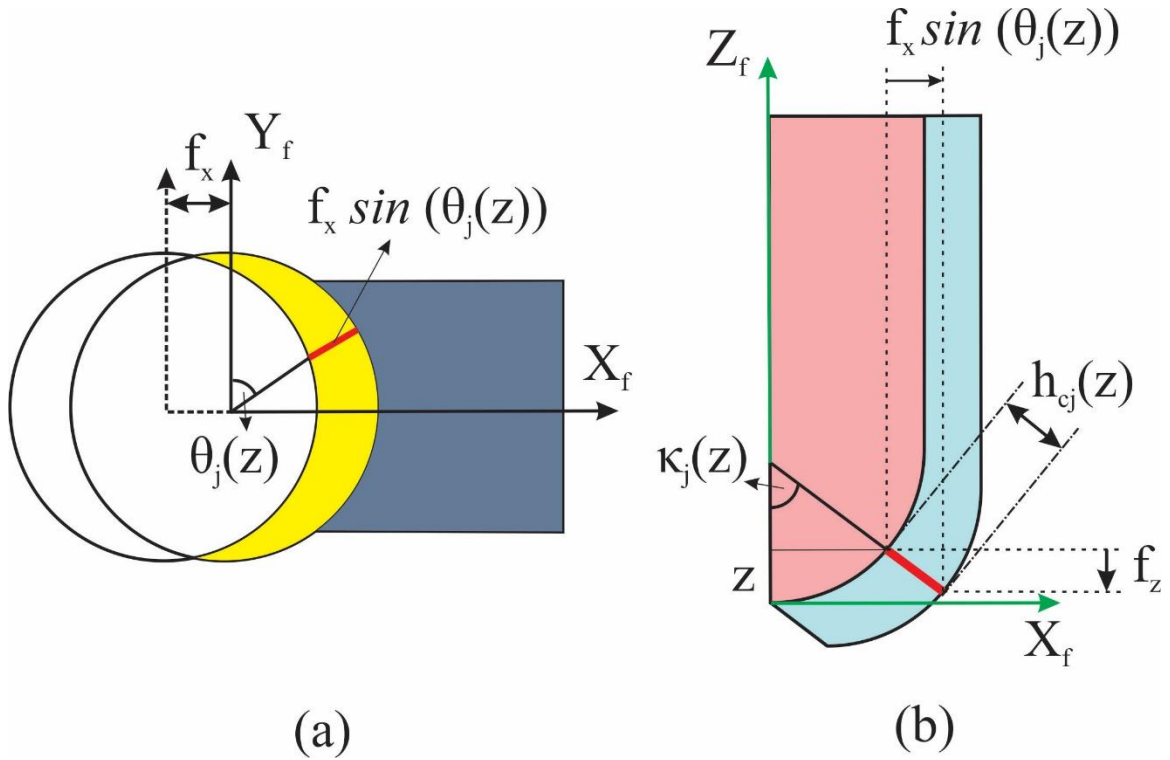


Figure 5-9 – Chip thickness model.

In Equation (5-7), $\theta_j(z)$ is the immersion angle of the cutting point and considering Figure 5-1, the immersion angle of a discrete cutting point on the flute of the cutter is given as:

$$\theta_j(z) = \theta + 2\pi(j - 1)/N_f - \psi(z) \quad (5-8)$$

Above, θ is the reference angle of the cutter, j represents the number of the current flute, N_f is the total number of flutes and $\psi(z)$ is the phase lag due to helix angle of the cutter in the corresponding k^{th} disk calculated from Equation (5-2).

Knowing the uncut chip thickness, the instantaneous infinitesimal chip load can be written as follows:

$$dA_c = h_{cj} \times dz \quad (5-9)$$

For a differential chip load dA_c in the engagement domain, the differential cutting forces in radial, axial, and tangential directions (r, a, t), shown in Figure 5-1, can be written as the following:

$$\begin{aligned} dF_r &= K_{rc} \times dA_c + K_{re} \times dz \\ dF_a &= K_{ac} \times dA_c + K_{ae} \times dz \\ dF_t &= K_{tc} \times dA_c + K_{te} \times dz \end{aligned} \quad (5-10)$$

Where K_{rc} , K_{ac} and K_{tc} are radial, axial and tangential cutting force coefficients and K_{re} , K_{ae} and K_{te} are cutting edge coefficients, respectively. Cutting force and edge coefficients are determined by the calibration procedure where those coefficients vary along the tool axis direction. A novel approach is introduced in Chapter 4 to accurately predict those cutting constants.

Transformation matrix A transforms the cutting forces from local coordinate into feed coordinate frame (FCF). As it was stated in the previous sections, FCF is defined in a way that feed vector locates in $X_f Z_f$ plane.

$$[A] = \begin{bmatrix} -\sin(\kappa_j(z))\sin(\theta_j(z)) & -\cos(\kappa_j(z))\sin(\theta_j(z)) & -\cos(\theta_j(z)) \\ -\sin(\kappa_j(z))\sin(\theta_j(z)) & -\sin(\kappa_j(z))\sin(\theta_j(z)) & \sin(\theta_j(z)) \\ \cos(\kappa_j(z)) & -\sin(\kappa_j(z)) & 0 \end{bmatrix} \quad (5-11)$$

Consequently, cutting forces in FCF can be predicted using the following equation:

$$\begin{bmatrix} F_{xf} \\ F_{tf} \\ F_{zf} \end{bmatrix} = [A] \times \begin{bmatrix} dF_r \\ dF_a \\ dF_t \end{bmatrix} \quad (5-12)$$

Considering the Equation (5-5), the elemental cutting forces in tool coordinate frame (TCF) can be represented as the following:

$$\begin{bmatrix} F_{xt} \\ F_{yt} \\ F_{zt} \end{bmatrix} = [T]^{-1}[A] \times \begin{bmatrix} dF_r \\ dF_a \\ dF_t \end{bmatrix} \quad (5-13)$$

Equation (5-12) is used for validation a table type dynamometer. However, Equation (5-13) is used for validation of the cutting forces via rotary type dynamometer. It is experimentally shown in the next section that introduced mechanistic cutting force model is capable of simulation of cutting forces with an acceptable accuracy.

5.6 Experimental validation

The proposed mechanistic cutting force model was used as a core program for several applications such as force based feedrate scheduling, feedrate regulation based on the constraints of the system, tool deflection model and tool orientation optimization. In all the

mentioned applications the cutting force model was able to predict the trend and magnitude of cutting forces with a very good accuracy. In order to show the accuracy of the force model two machining case are thoroughly presented in this section.

Figure 5-10 is an illustration of the experimental setup used for validation of the cutting force model. In this setup a Kistler rotary type dynamometer 9123C series was used for measuring the cutting forces in tool coordinate frame (TCF). All the test were conducted on a MORI SEIKI NMV 5000 DCG five-axis vertical milling center in TCP mode. The G-codes are exported from NX using a dedicated postprocessor for the machine center. The tool is adjusted in the tool holder to have less than 50 micron runout.

In the first validation test, the cutting forces were simulated and measured for a blisk geometry which is shown in Figure 5-11. The engagement for a sample CL point and the IPW for roughing operation is illustrated in this figure as well. The workpiece material was Al7075 and a two fluted Sandvik ball-end mill with 6 mm diameter was used. The other cutting conditions are listed in

Table 5-2 – Cutting conditions for machining of the blisk

Nominal helix angle	30°
Tool diameter	6 [mm]
Spindle Speed	5000 [rpm]
Feed Rate	750 [mm/min]
Step Over	5.4 [mm]
Maximum Depth of Cut	1.5 [mm]
Coolant	No coolant



Figure 5-10 – Experimental setup.

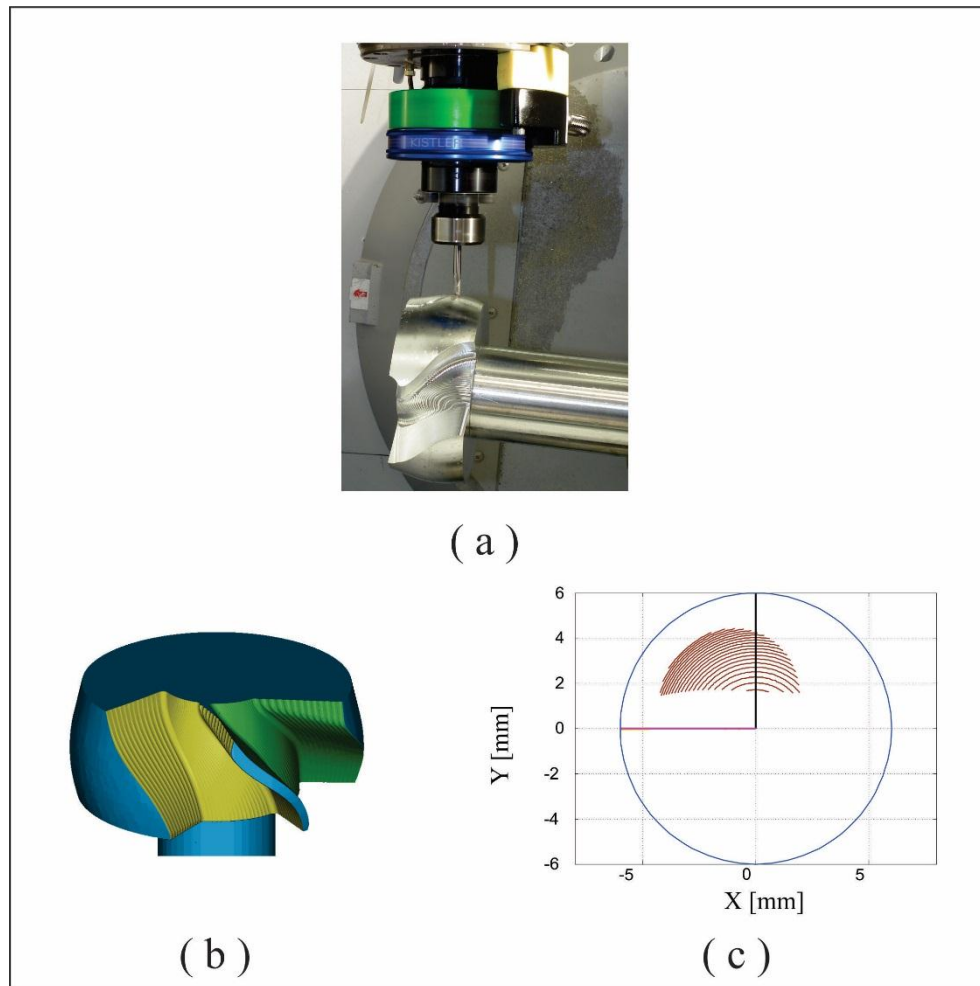


Figure 5-11 – (a) Experimental setup for machining of the impeller geometry, (b) Simulated In-process workpiece (IPW), (c) A sample of simulated 2D engagement map.

The simulated and measured cutting forces in X, Y and Z direction of TCF are respectively illustrated in Figure 5-12, Figure 5-13 and Figure 5-14. As it can be deduced from the provided validation graphs, the trend and the magnitude of the cutting forces are simulated with a good agreement with the experimental data. The simulated cutting force in Z direction shows more discrepancy which can be attributed to the ploughing and noncutting effects not considered in the mechanistic cutting force model.

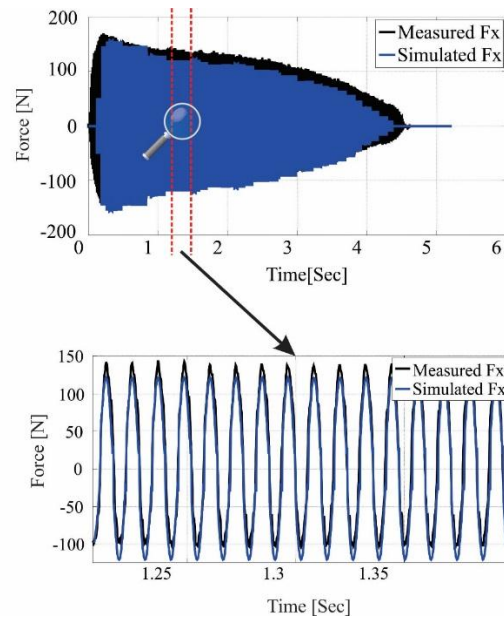


Figure 5-12 – Simulated and measured cutting forces for the blisk in X direction of FCF.

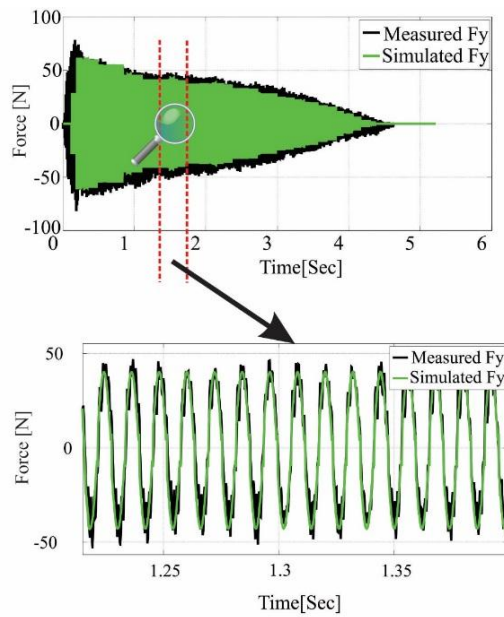


Figure 5-13 - – Simulated and measured cutting forces for the blisk in Y direction of FCF.

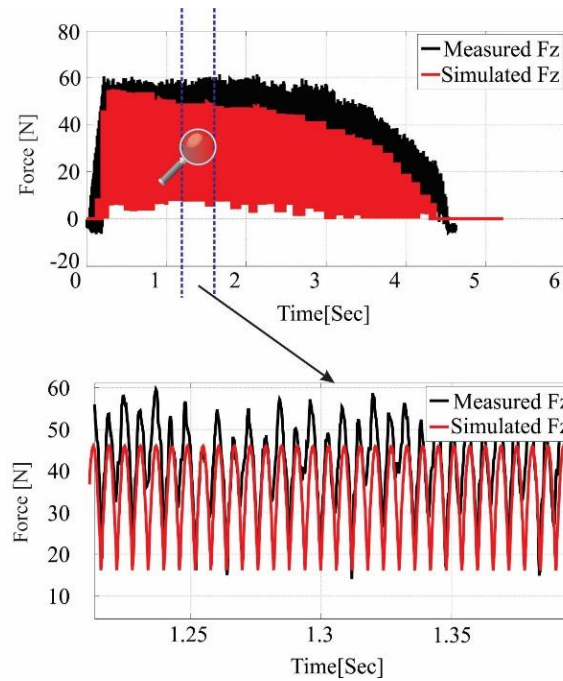


Figure 5-14 – Simulated and measured cutting forces for the blisk in Z direction of FCF.

In the second validation test setup, represented in Figure 5-15, an airfoil geometry was machined as a freeform surface geometry. The workpiece material was Al7050 and a two fluted Carbide ball-end mill tool with 12 [mm] diameter was used for the experimental validation. The other cutting conditions are tabulated in the Table 5-3.

The simulated and experimental cutting forces are represented in X, Y and Z directions in Figure 5-16, Figure 5-17 and Figure 5-18. As it can be inferred from the presented graphs, the trend and the magnitude of the cutting forces in X and Y directions are simulated in a good agreement with the experimental test. The discrepancy in Z direction can be accounted for by non-cutting phenomena such as ploughing and tool indentation [50].

Table 5-3 – Cutting conditions for machining of the airfoil geometry.

Nominal helix angle	30°	Feed Rate	250 [mm/min]
Coolant	No coolant	Step Over	6 [mm]
Spindle Speed	1500 [rpm]	Maximum Depth of Cut	2 [mm]

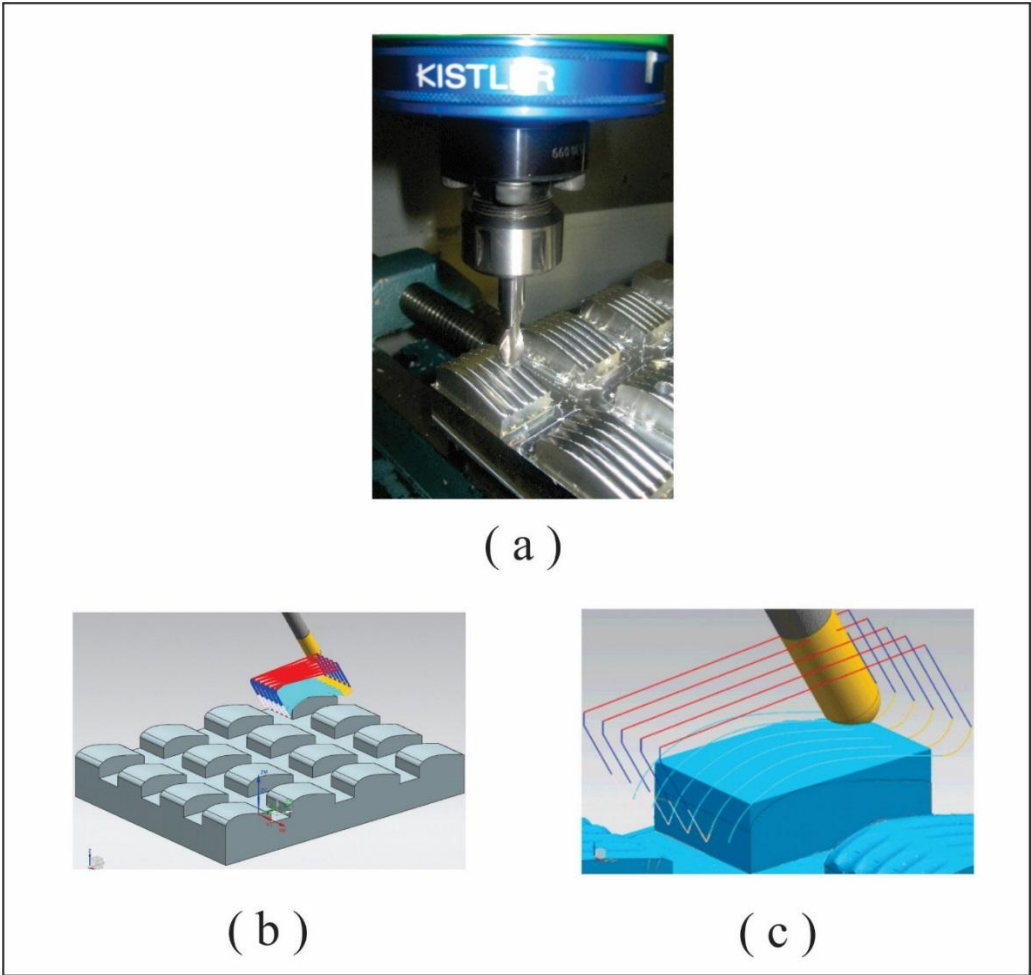


Figure 5-15 - Experimental setup for machining of an airfoil geometry.

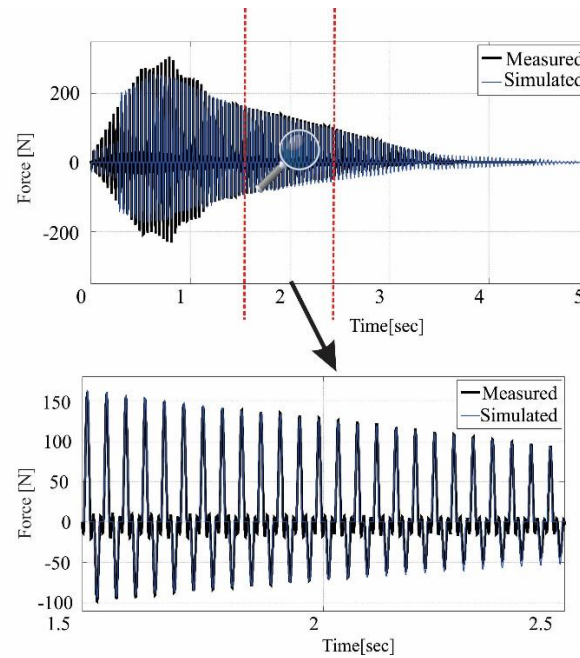


Figure 5-16 – Simulated cutting forces for airfoil geometry in X direction.

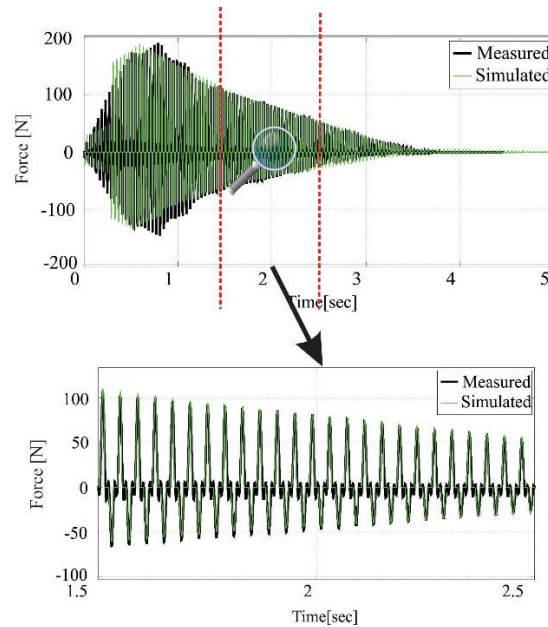


Figure 5-17 - Simulated cutting forces for airfoil geometry in Y direction.

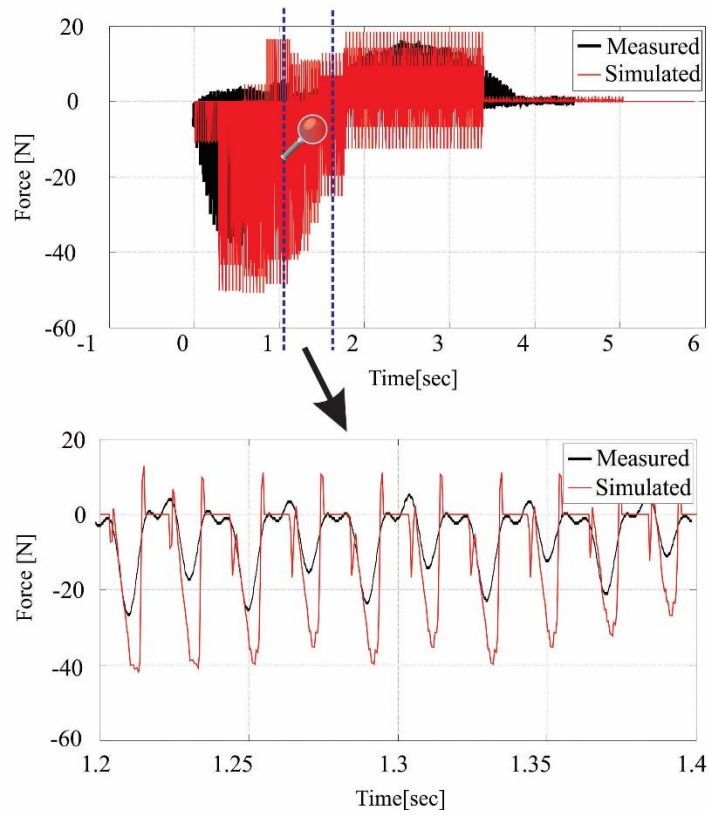


Figure 5-18 - Simulated cutting forces for airfoil geometry in Z direction.

5.7 Summary

A mechanistic cutting force model for five-axis ball-end milling operation is introduced in this chapter. The helix and phase lag angles of the tool are accurately measured using a laser displacement sensor to increase the accuracy of the force prediction. The necessary transformation matrixes are calculated by defining the reference coordinate systems. The cutting force elements are predicted in the local coordinate frame and transferred to the feed coordinate frame. The elemental cutting forces are integrated along the cutting flutes and within the engagement domain to come up with the total cutting forces in the desired coordinate frame.

The proposed force model is validated experimentally in five-axis ball-end milling of freeform geometries. It is shown that the trend and magnitude of the cutting forces can be predicted with an acceptable accuracy in X, Y and Z directions.

The cutting force model introduced in this chapter is used in other applications that cutting forces are needed as an input parameter. Using this model some algorithms are developed such as force based feedrate scheduling, feed drive constraint based feedrate scheduling, tool deflection estimation and tool orientation identification. As is mentioned in the following chapters, the offered cutting force model can be easily integrated to those algorithms.

Chapter 6

FORCE BASED FEEDRATE SCHEDULING

6.1 Overview

Conventional CAD/CAM approaches generate NC codes based on the geometry of the process, regardless of the potential for increasing the efficiency and productivity of the machining operation. Some material removal based feed scheduling is introduced in the literature. However, it is shown that these strategies may not lead to higher efficiency because of ignoring the mechanics of the process [52].

Using a mechanic based model capable of predicting the cutting forces, it is possible to schedule the feedrate off-line. Based on the force model, feed can be increased at the CL points that force is relatively lower. This idea tremendously leads to lower machining cycle time and higher productivity. By knowing the feed drive constraints of the system, it is also possible to generate the off-line scheduled feedrate considering the velocity, acceleration, and jerk limits of each actuator.

Because of the complex geometry of five-axis milling and the change of engagement region between cutter and workpiece through the toolpath, modeling of cutting forces and scheduling the feedrate are quite difficult. In this thesis project an improved force model based feedrate scheduling (FFS) technique for rough cutting of freeform geometries are presented. The aim of the feedrate scheduling technique is to keep resultant cutting forces at a desired limit all along the tool paths in order to decrease the machining time and increasing the productivity. Using this strategy, appropriate feedrate is calculated between consecutive cutter location points in NC codes to maintain the cutting forces at a constant magnitude.

In order to perform a precise and reliable feedrate scheduling algorithm, the kinematics and mechanics of five-axis ball-end milling process should be modeled comprehensively.

In the proposed approach, CL file is extracted from CAM generated toolpath and by sending it to a Parasolid solid modeler kernel the engagement map is calculated. Then, according to instantaneous chip thickness and using a mechanistic model, cutting forces are modeled. Simulated cutting forces for each position of cutter are used in force based feedrate scheduling strategy to select appropriate feedrate at each CL point in order to keep cutting forces below a known threshold. Finally, it will be shown experimentally that this strategy is able to reduce the cycling time and increase the productivity.

6.2 Force based feedrate scheduling method

Because of the complex geometry of the five-axis milling, CAM processors and CNC operators tend to select conservative cutting parameters to avoid tool breakage, low surface quality, tool deflections, machine tool damages, etc. On the other hand, process planning engineers would like to increase the productivity of the machining operation by reducing the machining cycle time. One of the cutting parameters that affects the productivity in a direct manner is feedrate. Conservative constant feedrate values have been widely used in industry for five-axis freeform surface machining due to lack of a comprehensive model that is able to take into account the mechanics of the process. Besides, currently used CAM programs are only based on the geometry of the process and volumetric analysis. Therefore, the next step in developing smart CAM packages is introducing effective algorithms to update the cutting parameters according to different cutting conditions during freeform surface machining.

The offline feedrate scheduling model that is introduced in this study is able to adjust the feedrate values for each NC block based on cutting force model that was introduced in the previous chapter. In five-axis freeform surface milling because of the fluctuation in depth of cut and engagement domain, the resultant cutting force is not constant. By determining the cutting forces using above mentioned force model, appropriate feedrate

values can be selected to keep the cutting forces below a threshold which is determined by the user.

According to Equation (5-10), there is a linear relation between cutting forces and feedrate values. This provides to derive a linear relation depending on feedrate as follows:

$$dF_i = A.f + B \quad (6-1)$$

Where dF_i is differential cutting force, f is feedrate value, and A, B are constant values. An algorithm is developed to keep the resultant force at the desired limit along the tool path for each CL point. The model uses the engagement domain for each CL point defined by the solid modeler kernel. The cutting force based feed limits determined by running the process simulation at two different constant feed values (f_2 and f_1) and logging the resultant cutting force ($F_{1,i}$ and $F_{2,i}$) at each CL point. Here, $F_{1,i}$ is the simulated resultant force at point i is then determined as:

$$f_{lim,i} = (F_{lim,i} - F_{1,i}) \cdot \frac{f_2 - f_1}{F_{2,i} - F_{1,i}} + f_1 \quad (6-2)$$

The calculated feedrate from the above equation, are plugged into the corresponding NC code line to keep the resultant cutting force constant. It is experimentally shown in the next section that this strategy leads to a considerable decrease in the cycle time.

6.3 Simulation and experimental result

Some simulation and experimental validation tests were conducted to show the performance of the proposed feedrate scheduling strategy in five axis milling of complex freeform surfaces. Figure 6-1 illustrates the CAD/CAM model used as experimental setup. During the machining of such a complex geometry the engagement domain is not constant,

thus leading to variable resultant cutting force at each cutter location point. Consequently, this part can be a good example for demonstration of the proposed strategy.

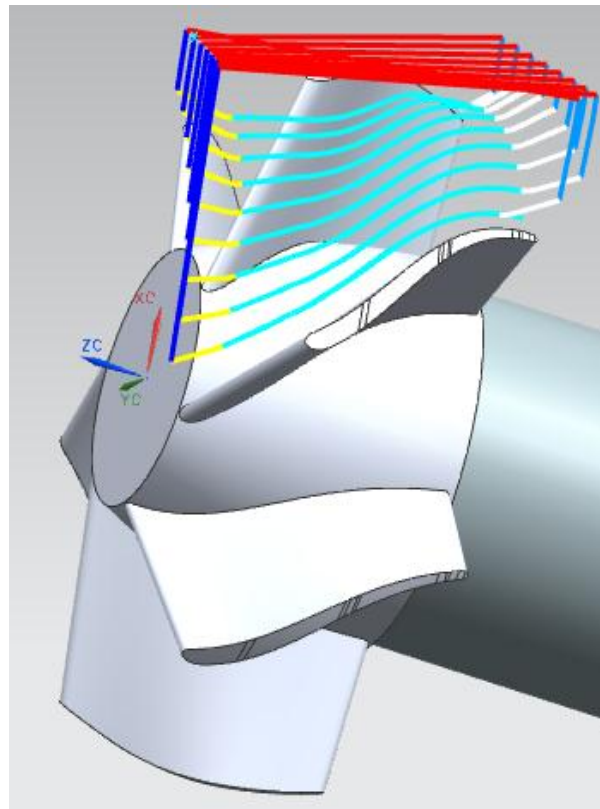


Figure 6-1 - CAM of the experimental workpiece.

