

**Chip Load, Calibration Analysis and Dynamics of Ball-End
Milling for Free-form Surface Machining**

by

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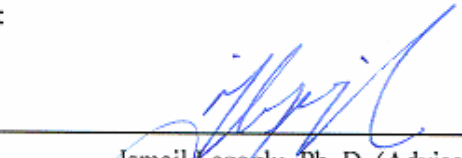
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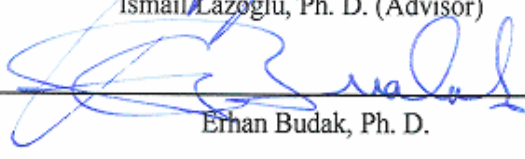
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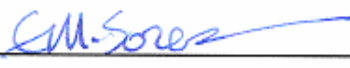
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ABSTRACT

The ball-end milling process is used extensively in machining of free-form surfaces in automobile, die/mold and aerospace industries. In planning machine operations, the process planner has to be conservative in selecting machining conditions with respect to metal removal rate in order to avoid unwanted results such as chipping, cutter breakage or overcut due to excessive cutter deflection. These problems are particularly important for machining of free-form surfaces where axial and radial depths of cuts are abruptly changing. For this reason it will be very critical to provide the process planner with guidance in selecting machining conditions. One solution to this problem is to consider the cutting force in the stage of selecting machining conditions. If the process planner can predict the cutting force for a certain machining condition, it would be very helpful in judging whether the selected condition is appropriate. In the prediction of cutting forces exact determination of engagement regions, calibration coefficients and force model play very important roles.

Firstly, cutting force models for ball-end milling are based on the undeformed chip thickness. Undeformed chip load can be constructed from the boundaries of instantaneous engagement region between the ball-end mill and workpiece. In order to predict the cutting forces accurately, determination of the varying engagement region is important. In the literature, there are two main engagement region constructing methods; one is the Z-mapping and the other one is using a solid modeler based on Boolean operation method. Both methods construct the engagement region within accuracy limits, on the other hand the computational time for these methods are long such that it is not possible to calculate the forces at the same time of CL-point construction in a CAM package. This work presents development of an analytical method for fast and accurate determination of the instantaneous engagement regions in the 3D machining of monotonic free-form surfaces. The analytical tool is grid size independent, thus it is much faster than discretization and Boolean methods. In addition to that, there is no need for smaller grid sizes to have the exact engagement domain since the equations are analytical and thus give the exact engagement regions.

Secondly, obtaining the calibration coefficients from calibration tests is a very influential process in the prediction of cutting forces. Accurately obtained calibration coefficients lead to better force predictions. In the literature the calibration coefficients are assumed to be independent of the change of the start and exit angles within the engagement region, thus they are assumed to be identical for

any engagement region. Calibration coefficients are obtained from the horizontal slot cutting tests and these tests are repeated for different depth of cuts. In this work, in order to achieve more precise force predictions in free-form machining simulations, a new modification algorithm for calibration coefficients is presented. In this research, with theoretical analysis and experimental force signals, it is shown that the inclination angle has a great importance in calibration and in force simulation of 3D free-form machining. CAD drawings are performed to validate the analytical engagement model with measured forces and extensive experiments are performed to validate both the analytical engagement region and the modification model of calibration coefficients. Theoretical Dynamic analysis of ball-end milling is also studied.

In addition to predicting the cutting forces, five-axis ball-end milling is also studied up to an informative level for the future work. Experiments are performed in five-axis ball-end milling and effect of lead/tilt angles on force signals is stated. Dynamics of ball-end milling is coupled with engagement model and an analytical method is presented which needs to be developed as a future work.

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NOMENCLATURE

a	axial depth of cut
r	tool radius in x-y plane at a point defined by ψ
c_{kn}, t_x	feed per tooth
h_{kn}	instantaneous chip thickness
i_0	helix angle at ball shaped flute and shank meeting point
β_f	lag angle between the tangent line at the tip of the flute and axial location z
θ	cutting element rotation angle
$\Delta\theta$	discrete cutting element rotation angle
ψ	cutting element position angle
R	radius of cutter
N_f	number of flutes
ω	spindle speed in revolution per minute
F_r, F_ψ, F_t	cutting forces in tangential, radial and axial directions
dF_x, dF_y, dF_z	differential cutting forces in x, y, and z directions
dF_b, dF_ψ, dF_r	differential cutting forces
$K_{rc}, K_{\psi c}, K_{tc}$	tangential, radial and axial cutting force coefficients in milling
$K_{re}, K_{\psi e}, K_{te}$	tangential, radial and axial edge force coefficients in milling
$(dz)_{kn}$	differential height along the longitudinal cutter axis at the k^{th} discrete point on the n^{th} flute
Ω	rotation angle of flute
ϕ_p	cutter pitch angle
n	flute number
β_k	lag angle between the k^{th} discrete point on cutting edge and the cutter tip
dA_c	differential chip load
f	data sampling frequency
D	diameter of cutter
l_2	Total length of the tool
a_p	total length of the flute

N_d	number of discs in contact
x_w, y_w, z_w	workpiece coordinates
x_0, y_0, z_0	CL-points
x_c, y_c, z_c	cutter coordinates
$\hat{x}, \hat{y}, \hat{z}$	rotationary cylinder coordinates
u, v	parameters of sphere equation
c	peek feed
h	vertical distance between the tip of the cutter and surface of the workpiece
α	start angle of Boundary – 1
β	finish angle of Boundary – 1
τ	start angle of Boundary – 2
ϕ	finish angle of Boundary – 2
e	offset from drill center
R_1	radius of engagement region at z_{surface} level
$y_{\text{contact}}, z_{\text{contact}}$	coordinates of the first contact point of workpiece with the ball end mill
A_i	instantaneous rotation matrix
λ	inclination angle
x_{eci}	x-coordinate of the intersection of Boundary – 1 and Boundary – 2
R_2	Radius of engagement region at z_{contact} level
m^i	slope of the line connecting two consecutive CL points.
$\bar{y}^i, \bar{z}^i$	coordinates of the point approximation to be on the surface
$\bar{F}_x, \bar{F}_y, \bar{F}_z$	average end mill forces
$K_{rc}^{new}, K_{tc}^{new}, K_{\psi c}^{new}$	modified cutting coefficients
$K_{rc}^{old}, K_{tc}^{old}, K_{\psi c}^{old}$	old cutting coefficients
$K_{re}^{new}, K_{te}^{new}, K_{\psi e}^{new}$	modified edge coefficients
$K_{re}^{old}, K_{te}^{old}, K_{ec}^{old}$	old cutting coefficients

F	total number of discs
$K_{rc}^c, K_{tc}^c, K_{\psi c}^c$	cutting correction factors
$K_{re}^c, K_{te}^c, K_{\psi e}^c$	edge correction factors
$\bar{F}_x^{new}, \bar{F}_y^{new}, \bar{F}_z^{new}$	inclined slot cutting force readings
$\bar{F}_x^{old}, \bar{F}_y^{old}, \bar{F}_z^{old}$	horizontal slot cutting force readings
h_j	dynamic chip thickness
T	tooth period
$\Delta x, \Delta y$	vibration amplitudes
A_0	time-invariant but immersion-dependent directional cutting coefficient matrix
$\Phi(i\omega)$	transfer function matrix identified at the cutter-workpiece engagement zone
$\Phi_0(i\omega)$	oriented transfer function matrix
κ	lead angle
η	tilt angle

Chapter 1: INTRODUCTION

There are lots of researches performed and being performed about the precision and optimum manufacturing of parts. Although scheduling of parts and economics play an important role in cost effective manufacturing, this study focuses only on the physical principles of machining, mainly on a certain type of metal cutting operations.

Metal cutting is one of the most widely used manufacturing processes to produce final shape of products, and its technology continues to advance in parallel with developments in materials, computers and sensors. A blank workpiece is converted into a final product by cutting excess material away by turning, drilling, milling, broaching, boring and grinding operations conducted on Computer Numerically Controlled (CNC) centers.

Metal cutting operations take place between a cutting tool and workpiece material mounted on a machine. The motion of the machine tool is controlled by its CNC unit, and the Numerically Controlled (NC or G-codes) commands to CNC are generated by the help of computer-aided design/computer-aided manufacturing (CAD/CAM) systems. The productivity and accuracy of the metal removal operation depend on the preparation of NC programs, planning of machining process parameters and cutting conditions, cutter geometry, work and tool materials, machine tool rigidity, and performance of the CNC center.

The milling operation, which is the subject under investigation in this study, is an intermittent cutting process, commonly using a multi-flute cutter. A milling cutter is clamped in a rotating spindle, while the workpiece mounted on the table is linearly moved toward the cutter. Each milling tooth therefore traces a trochoidal path, producing varying but periodic chip thickness at each tooth passing interval.

The ball-end milling process is used extensively in the machining of free-form surfaces in die/mold and aerospace industries. However, since its cutting geometry is often changing, it is somehow difficult to predict the forces which are going to be used to determine the optimum cutting

conditions. The main difficulties are chip load calculation and calibration coefficients. These problems are particularly important for the machining of free-form surfaces where axial and radial depths of cut are changing. One solution to this problem is to determine the engagement regions analytically and analyze the effects of the region to the force model. Besides, knowledge on the cutting forces helps to design better fixtures, contributing to an increase in surface qualities of parts.

In order to produce the parts with free-form surfaces in an optimum manner in terms of production cycle time, cost and product quality, the machining process of free-form surfaces needs to be simulated faster and more accurately in advance. For the mechanical and dynamical simulations of the free-form process, perhaps the most important parameters are the fast and precise determination of the instantaneous chip loads along the tool path and calibration coefficients.

Chapter 2 provides necessary background and literature review on related analyses. The fundamentals of metal cutting analysis, cutting mechanics and previous models for prediction of cutting forces in ball-end milling are reviewed.

Chapter 3 describes the force model used in this study and states the mechanistic force model.

Chapter 4 describes analytical engagement region determination algorithm; the mechanism underlying the 3D visualization of the system is explained.

Chapter 5 describes the mechanistic calibration which is to obtain the calibration coefficients that are going to be used in the force model.

Modification algorithm for calibration coefficients is given in Chapter 6, where the methodology that is employed to determine the cutting constants for the force model is described.

Validation tests under various conditions are described and their results given in Chapter 7, together with the discussion of these results.

The thesis is concluded with some future work and a short summary of the performed study.

Chapter 2: LITERATURE REVIEW

2.1 Overview

Milling is used extensively in the manufacturing industry where both precision and efficiency are critical. In milling, the component shape, size and its surface finish are generated by systematically removing the excess material from the original workpiece by a cutting tool. The milling cutter has a number of cutting edges. The workpiece is gradually fed to the rotating cutter during milling. Chips are removed by each cutting edge during revolution and a surface is produced. Milling is one of the most important manufacturing processes because it can produce a wide variety of component shapes and sizes with high dimensional accuracy and good surface finish. Besides milling can be computer controlled and automated through use of CNC machine tools. In many of these operations the cutting force is an important parameter either with respect to the deflection (dimensional tolerance), breakage of the cutter or the stability of the process. Moreover, knowledge of the magnitude and variation of the cutting force with respect to the cutting conditions is essential for the process planning of milling operations.

The mechanics of milling can be best understood by examining the chip formation during the process. An early detailed kinematic analysis by Martelotti [1] showed that, due to the combined rotational and linear feeding motions of the cutter towards the workpiece, the true path of a cutter tooth is an arc of a trochoid, rather than a circle, which complicates the mathematics in the analysis. However, Martelotti claimed that, in most practical cutting conditions where the cutter radius is much larger than the feed per tooth, the circular tool path assumption is valid and the error introduced is insignificant. As the cutter rotates, the immersion angle (θ) varies, hence the chip thickness changes sinusoidally and given by Martelotti as:

$$h_{kn} = c_{kn} \cdot \sin(\theta) \quad (2.1)$$

where h_{kn} is the instantaneous chip load, c_{kn} is the feed per tooth and θ is the immersion angle of cutting point.