The experimental tests were performed on Mori Seiki NMV 5000 DCG five-axis CNC machine using AL7075 workpiece material. A Sandvik two fluted 12 mm ball-end mill with helix angle of 30 degree was used to machine the impeller. Kistler rotary type of dynamometer (model 9123) was used to measure the cutting forces. All of the experiments were performed with the spindle speed of the 4000 rpm. In all the simulations, ball part of the cutter was discretized into disks with 0.1 mm height. Figure 6-2 shows the experimental setup that has been used in this study.

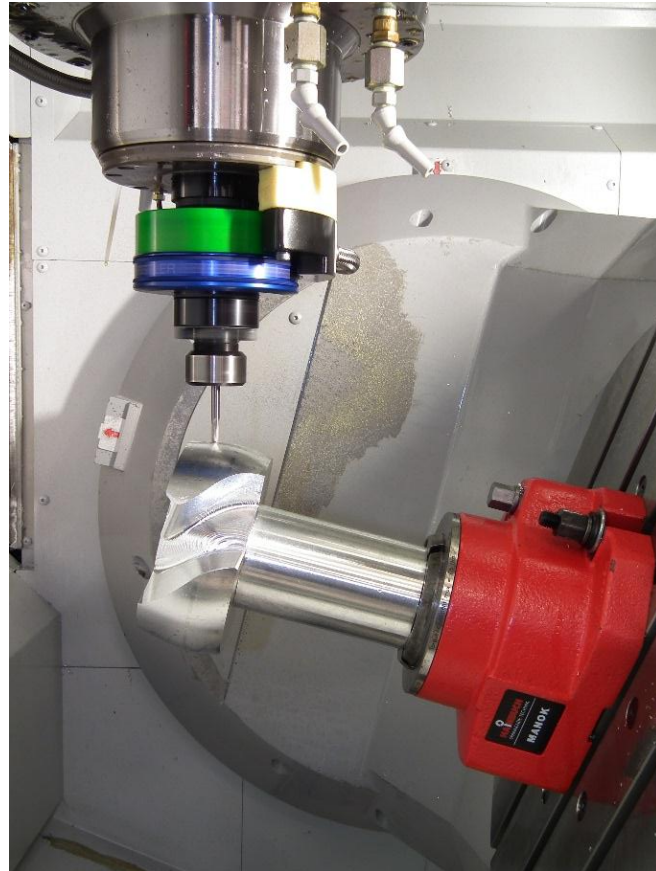


Figure 6-2 - five-axis machining center and experimental setup used to validate the feedrate scheduling strategies.

Figure 6-3 illustrates the envelope of the resultant cutting force. As it can be inferred from this figure, the resultant cutting force is not constant due to the change in engagement domain. The cutting force is maximum at the beginning of the engagement and as the tool moves through the toolpath, resultant cutting force is gradually decreasing. In this figure the dashed black line represents estimated cutting force for constant feedrate of 450 [mm/min] from the mechanistic cutting force model. The solid red line shows the envelope

of measured resultant cutting force using rotary dynamometer. The cutting parameters are tabulated in Table 6-1.

The main source of discrepancy between measured and estimated model in Figure 6-3 is runout and ploughing effect. Figure 6-4 depicts a detailed view of predicted and measured resultant cutting force.

Table 6-1 – Cutting Parameters

Max. Depth of cut [mm]	Step over [mm]	Spindle Speed [rpm]	Feedrate [mm/min]	Tool Diameter [mm]
1	8	4000	450	12

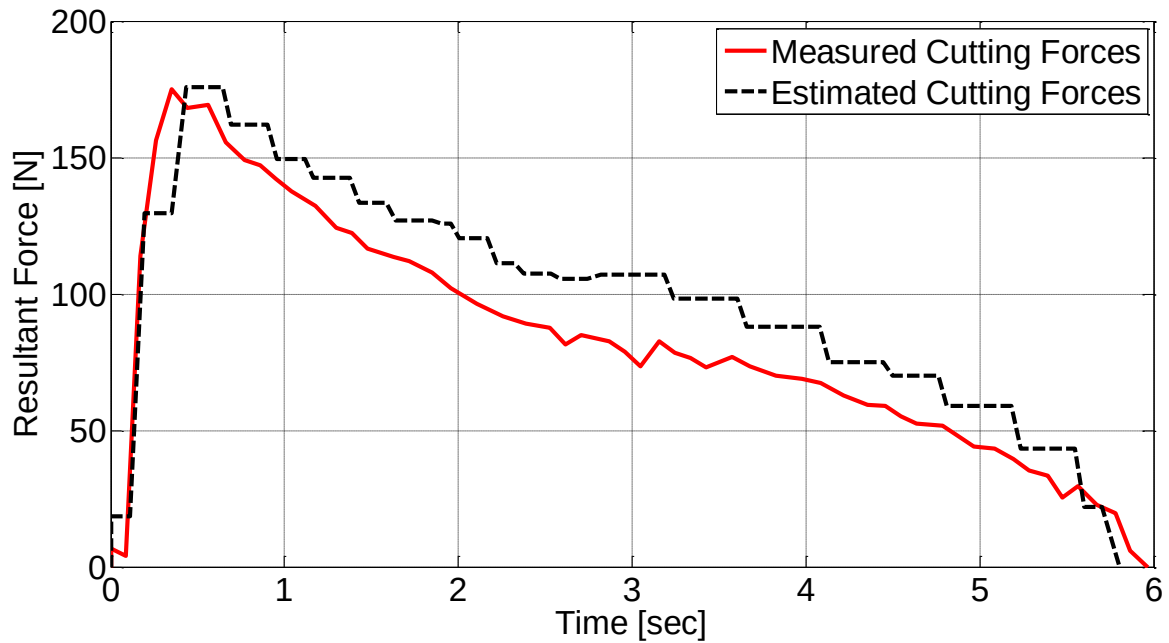


Figure 6-3 - Variation of resultant cutting force through toolpath

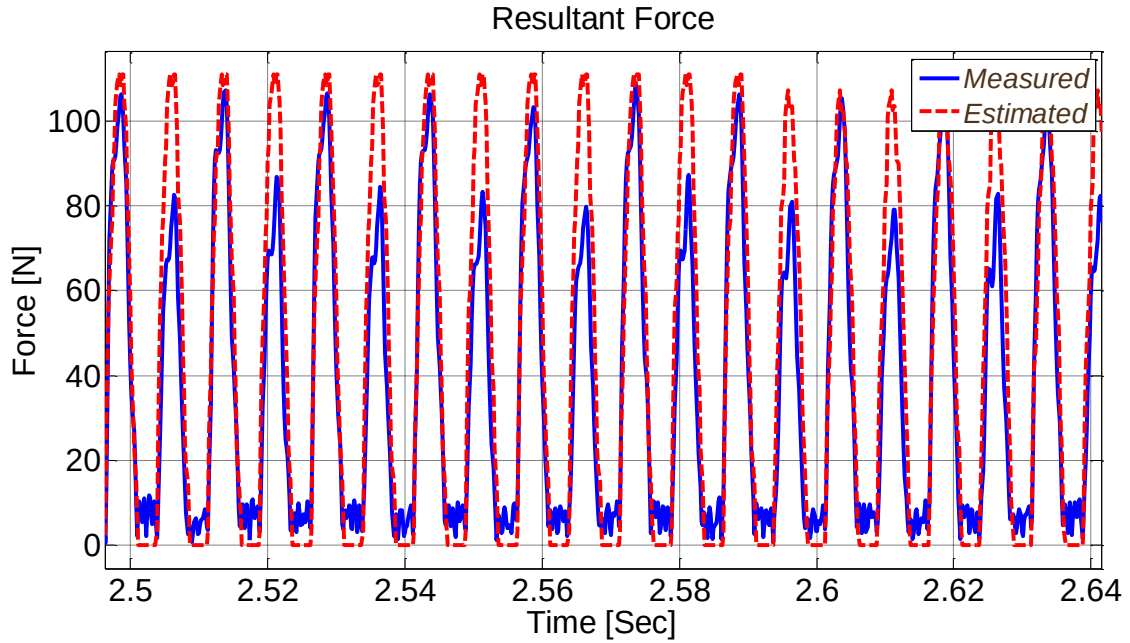


Figure 6-4 Force model validation test for impeller.

As is mentioned in Section 0, the threshold cutting force, which is represented by $F_{lim,i}$ in Equation (6-2), is the limit defined by the user for resultant cutting forces. The feedrate scheduling algorithm regulates the feedrate value in each cutter location to keep resultant cutting forces close to this threshold.

As can be found from Figure 6-3, the cutting forces are not constant through the toolpath and by decreasing the engagement area the cutting force decreases. Therefore, by scheduling the feedrate value, it is possible to keep the cutting forces constant and increase the productivity. In this case, the threshold resultant cutting force in Equation (6-2) was set to 176 (N) which is the maximum resultant cutting force at the beginning of cutting as is shown in Figure 6-3.

The feedrate scheduling algorithm calculates the scheduled feedrate for each CL point and updates the NC code. Figure 6-5 represents the change of feedrate values in each cutter locations to keep the resultant cutting force constant. It should be mentioned that because of the safety, the maximum feedrate was set to be 2500 [mm/min]. This value can be adjusted by the process engineer. As it can be inferred from Figure 6-5, feedrate is lower in the beginning of the tool path at CL # 5. However, the scheduled feed is increasing through the tool path due to the decrease of engagement area and the cutting force, as it was demonstrated in Figure 6-3.

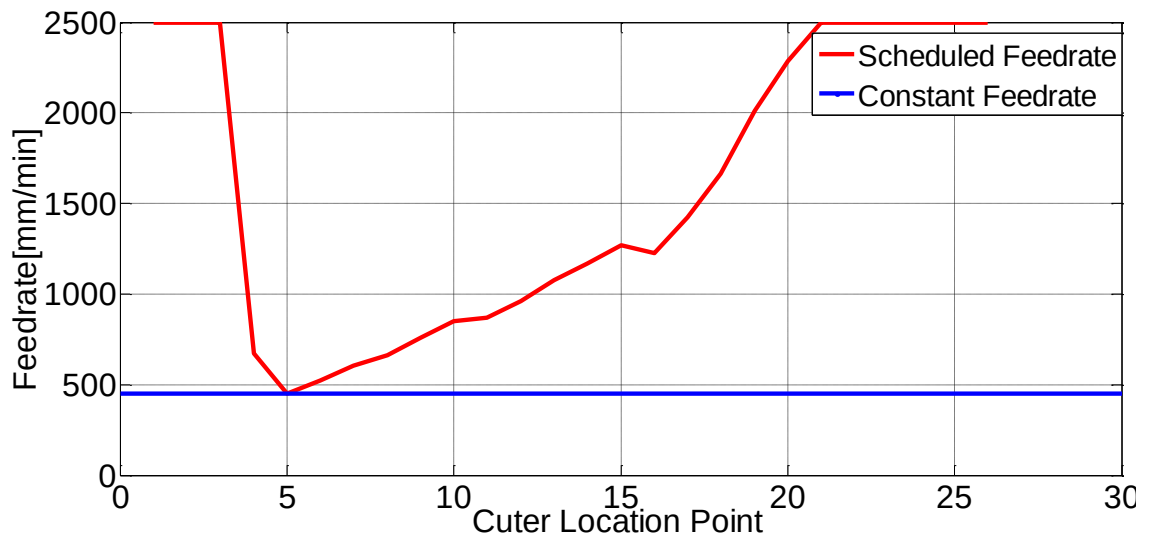


Figure 6-5 - Scheduled feedrate values for each cutter location

Figure 6-6 demonstrates the NC code for conventional machining with constant feedrate and the modified NC code using the scheduled feedrate. A postprocessor is developed to plug in the scheduled feedrate into the corresponding line of the NC code and generate the new modified NC code to be used in the machining center.

<pre> 13 M69 14 M11 15 G00 G90 G54 X10.098 Y2.101 B-33.666 C62.668 S4500 M03 16 G43 Z131.743 H04 17 Z119.743 18 G01 X10.173 Y2.031 Z118.967 F450. 19 X10.391 Y1.826 Z118.243 20 X10.738 Y1.498 Z117.622 21 X11.19 Y1.072 Z117.145 22 X11.717 Y.576 Z116.846 23 X12.281 Y.043 Z116.744 24 X13.24 Y.076 Z116.684 B-33.931 C63.12 25 X14.282 Y.112 Z116.613 B-34.225 C63.609 26 X15.343 Y.149 Z116.534 B-34.53 C64.104 27 X16.423 Y.187 Z116.445 B-34.845 C64.607 28 X17.522 Y.226 Z116.348 B-35.17 C65.116 29 X18.639 Y.266 Z116.241 B-35.506 C65.631 30 X19.773 Y.306 Z116.124 B-35.851 C66.152 31 X20.924 Y.347 Z115.996 B-36.205 C66.68 32 X22.09 Y.388 Z115.858 B-36.567 C67.213 33 X23.271 Y.43 Z115.709 B-36.939 C67.753 34 X24.465 Y.471 Z115.548 B-37.317 C68.297 </pre>	<pre> 13 M69 14 M11 15 G00 G90 G54 X10.098 Y2.101 B-33.666 C62.668 S4500 16 G43 Z131.743 H04 17 Z119.743 18 G01 X10.173 Y2.031 Z118.967 F2500. 19 X10.391 Y1.826 Z118.243 20 X10.738 Y1.498 Z117.622 21 X11.19 Y1.072 Z117.145 22 X11.717 Y.576 Z116.846 23 X12.281 Y.043 Z116.744 24 X13.24 Y.076 Z116.684 B-33.931 C63.12 F674 25 X14.282 Y.112 Z116.613 B-34.225 C63.609 F450.2 26 X15.343 Y.149 Z116.534 B-34.53 C64.104 F520.8 27 X16.423 Y.187 Z116.445 B-34.845 C64.607 F603.3 28 X17.522 Y.226 Z116.348 B-35.17 C65.116 F661.3 29 X18.639 Y.266 Z116.241 B-35.506 C65.631 F759.4 30 X19.773 Y.306 Z116.124 B-35.851 C66.152 F852.3 31 X20.924 Y.347 Z115.996 B-36.205 C66.68 F886.6 32 X22.09 Y.388 Z115.858 B-36.567 C67.213 F960.6 33 X23.271 Y.43 Z115.709 B-36.939 C67.753 F1078.4 34 X24.465 Y.471 Z115.548 B-37.317 C68.297 F1170.2 </pre>
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Figure 6-6 – left: NC code for constant feed, right: modified NC code using the scheduled feedrate.

Figure 6-7 illustrates the result of feedrate scheduling validation test. In this figure, the red line represents measured cutting forces during running scheduled feedrate values and the blue line represents measured cutting forces during constant feedrate, which is equal to 450 [mm/min]. According to Figure 6-7 machining time for one single path using constant feed is 5.9 (sec) and using scheduled feed is 2.6 (sec) which means 60% reduction in cycling time. The roughing milling process consists of several levels of machining. This test was conducted for second level of machining.

To check the performance of the proposed feedrate scheduling algorithm, several tests were conducted at different levels and toolpaths. Figure 6-8 represents the result of feedrate scheduling technique for the third level of the rough milling. It can be found from this figure that the optimization in feedrate value ended up to 44% reducing in machining cycle time.

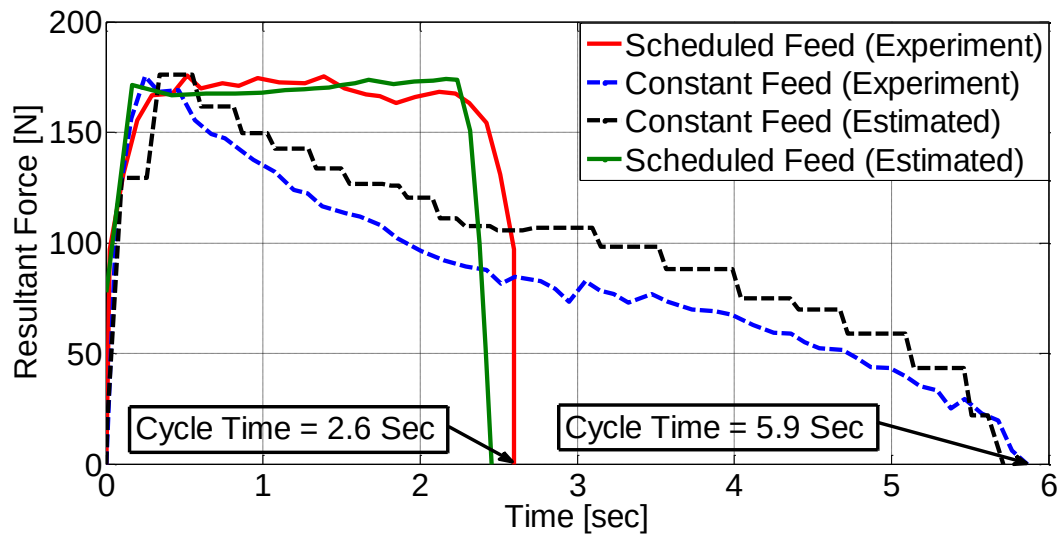


Figure 6-7 - Measured and simulated cutting forces during constant feedrate and scheduled feedrate cases for the second level of rough milling.

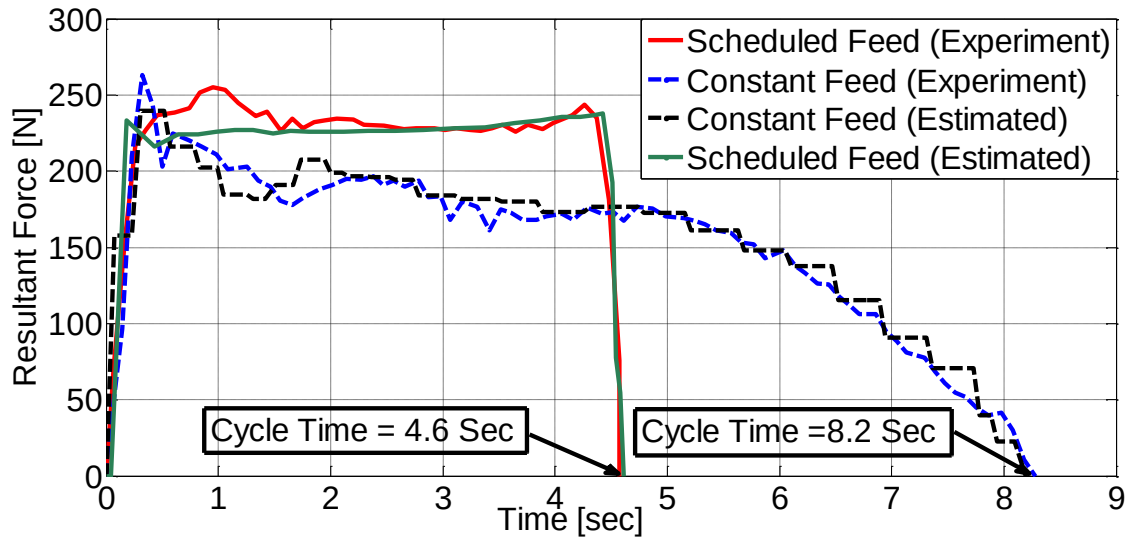


Figure 6-8 – Feedrate Scheduling for the third level of rough milling.

Consequently, using the proposed force based feedrate scheduling model, the cycle time can be decreased dramatically for machining complex freeform surfaces. The presented model can be used in new generation of CAM packages to schedule the feedrate in order to decrease the cycle time and increase the productivity.

6.4 Force based Feedrate Scheduling considering constraints of feed drive system

The force based feedrate scheduling approach proposed in the previous sections is integrated with a comprehensive trajectory planning algorithm that is more suitable for freeform milling. Based on cooperation with Dr. Kaan Erkorkmaz, associate professor in Waterloo University, an algorithm is proposed that generates B-spline toolpaths and optimized feedrate to have uninterrupted tool trajectories[35]. Considering the constraints of the feed drive system, the generated tool path and feedrate is bounded to the force, acceleration and jerk limits of the operation. Limiting the velocity, acceleration and jerk help avoiding structural vibration, dynamic positioning error and actuator saturation. This also will help to increase the life of feed drive components.

The force based feed limits are predicted using the model that introduced in section 0. Considering the feed drive limits as axis velocity, acceleration, and jerk ($\dot{\mathbf{r}}_{\max}, \ddot{\mathbf{r}}_{\max}, \dddot{\mathbf{r}}_{\max}$) the acceptable feedrate f_k can be calculated from the following equations:

$$\left. \begin{aligned} |\dot{\mathbf{r}}| = |\mathbf{r}_s \dot{s}| &\leq |\mathbf{r}_s| f_k \leq \dot{\mathbf{r}}_{\max} \\ |\ddot{\mathbf{r}}| = |\mathbf{r}_{ss} \dot{s}^2 + \mathbf{r}_s \ddot{s}| &\leq |\mathbf{r}_{ss}| f_k^2 + |\mathbf{r}_s| a \leq \ddot{\mathbf{r}}_{\max} \\ |\dddot{\mathbf{r}}| = |\mathbf{r}_{sss} \dot{s}^3 + 3\mathbf{r}_{ss} \ddot{s} \dot{s} + \mathbf{r}_s \dddot{s}| &\leq \dots \\ |\mathbf{r}_{sss}| f_k^3 + 3|\mathbf{r}_{ss}| a f_k + |\mathbf{r}_s| J &\leq \dddot{\mathbf{r}}_{\max} \end{aligned} \right\} \quad (6-3)$$

Above, tool position is expressed as a function of arc displacement (s) over time (t) as $\mathbf{r} = \mathbf{r}(s(t))$.

Figure 6-9 demonstrates a sample workpiece and toolpath used for validation test. A two fluted ball-end mill tool with 12 mm diameter was used in the experiment. The workpiece was Al-7050 and the CL points are generated using NX program with 0.04 mm tolerance. The whole toolpath contains 11,185 CL points. In force based feedrate scheduling the force limit was set to 200N. All the machining operations were performed on a Mori Seiki NMV 5000DCG machining center.

First of all, the B-spline toolpath is generated using the CL points extracted from CAM software (Figure 6-10 (b)). Then, the local feed limits based on cutting forces and feed drive constraints are predicted. Figure 6-10 (c) illustrates the force based feedrate by pink color and the feed drive constraints based feedrate by green color. As it can be inferred from this figure, during the cutting, most of the time force based feedrate is dominant. However, during air cutting trajectory, the feedrate is determined by the constraints of the feed drive system. It is also shown in this figure that the limits of the system are not reached by modifying the toolpath and feedrate.

Figure 6-11 represents the measured resultant cutting force vs cycle time. As it can be seen in this figure, there is an overall 16% reduction in machining time of the sample workpiece while the limits of the system is also considered.

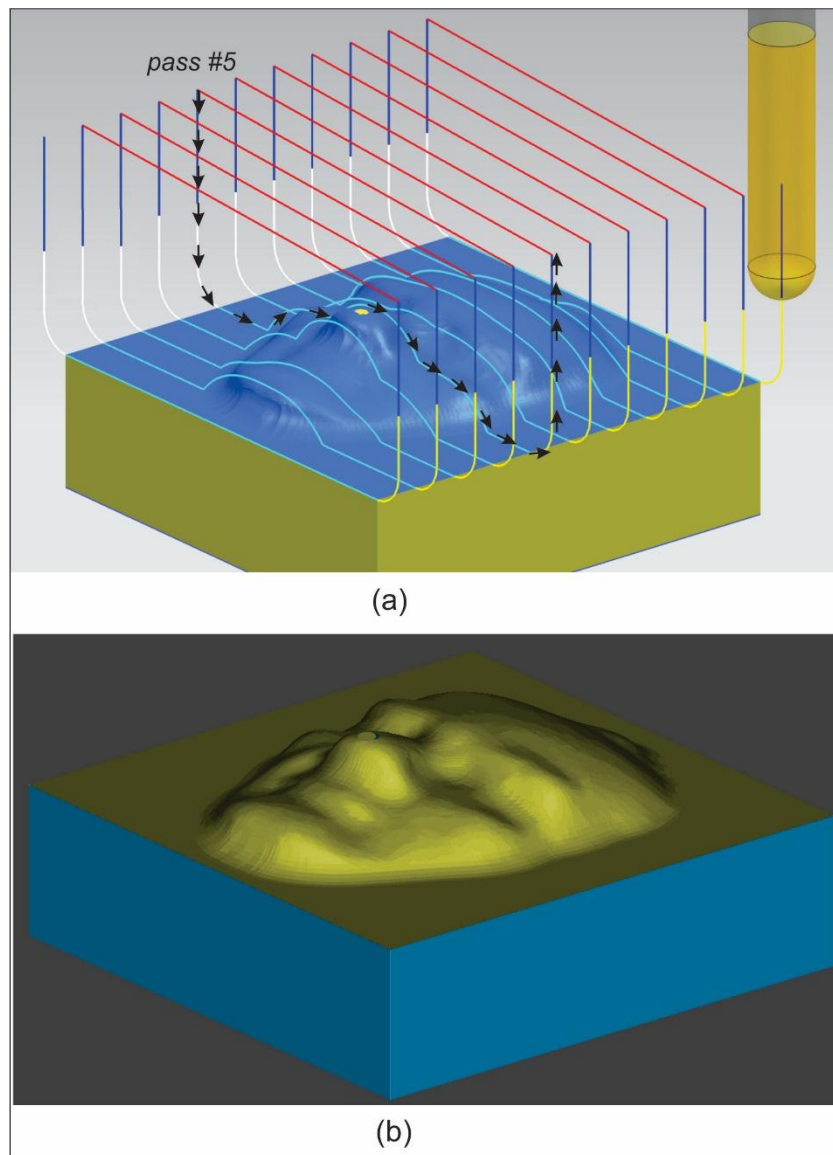


Figure 6-9 – Geometry of the setup used for experimental validation, (a) Toolpath #5, (b) Workpiece after finishing operation.

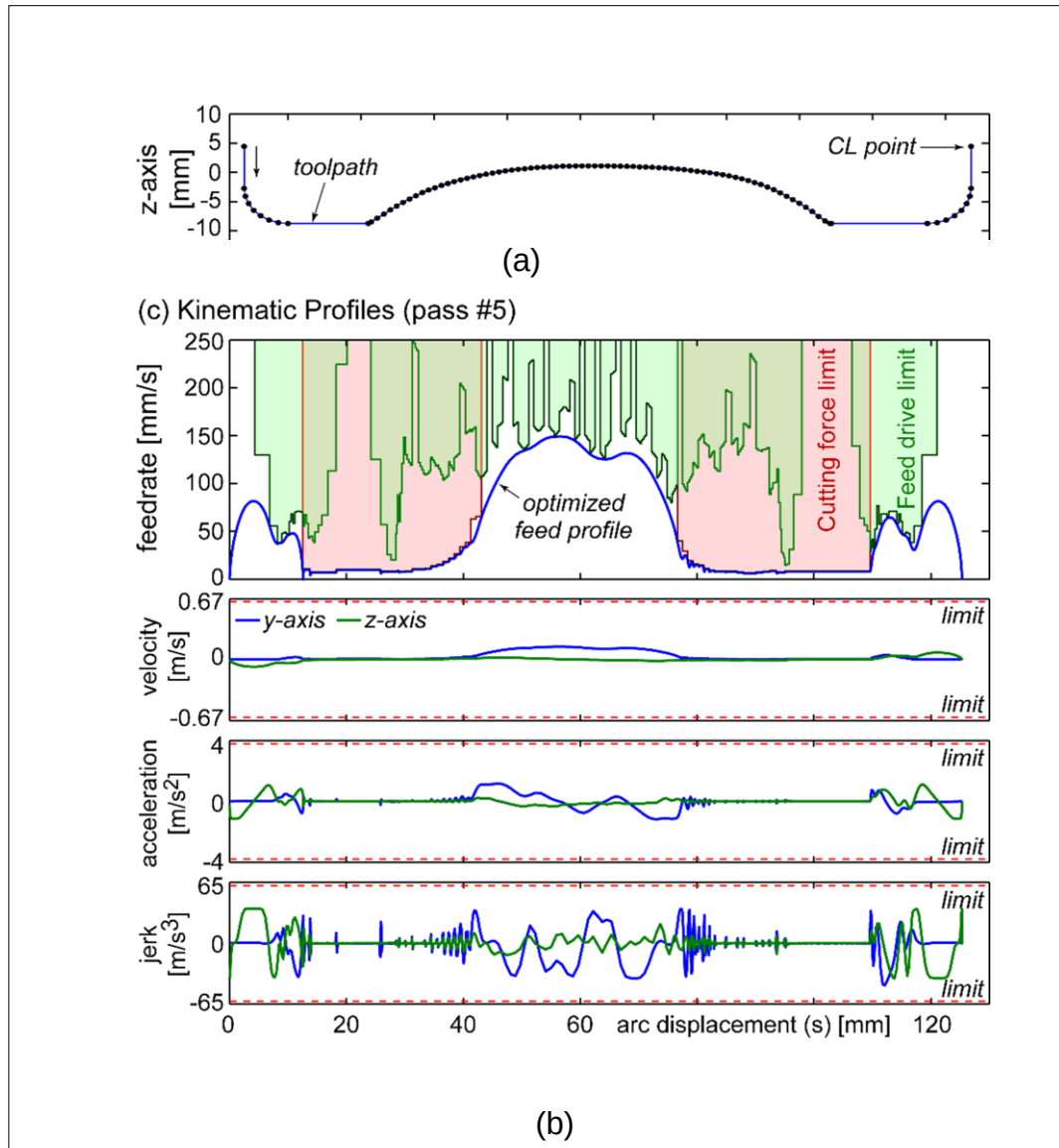


Figure 6-10 - Roughing toolpath and kinematic profiles for a sample pass [35], (a) Y-z plane toolpath (path #5), (b) Kinematics profiles (path #5).

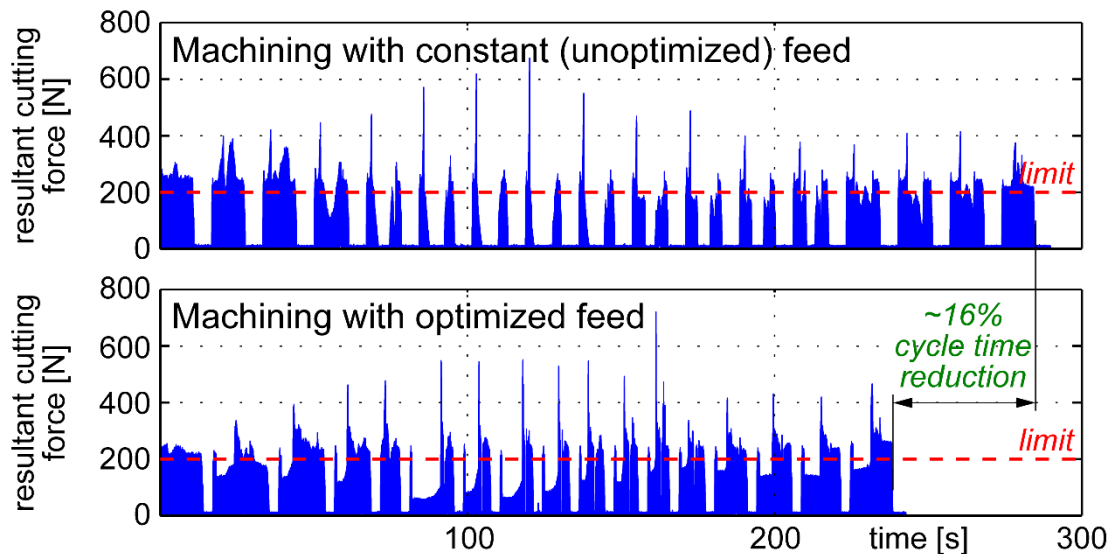


Figure 6-11 - Resultant cutting forces and cycle time comparison.

6.5 Summary

In this section, an offline force based feedrate scheduling algorithm is proposed. It was experimentally demonstrated that using the introduced approach, it is possible to dramatically decrease the machining cycle time while keeping the cutting forces below a user defined threshold. As it can be seen in Figure 6-2, the approach is applicable for complicated freeform surface geometries such as blades, blisks and impellers.

Furthermore, the force based feedrate scheduling method was merged with a novel feed drive limit based feed optimization algorithm. As it can be inferred from the Figure 6-10, the optimized feedrate follow the limits of the feed drive mechanism and simultaneously the mechanics of the process is taken into account by keeping the cutting forces below a limit shown in Figure 6-11. In the middle of the toolpath, during the air cutting, the drive limits are dominant and higher feed are commended. However, during the cutting, the feed values are commanded based on the mechanics of the process. In the above mentioned study, roughing operation is modelled and tested. In contrast to finishing operation, the

cutting forces are higher, thus the cutting feed values are generated based on the mechanics of the process. However, in finishing operation, different results may be achieved; where cutting forces are lower.

Chapter 7

TOOL DEFLECTION IN FIVE-AXIS MILLING OPERATION

7.1 Overview

Tool deflection accounts for dimensional error in all machining operations. Dynamic and static tool deflection lead to undercutting or overcutting issue, which in turn reduces efficiency and productivity of the operation. The cutting parameters must be selected in a way that the tool deflection doesn't exceed the acceptable tolerance.

So far, most of the deflection models, developed in the literature, are for three-axis milling operation. Moreover, most of the available research works consider the cutting force as a vector that is applied to tip of the tool rather than take it as distributed load acting on cutting edge.

In this thesis research, a new mathematical model for estimation of tool deflection in five-axis ball-end milling process using the FEM method is proposed. Cutting forces for the engagement domain is predicted from the model presented in Chapter 5. The tool is considered as a cantilever beam supported in the tool holder. Timoshenko element beam is used for FEM analysis of the tool under distributed cutting force load. Specially designed setup with two laser displacement sensors is attached to the spindle of the machine to monitor the instantaneous tool deflection and to validate the mathematical model of tool deflection.

The proposed approach has the following benefits:

- The model is able to consider the cutting forces as a distributed load acting on the corresponding node of the tool geometry.
- The proposed model can be embedded into the force model code that leads to lower computation time and no need for employing commercial CAE packages.

- This model can also be implemented in CAM packages for future generations to take into account the deflection of the tool and dimensional error.

7.2 Theory of tool deflection modeling

As was mentioned in the previous section, in most of the research works, the deflection of flat-end mill and ball-end mill tools are calculated by considering the cutting force as concentrated load applying on a point of the tool tip. This methodology is feasible, if the depth of cut is low and the contact area between tool and workpiece is close to a point. However, in the real roughing process, the contact area is a line across the cutting flute and the cutting force should be considered as a distributed load applying on contact line.

In contrast to flat end mill, helix angle and cutting coefficients change considerably along the cutting flute by changing the local diameter in ball part of the tool [96]. Due to this reason, the disk thickness for force modeling should be thinner close to the tip of the tool to calculate the cutting forces accurately. Figure 7-1 is representing the schematic view of the tool which is considered as a cantilever beam with circular section. For the sake of simplicity, the ball part of the tool is shown in flat form.

Due to the flute-groove design of the tool, it is not accurate to consider the radius of the cutter as the nominal radius of the tool section in deflection model. In order to overcome with this problem, the equivalent diameter of the tool is determined based on its compliance factor [97].

Timoshenko beam element is used for computation of element matrices and element forces. Figure 7-2 illustrates a schematic view of the Timoshenko beam and its 6 degrees of freedom which is used in the FEM mathematical tool deflection model. In this figure E , G , A , I and K_s are modulus of elasticity, shear modulus, cross section, moment of area and shear correction factor, respectively.

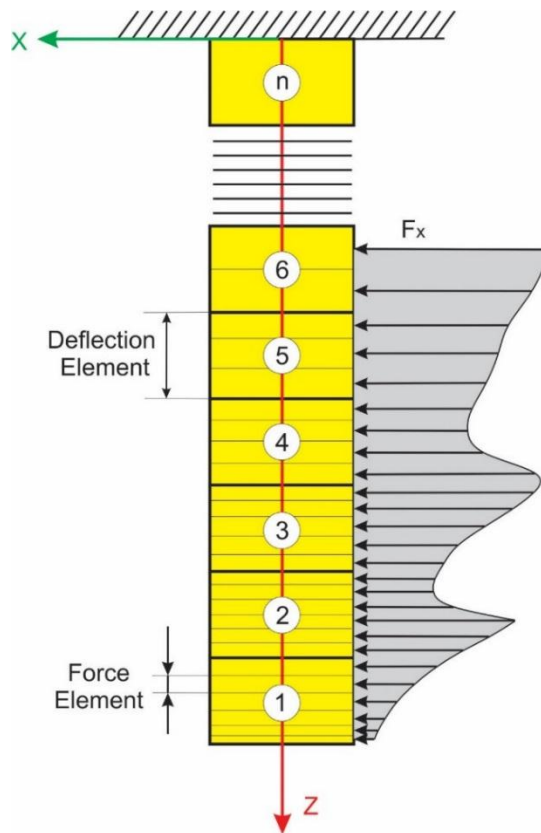


Figure 7-1: FEM model of tool deflection

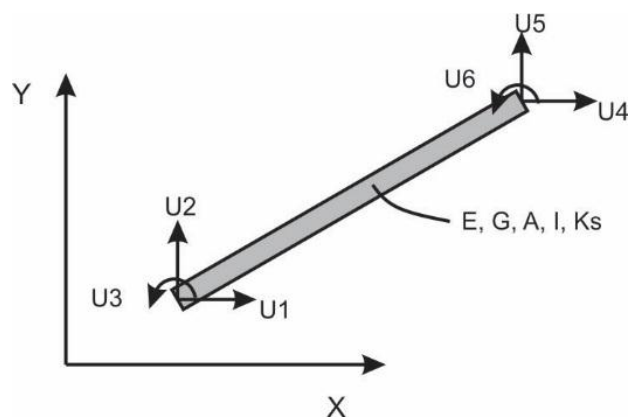


Figure 7-2: Timoshenko element beam

The element stiffness matrix K^e is computed according to

$$K^e = T^T \bar{K}^e T \quad (7-1)$$

Where, T is transformation matrix and can be calculated as:

$$T = \begin{bmatrix} n_{x\bar{x}} & n_{y\bar{x}} & 0 & 0 & 0 & 0 \\ n_{x\bar{y}} & n_{y\bar{y}} & 0 & 0 & 0 & 0 \\ 0 & 0 & 1 & 0 & 0 & 0 \\ 0 & 0 & 0 & n_{x\bar{x}} & n_{y\bar{x}} & 0 \\ 0 & 0 & 0 & n_{x\bar{y}} & n_{y\bar{y}} & 0 \\ 0 & 0 & 0 & 0 & 0 & 1 \end{bmatrix} \quad (7-2)$$

$n_{i\bar{l}}$ in matrix T are the direction cosines and can be calculated as:

$$n_{x\bar{x}} = n_{y\bar{y}} = \frac{x_2 - x_1}{L}, \quad n_{y\bar{x}} = -n_{x\bar{y}} = \frac{y_2 - y_1}{L}$$

Above, L is the element length which is set as 0.6 mm for the cutting zone of the tool and 0.8 mm for the non-cutting zone of the tool. The elements are considered in Y direction shown in Figure 7-2. Consequently, the magnitudes of $n_{x\bar{x}}$ and $n_{y\bar{y}}$ are zero.

In Equation (7-1), $\bar{K}^e$ is given by:

$$\bar{K}^e = \begin{bmatrix} \frac{EA}{L} & 0 & 0 & -\frac{EA}{L} & 0 & 0 \\ 0 & A & B & 0 & -A & B \\ 0 & B & C & 0 & -B & D \\ -\frac{EA}{L} & 0 & 0 & \frac{EA}{L} & 0 & 0 \\ 0 & -A & -B & 0 & A & -B \\ 0 & B & D & 0 & -B & C \end{bmatrix} \quad (7-3)$$

above;

$$\mu = \frac{12EI}{L^2 GAK_s}, A = \frac{12EI}{L^3(1+\mu)}, B = \frac{6EI}{L^2(1+\mu)},$$

$$C = \frac{4EI(1+\frac{\mu}{4})}{L(1+\mu)}, D = \frac{2EI(1-\frac{\mu}{2})}{L(1+\mu)}$$

E is the modulus of elasticity of the tool, which is considered as $4.84 \times 10^5 \text{ N/mm}^2$ for a two fluted ball-end mill carbide tool [98]. I and A are the second moment area and the cross area of the tool, respectively. The cross section of the tool is considered as circle with the correction factor of 0.8 as is stated in ref [97]. G and K_s are respectively the shear modulus of the tool material and Timoshenko shear correction factor that can be calculated from the following equations by knowing the poisson's ratio (ν) which is considered as 0.3 in this research thises:

$$G = \frac{E}{2(1 + \nu)}$$

$$K_s = \frac{6\nu^2 + 12\nu + 6}{4\nu^2 + 12\nu + 7}$$

K^e matrices are going to be assembled in the following form to generate the structure stiffness matrix K :

$$K = \begin{bmatrix} k_{11} & k_{12} & \vdots & & & \\ k_{21} & & \vdots & & & \\ \cdots & \cdots & k_{ii} + k_{ii}^e & k_{ij} + k_{ij}^e & \cdots & \cdots \\ \cdots & \cdots & k_{ji} + k_{ji}^e & k_{jj} + k_{jj}^e & \cdots & \cdots \\ & & \vdots & \vdots & & \\ & & \vdots & \vdots & & k_{nn} \end{bmatrix} \quad (7-4)$$

Above, n is the number of discrete elements on the tool. After finding the elemental cutting forces for each node in Figure 7-1, the deflection of each node in x and y direction can be found using the following equation:

$$\begin{cases} K \times X_x = F_x \\ K \times X_y = F_y \end{cases} \quad (7-5)$$

F_x and F_y are respectively cutting forces vectors in x and y direction. Those vectors are predicted using the cutting force model introduced in Chapter 5.

7.3 Simulation and Validation

The result of simulation is validated by conducting five-axis ball milling of an airfoil geometry as a freeform surface. The geometry of the workpiece, tool and generated toolpath is illustrated in Figure 7-3. The cutting parameters are tabulated in Table 7-1. In the simulation, the tool holder and spindle of the tool is considered as a rigid body.

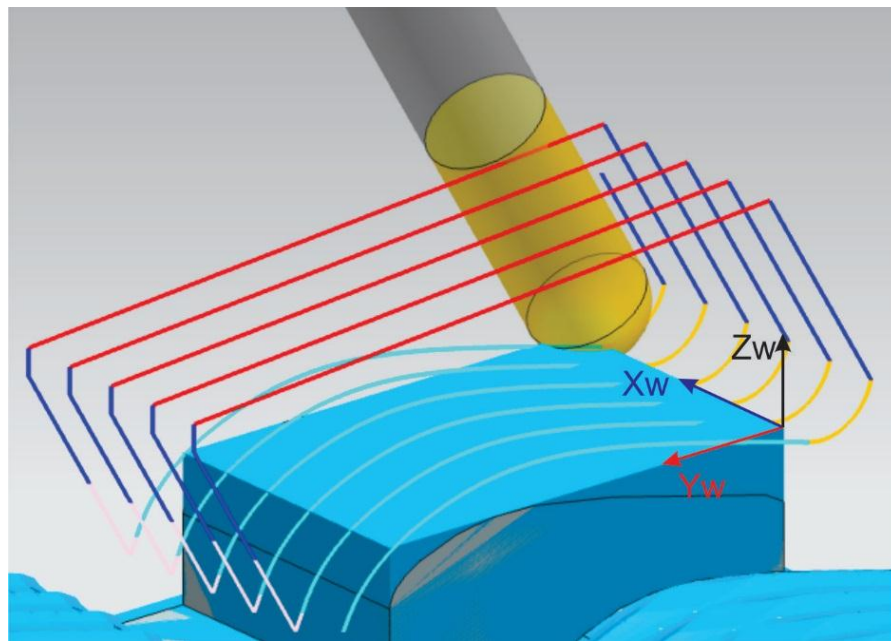


Figure 7-3 - Cutter and workpiece geometry

Table 7-1- Cutting conditions for simulation of tool deflection

Tool Diameter [mm]	Spindle Speed [rpm]	Feedrate [mm/min]	Tool Length [mm]	Maximum Depth of Cut [mm]
12	4000	500	66	5.5

Engagement and cutting forces are estimated according to the model that is introduced in Chapter 3. Figure 7-4 represents the geometry of the engagement model and the cutting flute for a sample CL point. Cutting force as a distributed load is calculated for the corresponding engagement domain. Force vectors, mass matrix and stiffness matrix are computed according to the Timoshenko beam approach explained in the previous section.

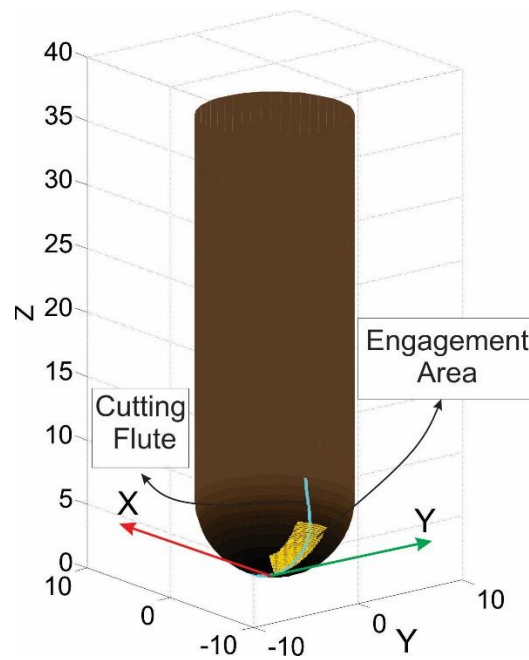


Figure 7-4: Modeled engagement for five-axis ball-end milling of airfoil geometry.

A specially designed fixture was used to measure instantaneous tool deflection during the machining. The fixture orients two laser displacement sensor to measure the tool deflection in X and Y directions. Figure 7-6 shows the the fixtured assembled on the spindle of CNC machining center.

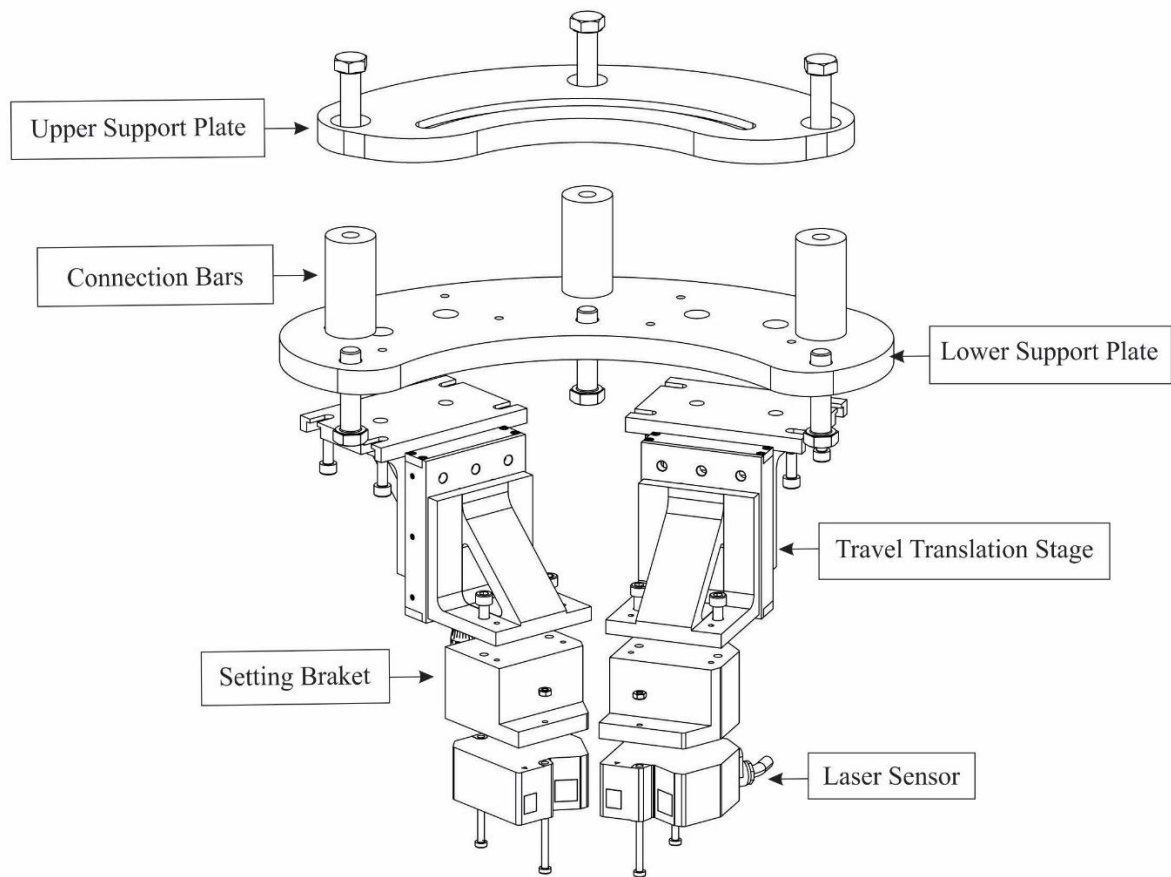


Figure 7-5 – Exploded view of the laser displacement sensor fixture.

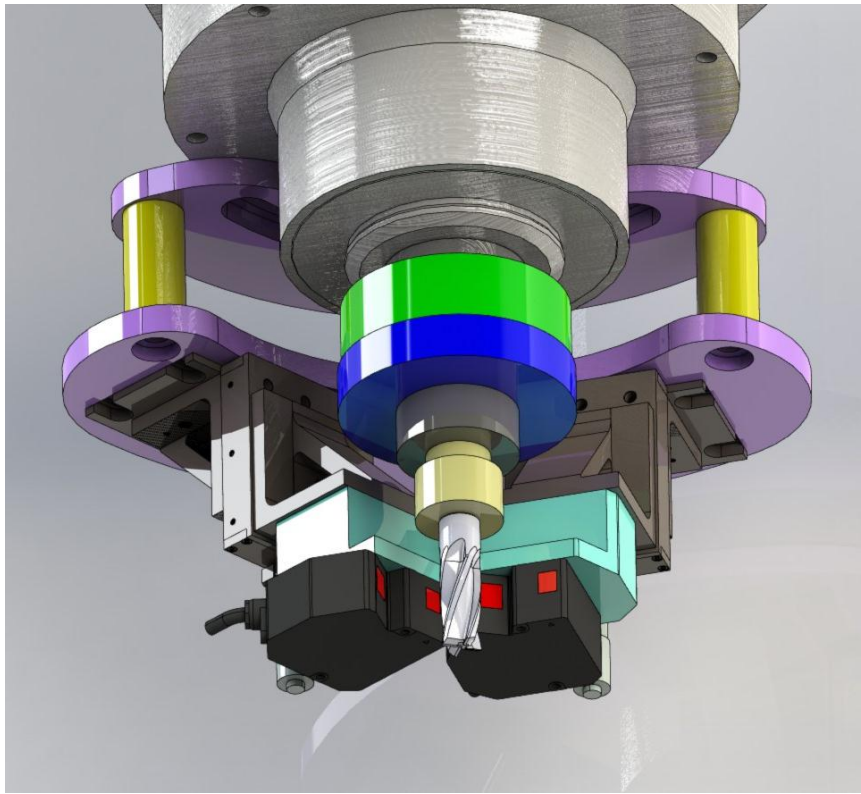


Figure 7-6 – Tool deflection measurement setup.

All the tests were conducted on five-axis Mori Seiki NMV5000DCG milling center. A rotary type of Kistler dynamometer was used to measure the forces during the cutting process. Two Keyence laser sensor head with precision of 1 micron and sampling rate of 50 kHz were utilized to measure the tool deflection. Figure 7-7 demonstrates the real experimental setup which is used to validate the force and deflection model.

Figure 7-8 represents the simulated and measured cutting forces for the conditions mentioned in Table 7-1. As it is obvious from the figure there is a good agreement between the simulated and measure cutting forces. Since the tool and spindle system is highly rigid in Z direction, the z component of cutting force and deflection has not considered.

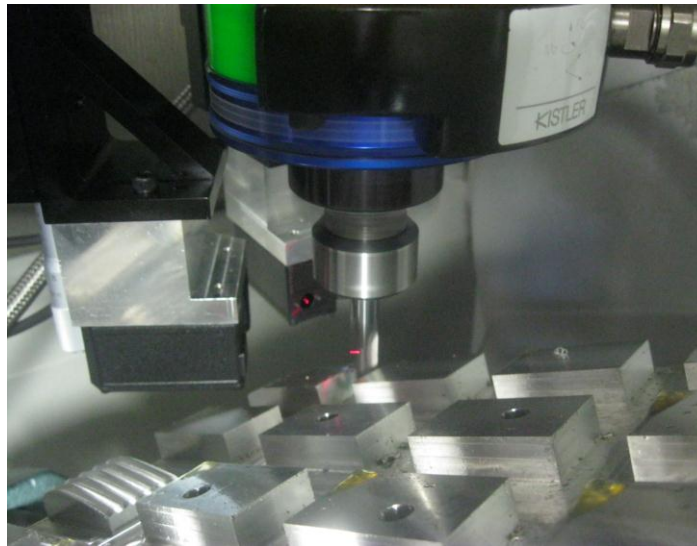


Figure 7-7: Experimental setup for measuring force and tool deflection.

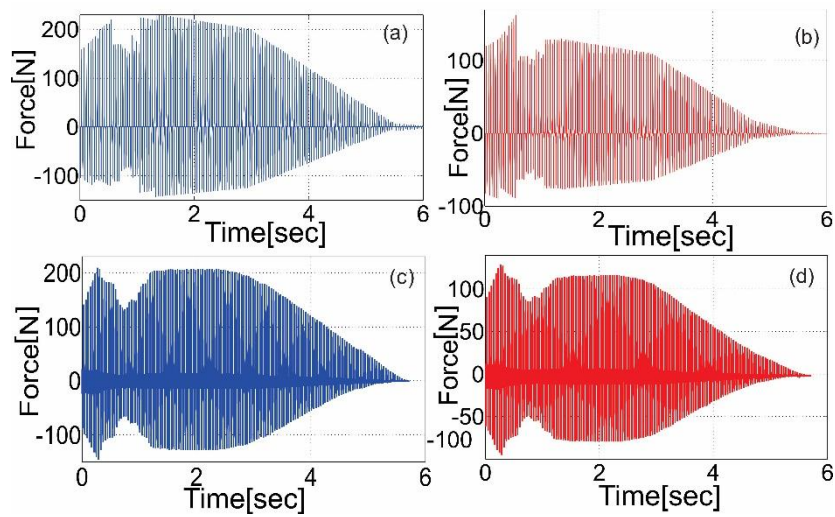


Figure 7-8: Simulated and measured cutting forces. (a) Simulated forces in y direction, (b) Simulated forces in x direction, (c) Measured forces in y direction and (d) Measured forces in x direction.

Figure 7-9 represents the modeled deflection for the cutting conditions mentioned in Table 7-1. The numerical inputs of the FEM code are listed in Table 7-2. In order to validate the results, the deflection is measured and compared with the simulation results at the tip of the tool.

Figure 7-10 demonstrates the simulated and measured deflection at the tip of the tool. In order to avoid of any collision during the machining, laser heads are set to be 20 mm upper than the tool tip. Using an extrapolation algorithm the measured deflection at the tool tip was predicted. As it can be seen from Figure 7-10, there is a good agreement between experimental and simulated results. The maximum error in the deflection model is 20%, which is acceptable.

Table 7-2 – Inputs for the FEM analysis of tool deflection.

Cutting element length [mm]	Non-cutting element length [mm]	Young modulus [N/mm ²]	Tool overhang Length [mm]	Poisson's ratio
0.6	0.8	4.84×10^5	66	0.3

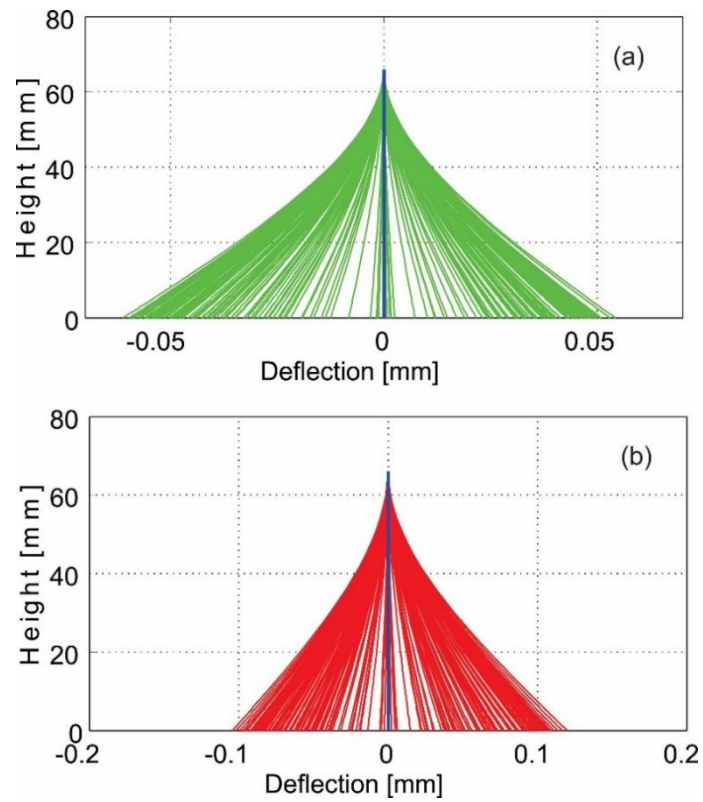


Figure 7-9: (a): Deflection in X-direction, (b): Deflection in Y-direction.

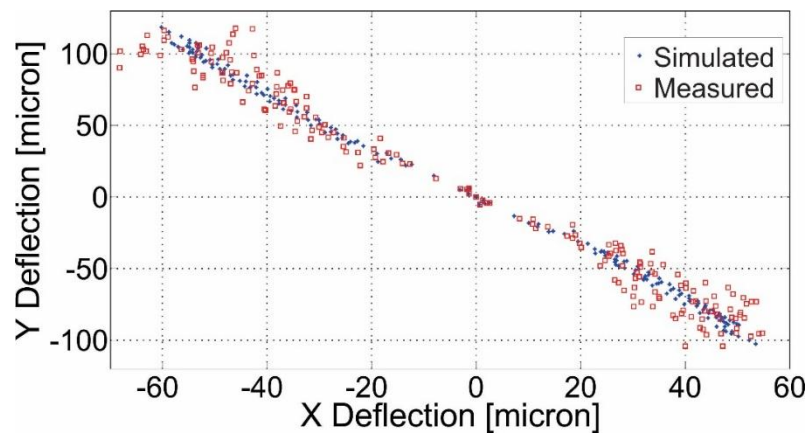


Figure 7-10: Simulated and measured deflection during the cutting process at the tool tip.

7.4 Summary

In this chapter, a deflection model for five-axis end milling operation is presented. The model considered the tool as a cantilever beam which is supported in the tool holder. The spindle system of the machine was considered as a rigid body. The Timoshenko element beam was considered to perform FEM analysis of the tool deflection. A correction factor of 0.8 was employed to consider the effective cross section of the tool. Cutting forces are considered as a distributed load on the cutting flutes to increase the accuracy of the model.