2.2 Milling Force Models

An early analysis of milling forces produced by a helical flute was presented by Koenigsberger et al. [2]. They related the elemental chip loading to rigid cutting forces through experimentally calibrated milling force coefficients K_t and K_r , in tangential and radial directions, respectively. In general, these coefficients are identified through empirical curve fitting technique and provide little physical insight in terms of the nature of the process. The cutting forces were expressed as a function of cutting pressure exerted on the instantaneous chip area:

$$\begin{aligned} F_t &= K_t \cdot a \cdot t_x \cdot \sin(\theta) \\ F_r &= K_r \cdot F_t \end{aligned} \quad (2.2)$$

where a is the axial depth of cut, t_x is feed per tooth per revolution, θ is immersion angle, F_t and F_r are the cutting forces in tangential and radial directions respectively.

Other researchers employed the same model with improvements such as including the effect of runout and tool deflections [3, 4]. Tlustý and MacNeil [5], Armarego and Budak et al. [6] further improved the accuracy in predicting the milling force coefficients for cylindrical helical end mills by introducing an edge force component to their model.

While plenty of work has been done related to the mathematical models of flat end milling processes, those of ball-end milling processes were developed only recently due to the complexity of the geometry. The mechanistic approach had limitations on milling with complex cutters such as ball-end mills which have variable geometry in the axial direction. As a result, Lim and Feng [7, 8] approximated the cutting force coefficients with a third degree polynomial which depends on the axial depth of cut. They then estimated the coefficients by iteratively curve fitting using average runout values on the cutters. Similarly, Yucesan and Altintas [9] presented a semi-mechanistic model which predicts the shear and friction load distribution on the rake and flank faces of the helical ball-

end mill flutes. They used a complex mathematical model and identified the milling force coefficients through an empirical curve fit to measure average milling forces.

With the mechanistic model, prior evaluation of the milling force coefficients is necessary for any analysis and prediction. Milling calibration tests are required to be run at different feedrates, cutting speeds, and constant cutting geometry, and the results are applicable to a particular workpiece material-cutter pair only. However, the model allows a quick calibration of existing cutters and may turn out to be a practical and useful approach even in the case of complex cutters such as the ball-end mill if an appropriate methodology is applied.

As an alternative to the mechanistic calibration approach, mechanics of milling has been studied to make use of the orthogonal cutting parameters for cutters with well defined geometry (without grooves, chip breakers, etc.), thus eliminating the need for new calibration tests each time the cutter geometry changes. Armarego [10, 11] was among a few researchers to apply the orthogonal to oblique transformations on milling force predictions. The basic idea is to represent the milling geometry by segments of oblique cutting processes. By measuring the forces in turning, Budak, Altintas and Armarego [11] identified the traditional material properties such as shear flow stress and friction angle from a set of standard orthogonal turning tests. They showed an accurate method of transformation between the orthogonal cutting parameters (i.e. shear stress, shear and friction angles) and local milling force coefficients, taking into consideration the cutter geometry such as helix angle and rake angle. Yang and Park [12] took a similar approach and applied the orthogonal theory on ball-end mills. They obtained the fundamental cutting parameters from orthogonal turning tests and simplified the analysis by approximating the oblique cutting on each ball-end flute by infinitesimal orthogonal processes. Later they considered the variation in the chip loads due to static tool deflections. Tai and Fuh [13] used a similar cutting model but they represented the cutting edge geometry as intersections between skew planes and spherical ball-end mill surface. Lazoglu and Liang [14] established a closed-form model for ball-end milling cutting force system in the frequency domain using an angular convolution technique, and analytical time domain predictions were obtained via inverse Fourier transformation of the cutting force frequency spectrums. Lamikiz et al. [15] performed a similar approach to Lazoglu [14] and he assumed that the calibration coefficients are polynomial of the z-coordinate of the disc elements. They state that assuming friction coefficients as a constant is a valid assumption and assumed edge coefficients constant throughout the machining

process. They state that obtained results were only valid for a specific testing case under a given set of conditions and only for the case of horizontal slot cutting, they do not include any explanation of the effect of inclination angle on the calibration coefficients.

A cutting system is often subjected to some type of external dynamic forces. This forcing function can be time-dependent, harmonic, periodic, or random in nature. In dynamic milling, under some conditions, force induced vibrations may be inherent into the process, at the tooth passing frequency. For other conditions, the vibration may cause the cutting process to vary so that it supplies a positive input of energy to maintain the vibration, which is known as self induced or self-excited vibration in machining. Chatter is a self-excited type of vibration which occurs in metal cutting due to the lack of stiffness in the process, or can be best explained by a phenomenon called ‘regeneration of waviness’. ‘Regeneration of waviness’ is a mechanism by which the input force is modulated by system dynamics so as to produce force variations and vibration. Chatter vibrations occur around the natural frequency of the most flexible part of the machine cutter/workpiece structure which is closest to the cutting point.

Tobias [16] and Tlustý [17] were among the few early researchers to study dynamic cutting. They explained the fundamental mechanism of chatter during turning and derived the stability theory which analytically predicts chatter free cutting conditions. Their theory was based on the orthogonal cutting model where the directions of cutting forces and the structural dynamics of the cutter/workpiece system were assumed constant. Chatter in milling, however, is different from turning in which the cutter rotates with respect to the structure/workpiece system. Sridhar [18] presented a milling stability model which requires numerical solution of the milling equation. Later Minis [19] proposed an improved method of solving the stability equation analytically by iteration. Budak et al. [20] have solved the chatter stability in milling for helical end mills by representing the time varying milling force coefficients in Fourier series, hence they were able to solve the stability equation analytically and more efficiently, taking into account the change in the milling force directions. Altintas and Lee [21] applied their theory to ball-end mills as well. Abrari and Elbestawi [22] also made a dynamic force model and a stability analysis for ball-end milling. They used the method of regenerative chip load calculation which accounts for the effects of both the surface undulations and the instantaneous deflection.

Recently, following the substantial improvement in computational power which is inexpensive to obtain and operate, numerical approaches are getting even more appealing for the researcher. Z-mapping is such an approach used by various authors including Kim et al. [23] and Lazoglu [14]. Lazoglu employed a Boolean approach in the model and in addition to predicting the cutting forces, the model determined the resulting surface topography and scallop height variations along the workpiece for given cutter/workpiece geometries and CL file. Kim estimated the cutter deflection and form error in a recent work [24], building upon his force prediction model. Ikua et al. [25, 26] constructed a model for prediction of cutting forces and machining error in ball-end milling of curved surfaces, investigating various cutting conditions. Jung et al. [27] suggested the exact chip engagement (ECES) method in order to calculate the chip thickness distribution along the cutting edge when the cutting velocity is not significantly large compared with the cutter feed. Shatla and Altan [28] developed a force model both for drilling and ball-end milling which also predicted temperatures on the flank side of the cutter. Bouzakis et al. [29] developed a milling simulation algorithm predicting the surface topography, cutting forces and the surface roughness.

In order to produce the parts with free-form surfaces in an optimum manner in terms of production cycle time, cost and product quality, the machining process of free-form surfaces needs to be simulated faster and more accurately in advance. For the mechanical and dynamical simulations of the free-form process, perhaps the most important parameter is the fast and precise determination of the instantaneous chip loads along the tool path. Most of the models mentioned here are applicable to either machining of simple and limited workpiece geometries, such as 2½ axis milling cases, or can not detect the cutter/workpiece engagement regions automatically and can not simulate multi-pass machining operations.

Some researchers used Z-mapping technique [30-32, 35], octree method [33] for determining engagement regions between tool and workpiece along the tool path. Some other researchers used solid-modeling based approaches [34]. Terai et al. [36] used a geometrical analysis of chip load by stating previously machined surface as a cylinder.

Even the more comprehensive, recent models do not consider the ball-end mill moving in a completely ‘free’ fashion in order to create free-form surfaces. The idea of defining a practical mechanistic methodology that is widely applicable to all forms of machining with ball-end mills through utilization of previously proposed methods has been the motivation in this study. An

analytical approach to the cutter/workpiece engagement region is developed for 3D free-form monotonic surfaces with ball-end mill. The presented method is analytical; therefore, there is no need for discretization method and or Boolean approach. The analytical approach developed in this work is much faster and quite accurate compared with the numerical approaches used in the literature up to now.

Chapter 3: CUTTING FORCE MODEL

3.1 Introduction

A large variety of milling tools are available and used in die and mold manufacturing. Each cutting edge on the tool is carefully designed to ensure maximum cutting performance while minimizing the resulting cutting forces. A detailed analysis of the cutting edge geometry is also necessary for the development of the force model. In order to form the model, all of the acting components in cutting must be represented mathematically first, and thereafter, the relationships between these components in the real process must be defined building upon these representations.

In this chapter, the mechanistic force model is explained in detail. The strength and weaknesses of the model are discussed.

3.2 Tool Geometry

The important characteristics of milling tools are the macro geometry (cylindrical, ball end, bull-nose, tapered ball-end mills, etc.), the micro geometry (helix angle, rake angle, relief angle, ball radius, etc) and the cutting material (e.g. carbide, HSS and PCBN), and coating material (e.g. TiN, TiALN).

In force model macro and micro geometries play very important role. The calibration coefficients and force model are based on these geometries and material properties of the workpiece.

Figure 3.1 shows the common nomenclature for a ball-end mill with N teeth. Each flute is inclined at a constant helix angle and the immersion angle, given in Figure 3.2, for flute n changes along the axial direction.

As opposed to the straight-end mill, ball-end mill cutting edge geometry varies locally in the ball end part. The varying radius r directly affects the cutting forces through its effect on the cutting

velocity. With end mills, however, the material is always cutting at the periphery of the cutter at a constant speed.

The detailed geometry of a helical ball-end mill cutter is shown in Figure 3.1. Each flute lies on the surface of the hemisphere, and is grounded with a constant helix lead. The flutes have a helix angle of i_0 at the ball-shank meeting boundary. Due to the reduction of radius at x - y planes towards the ball tip in axial z direction, the local helix angle for a discrete point along the cutting flute varies for constant helix-lead cutters.

The expression for the shape of the ball part is given by,

$$x^2 + y^2 + (z - R)^2 = R^2 \quad (3.1)$$

where R is the ball radius of the cutter measured from the center of the sphere part. The cutter radius r