A special setup was designed to monitor and measure the instantaneous tool deflection for milling operation. The setup consists of two laser displacement sensor head to measure the tool deflection in X and Y directions. Due to the high rigidity, deflection in Z direction was neglected. The experimental results showed a good agreement between the experimental and simulation results.

Chapter 8

ANALYSIS OF TOOL ORIENTATION FOR FIVE-AXIS BALL-END MILLING

8.1 Overview

Nowadays, there are significant demands of hi-tech industries for high-performance five-axis milling of complex parts with freeform geometries. The performance of five-axis milling is highly dependent on the tool posture for different operations [6][99][100][101][102][103][104]. In the current commercial CAM packages, tool posture (lead and tilt angles) is determined only by geometrical concerns and the mechanics of the process has not been included yet. Ignoring the mechanics of the process may lead to the risks of suboptimal performance, such as undesired forces, tool/workpiece deflection, excessive vibration, poor surface quality, longer machining cycle time and relatively higher machining costs especially for freeform parts such as turbine blades, blisks and impellers. In today's competitive five-axis machining, only geometry-based analysis for the selection of tool posture is not sufficient. In order to explore the full potential of five-axis machining for higher performance, current selection strategy of tool orientation must be enhanced by considering mechanical constraints of the process. Tool orientation can be defined by two angles, namely lead and tilt. The geometrical representation of those angles is shown in Figure 8-1. Lead and tilt angles are depicted as l and t angles in the following figure and can be defined as the rotation angle of the tool axis around cross-feed and feed direction, respectively. In some literature, the tool orientation is defined by θ and φ angles which are named as inclination and screw angles.

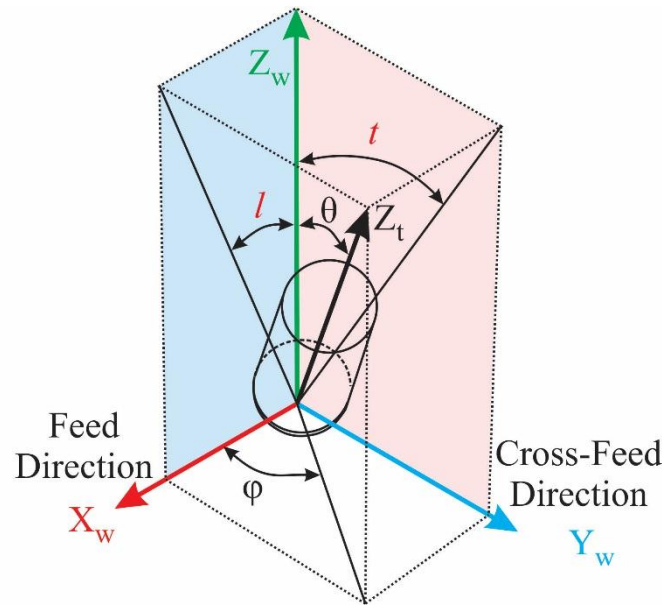


Figure 8-1 - Definition of lead and tilt angles.

Despite all the research work, which are cited in section 2.4, up to today, there is still a lack of mechanics-based strategy that is able to predict the ideal tool posture in five-axis milling of flexible workpieces. Therefore, the aim of this chapter is to investigate the effects of lead and tilt angles on cutting force, torque, deflection and surface quality in five-axis ball-end milling of flexible parts such as turbine blades. The novelty of the proposed strategy lies in presenting a mechanics-based reference map that suggests the appropriate tool orientation considering above-mentioned parameters. The effectiveness of the approach is demonstrated experimentally.

8.2 The effects of tool orientation on cutting force and torque

Due to the complexity of the five-axis milling operation, up to today, there is no analytical model to predict the effects of tool orientation on the outputs of the process such as: cutting forces, cutting torque, tool/workpiece vibration, surface quality and other physical parameters. According to Figure 8-1, the orientation of the tool can be defined by

lead and tilt angles. The trend of change in cutting force and torque by variation of lead and tilt angles can be determined using the mechanistic force model which is introduced in Chapter 5.

In order to study the effects of tool orientation on five-axis milling, three different setups were designed. In the first setup, the effects of tool posture during machining of Ti6Al4V was investigated. In this design, the workpiece was a solid block and the depth of cut and width of cut were kept constant. The force magnitude was investigated and the workpiece deflection was negligible.

The second setup was identical to the first setup, but the workpiece material was Aluminum 7050. Depth of cut was constant and cutting force and torque during slot cutting with different tool postures were investigated.

In the third setup, five-axis milling of a flexible turbine blade with different tool orientations was considered. The workpiece material was Aluminum 7050 and cutting force, torque, workpiece deflection and surface quality was investigated under machining with different tool orientation.

By varying the tool orientation, the position of tool tip (TT) changes. Since the NC code in machining center is based on the TT positioning, the position of TT should be recalculated in each case of lead and tilt angle pairs to reach the nominal depth of cut. Relative position of TT with respect to CC point is illustrated in Figure 8-2. One can easily derive the following equation to find TT point position with respect to CC point:

$$TT \begin{cases} P_x = -r \cos(l) \sin(l) \\ P_y = r \cos(l) \sin(t) \\ P_z = r(1 - \cos(t) \cos(l)) \end{cases} \quad (8-1)$$

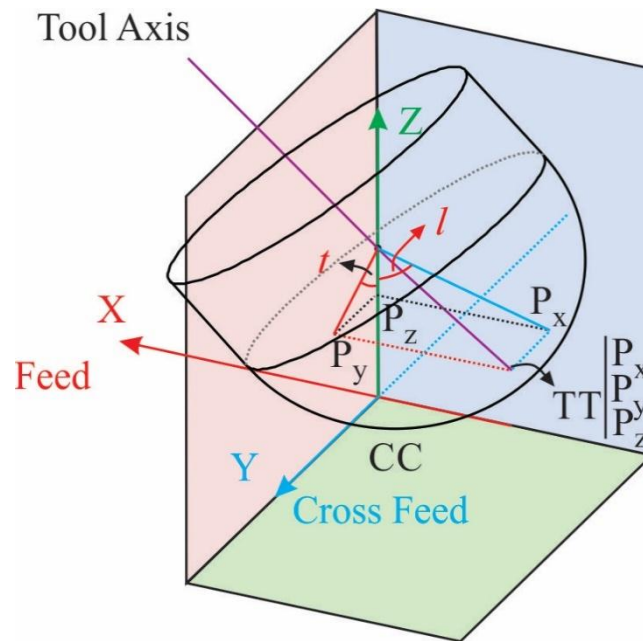


Figure 8-2 - Tool tip (TT) position with respect to contact point (CC) position.

8.3 The effect of tool orientation on five-axis ball-end milling of Ti6Al4V

Ti6Al4V is an advanced engineering alloy with a wide range of applications. The main characteristics of this alloy are high strength, corrosion and fatigue resistance at elevated temperatures, which make Ti6Al4V as a superior alloy. Ti6Al4V is being used in various industries such as aerospace, nuclear energy generation plants, food processing machinery, biomedical product and implant manufacturing.

By knowing the cutting coefficients, engagement condition and kinematic of the process, cutting force and torque can be predicted using the mechanistic cutting force model introduced in Chapter 5. Table 8-1 illustrates the cutting conditions and tool specifications in the simulation and the experiment.

Table 8-1 - Cutting Conditions

Tool Diameter	6 [mm]
Over hang	30 [mm]
Number of flutes	2
Spindle Speed	7700 [rpm]
Feedrate	1540 [mm/min]
Depth of cut	0.5 [mm]
Step over	0.5 [mm]

Figure 8-3 and Figure 8-4 illustrate the simulated and measured cutting forces. In all of the cases a very good agreement can be observed between the simulated and measured forces. However, the discrepancy in z direction can be attributed to the ploughing and size effect phenomena during the ball-end milling.

In order to investigate the effect of lead and tilt angle, a series of tests were conducted with different lead and tilt angles. The average of the resultant cutting force and the normal component of the cutting force with respect to the machined surface are collected and illustrated as 3D plots for up and down milling operations.

Figure 8-5 and Figure 8-6 represent the average of resultant cutting force for different lead and tilt angles. It can be inferred from those figures that the maximum average resultant force occurs at zero lead and tilt angle for down milling. In contrast, the maximum resultant cutting force for up milling happens in zero lead angle and higher negative tilt values.

It can be concluded from Figure 8-5 that in order to have lower amount of resultant cutting force in down milling operation, the optimum tilt angle should be around 4° and the

lead angle should be selected more than 8° . For the up milling case, however, the optimum tilt angle is zero and the lead angle should be selected higher than 8° .

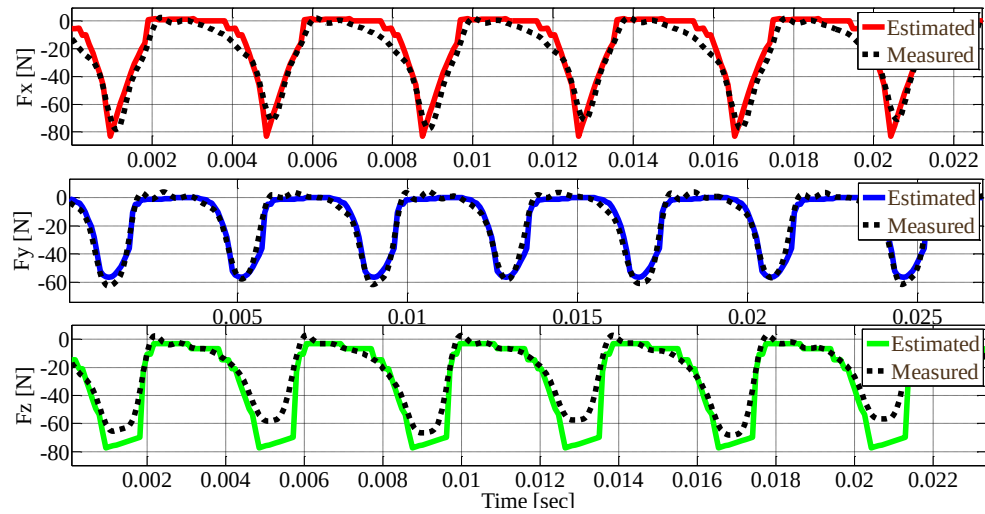


Figure 8-3 - Cutting Forces for Lead = 1.5 [deg] and tilt = 3.7 [deg]

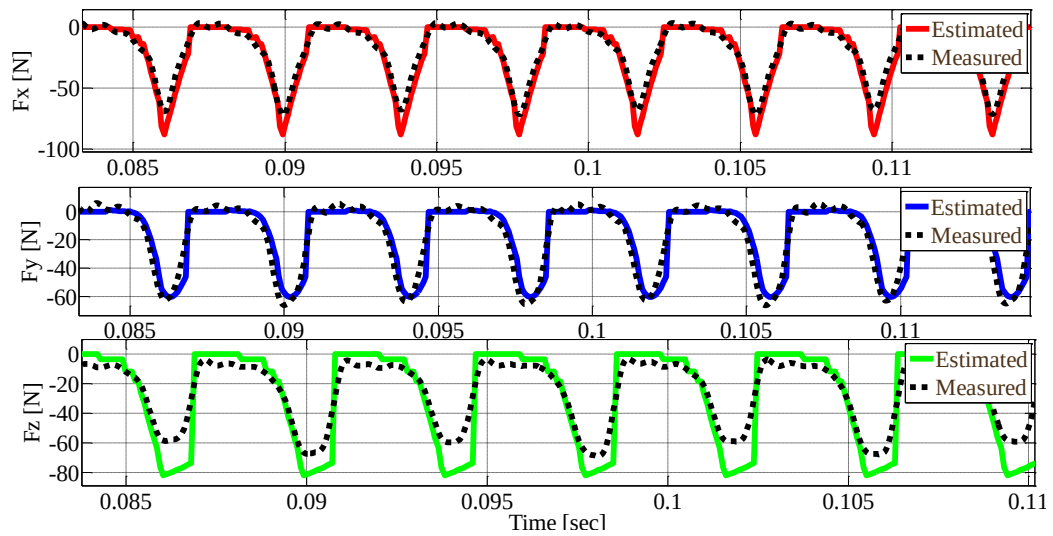


Figure 8-4 - Cutting Forces for Lead = 0 [deg] and tilt = 4 [deg]

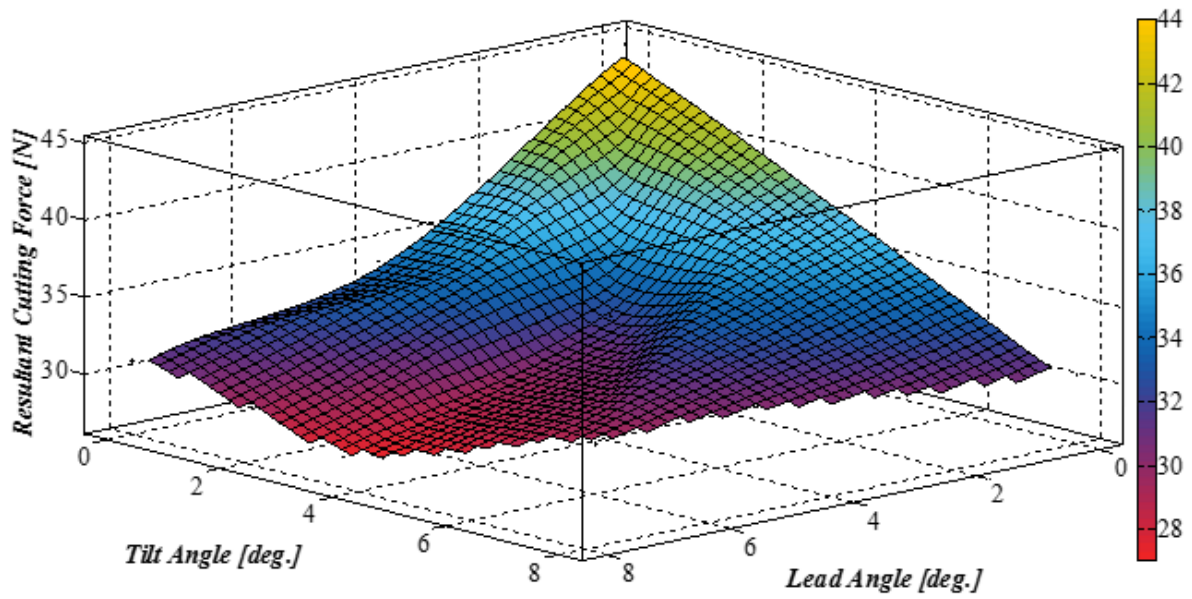


Figure 8-5 - Average resultant cutting force in down milling operation.

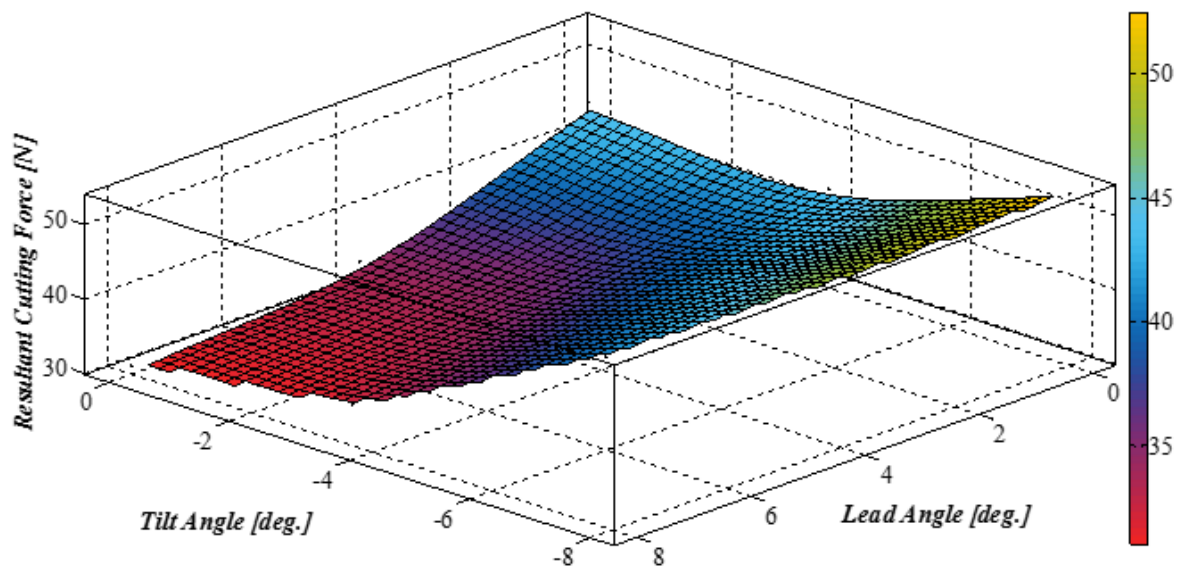


Figure 8-6 - Average resultant cutting force in up milling operation.

The surface quality of finished workpiece is highly dependent on the component of machining force which is normal to the final surface. Since the normal component force with respect to the machined surface depends on the lead and tilt angles, it would be necessary to find the optimum orientation in which lower cutting force is exerted on the workpiece. Figure 8-7 and Figure 8-8 show the contour plot in which the optimum tool orientation can be chosen. According to Figure 8-8, the maximum average of normal cutting force occurs at zero lead and tilt angles. However, for the up milling case, the highest normal force happens at zero lead and higher negative values of tilt angle. The optimum lead and tilt angles are defined with red color in Figure 8-7 and Figure 8-8. Obviously, the selection of optimum lead and tilt angle should be based on the geometry of the process as well. There should always be checking criteria to make sure that the selected lead and tilt angle is feasible from geometrical point of view. This literally means to check for having collision free machining operation.

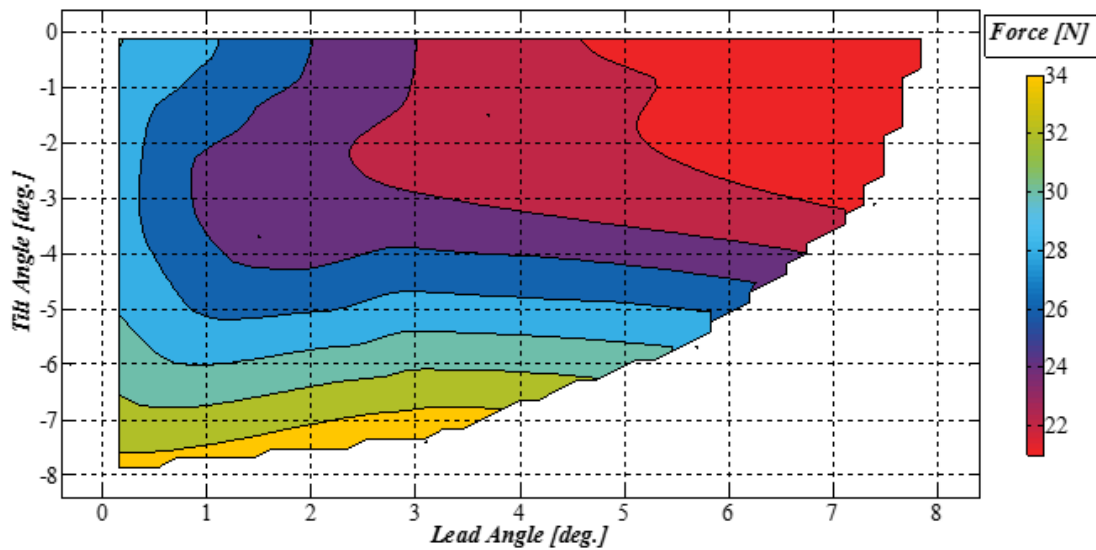


Figure 8-7 - Average of cutting force normal to machined surface for down milling.

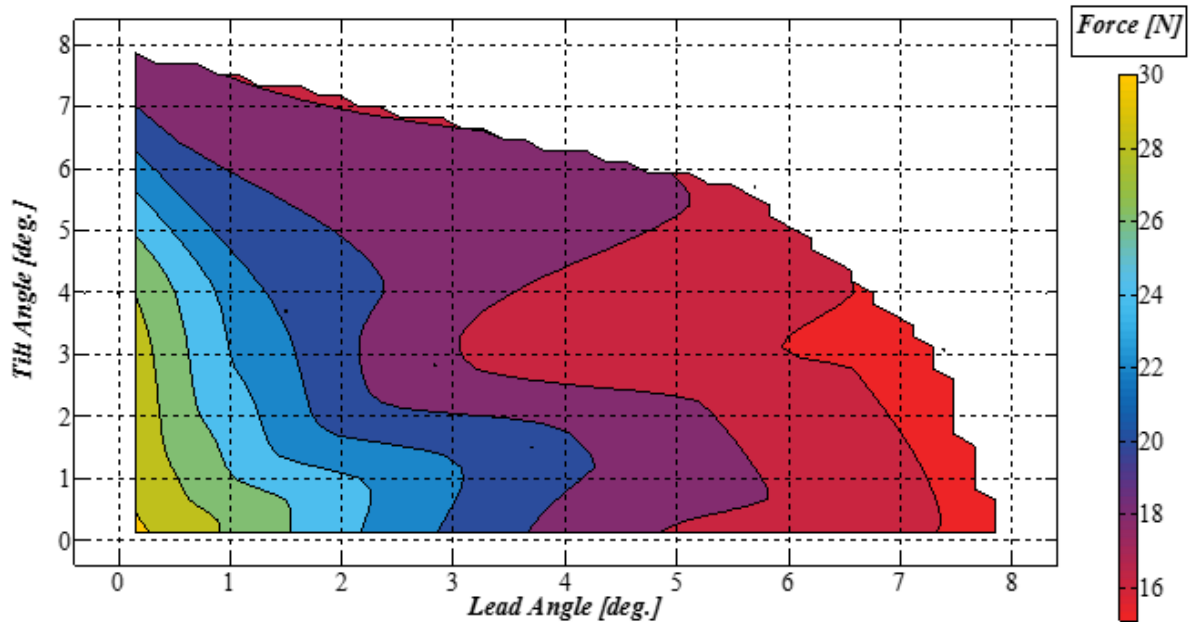


Figure 8-8 - Average of cutting force normal to machined surface for up milling operation

8.4 The effect of tool orientation on five-axis ball-end milling of Al 7050

The effects of tool orientation on cutting force and torque is studied during five-axis milling of an Aluminum 7050 solid block. All of the simulation and experiments were designed for slot cutting with constant 2 [mm] depth of cut. Spindle speed and feedrate were respectively 2500 [rev/min] and 750 [mm/min]. A two fluted ball-end mill with the diameter of 12 [mm] was used for all the cutting cases. As is demonstrated in Table 8-2, the cutting forces and torque were simulated and measured for 30 pairs of lead and tilt angles ranging from -25° to 25° . Cutting forces were measured in X, Y and Z directions which are demonstrated in Figure 8-2. In order to measure the cutting force and cutting torque, a rotary and a table type dynamometer was simultaneously used to increase the accuracy of

the measurement. The cutting force signal was acquired with the sampling rate of 10000 Hz. Force signals were also filtered using a low pass Butterworth filter.

Table 8-2 - Lead and tilt angle pairs used for experimental validation.

Lead	Tilt	Lead	Tilt	Lead	Tilt
0	0	-5	15	15	10
0	10	-5	-15	15	-10
0	-10	5	25	-15	10
0	25	5	-25	-15	-10
0	-25	10	5	15	25
5	0	10	-5	15	-25
-5	0	10	15	25	0
0	0	10	-15	25	10
5	5	15	0	25	-10
5	-5	-15	0	25	25

Figure 8-9 shows the accuracy of the simulation with respect to the measured cutting force using rotary dynamometer for two cases. Generally, the force model is able to predict the cutting forces with less than 15% estimation error. As it can be inferred from Figure 8-9, tool orientation can drastically affect the magnitude and trend of the cutting force in five-axis ball-end milling.

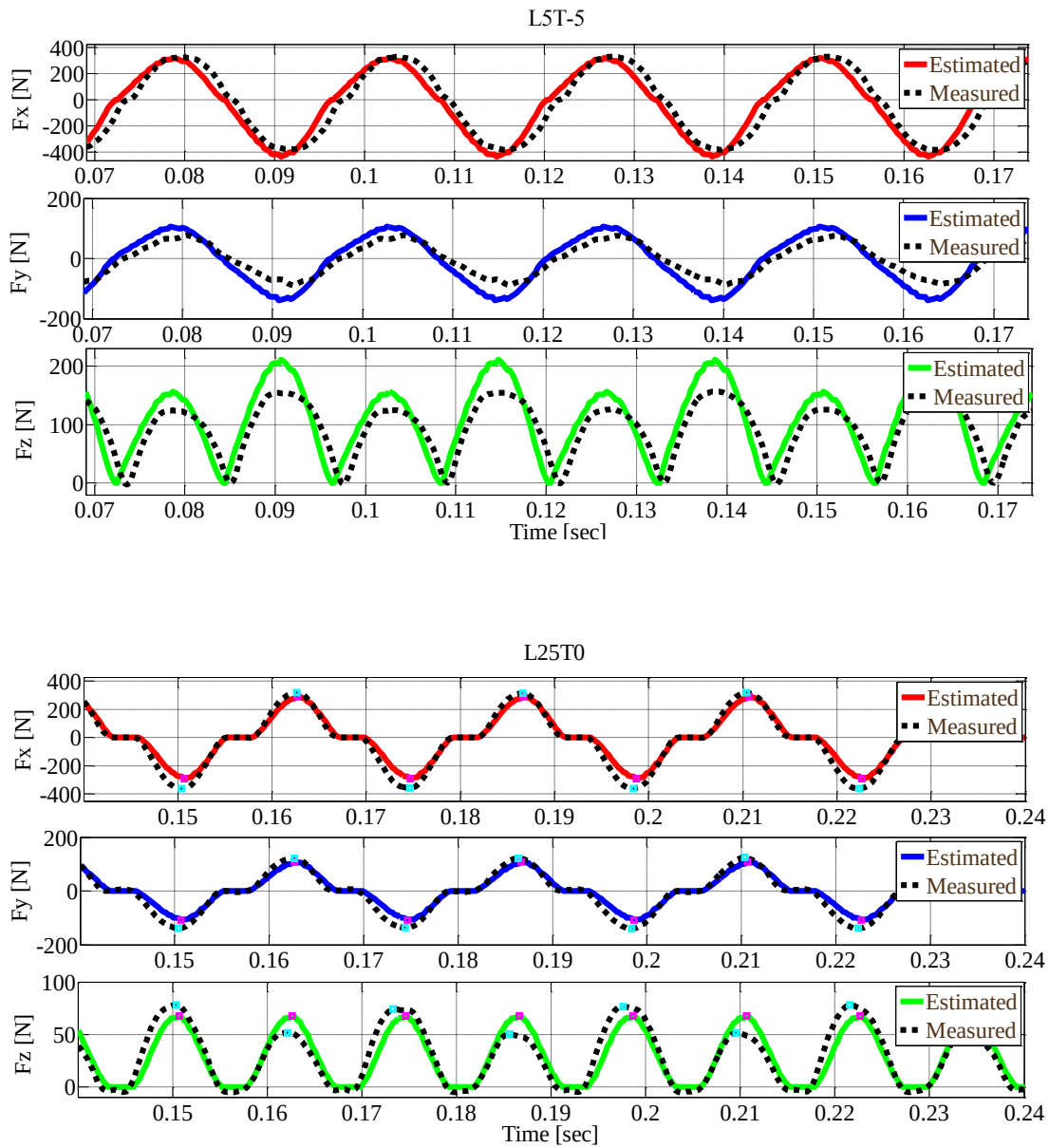


Figure 8-9 – Simulated and measured cutting forces for L5T-5 and L25T0 cases.

Variation of tool orientation in five-axis milling can affect workpiece deflection, surface quality, consumed energy and tool deflection. In order to investigate the effects of tool orientation on those parameters, the results of experimental tests were analyzed and

plotted, as are shown in Figure 8-10. The 3D surfaces are fitted to experimental results with the minimum R-square of 0.9.

The effect of tool orientation on average normal force and cutting torque average is respectively demonstrated in the following figures. These parameters are important because of their effects on the output of the five-axis ball-end milling process. For the flexible parts, the deflection of workpiece is directly related to the component of cutting force which is normal to the machined surface. Due to this reason, the effect of tool orientation on the average cutting force normal to the machined surface is shown in Figure 8-10 (a). The importance of cutting torque is because of its direct relationship with cutting power.

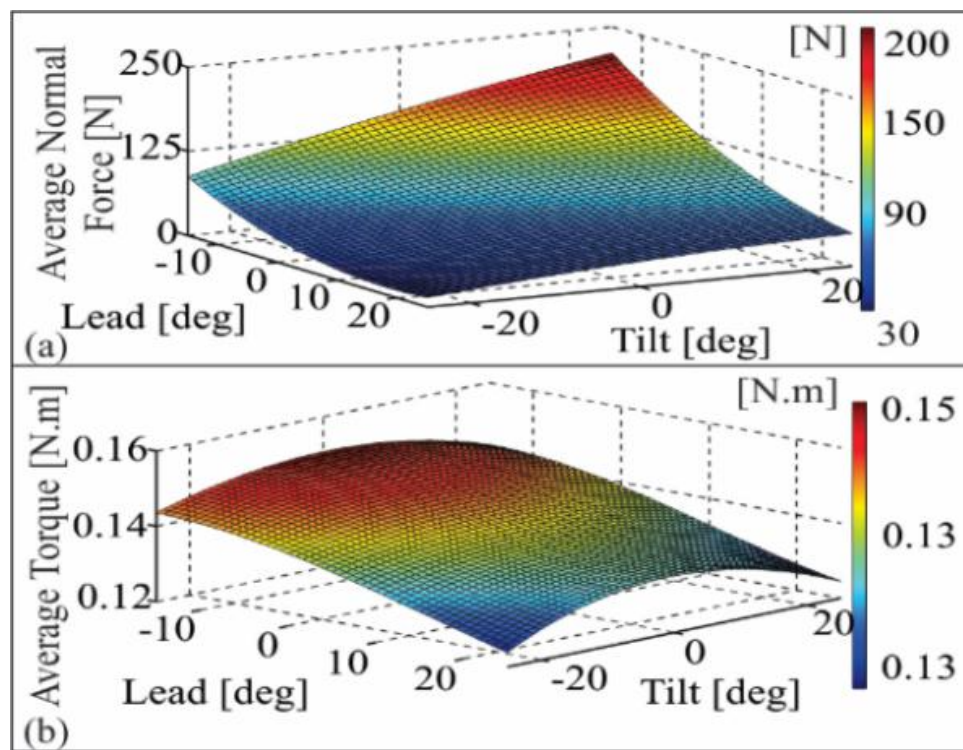


Figure 8-10 – (a) normal cutting force and (b) average cutting torque five-axis ball-end milling of Al 7050.

Figure 8-11 shows the projection of the spherical part of the tool on a planar surface perpendicular to the tool axis. The feed and cross feed directions are also represented in this figure by red and blue arrows, respectively. The projected planar surface in Figure 8-11 is used to clearly show the effect of tool orientation on cutting force and cutting torque in Figure 8-12 to Figure 8-15. Considering Figure 8-2, by varying the tool orientation, the location of CC point on the spherical part of the tool changes. Figure 8-12 to Figure 8-15 also show how the position of the CC point and the magnitude of torque and cutting forces change by changing the tool orientation. In the given figures, CC points are shown by circles and the magnitude of the torque and forces are represented by color coding and the size of the circles.

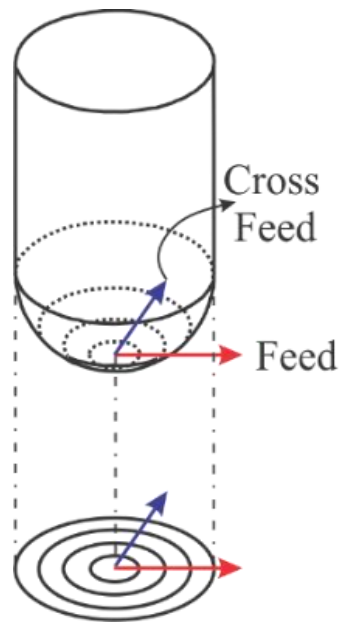


Figure 8-11 – Projection of the spherical part of the tool.

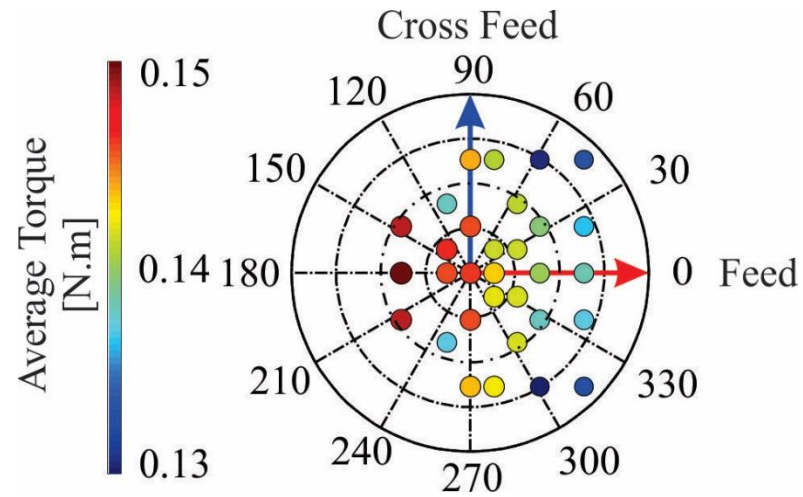


Figure 8-12 – Variation of average cutting torque by changing the CC point.

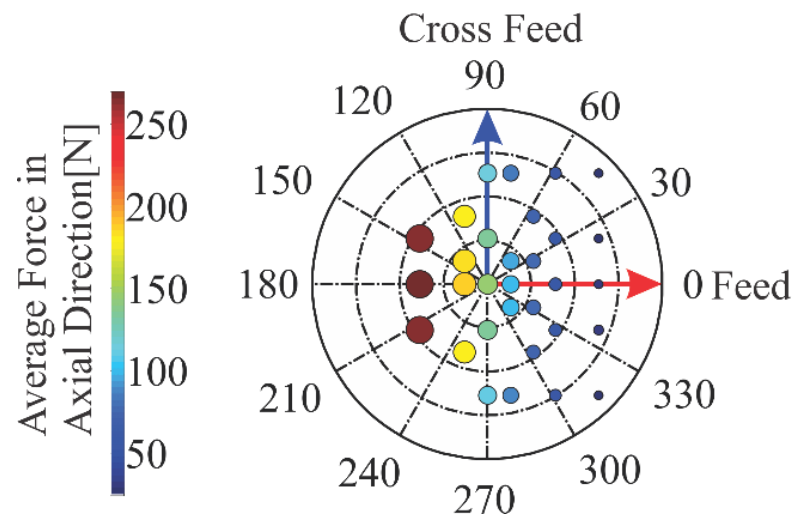


Figure 8-13 - Variation of average cutting force in tool axis direction by changing the CC point.

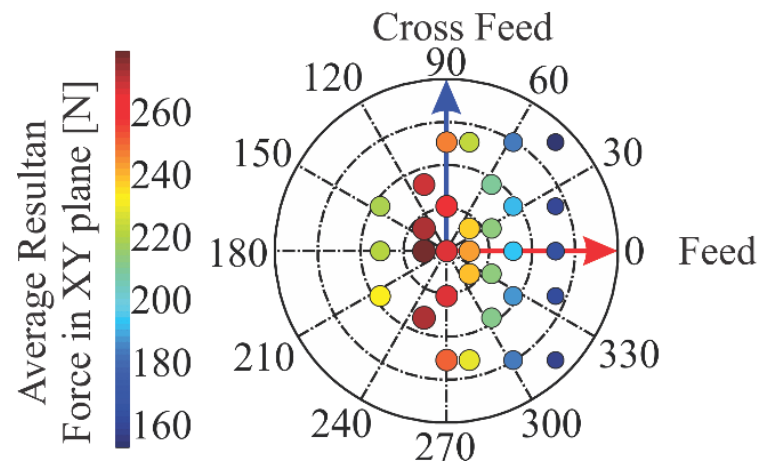


Figure 8-14 - Variation of average cutting force perpendicular to tool axis direction by changing the CC point.

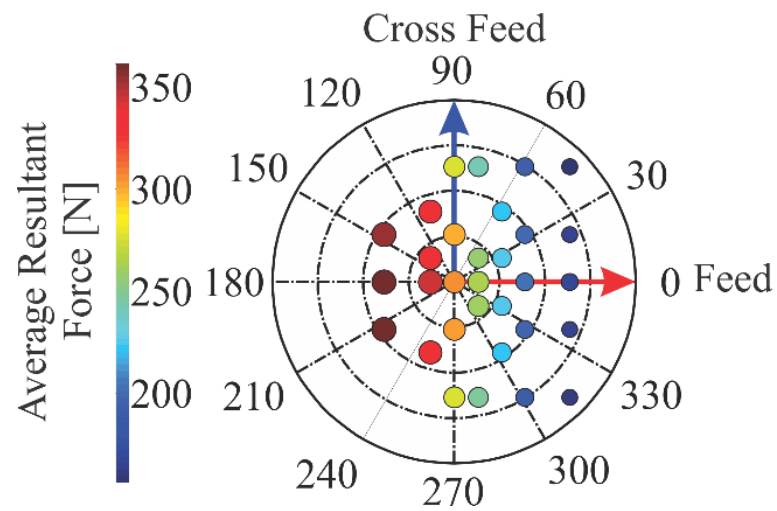


Figure 8-15 - Variation of average total cutting force by changing the CC point.

Figure 8-12 to Figure 8-15 reveal that the tool orientation can extremely affect the generated cutting force in 5-axis five-axis ball-end milling operation. According to the given figures, selecting the appropriate pair of lead and tilt angle can reduce the cutting force in the tool axis direction as much as 5 times. Moreover, the average resultant force and the average cutting force perpendicular to the tool axis can respectively be reduced as much as 2.33 and 1.75 times. Also, Based on Figure 8-12, appropriate tool orientation can reduce the average cutting torque as much as 1.3 times which in turn lead to reduction in consumed cutting energy.

8.5 The effect of tool orientation on the surface quality in five-axis ball-end milling of flexible thin parts

As it was discussed in the previous section, lead and tilt angles can drastically affect the cutting forces and torque in five-axis ball-end milling operation. It is also shown that the surface quality and deflection of thin flexible workpieces can be affected by tool orientation. In this section, based on the geometry of the process the best possible tool orientation for roughing, semi finishing and finishing operations are suggested. Using the suggested tool orientation, fluctuation and interruption of cutting force will be attenuated. Decreasing the fluctuation of forces leads to lower vibration of workpiece and having better surface quality.

Figure 8-16 depicts a schematic view of five-axis ball-end milling of a flexible freeform part. In this figure, l and t represent lead and tilt angles respectively. The real machining setup of the thin flexible workpiece is shown in Figure 8-17. The blank is an aerospace grade Aluminum alloy 7050 block with the dimension of $98 \times 36 \times 10$ [mm]. A two fluted ball-end mill with the diameter of 12 [mm] was used for finishing, semi-finishing and finishing process. The workpiece has a freeform blade shape geometry that has more

flexibility in Z direction shown in Figure 8-17. The other machining parameters are mentioned in Table 8-3.

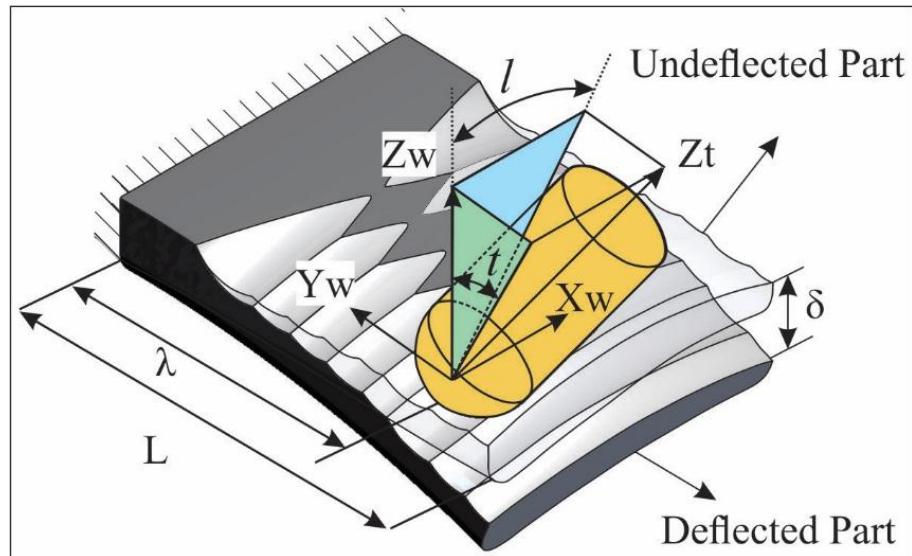


Figure 8-16 - Tool posture in five-axis ball-end milling of a flexible part.

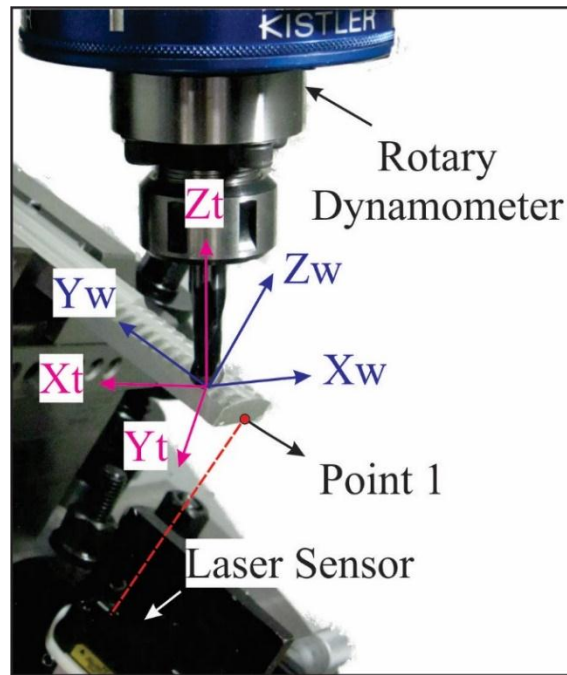


Figure 8-17 - milling of flexible cantilever workpiece (airfoil blade).

Table 8-3 Cutting parameters in ball-end milling of the flexible airfoil parts.

Operation	Roughing	Semi-finishing	Finishing
Depth of cut [mm]	1	0.6	0.15
Step over [mm]	5.8	1.4	0.35
Spindle Speed [rpm]	2500	3500	4500
Feedrate [mm/min]	250	350	500

The flexible workpiece shown in Figure 8-16 is modelled as a cantilever beam. The maximum static deflection of flexible part along Z_w at point 1 of Figure 8-17 can be determined from;

$$\delta = \frac{F_{zw}\lambda^2}{6EI}(3L - \lambda) \quad (8-2)$$

L and λ are the length of the flexible part and the distance of the tool from the fixture as shown in Figure 8-16. E and I are the elastic modulus and the area moment of inertia determined numerically.

Changing tool orientation in five-axis ball-end milling directly affects the instantaneous tool-workpiece engagement region. Therefore, magnitudes and waveforms of cutting forces, as well as forced vibration amplitudes of flexible parts are affected by tool posture. In the cases when the tool tip is in contact with the workpiece, the cutting process would be less interrupted which in turn reduces the magnitude of the tool and workpiece oscillations because of the lower fluctuation of cutting forces. However, due to the fact that cutting speed at the tip of the tool is zero, having the tip in contact causes poor surface quality and significant ploughing forces [50]. In order to solve this problem, all the possible discrete lead and tilt angle pairs, which make the tool tip in contact with the workpiece are determined from engagement model. Then, the tool tip was adjusted marginally to be located outside of the tool-workpiece engagement domain. It is experimentally proved that this strategy leads to pseudo-constant cutting force magnitudes with lower fluctuations on the waveforms and therefore lower forced vibration on the flexible parts.

In order to prevent the tool tip from contact with the workpiece, the critical positive and negative tilt angles (t_{crp} and t_{crn}) should be considered based on Figure 8-18 (b) and (c). In this figure, the critical orientation of the tool in which the tip of the cutter is in contact with the workpiece is shown for positive and negative tilt angles. In this figure a is depth of cut, R is tool radius, l is lead angle, t is tilt angle, so is step over and sh is scallop height,

respectively. Equation (8-2) gives the critical positive tilt angle to have the tip of the tool in contact with the workpiece.

$$t_{crp} = \cos^{-1}\left(1 - \frac{a}{R}\right) \quad (8-3)$$

Figure 8-18 (b) shows the case in which the tilt angle is negative. One can easily derive the following equations for scallop height and critical negative tilt angle:

$$sh = R - \sqrt{R^2 - \frac{so^2}{4}} \quad (8-4)$$

$$\tan(t_{crn}) = \frac{so - w}{R - a} \quad (8-5)$$

From Equation (8-4) and (8-5) the critical tilt angle for the second case of Figure 8-18 can be calculated as the following:

$$t_{crn} = -\tan^{-1}\left(\frac{so}{2(R - a)}\right) \quad (8-6)$$

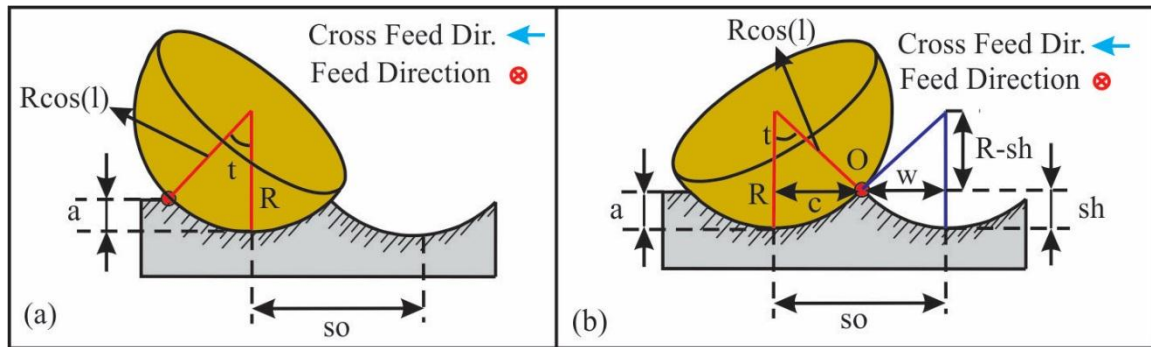


Figure 8-18 - (a) Critical positive tilt angle, (b) Critical negative tilt angle.

By changing the tilt angle between t_{crn} and t_{crp} , the boundary lead angle can be calculated using the following equations:

$$\begin{cases} l_{cr} = -\cos^{-1}\left(\frac{R - sh}{R\cos(t)}\right); & t_{crn} \leq t \leq 0 \\ l_{cr} = -\cos^{-1}\left(\frac{R - a}{R\cos(t)}\right); & 0 \leq t \leq t_{crp} \end{cases} \quad (8-7)$$

The effects of tool posture on the magnitudes and waveforms of cutting force, torque as well as on the forced vibration of the flexible part and the quality of machined surface are investigated. The cutting force and torque were measured using a Kistler 9123C rotary dynamometer. Deflection of the workpiece was measured at point 1, which is located at the tip of the workpiece, using a Keyence LK-H052 laser displacement sensor with 0.025 [μm] repeatability and +/- 0.02% linearity. All of the simulation and experimental cases were designed for Al 7050 blocks with 98×38×10 [mm] dimension. A two fluted ball-end mill with the diameter of 12 [mm] was used in all cases. Other cutting parameters are indicated in Table 8-4. Experiments were designed and conducted in Mori Seiki NMV 5000 DCG five-axis vertical machine.

Table 8-4 - Cutting parameters in ball-end milling of the flexible airfoil parts.

Operation	Roughing	Semi-finishing	Finishing
Depth of cut [mm]	1	0.6	0.15
Step over [mm]	5.8	1.4	0.35
Spindle Speed [rpm]	2500	3500	4500
Feedrate [mm/min]	250	350	500

The effects of the tool posture on the engagement domain and on the cutting force waveforms and magnitudes are investigated. Feasibility of the proposed strategy is illustrated with an example in Figure 3. The cutting forces were acquired while five-axis milling of the airfoil blade shown in Figure 2(a). Spindle speed, feedrate and depth of cut were respectively 2500 [rpm], 250 [mm/min] and 1 [mm].

In Figure 8-19 and Figure 8-20 simulated 2D and 3D engagement zones for the same cutting conditions, but two different tool orientations are shown. In Figure 8-20, the solid line (position A) represents the cutter flute after leaving the engagement area and the dashed line (position B) shows the cutter flute which is about to engage. In the case of lead angle of 25° and tilt angle of -5° , the engagement zone is far from the tool tip, and this causes the oscillatory waveform in the cutting forces. However, in the case of lead angle of 2° and tilt angle of 10° , the tool tip is marginally avoided from the cutting. As experimentally shown in Figure 8-20 (c), this tool posture results in lower oscillation in the cutting force waveforms.

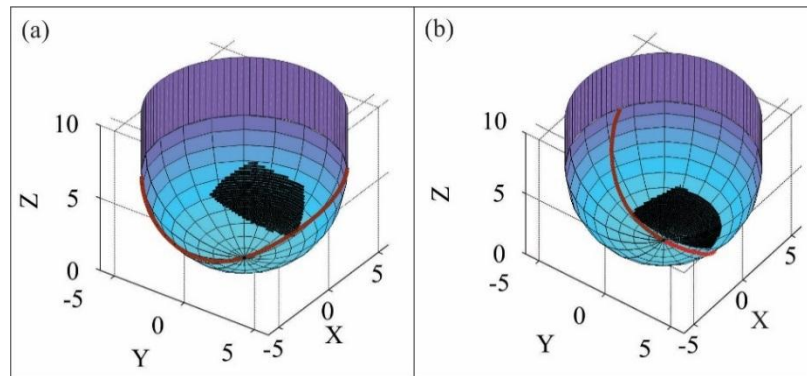


Figure 8-19 - (a) Lead angle of 25° and Tilt angle of -5° , (b) 3D engagement map for Lead angle of 2° and Tilt angle of 10° .

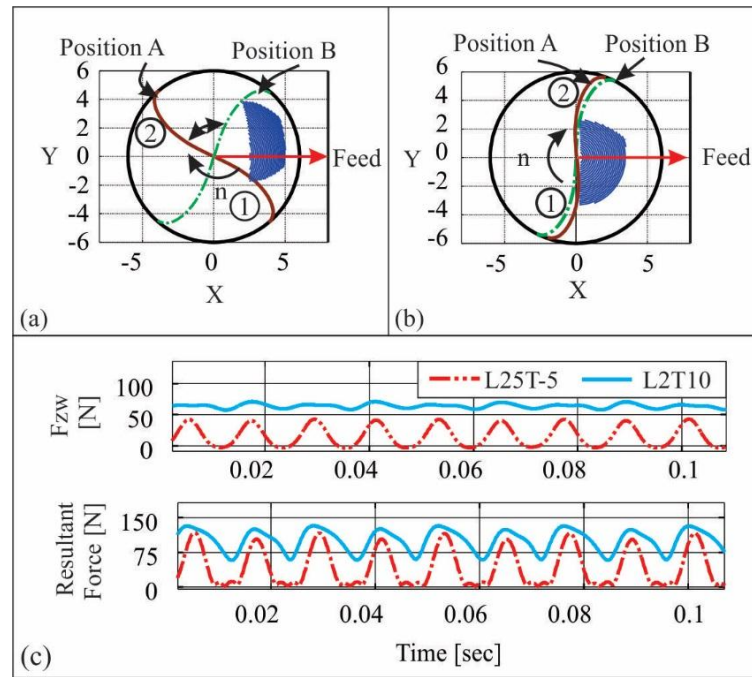


Figure 8-20 - (a) 2D engagement for Lead angle of 25° and Tilt angle of -5° , (b) 2D engagement for Lead angle of 2° and Tilt angle of 10° , (c) Normal and resultant cutting force measured by a rotary dynamometer.

8.6 Mechanics-based reference maps for tool orientation selection

In order to illustrate the effects tool orientation in roughing, semi-finishing and finishing processes, mechanics-based reference maps are generated by considering cutting forces, torque and the deflection of flexible part, as shown in Figure 8-21. Roughing, semi-finishing and finishing zones are depicted based on Equation (8-3), (8-6) and (8-7). It is experimentally proven in section 8.5 that inside of those zones, the amplitude of cutting force vibration is lower than other possible tool postures. Moreover, the hatched rectangular zone in Figure 8-21 is where the tip of the tool is marginally avoided from cutting and for less than 2% of the tool rotation period there is no tool-workpiece engagement. This region is highly recommended, especially for the finishing processes to

achieve better surface quality. The contours of average cutting torque and average workpiece deflection are extracted from the simulation shown in Figure 8-10 (a) and (b). The average deflection of the workpiece is simulated based on the analytical deflection model given in Equation (8-2). The red dots in Figure 8-21, named from A to M, are the test points for the experimental validations of the proposed approach.

Figure 8-21 (a) clearly indicates that the part deflections significantly changes by varying tool posture. According to the Figure 8-21 (b), the measured cutting torque does not change significantly inside the roughing, semi-finishing and finishing boundaries. However, it is possible to decrease the average of cutting torque and power up to 15% in the roughing process by selecting the appropriate tool orientation using Figure 8-21 (b).

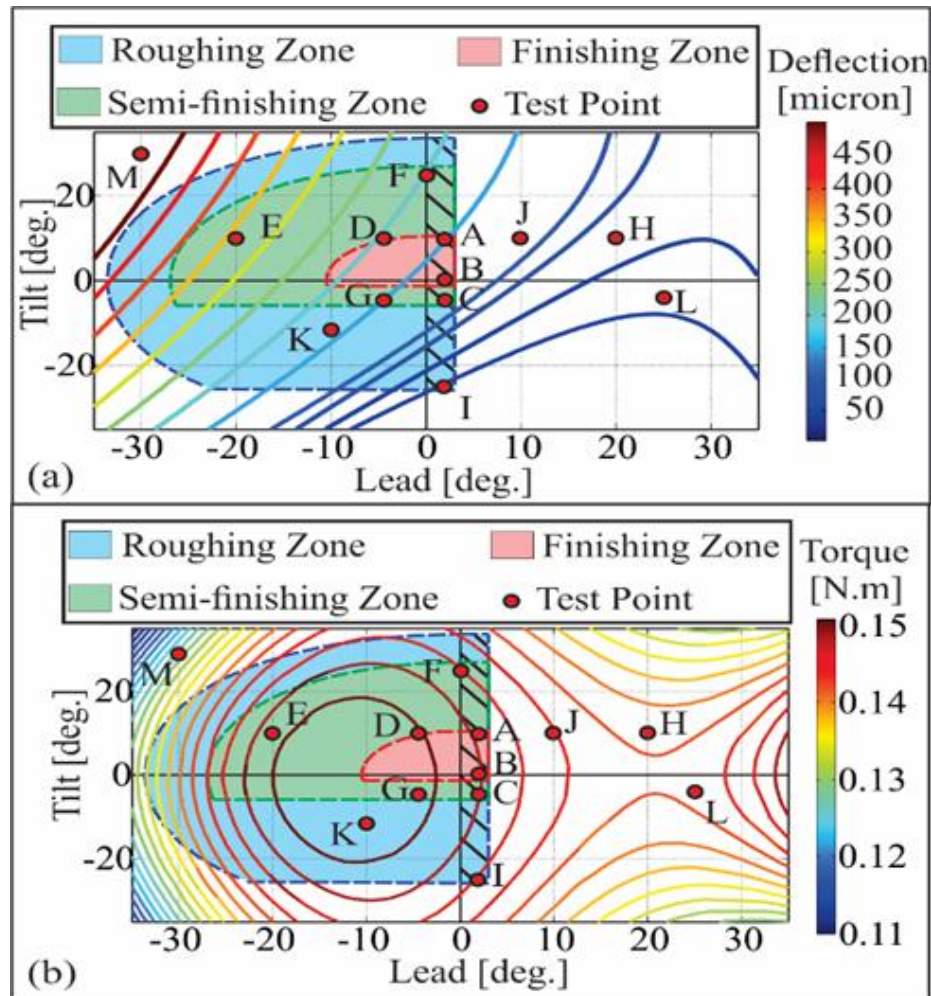


Figure 8-21 - Mechanics-based tool posture reference maps of part deflection (a) and cutting torque values (b) for the selection of lead-tilt angle combinations in the ball-end milling of the flexible airfoil blade.

8.7 Validation and discussion

In order to validate the reference maps given in Figure 8-21, experimental tests were conducted using the setup shown in Figure 8-17. As is shown in Table 2, the average

surface roughness (S_a) of the finished surfaces were measured using a White Light Interferometer (WLI) with the cut-off value of 0.25 mm.

Table 8-5 - Average surface roughness measured with the WLI.

Case Figure 8-21	A	B	C	D	E	F	G	H	I	J	L	M
Lead [$^{\circ}$]	2	2	2	-5	-20	0	-5	20	2	10	25	-30
Tilt [$^{\circ}$]	10	0	-5	10	10	25	-5	10	-25	10	-5	30
S_a [μm]	2.0	2.1	2.8	2.8	3.0	3.3	3.9	4.2	5.2	5.8	9.0	9.0

According to Table 8-5, the best surface roughness of all the cases is achieved in case A (lead= 2° and tilt= 10°). All the cases located in the red and red-hatched area of Figure 8-21 perform well with relatively lower S_a values. In the cases located outside of the red and green boundaries, up to four times increase in the S_a value was observed. Consequently, the experimental results prove that by selecting lead and tilt angles in the red finishing zone and red-hatched area in Figure 8-21, the best surface roughness can be obtained. In order to show the effectiveness of the proposed approach, the details of test results for the cases of A, D and H are demonstrated thoroughly in Figure 8-22 to Figure 8-24.