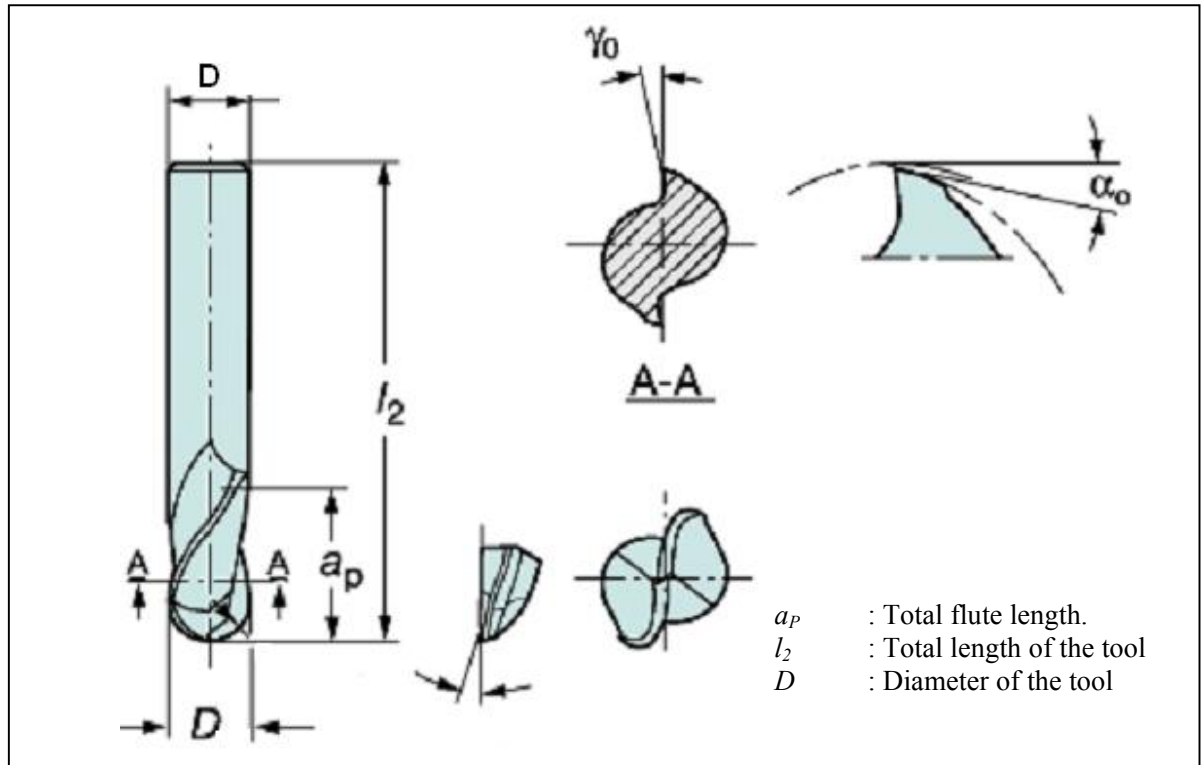


Figure 3.1: Macro geometries of a ball-end mill from Sandvik website

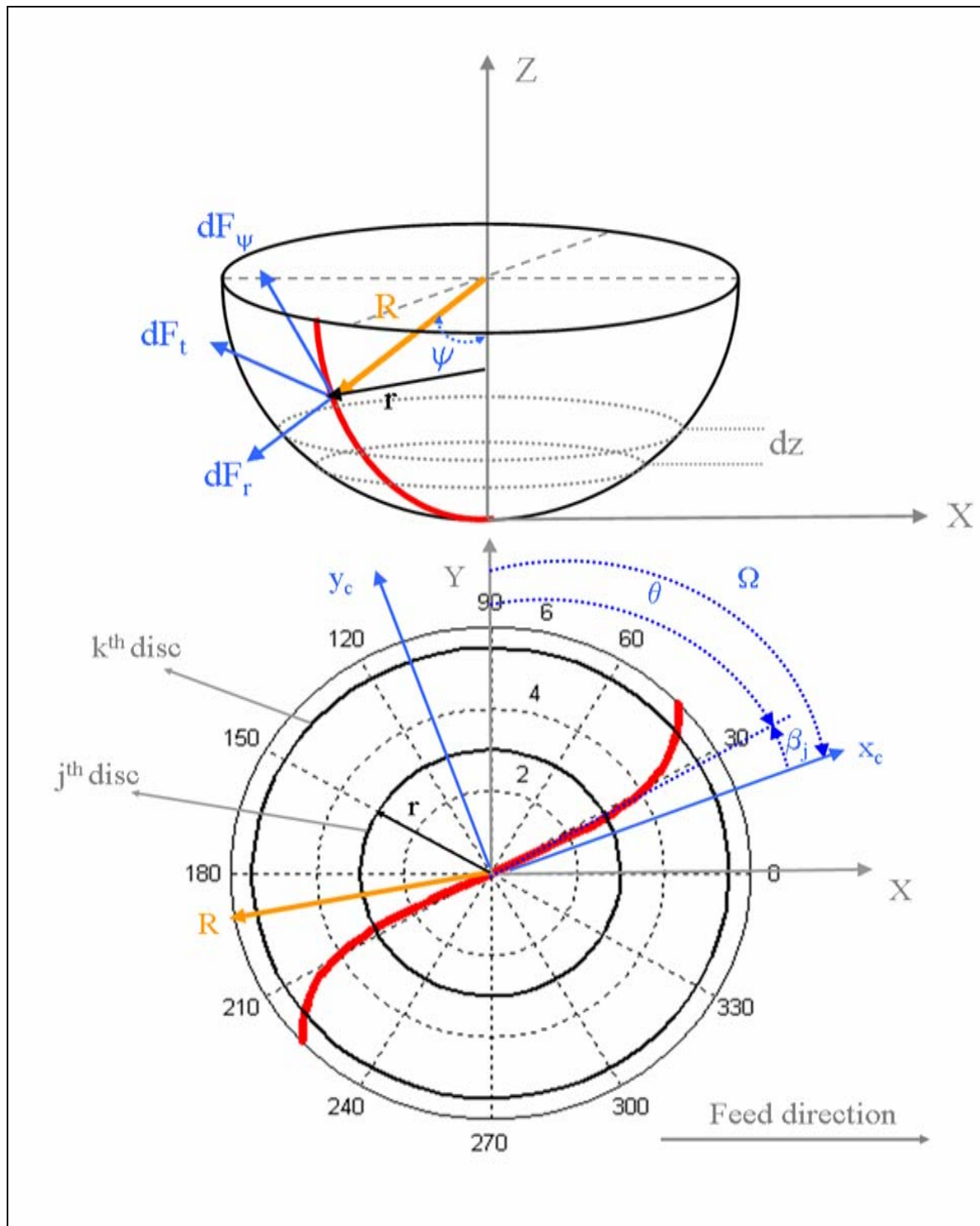


Figure 3.2: Illustration of the cutting force components and angular relationships

in x - y plane at axial location z is,

$$r^2 = x^2 + y^2 \quad (3.2)$$

and is zero at the ball tip. The z coordinate of a point located on the cutting edge can be found from Equations (3.1) and (3.2).

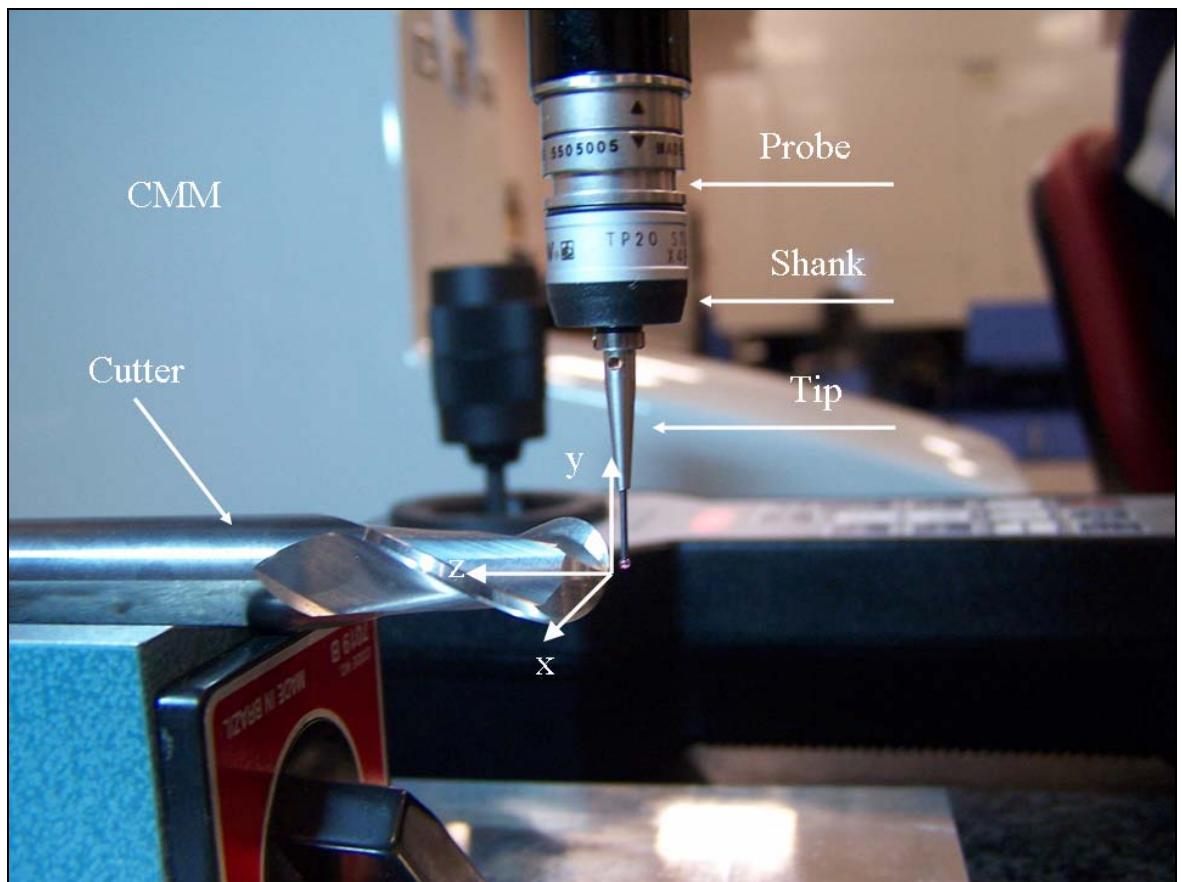


Figure 3.3: Cutting edge measurement setup.

There are two ways to represent cutting edge geometries of a cutter in the model:

-An explicit mathematical representation of the cutting edge supplied by the cutter manufacturer may exist: In this case, cutting edge formulation can be easily transformed into three orthogonal

cutter axes, namely x_c - y_c - z_c . The x_c axis is tangent to the cutting edge at the tip of the cutter, z_c is the axis along the cutter longitudinal axis (starting from the tip), y_c is the third orthogonal axis.

-Representation of the cutting edge via polynomial fitting to CMM data set: For the representation of the cutting edge geometry, the cutting edge coordinates can be measured using a coordinate measurement machine (CMM). Alternatively, the measurements can be made from a sufficiently magnified picture of the cutting edge. After collection of the data points on the cutting edges, polynomial fitting can be performed on this data. In the model, since the explicit mathematical representation of the cutting edge was not supplied by the cutter manufacturer, a polynomial fitting to a measured set of points has been performed. For this purpose DEA 700x500x500 Coordinate Measuring Machine is used to take point cloud of the cutter (with 12 mm diameter, two-flute carbide ball-end mill, CoroMill Plura series from Sandvik). The setup and cutter can be seen in Figure 3.3. The relative locations of these points have been used to obtain a third degree fitted polynomial that formed the cutting edge geometry for the mathematical model (Figure 3.4). The following equation has been obtained for β_k :

$$\beta_k = 0.0036r^3 - 0.0205r^2 + 0.0547r - 0.041 \quad (3.3)$$

where r is the radius (in [mm]) of a point on the cutting edge on xy plane and β_k is the angle (in [radian]) between the line which connects this point to the tip and x_c axis (Figure 3.5).

The cutting edge geometry can be represented in the cutter coordinate system as the following;

$$x_c = r \cdot \cos(\beta_k), \quad y_c = r \cdot \sin(\beta_k), \quad z_c = R - \sqrt{R^2 - r^2} \quad (3.4)$$

Therefore, by varying r value from the tip to cutter ball radius ($r \in [0 \quad R]$) the full cutting edge profile can be determined from these equations.

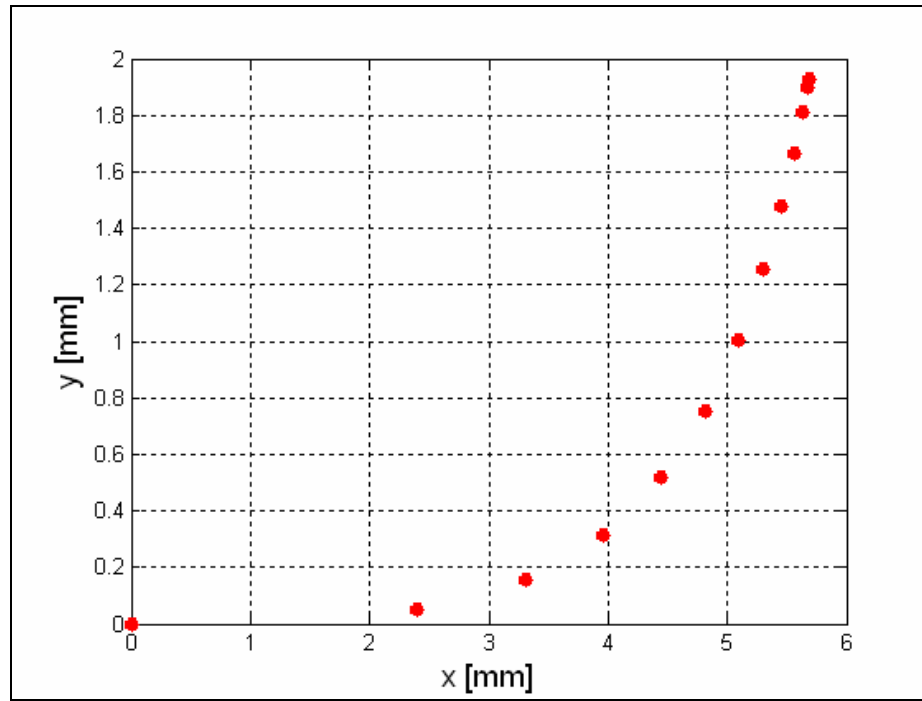


Figure 3.4: Measured data points for the cutting edge (x-y view).

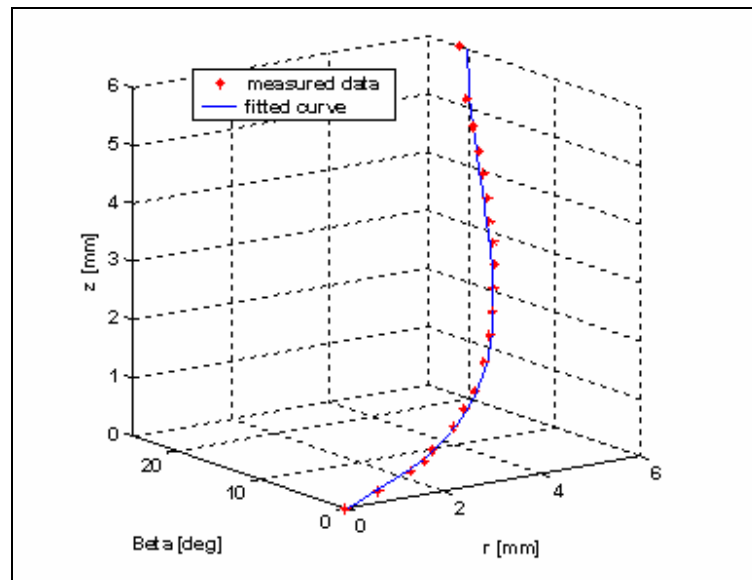


Figure 3.5: Third degree polynomial fit obtained for $\beta(r)$.

3.3 Chip Load Model

In order to determine the differential cutting forces at any cutter point in the engagement domain, the first step to be taken is to find the infinitesimal chip load for such a discrete element. Thereafter, for the chip load to be evaluated, chip thickness must be known.

As previously mentioned, the chip thickness formulation proposed by Martelotti (Equation (2.1)) has been utilized. The chip volume generated in a ball-end milling slot cutting operation is displayed in Figure 3.6. The Martelotti's formulation for straight end mills has been modified, since the chip thickness varies along the cutting edge as the depth of cut changes.

The instantaneous undeformed chip thickness for ball-end mill cutter is found as follows,

$$(h)_{kn} = c_{kn} \cdot \sin(\theta) \cdot \sin(\psi) \cdot \varepsilon_{kn} \quad (3.5)$$

where h_x [mm/rev-tooth] is the feed-per-tooth-per-revolution, and it is determined from feedrate [mm/min] / $N\omega$ [rpm]. Here, N and ω represent the number of flutes and spindle speed, respectively. θ is the cutting element rotation angle, and ψ is the cutting element zenith angle (Fig. 3.2) and where

$$\varepsilon_{kn} = \begin{cases} 1 & \text{if the } k^{th} \text{ discrete point on the } n^{th} \text{ cutting edge is in engagement with the workpiece.} \\ 0 & \text{otherwise} \end{cases}$$

Therefore, instantaneous infinitesimal chip load for each discrete element which is in contact with the workpiece can be written as;

$$dA_c = (h)_{kn} \cdot (dz)_{kn} \quad (3.6)$$

where $(dz)_{kn}$ represents differential chip height along the longitudinal cutter axis at the k^{th} discrete point on the n^{th} cutting edge. See Figure 3.7 for an illustration of the 3D view of ball-end mill.

3.4 Cutting Force Model

For a differential chip load (dA_c) in engagement domain, the differential radial (dF_r), zenith (dF_ψ) and tangential (dF_t) cutting forces can be written as follows [35],

$$\begin{aligned} dF_r &= K_{rc} \cdot dA_c + K_{re} \cdot dz \\ dF_\psi &= K_{\psi c} \cdot dA_c + K_{\psi e} \cdot dz \\ dF_t &= K_{tc} \cdot dA_c + K_{te} \cdot dz \end{aligned} \quad (3.7)$$

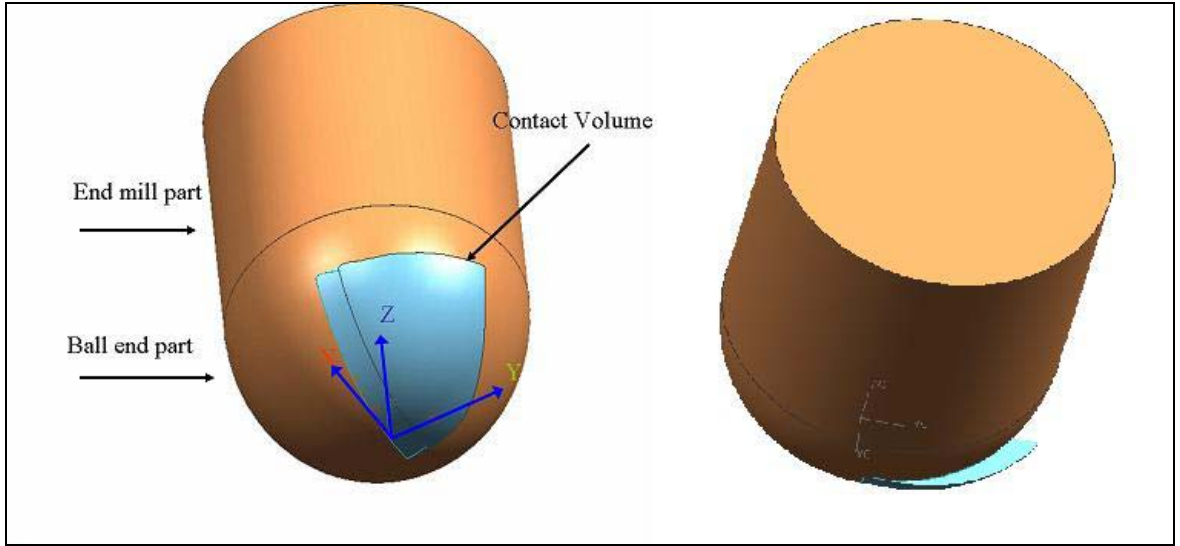


Figure 3.6: Ball-end mill geometry and chip formation

where K_{rc} , $K_{\psi c}$ and K_{tc} are the radial, zenith and tangential cutting constants, and K_{re} , $K_{\psi e}$ and K_{te} are the related edge coefficients respectively. These specific cutting energy coefficients vary based on the

cutter/workpiece material combination. In order to evaluate these parameters, calibration tests were performed with ball-end mill cutter and an Al7039 workpiece material which also has been used in the model validation tests. The 6 mm radius cutter has been divided into 7 separate disks along the cutter axis in the ball section and cutting constants were evaluated for each region individually by performing incremental slot cutting tests with different feedrates at the calibration phase (See Chapter 5 for a detailed description of the calibration process).

Once differential radial, zenith and tangential components, dF_r , dF_ψ and dF_t respectively were obtained from Equation 3.7, these cutting force components can be easily transformed into the X-Y-Z global coordinate system by using Equation (3.8).

$$\begin{bmatrix} dF_X \\ dF_Y \\ dF_Z \end{bmatrix} \bigg|_{k,n} = A \cdot \begin{bmatrix} dF_r \\ dF_\psi \\ dF_t \end{bmatrix} \bigg|_{k,n} \quad (3.8)$$

$$\begin{bmatrix} F_X \\ F_Y \\ F_Z \end{bmatrix} = \sum_{n=1}^{N_f} \sum_{k=1}^N \begin{bmatrix} dF_X \\ dF_Y \\ dF_Z \end{bmatrix} \bigg|_{k,n} \quad (3.9)$$

$$A = \begin{bmatrix} -\sin(\psi) \cdot \sin(\theta) & -\cos(\psi) \cdot \sin(\theta) & -\cos(\theta) \\ \sin(\psi) \cdot \cos(\theta) & \cos(\psi) \cdot \cos(\theta) & -\sin(\theta) \\ \cos(\psi) & -\sin(\psi) & 0 \end{bmatrix} \bigg|_{k,n} \quad (3.10)$$

$$\theta = \Omega + \frac{(n-1) \cdot 2\pi}{N_f} - \beta_k; \quad n = 1 \dots N_f \quad (3.11)$$

where N represents the total number of discrete points on a cutting edge, Ω is the cutting edge rotation angle and β_k is evaluated from Equation 3.3 (see Figure 3.2 for an illustration of the cutting force components and angular relationships).

Next step after prediction of cutting forces was to validate these predictions by performing various validation tests. Validation tests and their results are explained in Chapter 7. A summary chart of the proposed mechanistic approach is supplied in Figure 3.8.