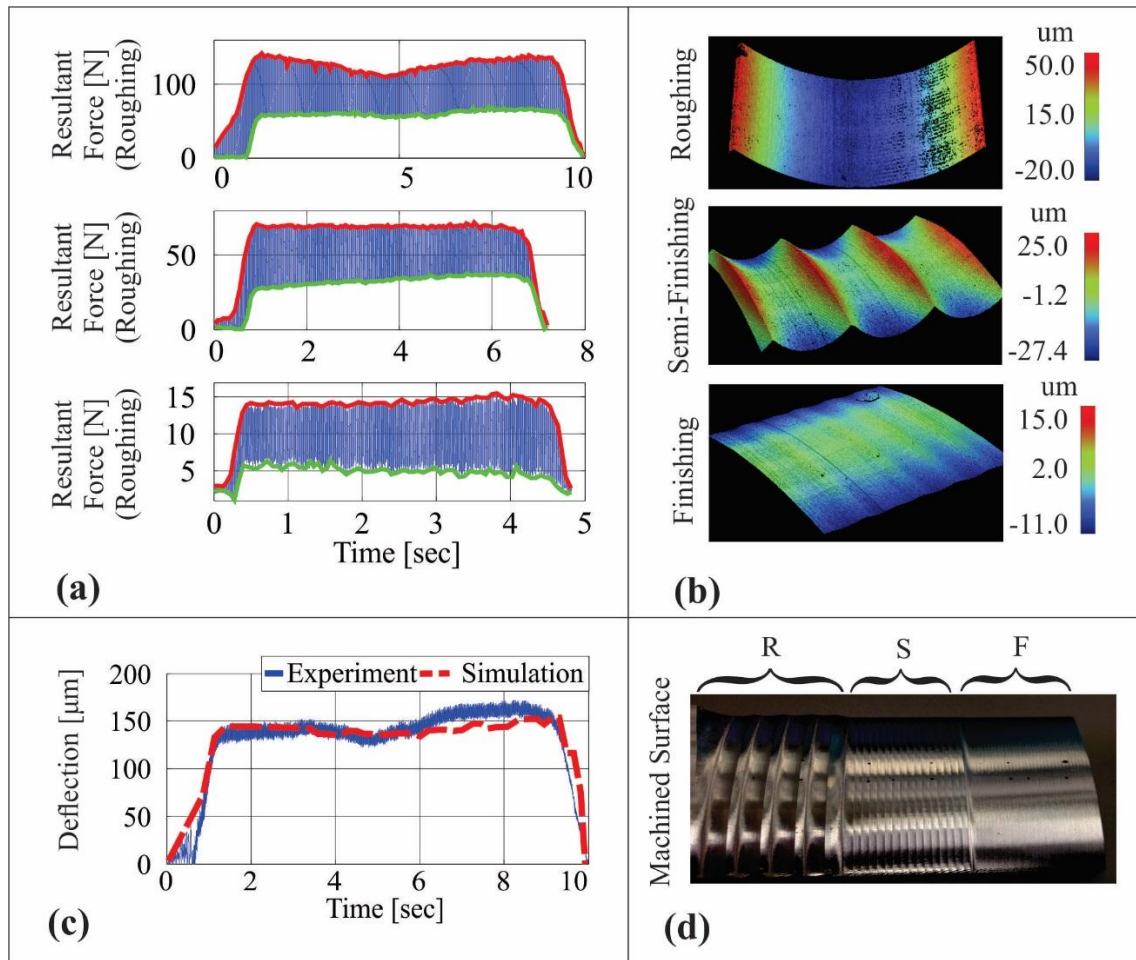


Figure 8-22 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughing, S-Semifinished, F-Finished) (d) for the cases of A (lead=2°, tilt=10°).

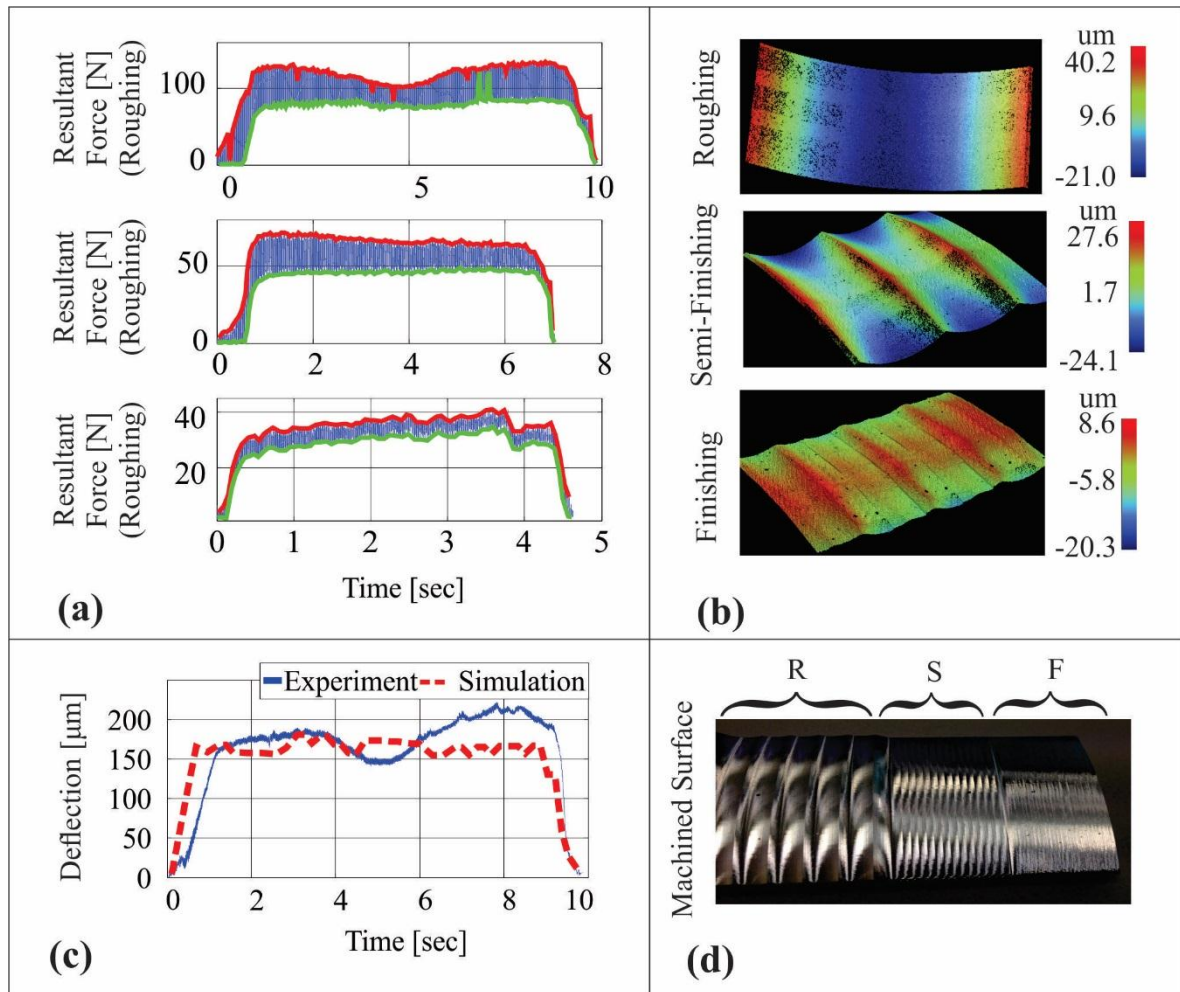


Figure 8-23 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughing, S-Semifinished, F-Finished) (d) for the cases of D (lead= -5° , tilt= 10°).

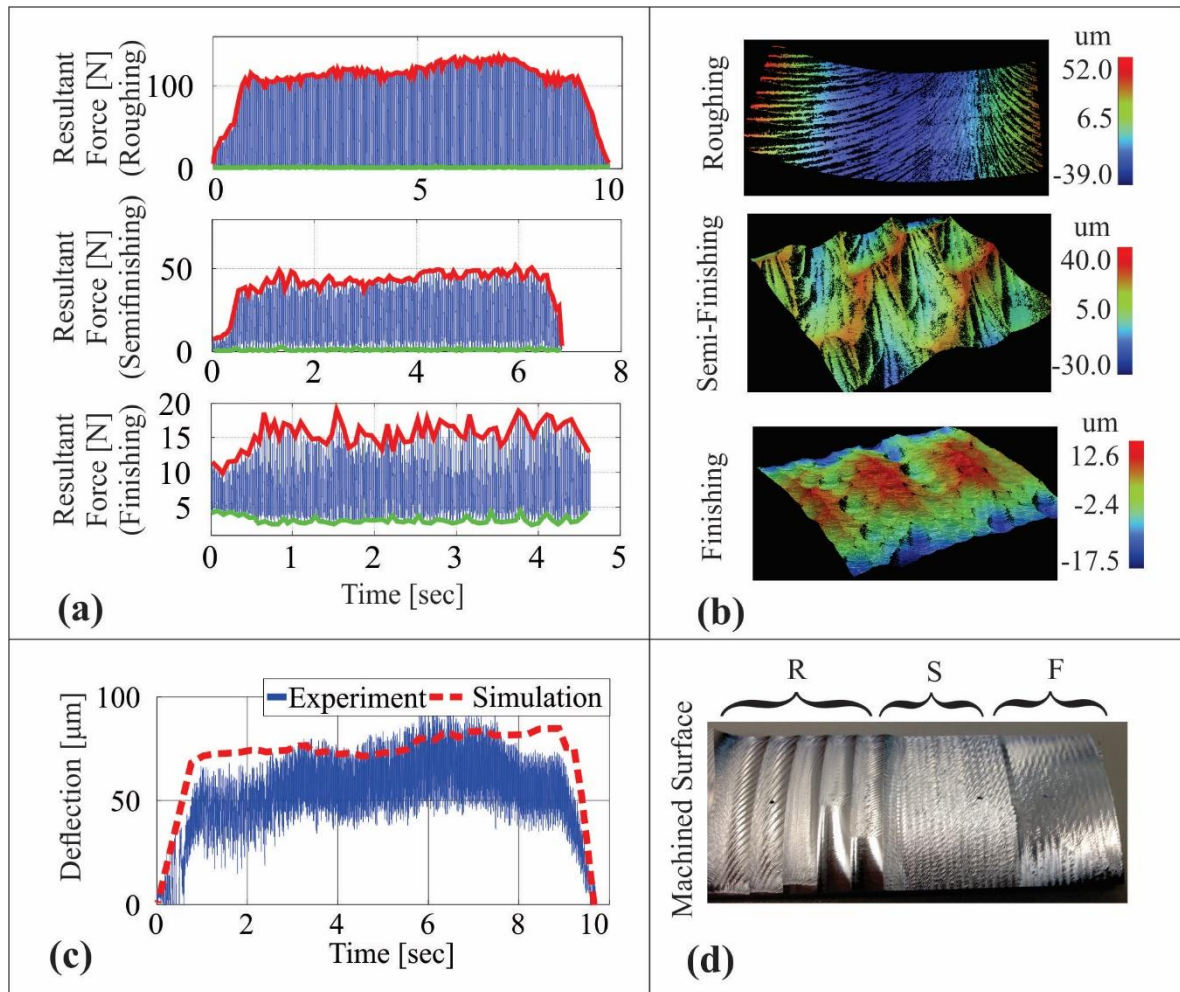


Figure 8-24 - Comparison of cutting forces (a), WLI surface roughness measurements (b), measured and simulated part deflections (c) and part surface images (R-Roughing, S-Semifinished, F-Finished) (d) for the cases of H (lead=20°, tilt=10°).

Measured resultant cutting forces are given in Figure 8-22 to Figure 8-24 (a), (e) and (i) to illustrate how the amplitude of force vibration is affected by the tool orientation. The instantaneous deflection of the flexible airfoil blade at the tip of the workpiece (Point 1 in Figure 8-17 (a)), was measured using the laser displacement sensor and presented in Figure

8-22 to Figure 8-24 (c), (g) and (k). For same cutting conditions, but different tool postures, in case D compared with case H, the peak to peak amplitude of cutting force vibration is 2.7, 2 and 1.7 times less for roughing, semi-finishing and finishing operations respectively. Because of this, as is seen in Figure 8-23 (c), lower peak to peak amplitude of part vibration was detected in case D. Moreover, as is presented in Table 8-5, the average of surface roughness (S_a) in case D is 33% better than case H. Significant improvement can be achieved by avoiding the tool tip from contact. As is shown in Figure 8-23 (b) for case D, tool leaves tip marks on the final finished surface due to the engagement with the workpiece. However, in cases of A, B and C the tool tip is not in contact with the workpiece. WLI measurement presented in Figure 8-22 (a) shows that the tip marks on the generated surface are removed after finishing operation of case A. Due to this reason, the surface roughness is improved more than twice compared to case H. Generally, it is possible to decrease S_a value from 9.0 [μm] in case M to 2.0 [μm] in case A which is 4.5 times improvement. The roughing, semi-finishing and finishing surface images are shown in Figure 8-22 to Figure 8-24 (d).

The measured deflection of the workpiece, which is shown in Figure 8-22 to Figure 8-24 (c), are in good agreement with the deflection contours in Figure 8-10(a). The results clearly show that tool posture in five-axis milling of flexible parts is critical. As a complementary solution to the current geometry-based CAM technology, the effectiveness of the mechanics-based modelling approach for the selection of lead and tilt angles is shown and validated.

8.8 Summary

In this chapter, the effects of tool orientation, which is defined by lead and tilt angles are investigated. It was shown that any change in the tool posture can directly affect cutting forces, cutting torque, surface quality and workpiece deflection. It was also demonstrated

that not only the magnitude of cutting force, but also the cutting force waveforms is depended on the lead and tilt angles. The critical positive and negative lead and tilt angles are calculated in order to have lower amplitude of cutting force fluctuations. It was experimentally shown that in order to have lower fluctuation of cutting waveforms, tool tip must be marginally avoided from cutting. The deflection of the tool is also calculated and measured by changing the tool orientation. The novelty of the approach was introducing a reference map to select the best tool orientation in order to reduce workpiece deflection and vibration and increase the surface quality. The 3D surface roughness are measured using a white light interferometer to validate the proposed reference map.

The presented strategy is developed as a complementary approach to the current geometry-based CAM technology. This approach considers cutting force, torque, part deflection, forced vibration and surface quality of flexible parts. Effectiveness of the strategy is validated by conducting experiments on five-axis ball-end milling of flexible freeform structures.

Chapter 9

MODELING OF SURFACE TOPOGRAPHY IN FIVE-AXIS BALL-END MILLING

9.1 Overview

Nowadays there are high demands for the best possible surface quality in five-axis milling operation. In order to meet those demands the lowest surface roughness and dimensional error is required. Roughness and surface topography of the final machined surface depends on the cutting parameters, a defect in the grinding of the cutter flutes, axial eccentricity of the tool/holder/spindle, axial inclination of the tool, tool/workpiece deflection, wear, and so on. The machining industry can no longer afford try and error methods to come up with the best cutting parameters to achieve the best possible surface quality. Due to this fact, there is a high quest for virtual models to anticipate the surface topography of the final product prior to real milling operation. Surf

As it was mentioned in Section 2.8, several authors have considered the effects of cutting parameters, runout, vibration, static deflection, and tool wear on the surface topography and surface texture in three-axis milling operations. However, there is no five-axis milling model capable of virtual simulation of the machined surface for different tool orientation. In the modern machining industry, the structure of the machine tool has been improved and the vibration of the tool and workpiece is controlled by accurate dynamic analysis and selecting the best cutting parameters. The tool and tool-holder manufacturer has also developed high quality products with almost zero runout and eccentricity. Consequently, the trochoidal motion of the tool and cutting parameters are the most important parameters in determining the surface topography.

In this chapter, an analytical model is presented to simulate the surface topography of the machined surface in multi-axis ball-end milling operation. The model is capable of considering the tool orientation as lead and tilt angles. The ball-end mill tool is divided into parallel slices perpendicular to the tool axis. The trochoidal motion of each cutting point on each slice is modelled by driving the analytical equation considering the runout and tool orientation. The trochoidal motions are expressed as a parametric equations with respect to the angular position of the tool. Then, the trochoidal curves are intersected with many planes perpendicular to the feed direction. Each trochoidal curve intersects each plane several times leading to a point cloud on the corresponding plane. The points on the convex hull of the point cloud are detected for each plane. By fitting a curve to the detected points the cross section of the machined surface of the corresponding plane can be obtained. The machining surface is modelled by fitting a surface to the obtained cross sections at the perpendicular planes.

The novelty of the proposed approach lies in considering multi-axis milling operation. The accuracy of the model depends on the number of the tool slices and perpendicular planes. Obviously, there is a reverse relationship between the accuracy of the model and computation time. The validity of the simulation is experimentally shown by conducting different tests with different tool orientation. The surface topography of the machined surfaces is measured using WLI and compared with the simulation results. As it is shown, a good agreement between the simulation results and experimental measurement is observed.

9.2 Trochoidal motion of the cutter points

The details of the ball-end mill tool geometry was comprehensively analyzed in section 5.2. As it was discussed in Chapter 5, the feed vector in five-axis milling is always located in XZ plane of the feed coordinate frame (FCF) shown as $X_f Y_f Z_f$ in Figure 9-1. Two components of feed vector are defined as f_x and f_z in FCF. For the sake of simplicity, a two

fluted tool is considered in this chapter, however, the equations are derived in general form to be compatible with any tool geometry. The trochoidal trajectory of point $P_{i,N}$, which is located at height Z_i of the flute number N , can be written in the feed coordinate frame as the following:

$$\begin{aligned}
 x_{P_{i,N}} &= \frac{f_x}{2\pi} \times \theta + \rho \cos(\theta) + R_i \cos(\zeta - \theta) \\
 y_{P_{i,N}} &= -\rho \sin(\theta) + R_i \sin(\zeta - \theta) \\
 z_{P_{i,N}} &= \frac{f_z}{2\pi} \times \theta + Z_i \\
 R_i &= \sqrt{R^2 - (R - Z_i)^2} \\
 \zeta &= \lambda + \psi + (k - 1) \cdot \left(\frac{2\pi}{N_t}\right)
 \end{aligned} \tag{9-1}$$

Above, ρ and λ are the runout offset and runout angles which are defined in reference [105]. In Equation (9-1), θ , ψ , N_t , k , and R are respectively rotation angle of the tool, local helix angle, total number of flutes, number of the current flute and tool radius.

A sample two fluted tool with 12 [mm] nominal diameter is shown in Figure 9-2. The cutting flutes are highlighted with red and blue colors. The ball nose part of the tool is divided into 100 slices perpendicular to the tool axis. The trajectories of two points located on different cutting flute for disk number #90 and #10 are shown in the figure as well. In order to show the trochoidal motion of the tool clearly and just for demonstration, the feed rate is exaggeratedly set as 5 [mm/rev]. Each point on the cutting flute, sweeps the space according to the Equation (9-1) as is shown for two sample case in Figure 9-2.

The trochoidal trajectories of two points located on disk #90 are shown in more details in Figure 9-3. In the presented case the runout offset ρ is 0.025 [mm] and the runout angle λ is 0° . The upper and lower envelopes represent the feed-marks on the machined surface for the up-milling and down-milling sides. Lower feedrate and runout offset leads to smoother feed-marks.

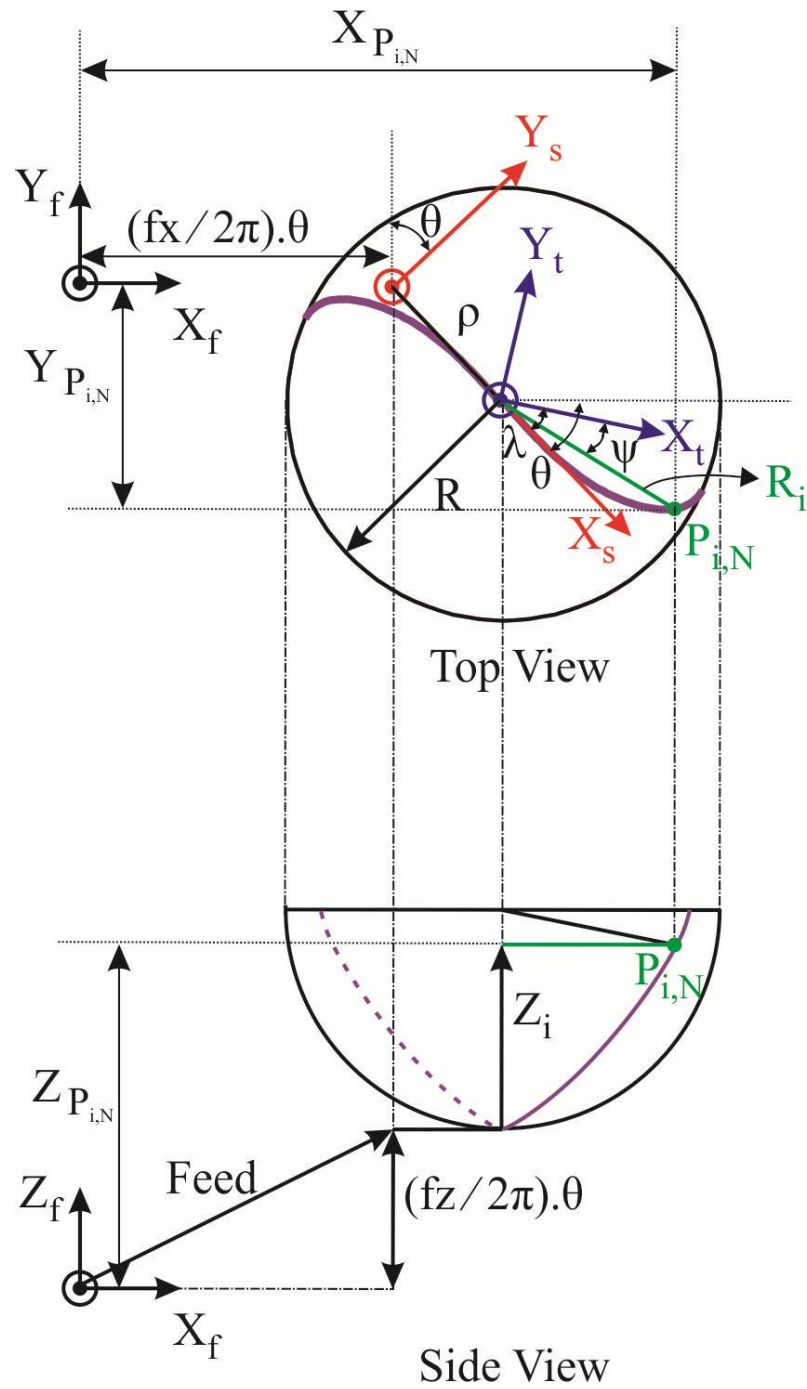


Figure 9-1 – Ball-end mill tool geometry and runout definition.

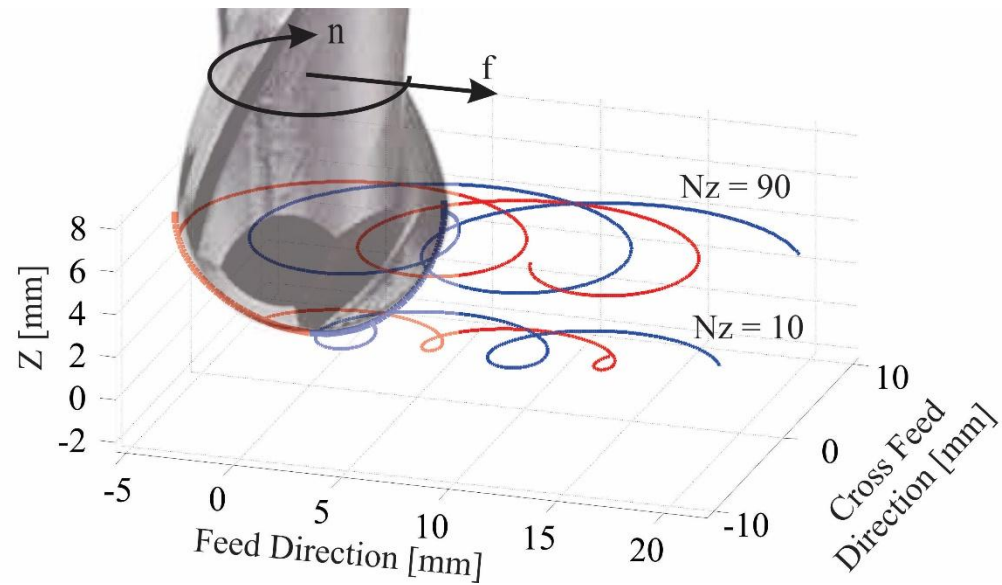


Figure 9-2 – Trochoidal motion of cutting flutes for two different height.

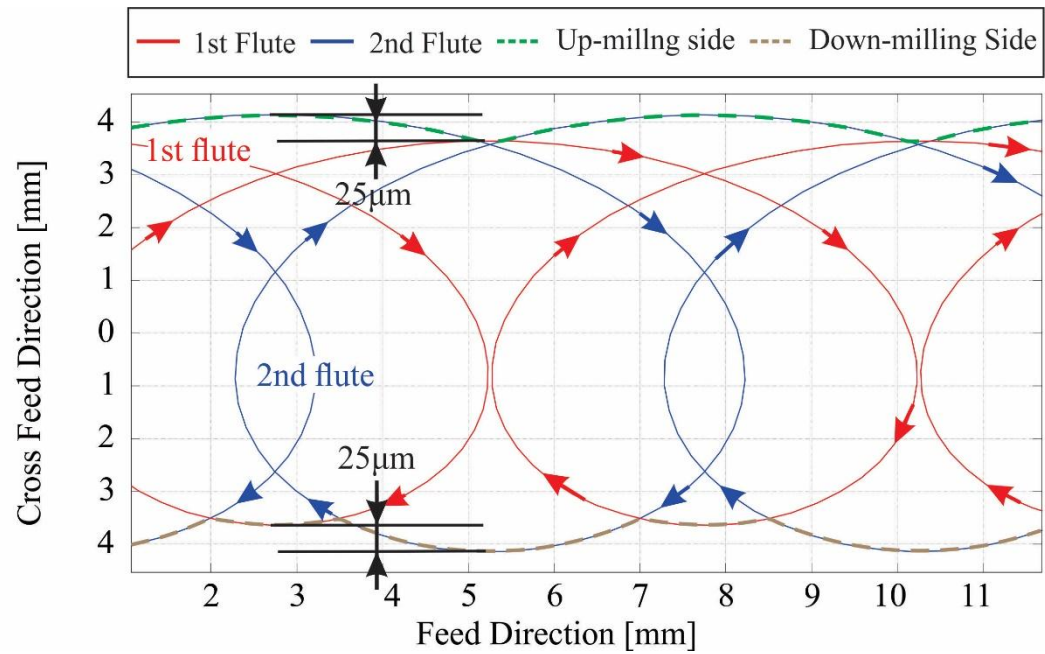


Figure 9-3 – Trochoidal motion of a point on cutting flute.

9.3 Simulation of surface topography

In order to simulate the topography of the machined surface in five-axis ball-end milling operation, the sweeping trajectory of each point on the cutting flutes is derived and plotted based on Equation (9-1). The tool is divided into slices perpendicular to the tool axis. Increasing the number of disk slices leads to better resolution of the simulated surface topography. However, the higher number of slices increases the computation time as well.

Simulated trajectories are intersected with some planes perpendicular to the feed direction to generate machining points as a point cloud. As it will be explained further, the cross section of the machined surface is simulated by finding the convex hull of the point cloud. Next, the surface topography is modelled by fitting 3D surface to the simulated cross section. The details of the approach are explained in the following sections.

9.3.1 Finding machining point cloud

In order to generate the topography of the final machined surface, the cross sections of the machined surface is needed. The intersection between the trochoidal trajectories of cutting points on the cutter flutes and the planes perpendicular to the feed direction gives the machining points. One example case of two points on the cutting flute is demonstrated in Figure 9-4. As it was mentioned in the previous section, the higher number of cutting points and perpendicular planes leads to the high resolution of simulated surface topography.

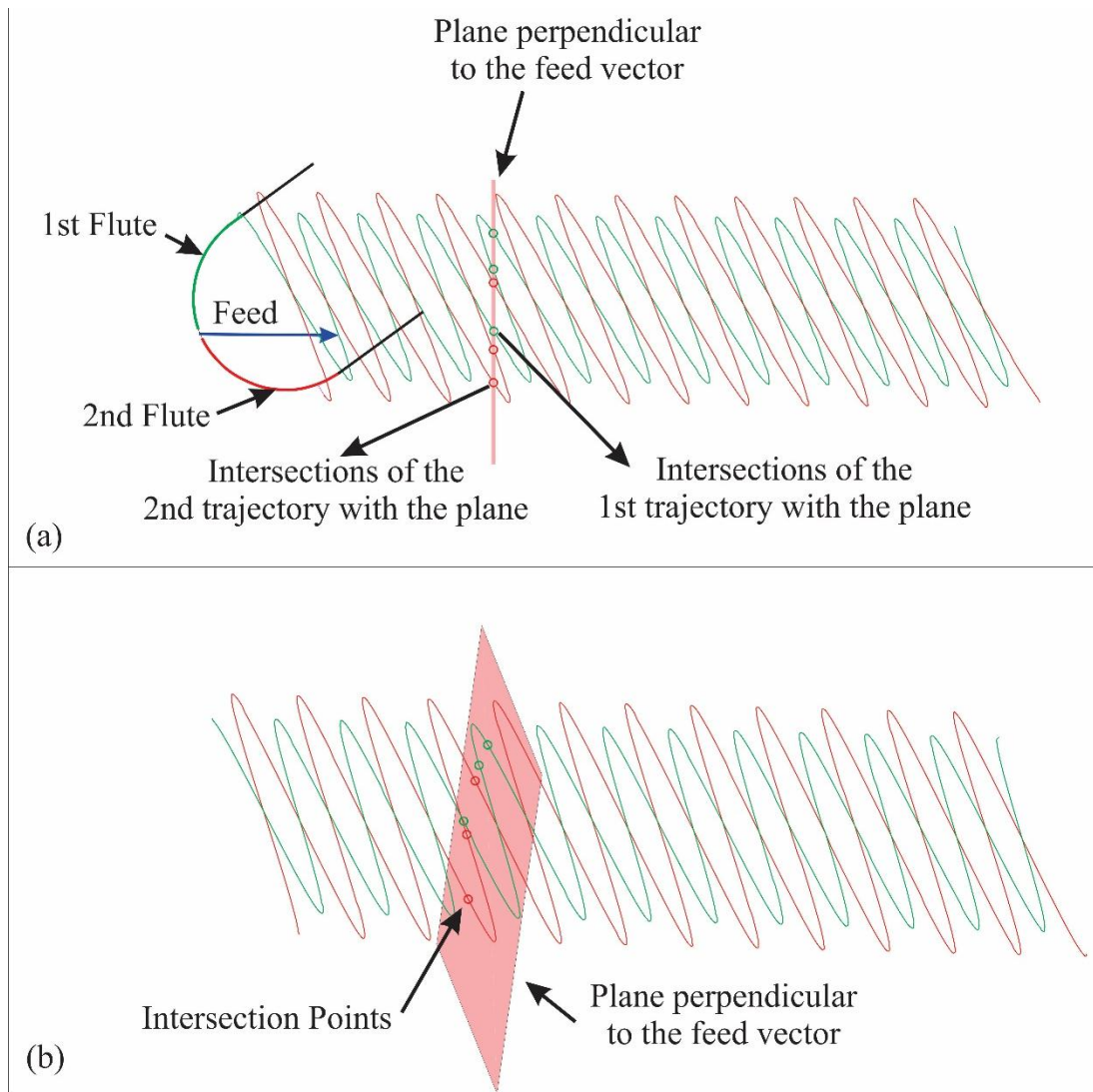


Figure 9-4 – Schematic view of the intersection between cutting point trajectories and a plane perpendicular to the feed direction.