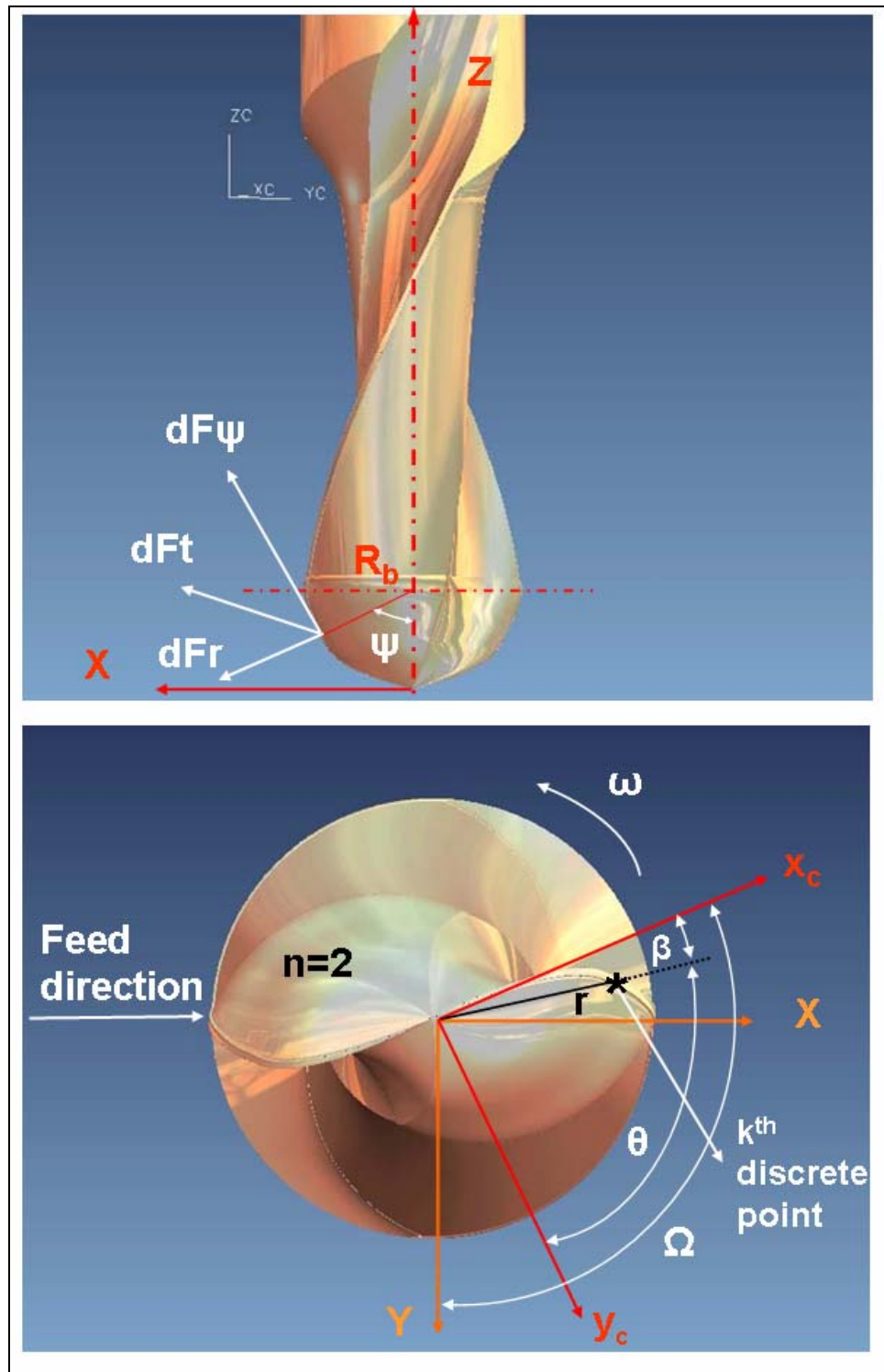


Figure 3.7: 3D illustration of ball-end mill

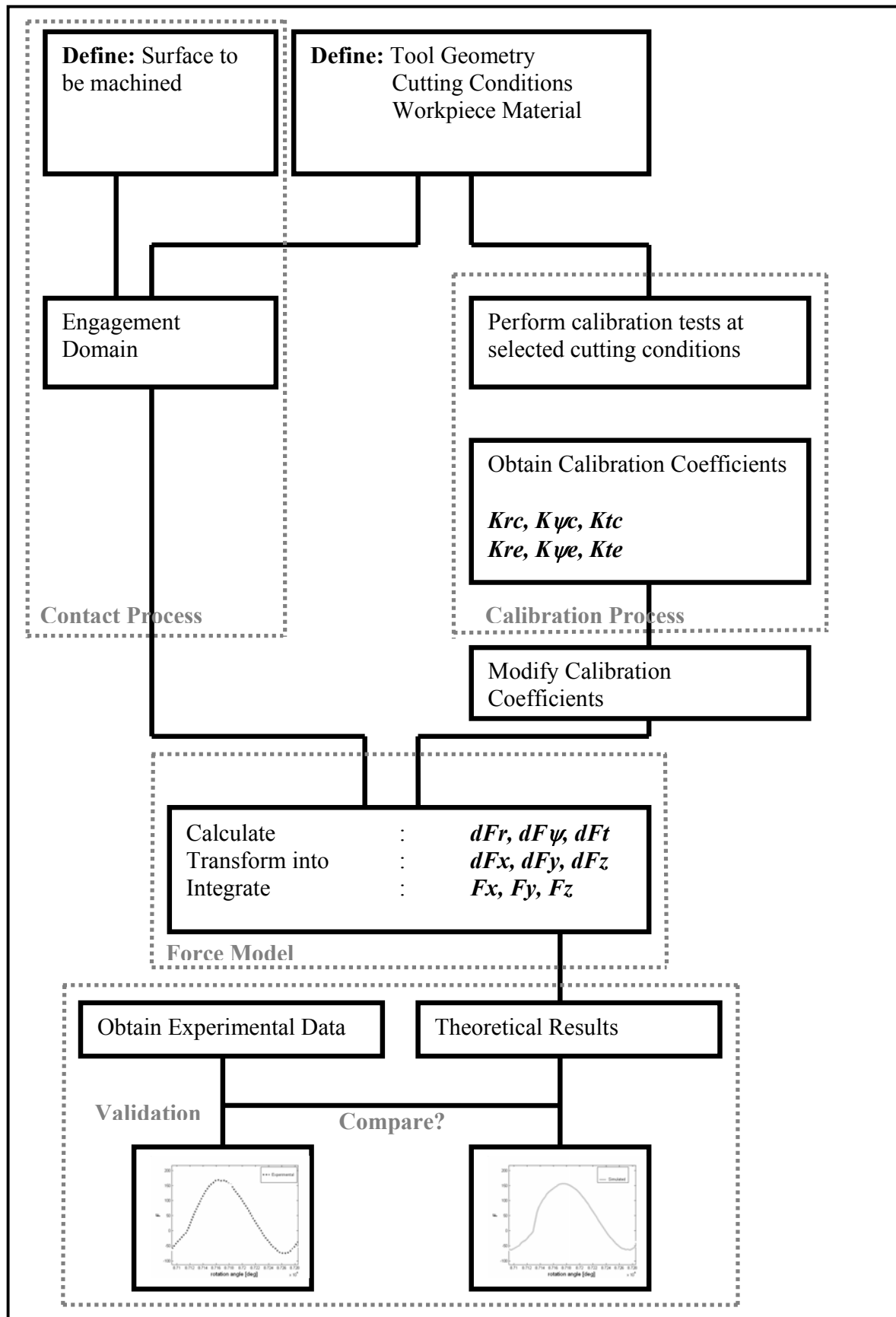


Figure 3.8: A summary chart of the proposed mechanistic approach.

Figure 3.8: A summary chart of the proposed mechanistic approach.

Chapter 4: ENGAGEMENT MODEL

4.1 Introduction

In order to produce the parts with free-form surfaces in an optimum manner in terms of production cycle time, cost and product quality, the machining process of free-form surfaces needs to be simulated faster and more accurately in advance. For the mechanical and dynamical simulations of the free-form surface machining process, perhaps the most important parameter is the fast and precise determination of the instantaneous chip loads along the tool path.

In this chapter, an analytical approach to the cutter/workpiece engagement region is developed for 3D free-form monotonic surfaces with ball-end mill. There is no need for discretization method and or Boolean approach, since the presented method is analytical. The analytical approach developed and presented in this chapter is much faster and quite accurate compared with the numerical approaches used in the literature up to now.

4.2 Coordinate Representations of Ball-end mill and Workpiece

In order to determine instantaneous engagement regions one has to present the ball-end mill and workpiece coordinates. Ball-end mill and workpiece are represented with different Cartesian frames as shown in Figure 4.1.

In the analysis below, x is representing the peek feed direction and y coordinate of the cutter is selected in the feed direction. The cutter coordinates are given as follows:

$$\begin{aligned}x_c &= x_w - x_0 \\y_c &= y_w - y_0 \\z_c &= z_w - z_0 - R\end{aligned}\tag{4.1}$$

$x_w - y_w - z_w$ is the Cartesian frame attached to the corner of the block workpiece, $x_c - y_c - z_c$ is the Cartesian frame attached to the center of the semi spherical cutter, x_0, y_0, z_0 are CL-point coordinates (see Figure 4.1) and R is the radius of the cutter. After this point forward, boundaries are going to be defined according to the cutter coordinate system.

Equations below are valid to represent cutter and workpiece in the space.

Parametric representation of a sphere is given as follows;

$$\begin{aligned} x &= R \cos(u) \cos(v) \\ y &= R \sin(u) \cos(v) \\ z &= R \sin(v) \end{aligned} \quad (4.2)$$

where $0 \leq u \leq 2\pi$ (counterclockwise sense from positive x direction) and $-\frac{\pi}{2} \leq v \leq \frac{\pi}{2}$ (counterclockwise sense from negative z direction).

Positions of a sphere and a cylinder in the space according to CL – points are;

$$x_w^2 + (y_w - y_0)^2 + (z_w - z_0 - R)^2 = R^2 \quad (4.3)$$

$$(x_w + c)^2 + (z_w - z_0 - R)^2 = R^2 \quad (4.4)$$

where c is the peek feed and represented in Figure 4.2.

The engagement region of a ball-end mill with the workpiece can be obtained by using Equations (4.1) or (4.2), (4.3) and (4.4).

The need for the Equation (4.4) which is basically a cylinder equation will be discussed in the following sections.

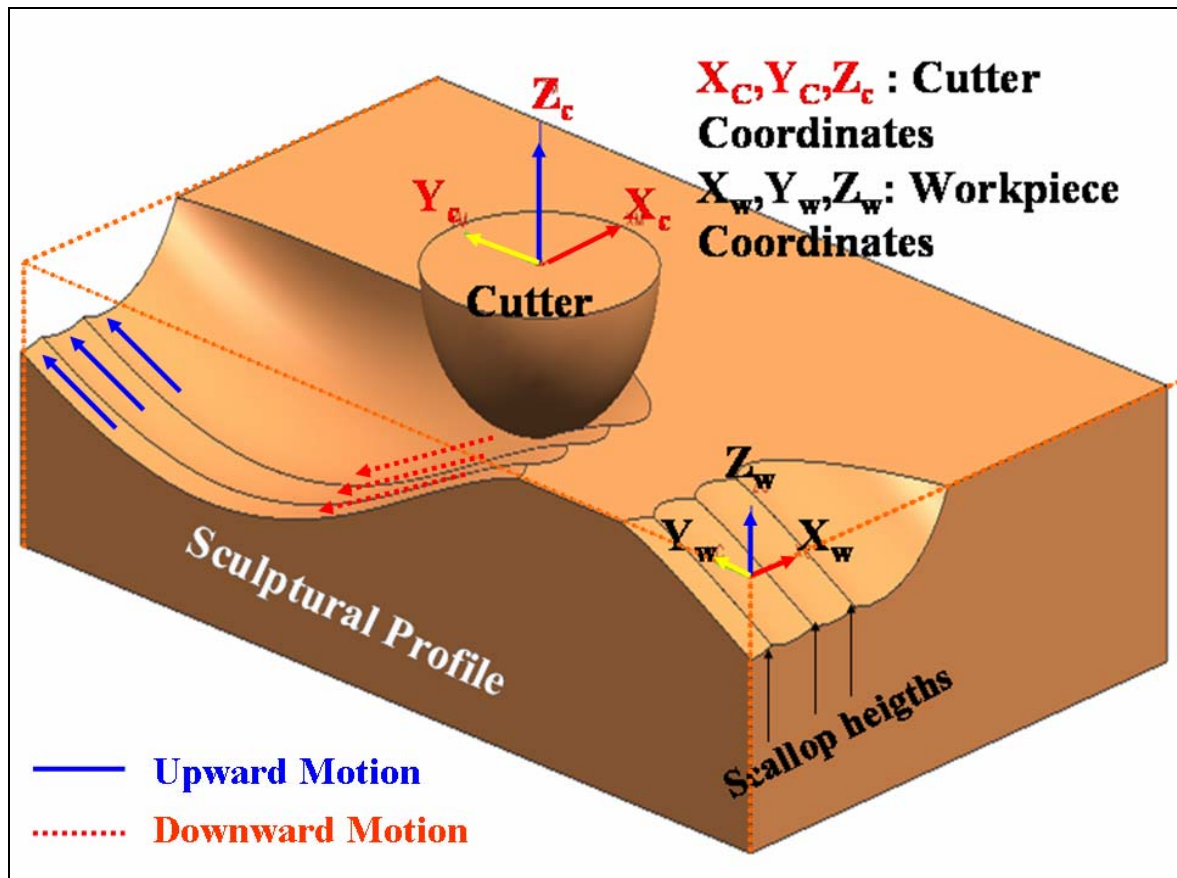


Figure 4.1: Ball-end mill model and coordinate frames for free-form surfaces

4.3 Boundaries of Instantaneous Engagement Region

For monotonic surfaces, axial depth of cut is not constant throughout the toolpath. Therefore engagement region changes at each CL – point. In this chapter, 3D monotonic surfaces are referred and defined as the surface which is formed after a ball-end mill machining with a CL-file whose CL-points defines the same toolpath for consecutive peek feeds (an example is shown in Figure 4.1). In monotonic surfaces there are three critical regions which are slightly different from each other; constant z motion region, upward motion region and downward motion region (see Figure 4.1). The difference between these three regions is; in upward motion region ball-end mill cuts with its front face in the feed direction from surface level down to a level which is higher than tip point, however in downward motion case cutter uses both front and back faces, there is also a drilling action in downward motion region, and for constant z motion case including front face; tip region is in contact with the workpiece, constant z motion is simply slot cutting at different depth of cuts. On the other hand, the similarity between these regions is that they are formed by previously machined, not machined and currently machined surfaces as shown in Figure 4.2.

β (finish angle of boundary – 1), e (offset from drill center) and R_1 (radius of engagement region at $z_{surface}$ level), shown in Figure 4.2, are some basic variables to define the engagement region boundaries and represented as follows;

$$\beta = \cos^{-1} \left(\frac{e}{R_1} \right) \quad (4.5)$$

$$e = \sqrt{(2Rh - h^2)} - c \quad (4.6)$$

$$R_1 = \sqrt{R^2 - (R - h)^2} \quad (4.7)$$

Here h is the vertical distance between tip point of the cutter and the upper surface of the workpiece. This is a variable for free-form surfaces since z coordinate of the cutting levels can be changed throughout the machining in free-form surfaces.

$$h = |z_0| \quad (4.8)$$

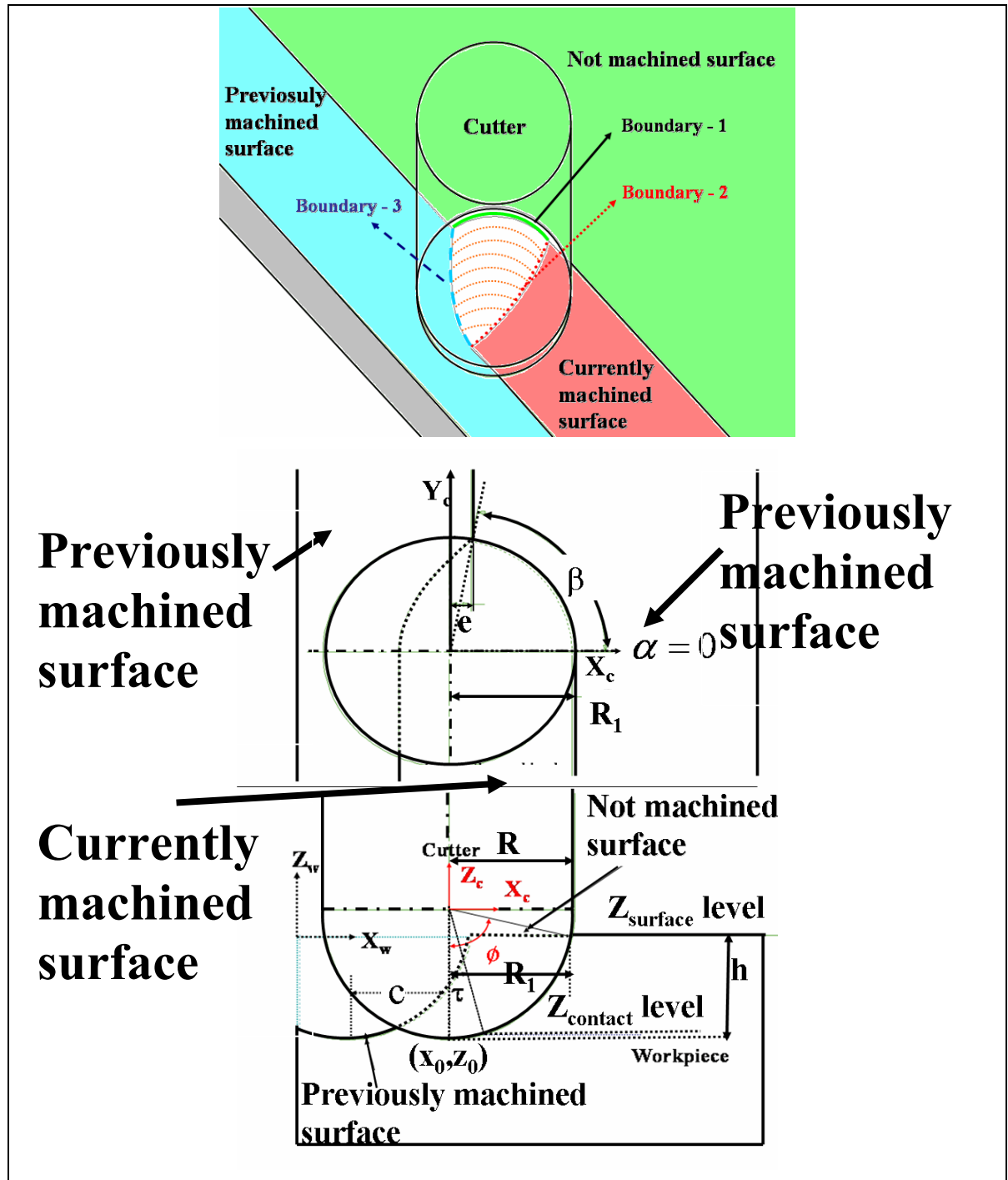


Figure 4.2: Top, front and detailed views of boundaries

$y_{contact}$ is found from the intersection of the surface function $f(y)$ and the sphere for each CL – point. A contact point can be found from equation (4.9);

$$f(y_{contact}) = -\sqrt{R^2 - (y_{contact} - y_0)^2} + z_0 + R \quad (4.9)$$

Equation (4.9) can be rearranged and stated as;

$$g(y_{contact}) = f(y_{contact}) + \sqrt{R^2 - (y_{contact} - y_0)^2} - z_0 - R \quad (4.10)$$

Equation (4.9) cannot be defined explicitly for most of $f(y)$'s. Therefore $y_{contact}$ must be found numerically. $y_{contact}$ is the root of equation (4.10), since $g(y_{contact}) = 0$. Muller method is chosen to solve equation (4.10), because Muller method is effective to find the complex roots. Complex roots are important because if rotation angle is small however not zero, due to numerical errors, complex roots can be seen. In this situation, the real part is taken and the real part of the root is a 10^{-8} error approximated to $y_{contact}$. Moreover the ball-end mill cannot be in contact with the surface because of the CL-file generation algorithm so all of the roots of equation (4.10) are complex. In this situation the nearest point on the ball-end mill to the desired surface is taken as $y_{contact}$. Muller method is modified to see all the roots of equation (4.10) in the neighborhood $[y_o(i) - R_1, y_o(i) + R_1]$. In the light of these constraints $y_{contact}$ can be found from equation (4.10).

In ball-end milling in order to determine the engagement region, previously machined surface is important since cutter also forms chips from this surface. Previously machined surface is analytically a cylinder with the radius equal to that of cutter. On the other hand, this cylinder is always in rotation according to the first derivative of the surface function.

Boundary – 1 is the intersection of the upper face of the workpiece and the cutter. Boundary – 2 is the intersection of the currently machined surface and the semi spherical cutter. Lastly, Boundary – 3 is the intersection of the cylinder (previously machined surface) and the semi spherical cutter which is a parabola on $\hat{x}\hat{y}$ plane where $\hat{y}$ and $\hat{z}$ are coordinates of rotated xyz system (Figure 4.3). The surface on the cutter with these boundaries is the engagement region.

Sphere equation at an instant with cutter coordinates;

$$\hat{x}^2 + \hat{y}^2 + \hat{z}^2 = R^2 \quad (4.11)$$

and the equation of the cylinder for the previously machined surface is;

$$(\hat{x} + c)^2 + \hat{z}^2 = R^2 \quad (4.12)$$

where c is the peek feed. The intersection of these two equations gives the equation of boundary – 3 as;

$$\hat{y} = \sqrt{2\hat{x}c + c^2} \quad (4.13)$$

$$\hat{z} = -\sqrt{R^2 - \hat{x}^2 - \hat{y}^2} \quad (4.14)$$

Rotation is due to the rotation of the cylinder according to the first derivative of the surface function. For constant z-region, since there is no rotation angle for cylinder, $\hat{y}$ and $\hat{z}$ are same with cutter coordinates. However, for upward and downward motion regions the coordinates are transformed to each other with equations (4.15), (4.16) and (4.17).

$$\begin{bmatrix} x \\ y \\ z \end{bmatrix} = A_i \begin{bmatrix} \hat{x} \\ \hat{y} \\ \hat{z} \end{bmatrix} \quad (4.15)$$

where A is the transformation matrix defined by;

$$A_i = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos(\lambda_i) & \sin(\lambda_i) \\ 0 & -\sin(\lambda_i) & \cos(\lambda_i) \end{bmatrix} \quad (4.16)$$

Instantaneous rotation angle is defined as;

$$\lambda_i = \left[\tan^{-1} \left(f'(y) \right) \right]_{y=y_{contact_i}} \quad (4.17)$$

at each CL-point.

From above equations cylinder coordinates are transferred to cutter coordinates at each CL-point in order to have coherency with boundary – 1 and boundary – 2. Cylinder coordinates representation and rotations are illustrated in Figure 4.3. Upward and downward motion regions are illustrated in Figure 4.1.

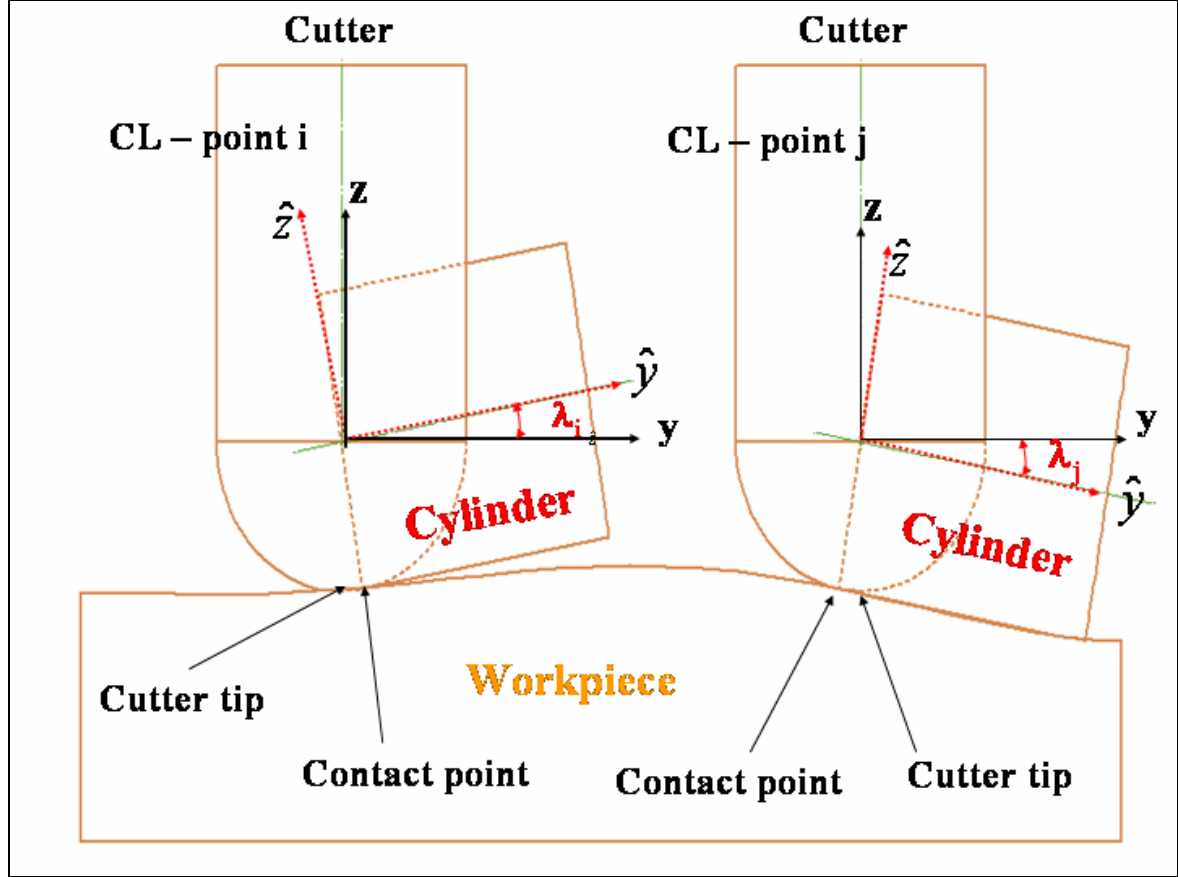


Figure 4.3: x, y, z coordinates for cutter and $\hat{x}, \hat{y}, \hat{z}$ for cylinder in inclined surfaces

4.3.1 Boundaries of Engagement Surface in Constant z Region

When the cutter moves in a constant z motion region, it cuts with front face including tip point. Figure 4.2 shows the boundaries for topview and frontview in that region.

Boundary – 1;

As mentioned previously boundary – 1 is the intersection of sphere with not machined surface (Figure 4.2). Since initial workpiece is taken as a block part, this intersection is latitude at $z_{surface}$ level. Then α is the start angle and β is the finish angle of the boundary – 1. In boundary – 1, equation (4.2) is used with variables $v = \phi, 0 \leq u \leq \beta$.

Boundary – 2;

Boundary – 2 is the intersection of sphere with currently machined surface (Figure 4.2). Since $\alpha = 0$ in that region, boundary – 2 is a meridian with sphere equation (4.2) with variables $\tau \leq v \leq \phi$, $u = 0$.

τ is the start angle and ϕ is the end angle which determines the lower and upper points of the boundary and are referred in Figure 4.2. ϕ locates the level of boundary – 1, in other words it states $z_{surface}$ level.

$$\begin{aligned}\tau &= \cos^{-1}\left(\frac{R + z_0 - z_{contact}}{R}\right) \\ \phi &= \cos^{-1}\left(\frac{z_0 + R}{R}\right)\end{aligned}\tag{4.18}$$

Boundary – 3;

Boundary – 3 is defined with equations (4.13), (4.14), (4.15), (4.16) and (4.17) in a range of;
 $-\frac{c}{2} \leq x_c \leq e$ with rotation matrix equal to identity matrix meaning no rotation.