By finding the intersection of all trajectories with the perpendicular plane, it is possible to find the point cloud for each plane. In order to find the intersection points, the following nonlinear system of equations needs to be solved in feed coordinate frame for each perpendicular plane:

$$\begin{cases} x_{P_{i,N}} = \frac{f_x}{2\pi} \times \theta + \rho \cos(\theta) + R_i \cos(\zeta - \theta) \\ y_{P_{i,N}} = -\rho \sin(\theta) + R_i \sin(\zeta - \theta) \\ z_{P_{i,N}} = \frac{f_z}{2\pi} \times \theta + Z_i \\ ax_{P_{i,N}} + by_{P_{i,N}} + cz_{P_{i,N}} = d \end{cases} \quad (9-2)$$

And

$$R_i = \sqrt{R^2 - (R - Z_i)^2}$$

$$\zeta = \lambda + \psi + (k - 1) \cdot \left(\frac{2\pi}{N_t}\right)$$

The first three equations of the system equations are explained in section 9.2. The last equation represents an arbitrary plane perpendicular to feed direction as is shown in Figure 9-4. The system of equations needs to be solved several times to find the intersection points. For 200 cutting points and 30 planes for instance, the system of equations must be solved 6000 times. Figure 9-5 depicts a sample point cloud obtained for 30° lead angle of a two fluted tool. In this case 200 cutting points on the cutter flutes are considered. The green and red points represent the points generated by each flute.

9.3.2 Finding the cross section of the machined surface

The final feed-mark on the machined surface can be obtained by finding the convex hull of the point cloud in Figure 9-5. The black points in this figure are determined using “conhull” syntax in MATLAB. By fitting a curve to the convex hull points, the cross section of the generated surface can be achieved. Considering the depth of cut and stopover, a sample cross section of the machined surface is shown in Figure 9-6. In this case, the depth of cut is 0.4 [mm] and the step over is 1.5 [mm].

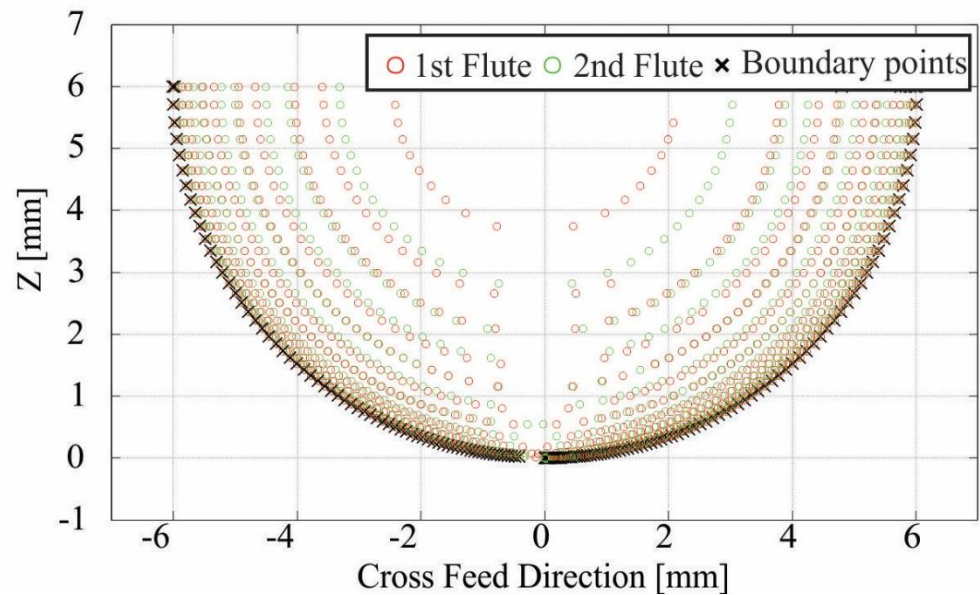


Figure 9-5 – Point cloud of the intersections between trochodal trajectories and planes perpendicular to feed vector.

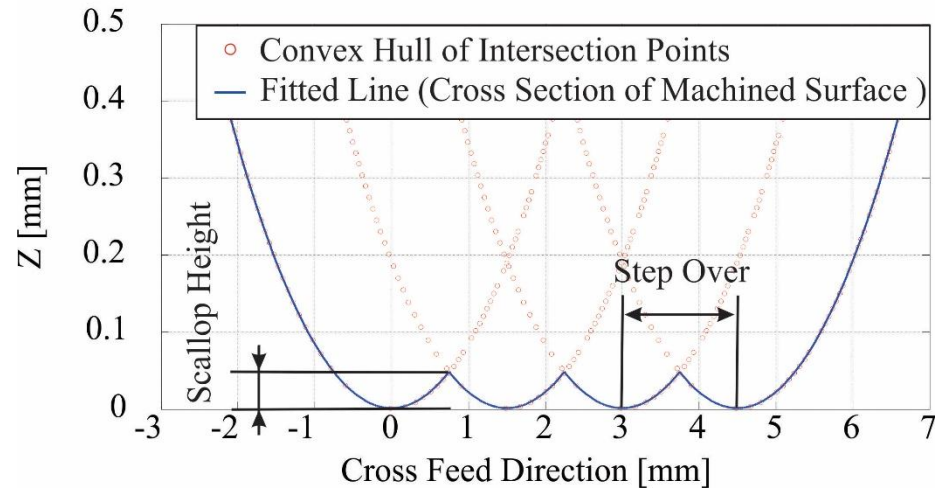


Figure 9-6 – Simulated cross section of the machined surface.

The generated cross-section are stored and used to construct the final texture of the machined surface. The higher number of cross-sections leads to have the higher resolution of the machined surface. However, in order to have more cross sections the nonlinear system of equations presented in Equation (9-2) must be solved several times.

9.3.3 Simulation of the machined surface

By plotting the obtained cross sections from the previous section, the structure of the machined surface can be visualized. Figure 9-7 is an illustration of the texture of the machined surface. The simulation parameters for the generation of this figure are mentioned in the figure caption.

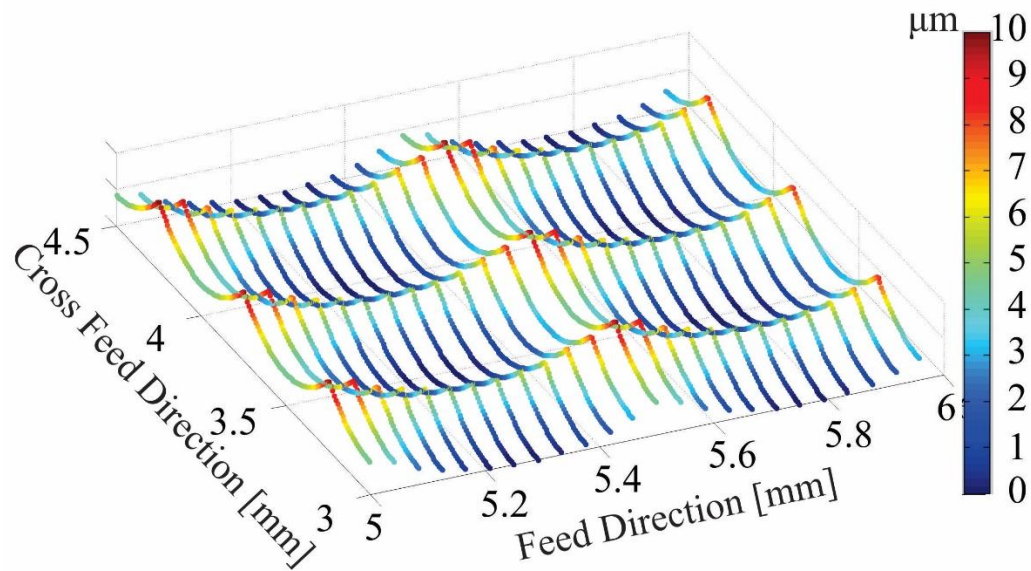


Figure 9-7 – Obtained machining cross sections for 25 plane perpendicular to the feed direction (Lead = 30° , Tilt = 0° , Feed = 1 [mm/tooth], Tool Radius = 12 [mm], Runout offset $\rho = 0$).

In order to simulate the machined surface, a 3D surface is fitted to the simulated cross sections using the curve fitting toolbox of MATLAB. A sample case is shown in Figure 9-8. The fitted surface in this case is obtained for the cross sections shown in Figure 9-7. For all the simulation cases the R-square of the fitness is less than 0.9.

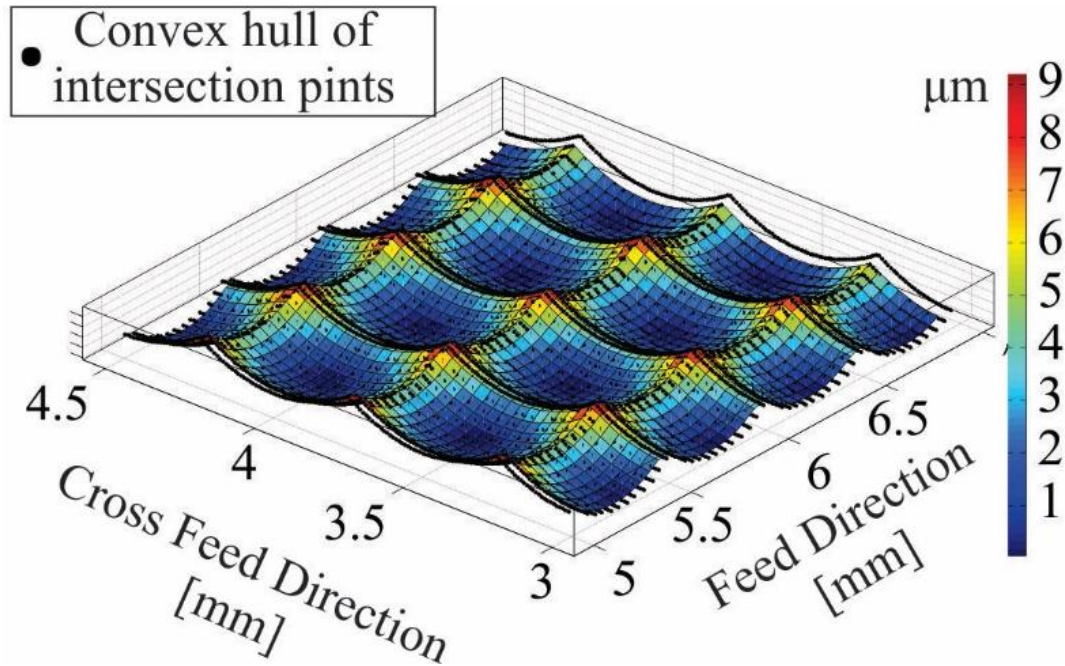


Figure 9-8 – Simulated machined surface by fitting a surface to the convex hull of intersection points.

9.4 Results and discussion

Using the approach explained above, some simulations and experimental validations are conducted for different cutting tool orientation, feed and runout parameters. The topography of the machined surfaces is measured using the white light interferometry approach. For all the experimental samples, the cut-off value of WLI was set as 0.25 mm. The simulation and experimental cutting conditions used in this chapter are mentioned in

Table 9-1. All the machining tests are conducted on an Aluminum 7050 block. Two sets of tool and tool holder with same tool geometry and different runout parameters are used. The runout offset and runout angle for each set were measured using the setup shown in Figure 5-2. For the first setup the runout offset and runout angles was zero. However, for the second setup the runout offset was 40 [μm] and the runout angle was 90°.

Table 9-1 - Cutting Conditions

Tool Diameter	12 [mm]
Number of flutes	2
Spindle Speed	5000 [rpm]
Feedrate	0.5-1 [mm/rev]
Depth of cut	0.5 [mm]
Step over	0.5 [mm]
Lead Angle	0-30 [°]
Tilt Angle	0-30 [°]

The simulated and measured surface topographies for five-axis ball-end milling operation are illustrated for some cases in Figure 9-9 to Figure 9-20. 2D and 3D surface texture of the machined surface are extracted from white light interferometer (WLI) equipment which are illustrated along with simulated data.

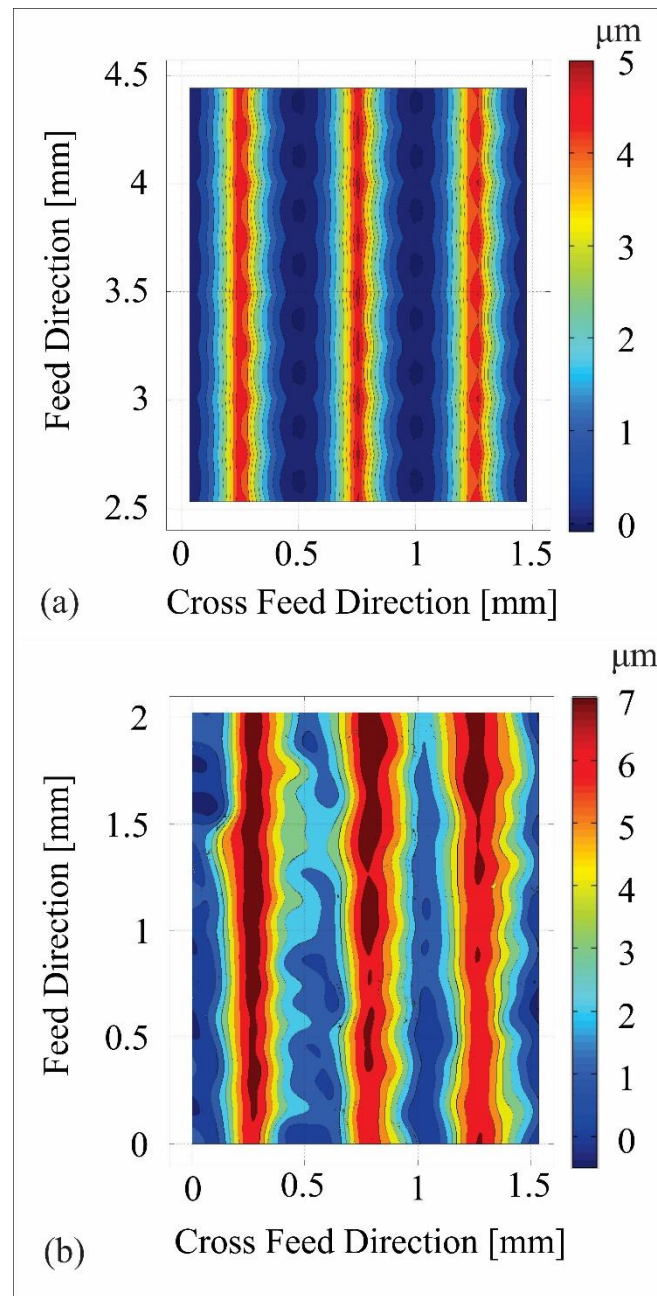


Figure 9-9– (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0° , Tilt = 0° , Feed = 0.5 [mm/tooth], runout offset $\rho = 0$).

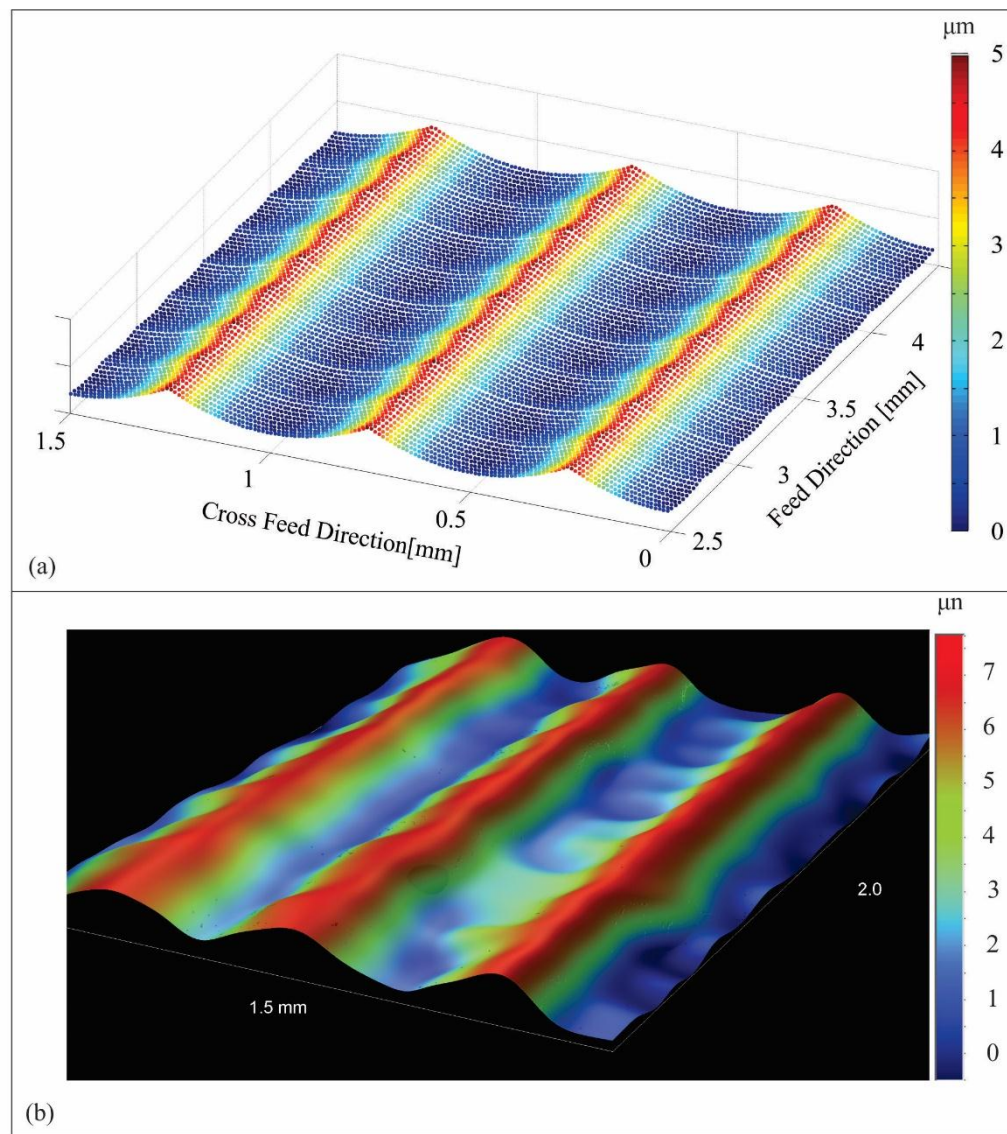


Figure 9-10 - (a) Simulated 3D surface topography (b) Measured 2D surface topography using WLI (Lead = 0° , Tilt = 0° , Feed = 0.5 [mm/tooth], runout offset $\rho = 0$).

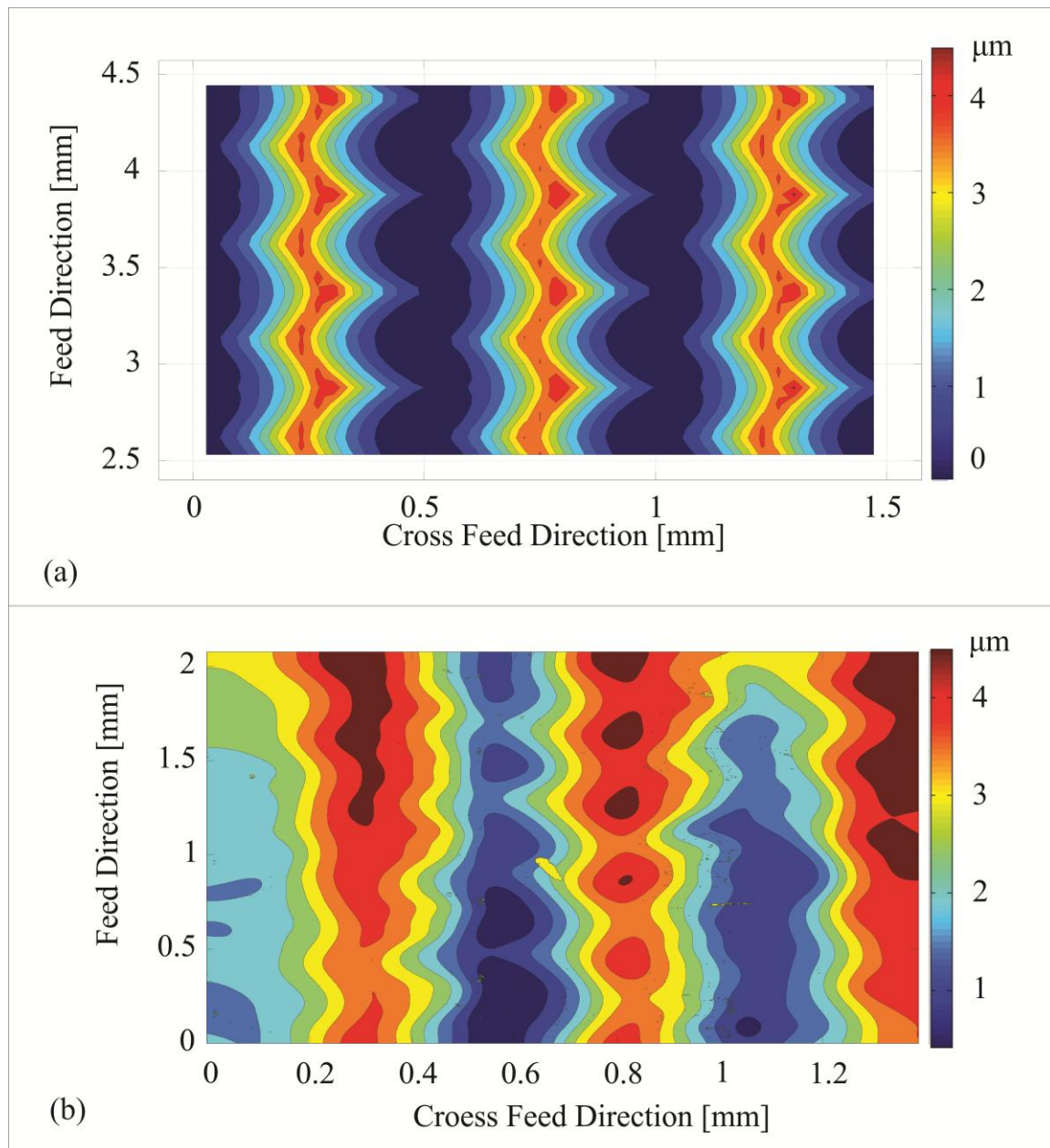


Figure 9-11 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0° , Tilt = 0° , Feed = 0.5 [mm/tooth], Runout offset $\rho = 40$ [μm], Runout angle 90°).

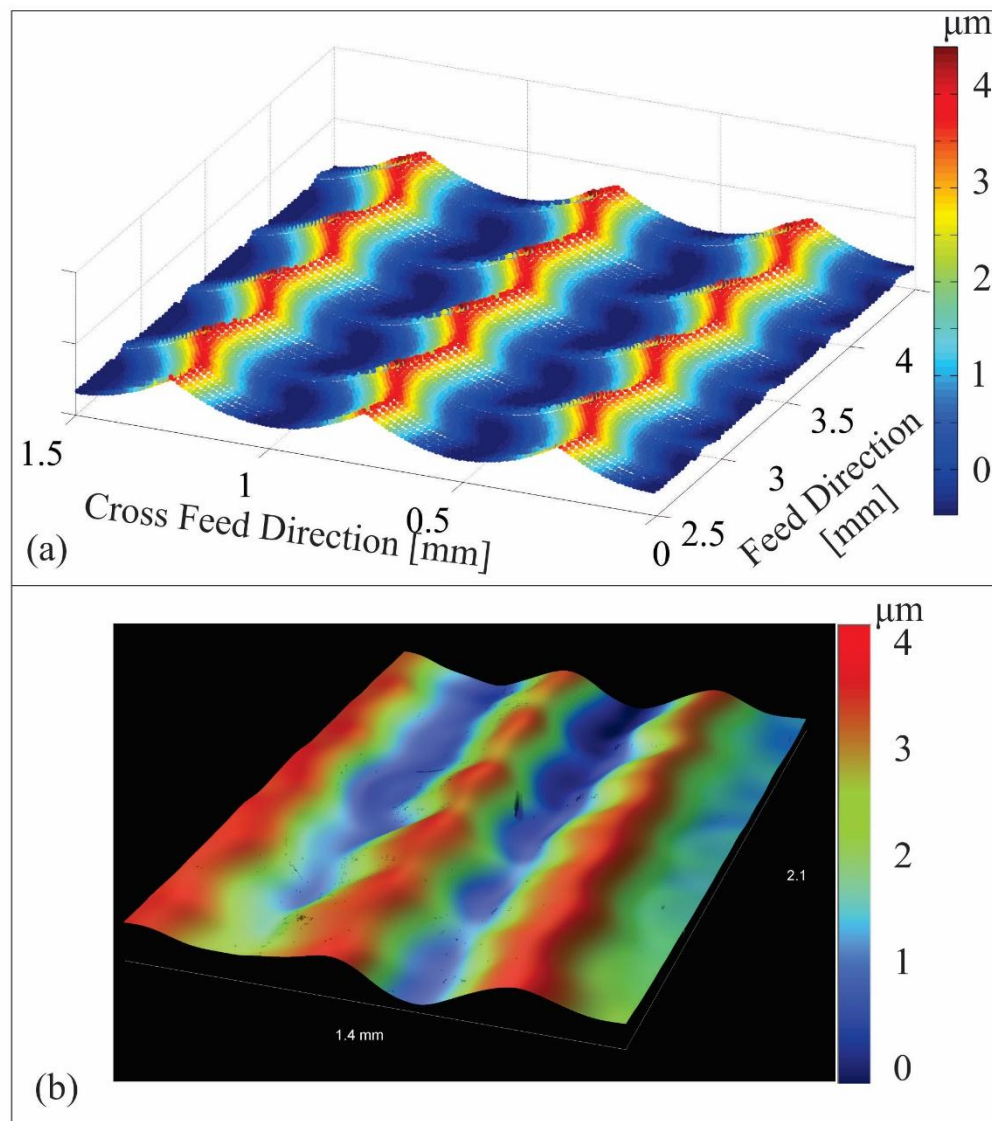


Figure 9-12 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 0° , Tilt = 0° , Feed = 0.5 [mm/tooth], $\rho = 20$ [μm]).

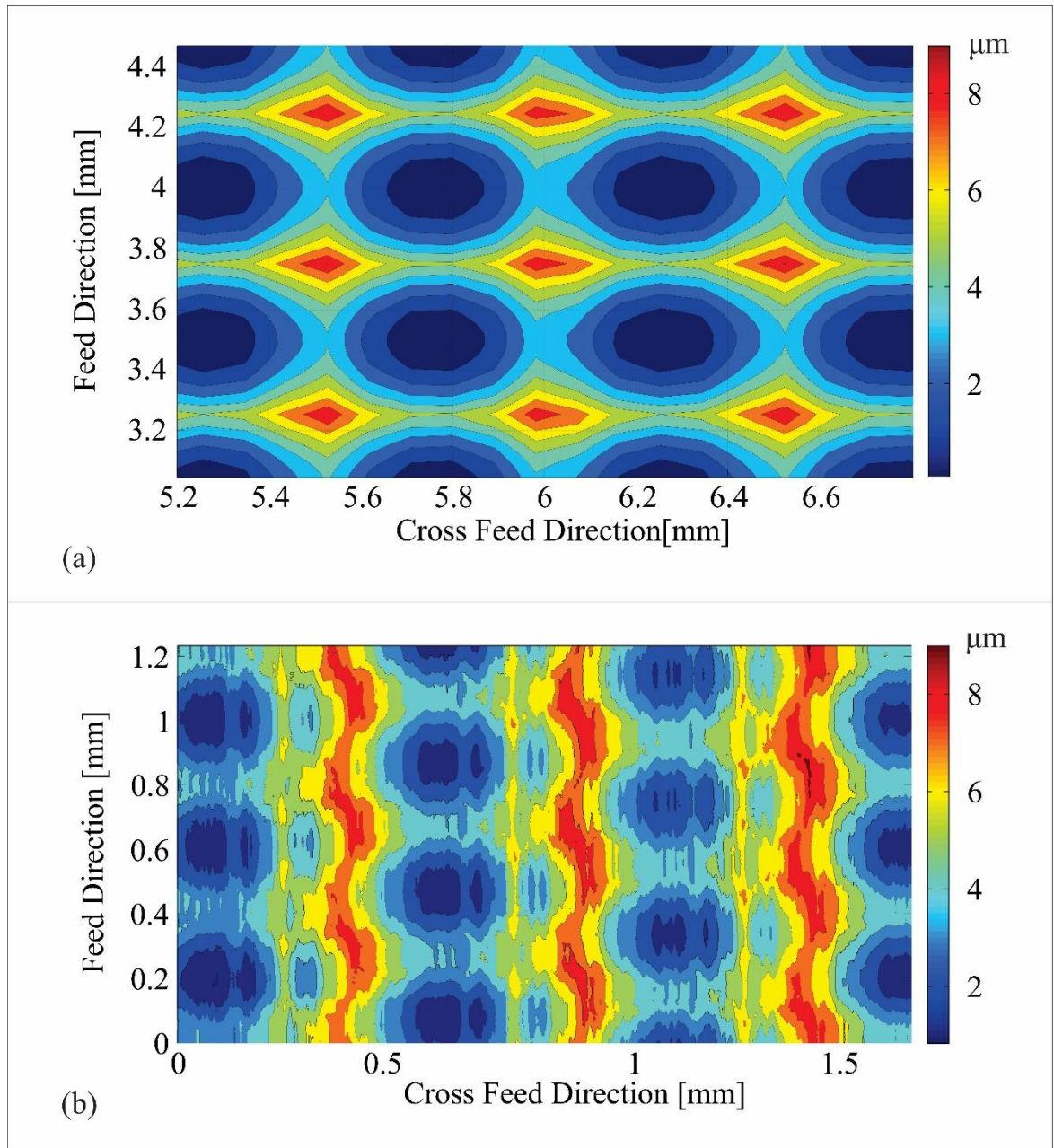


Figure 9-13 – (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 0° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 0$).