4.3.2 Boundaries of Engagement Surface in Upward Motion Region

In upward motion region the engagement region is on the front face of the cutter. On contrary to constant z motion region, the rotation matrix is not identity matrix meaning there is a positive rotation. This rotation affects all boundaries. Boundary – 2 is a circle on the rotated cylinder frame, thus an ellipse on cutter frame. Boundary – 3 is still a parabola on the rotated $\hat{x}\hat{y}$ plane.

Boundary – 1;

Boundary – 1 is again latitude at $z_{surface}$ level. On the other hand, since $\lambda \neq 0$ and due to upward motion $\alpha > 0$.

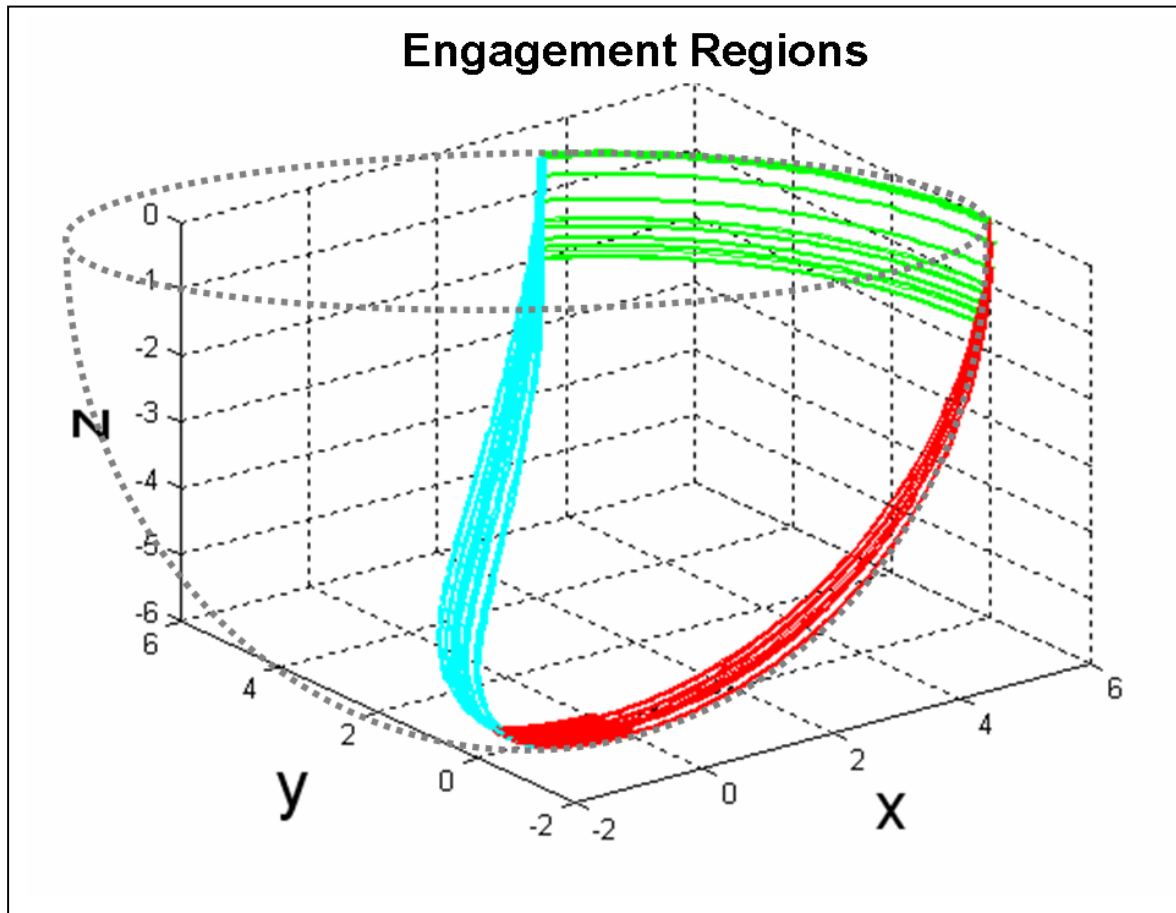


Figure 4.4 Variation of engagement regions of a ball-end mill with a surface of $z(y) = (2\sin(2\pi y/L) - y\cos(2\pi y/L))/25 - 5$, $[R=6 \text{ mm}, c=3\text{mm}, L=50\text{mm}]$.

$$\alpha = \tan^{-1} \left(\frac{\sqrt{R_1^2 - x_{eci}^2}}{x_{eci}} \right) \quad (4.19)$$

In boundary – 1, equation (4.2) is used with variables $v = \phi$, $\alpha \leq u \leq \beta$.

where

$$x_{eci} = R \sqrt{\frac{R_1^2 - R_2^2}{R^2 - R_2^2}} \quad (4.20)$$

is found from the intersection of the ellipse equation of boundary – 2 and the circle $x_c^2 + y_c^2 = R_1^2$

where R_2 (radius of engagement region at $z_{contact}$ level) is defined as;

$$R_2 = R \sin(\lambda) \quad (4.21)$$

Boundary – 2;

Due to rotation phenomenon, boundary – 2 is an ellipse on $x_c y_c$ plane. Since a sphere is symmetric due to a plane which passes from its center and since the cylinder cross-sectional plane passes from its center, the intersection is an ellipse with equation (4.22).

$$\begin{aligned} -\frac{c}{2} &\leq x_c \leq x_{eci} \\ y_c &= \sqrt{1 - \left(\frac{x_c}{R}\right)^2} R_2 \\ z_c &= -\sqrt{R^2 - x_c^2 - y_c^2} \end{aligned} \quad (4.22)$$

Boundary – 3;

Boundary – 3 is $-\frac{c}{2} \leq x_c \leq e$ with equations (4.13), (4.14), (4.15), (4.16) and (4.17). In upward motion region rotation matrix is not equal to identity matrix. Equation (4.16) and (4.17) is used with $\lambda > 0$ to determine rotation matrix.

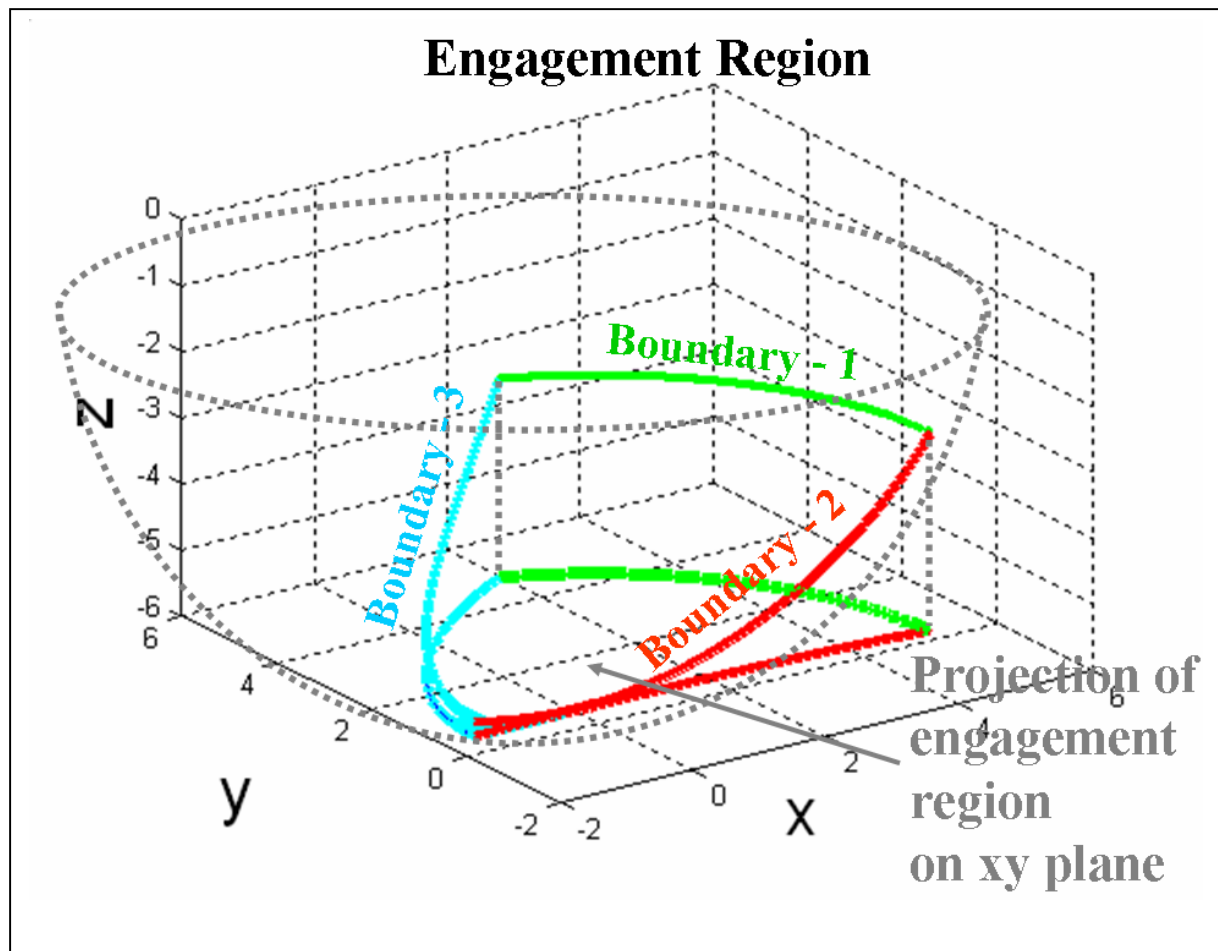


Figure 4.5: Engagement region at a CL point of upward motion region.

4.3.3 Boundaries of Engagement Surface in Downward Motion Region

In downward motion region the engagement region is on both front and back faces of the cutter. On contrary to constant z motion region, the rotation matrix is not identity, that's why the rotation angle is negative. Boundary – 2 is a circle on the rotated cylinder frame, thus an ellipse on cutter frame. Boundary – 3 is still a parabola on the rotated $\hat{x}\hat{y}$ plane.

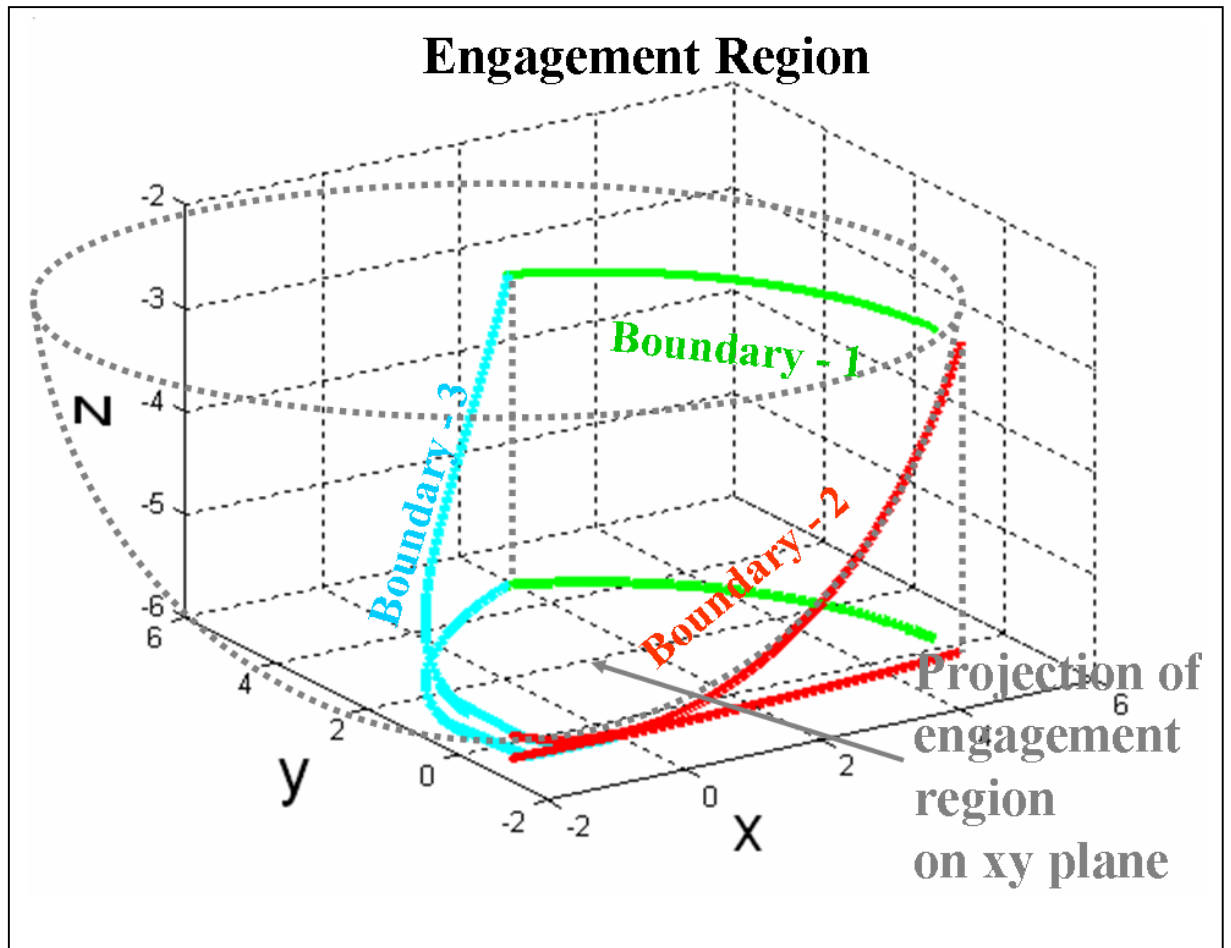


Figure 4.6: Engagement region at a CL point of downward motion region.