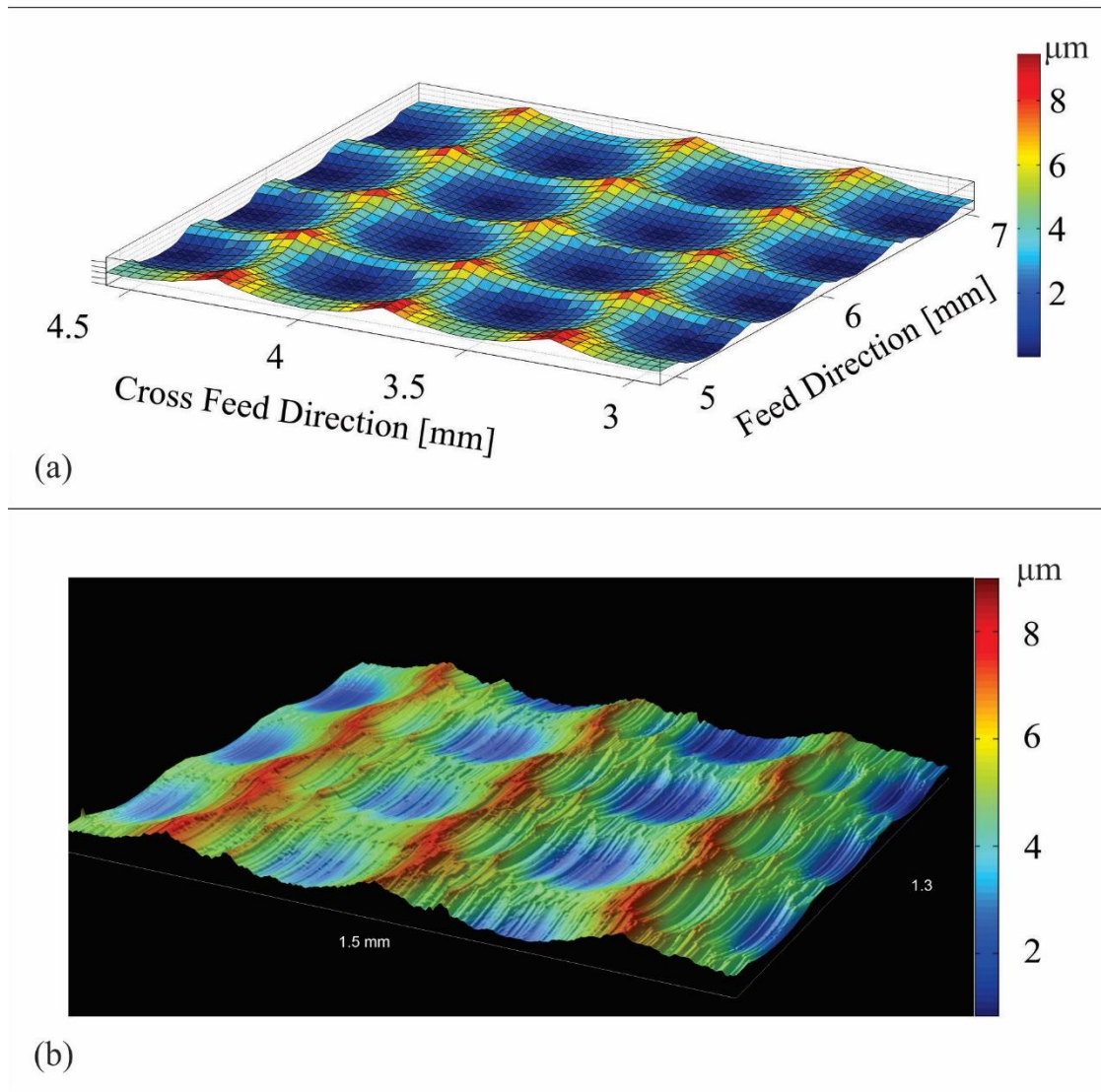


Figure 9-14 - (a) Simulated 3D surface topography (b) Measured 3D surface topography using WLI (Lead = 0° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 0$).

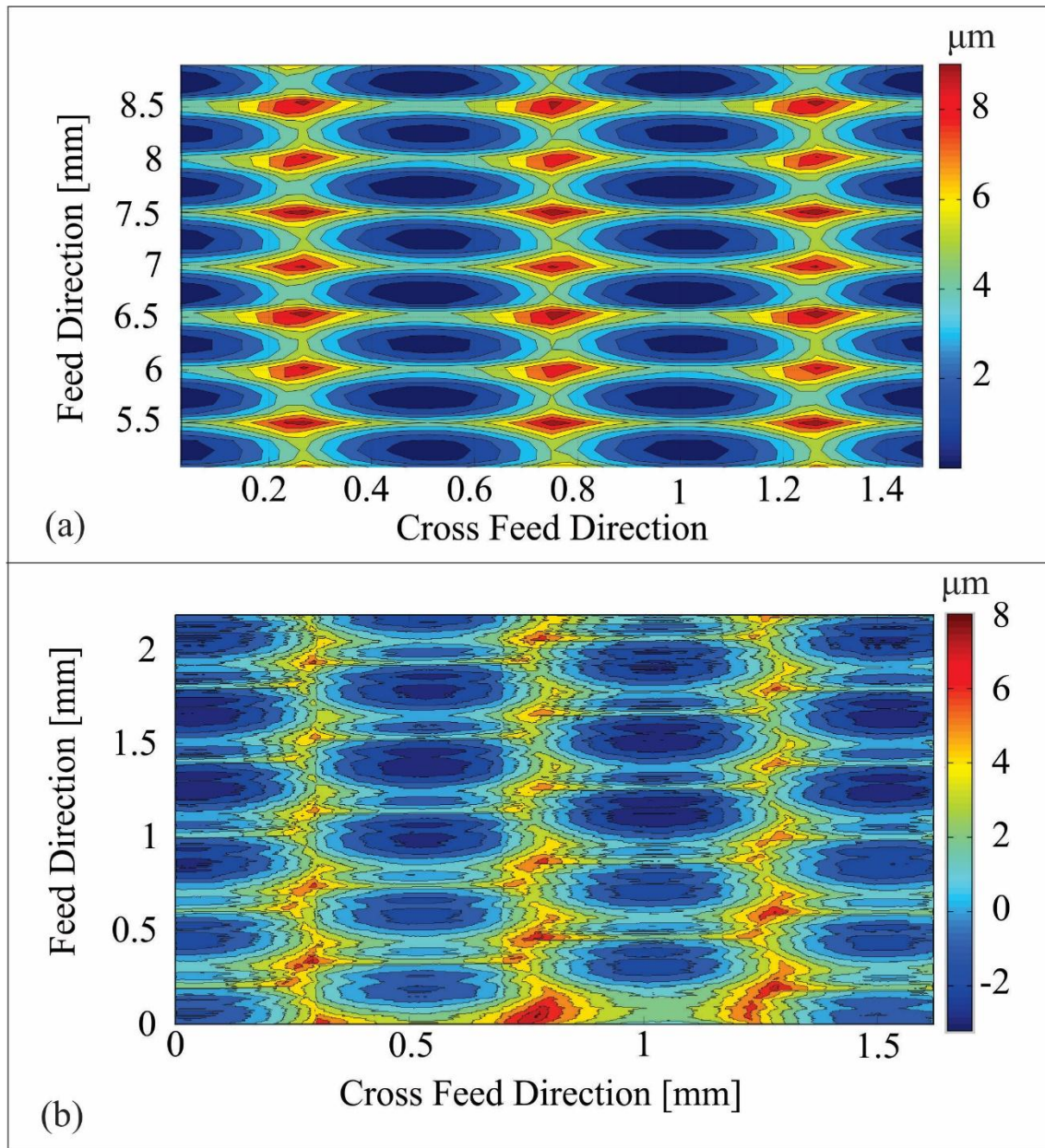


Figure 9-15 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 30° , Tilt = 0° , Feed = 1 [mm/tooth], $\rho = 0$).

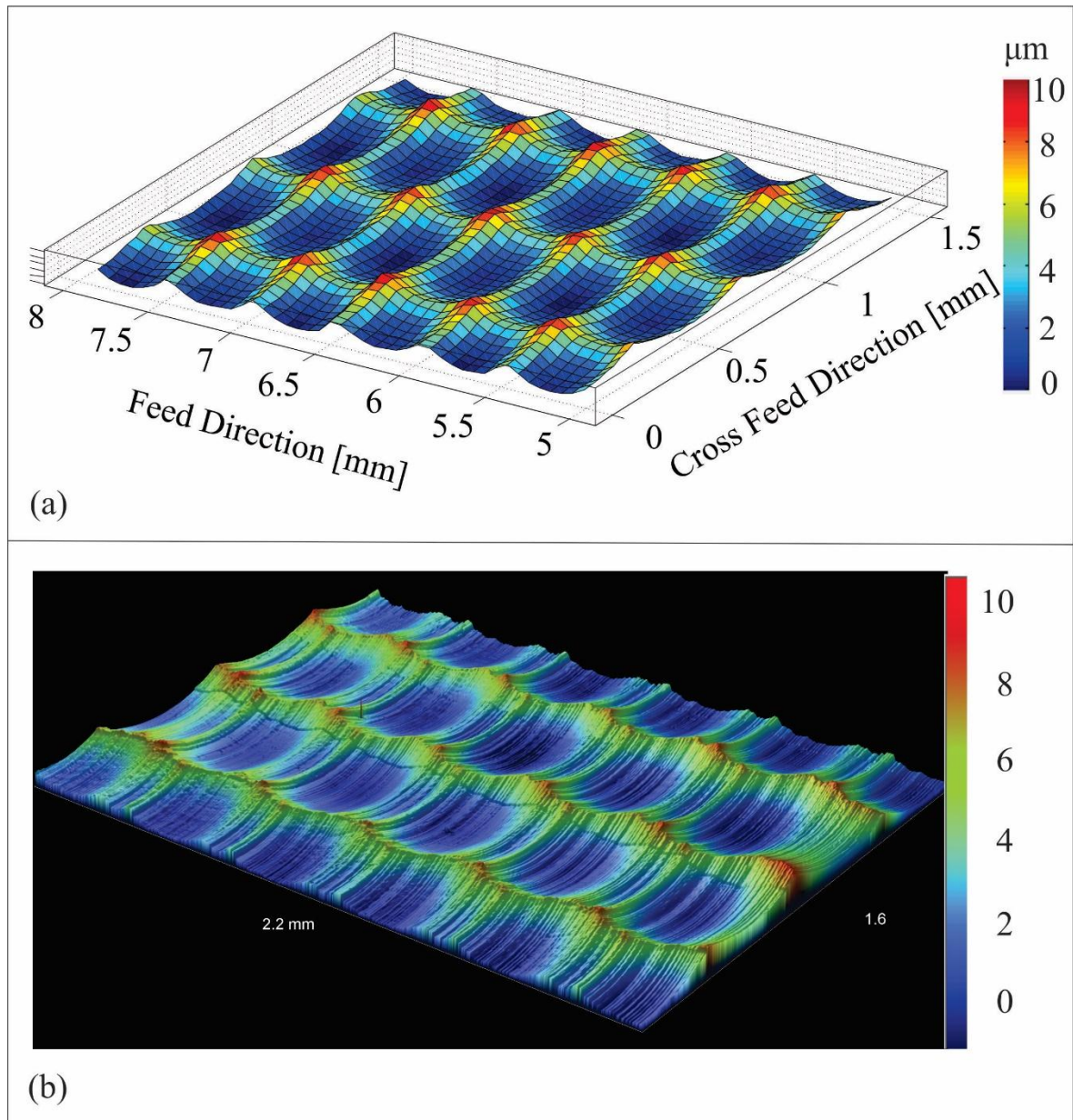


Figure 9-16 - (a) Simulated 3D surface topography (b) Measured 3D surface topography using WLI (Lead = 30° , Tilt = 0° , Feed = 1 [mm/tooth], $\rho = 0$).

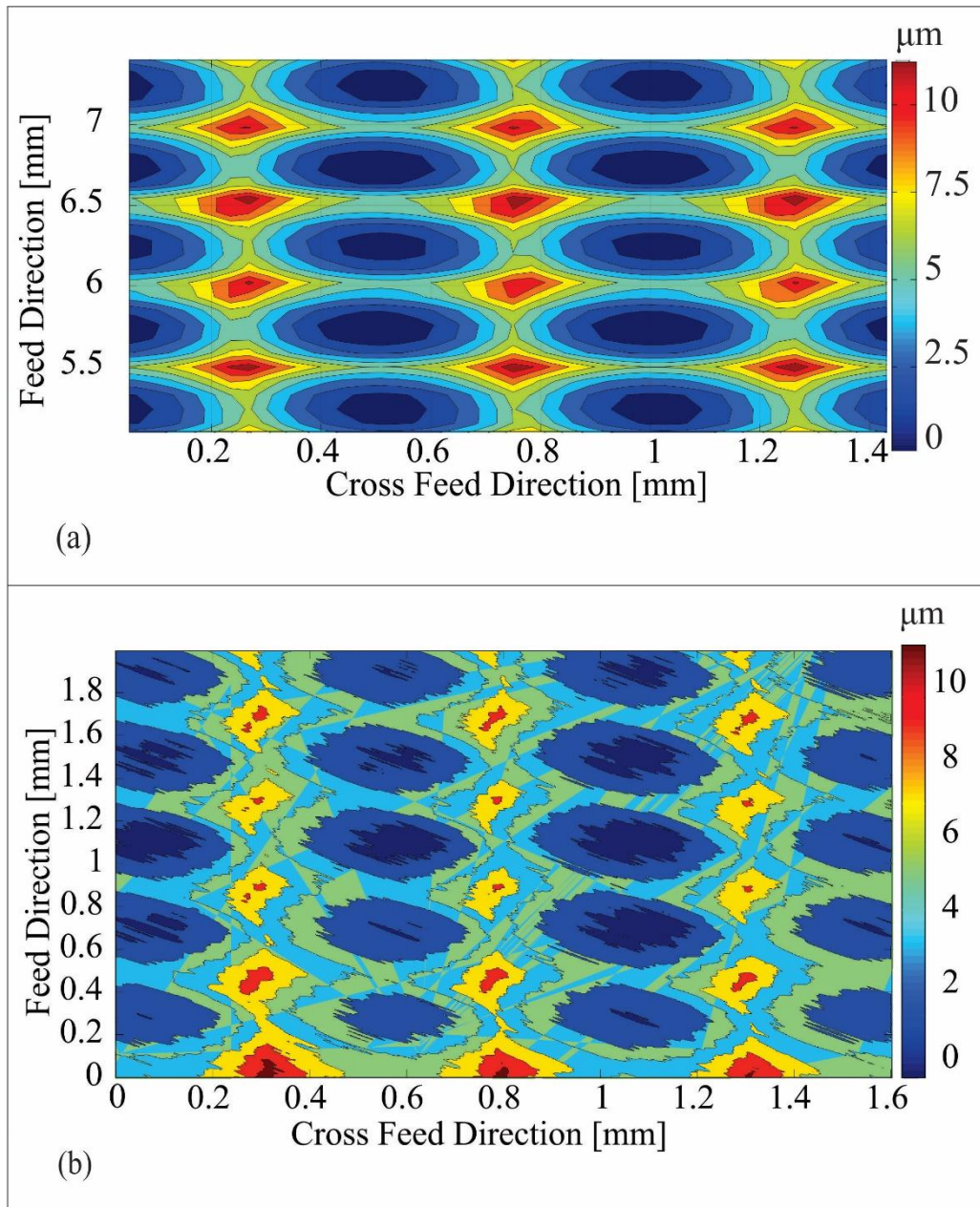


Figure 9-17 - (a) Simulated 2D surface topography (b) Measured 2D surface topography using WLI (Lead = 30° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 0$).

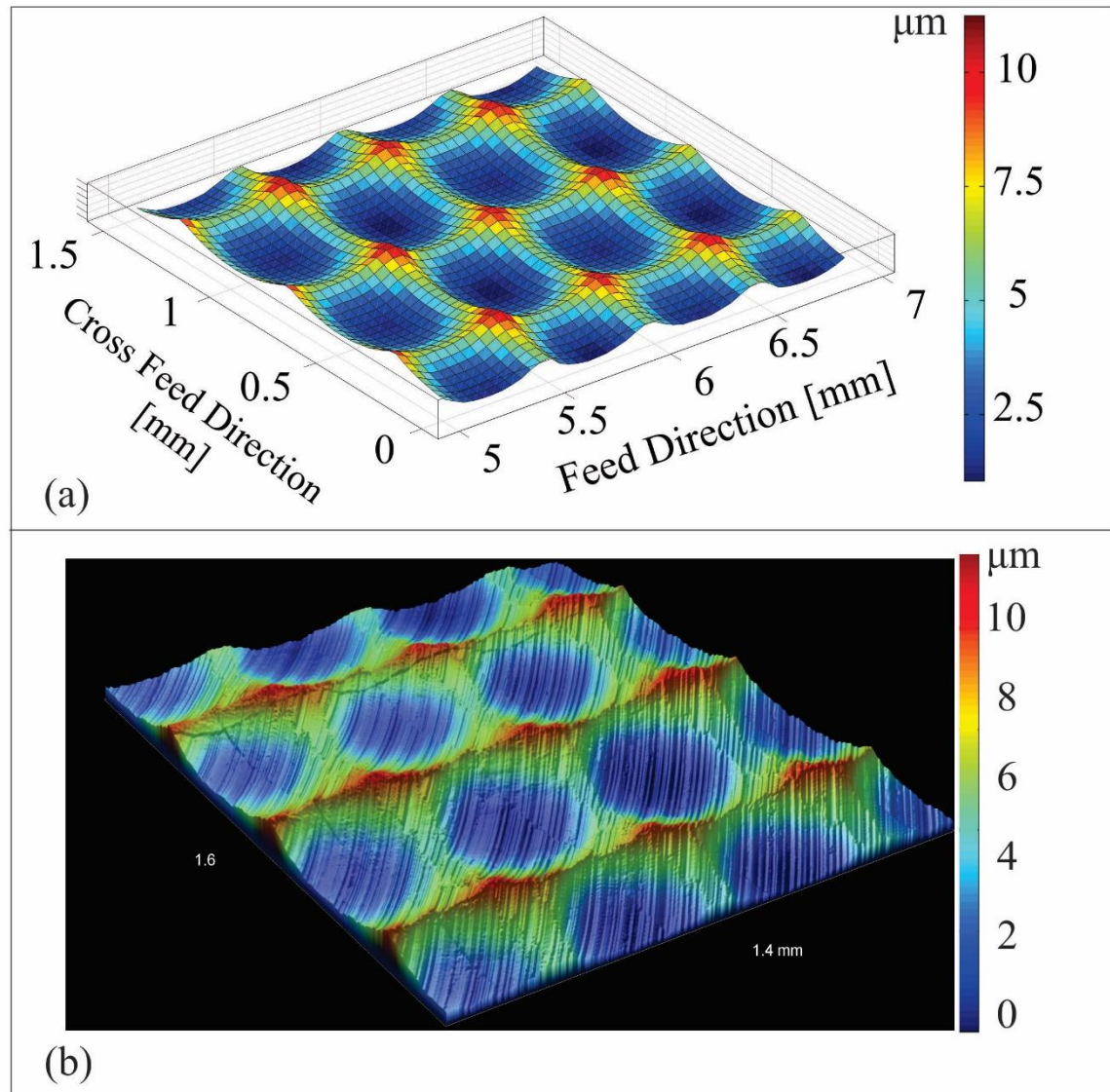


Figure 9-18 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 30° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 0$).

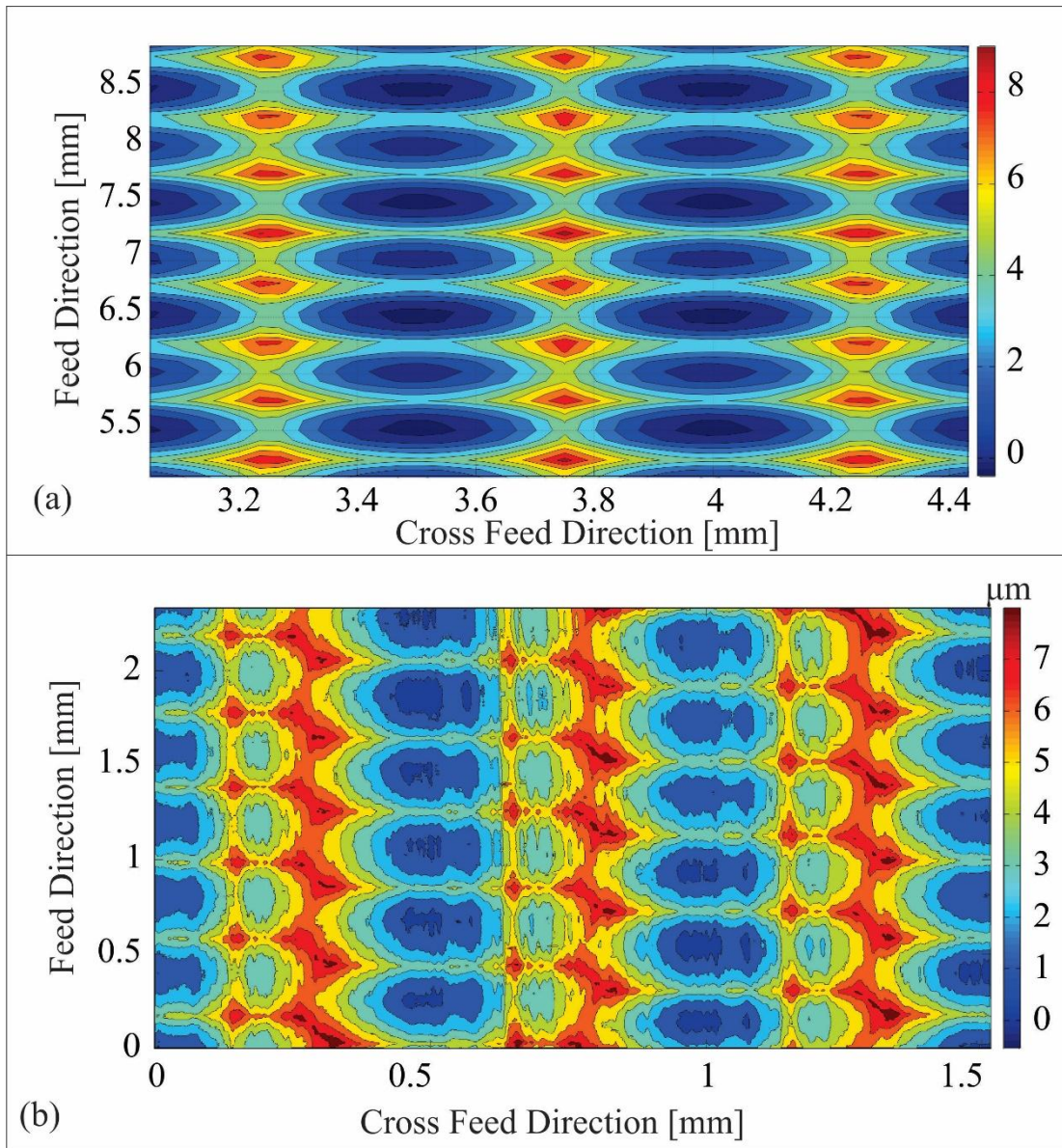


Figure 9-19 - (a) Simulated 2D surface topography (b) Measured surface topography using WLI (Lead = 0° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 40$ [μm], $\lambda = 90^\circ$).

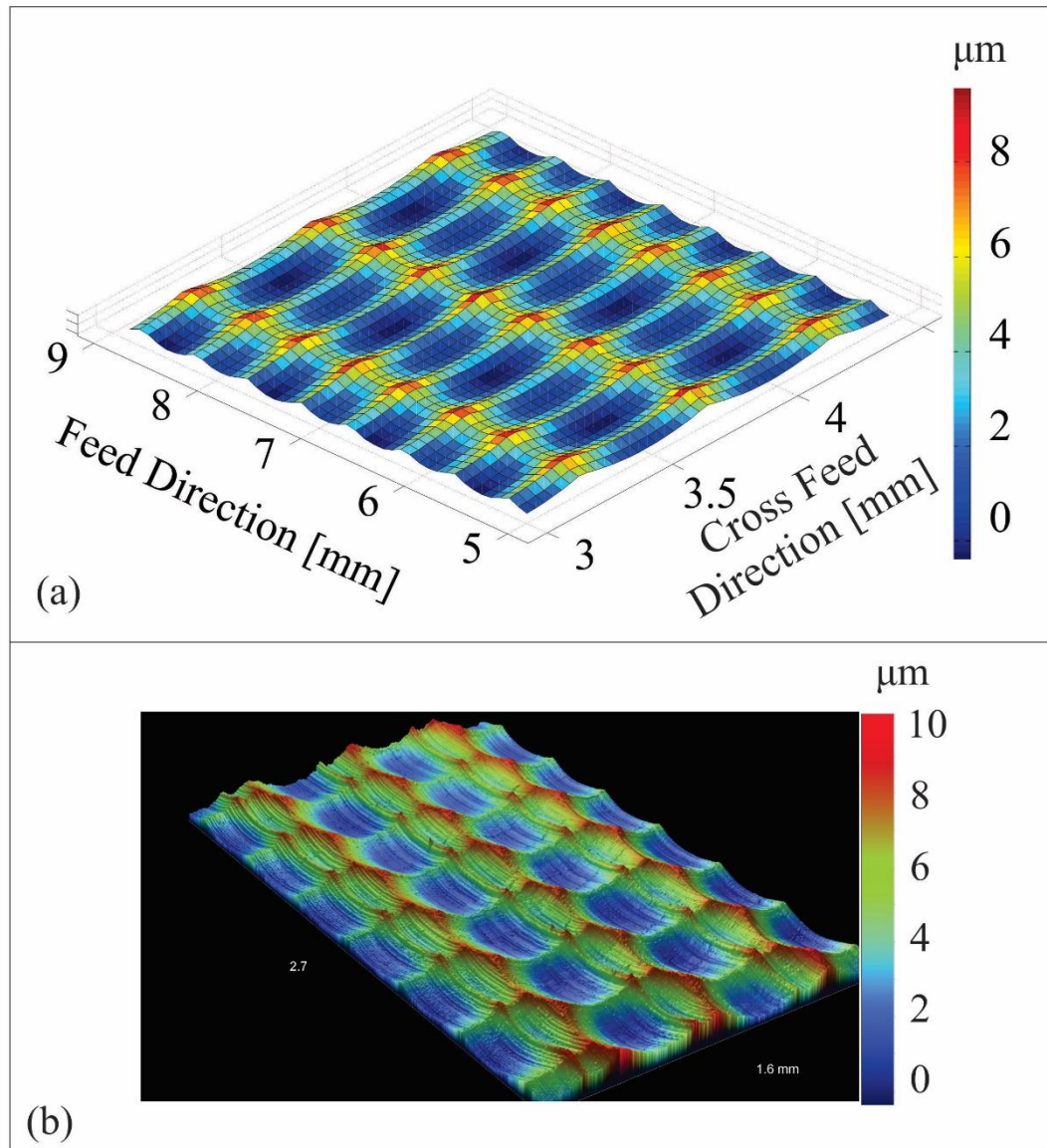


Figure 9-20 - (a) Simulated 3D surface topography (b) Measured surface topography using WLI (Lead = 0° , Tilt = 30° , Feed = 1 [mm/tooth], $\rho = 40$ [μm], $\lambda = 90^\circ$).

As it can be seen from Figure 9-9 to Figure 9-20, the proposed approach is able to simulate the surface topography in a good agreement with the experimental results. There is some inconsistency between the modelled and simulated surface topography in some

cases. This inconsistency is most likely due to the dynamics of the process and wearing effect at the cutting edge which has not been considered in the model. Also, the effects of ploughing and tool indentation has not been modelled in the surface generation model which is another source of discrepancy. However, in all the cases the surface texture and the pattern of the machined surface is modeled with a good agreement. The peaks and valleys are also predicted accurately.

The effect of runout can be observed by comparing Figure 9-14 (b) and Figure 9-20 (b). As is manifested in these two figures, in the machining case with 40 [μm] runout, the peaks in the generated surface are about 3 [μm] higher. This fact can also be observed in the simulated cases in Figure 9-14 (a) and Figure 9-20 (a). It can also be inferred that the runout changes the pattern of scallops on the machined surface.

All in all, it can be concluded that the modeled and measured surface topographies show good agreement in pattern and the height of the peaks and valleys. The accuracy of the model can be increased by considering the dynamics and wearing effect in the cutting process. This proposed technique can be used in virtual machining systems to simulate the surface topography prior to physical machining of the part.

9.5 Summary

In this chapter, a new approach for simulation of surface topography in five-axis ball-end milling operation was introduced. The equation of trochoidal trajectory of the cutting flutes as a function of instantaneous angular position of the tool were analytically derived. The cross sections of the machined surface perpendicular to the feed direction were obtained by intersecting the trochoidal trajectories and planes perpendicular to the feed direction. The texture of the machined surface was modelled by plotting the obtained cross sections side by side. Using the curve fitting toolbox of the MATLAB a 3D surface was fitted to the cross sections to simulate the machined surface.

In order to validate the proposed approach, some experimental tests were conducted for different cutting conditions. The surface topography of the specimens were measured using WLI technique. The agreement between the experimental and theoretical data was demonstrated by plotting 2D contours and 3D representation of the surface topography. Although the dynamic of the machining, ploughing and tool wear phenomena are not included in the model, the proposed approach can simulate the surface topography in the five-axis ball-end milling with a good accuracy.

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