Boundary – 1;

Boundary – 1 is again latitude at $z_{surface}$ level. $\lambda \neq 0$ and due to downward motion of the ball-end mill $\alpha < 0$.

$$\alpha = -\tan^{-1}\left(\frac{\sqrt{R_l^2 - x_{eci}^2}}{x_{eci}}\right)$$

In boundary – 1, equation (4.2) is used with variables $v = \phi$, $\alpha \leq u \leq \beta$.

Boundary – 2;

Due to rotation boundary – 2 is an ellipse on $x_c y_c$ plane. Since a sphere is symmetric due to a plane which passes from its center and since the cylinder cross-sectional plane passes from its center, the intersection is an ellipse with equation (4.22) with a negative sign in y_c .

Boundary – 3;

$-\frac{c}{2} \leq x_c \leq e$ with equations (4.13), (4.14), (4.15), (4.16) and (4.17). In downward motion equation (4.16) is used with $\lambda < 0$ to determine the rotation matrix.

Above equations and boundaries are sufficient to determine the engagement region at any CL-point in free-form machining operations of monotonic surfaces. Below table is an example of a typical CL-file.

```

TOOL PATH/CONTOUR_AREA,TOOL,UGT0203_005
TLDATA/MILL,12.0000,6.0000,26.0000,0.0000,0.0000
RAPID
.
.
.
GOTO/-35.9543,0.1706,6.0019,0.0000000,0.0000000,1.0000000
RAPID
GOTO/-35.9543,0.1706,20.8603
PAINT/COLOR,42
FEDRAT/MMPM,212.4000
GOTO/-35.9543,0.1706,3.7488
$$ START OF CHECK GEOMETRY INTERFERENCE
FEDRAT/141.6000
GOTO/-6.0199, 0.5013, -2.0075
GOTO/-6.0199, 6.4018, -2.0996
GOTO/-6.0199, 9.6798, -2.1318
GOTO/-6.0199, 13.0672, -2.1471
GOTO/-6.0199, 17.7657, -2.1175
GOTO/-6.0199, 20.2789, -2.0757
GOTO/-6.0199, 24.9775, -1.9565
GOTO/-6.0199, 29.5667, -1.7876
GOTO/-6.0199, 33.9375, -1.5975
GOTO/-6.0199, 41.4770, -1.2634
GOTO/-6.0199, 45.6293, -1.1089
GOTO/-6.0199, 49.1259, -1.0165
GOTO/-6.0199, 52.1854, -0.9728
GOTO/-6.0199, 54.8078, -0.9677
GOTO/-6.0199, 57.2117, -0.9892
GOTO/-6.0199, 59.6156, -1.0420
GOTO/-6.0199, 61.1454, -1.0898
.
.

```

Table 4.1: An example of a CL-file

Since boundary conditions are set, it becomes easier to define the engagement domain at each CL-point. As shown in CL-file table (Table 4.1), the points which are stated after GOTO/ Boolean operator are the tip point coordinates of the cutter used to machine the workpiece shown in Figure 4.3. From left to right, after GOTO/ Boolean operator, columns are x_0, y_0, z_0 coordinates respectively, where x direction is selected as feed direction.

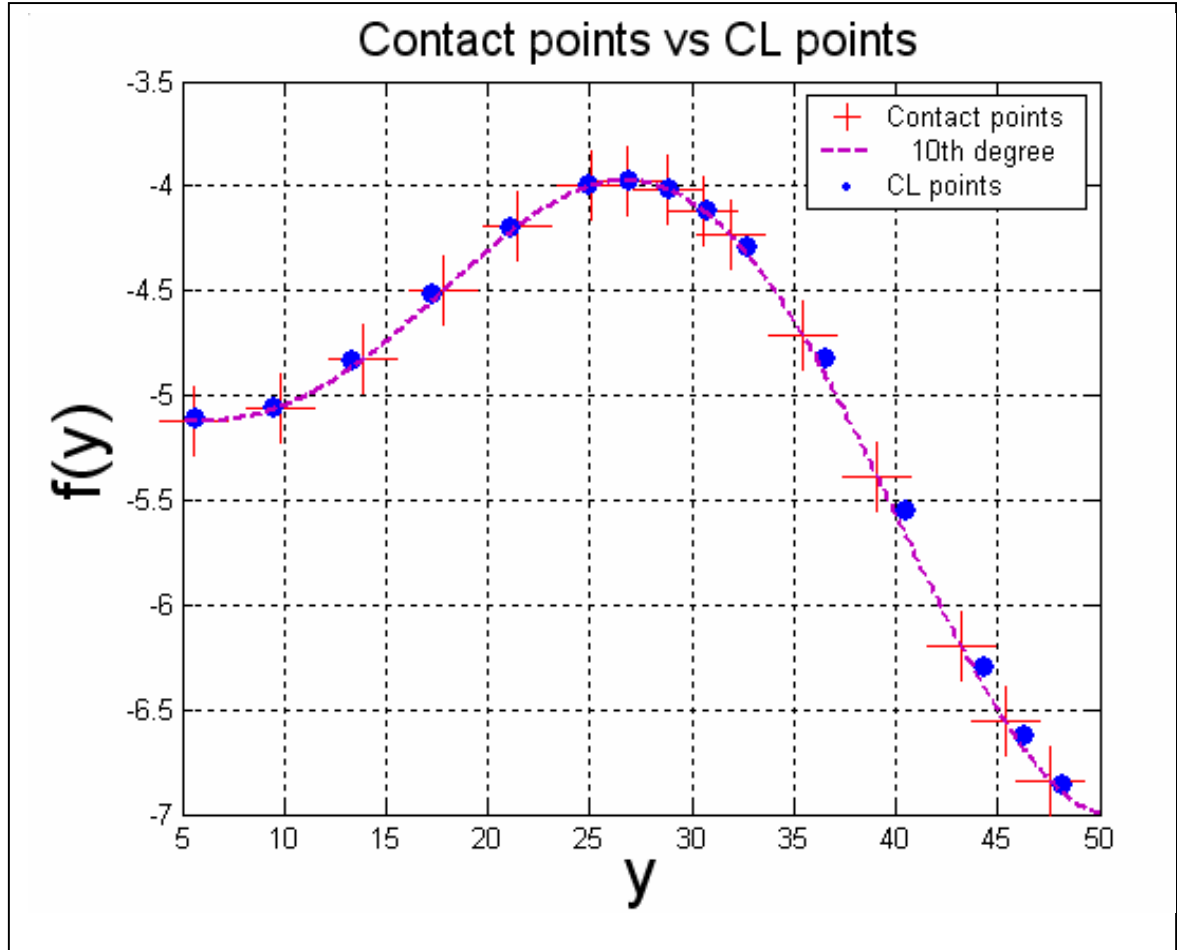


Figure 4.7 Contact points, CL points and the surface.

If the boundaries are inspected, it will be seen they are dependent on c and x_0, y_0, z_0 and L (length of the workpiece which is in y direction for polynomial fitting). A code, which takes CL-file

obtained from a commercial CAD/CAM package as an input to construct engagement domain of each CL-point as shown in Figure 4.3, is developed.

Figure 4.4 is a combination of totally 17 CL-points. An example of upward motion region is Figure 4.5 and downward motion region is Figure 4.6. In Figure 4.6 it is seen that the boundary – 2 is at the front face, in Figure 4.6 it is at the back face due to rotation in the cylinder. For this surface function, since there is no constant z-region, it is not possible to observe any front cut including tip point which is a common condition of free-form surface machining operations (also see Figure 4.9).

4.4 Surface Equation

Surface equation is the key information for the post processing code, ACRsim, developed to determine engagement regions. ACRsim is a post processing code which requires a function of the desired surface to solve the equation (4.10). Equation (4.10) is the start point in the process of engagement region determinations. Because CL-point is not the first intersection point with cutter and workpiece; it gives only the coordinates of tip of the cutter, which means that the contact point determines the axial depth of cut and inclination angle thus boundaries – 2 and 3. The contact point represented in this work is the intersection of the sphere and the surface function in yz -plane in free-form surfaces.

Figure 4.7 shows the CL points as dots and contact points as pluses. As it is clear that they are not equal and the distance between a CL point and its contact point is determined by radius R and first derivatives of surface function at that point. If the absolute value of the derivative is large, cutter tip and contact point are far away from each other. On the other hand, since the derivative sign changes at tip points of the surface function, number of CL points in that region is more and they are close to each other.

4.5 Constructing Surface Function from the CL-file

In industry since the designed parts are very complex, the CL – files of these parts are very long. For most of the cases, functions of the surface of these parts are not known analytically and most commonly surfaces are like B-splines or NURBS in patches. In order to find the equation of each

patch is an undesired situation and a tedious work. However the problem is that a direct polynomial fitting to CL – points will not give the surface function since CL files are generated by including the geometry of the cutter and the workpiece. In other words, CL-points are not always on the desired surface. Figure 4.7 shows the difference between contact points and CL – points of a surface.

In order to obtain the surface function, a method which is an approximation to the first derivative of the surface is used. Method is simply modifying the CL – points by intersecting a line, whose slope is a forward difference of consecutive points, and the circle on the surface on yz plane.

$$\frac{z_0^{i+1} - z_0^i}{y_0^{i+1} - y_0^i} = - \frac{\bar{y}^i}{\sqrt{R^2 - \bar{y}^{i2}}} \quad (4.23)$$

where R is the radius of cutter, $\bar{y}$ is a point approximation to be on the surface and i denotes the i^{th} CL point.

Simplifying equation (4.23) results;

$$\begin{aligned} \bar{y}^i &= \frac{m^i R}{\sqrt{1 + m^{i2}}} \\ m^i &= \frac{z_0^{i+1} - z_0^i}{y_0^{i+1} - y_0^i} \end{aligned} \quad (4.24)$$

From an $N+1$ number of CL – points in one direction excluding ‘air cut’ points, N number of point approximation can be formed by using equations (4.23) or (4.24). From this point forward, a direct polynomial fitting to these approximated points can be used however since the number of CL – points may be too much, direct fitting may not be suitable. In this case, a 3rd or 4th order polynomial can be fitted to the patches of 5 or 6 CL-points respectively. Figure 4.8 shows the difference between the engagement domains obtained by using exact surface function and 3rd order polynomial fitting to 5 point patches. Number of point patches and degree are variables and can be changed for fast solution.

4.6 Verification of the Analytical Chip Load Simulation Results

In order to verify the boundaries of the engagement region found by analytical method, a commercial software package (Unigraphics) is used. Firstly, 3D CAD scenarios of constant z motion, upward motion and downward motion regions of a random selected point from Table 4.1 are

constructed. Then the points of engagement region from the ACRsim are imported to Unigraphics. Figure 4.9 shows the CAD drawings and ACRsim outputs of the engagement domain.

In constant z motion region and upward motion regions, one can observe the exactness of the boundary variables. There are slight differences for downward motion region, due to representation of the boundary curves with less number of points and due to spline-fitting function of Unigraphics.

One thing to comment on the equations is that rotation phenomenon of cylinder is taken with $y_{contact}$ point. On the other hand, in real previously machined surface is a circular tube with radius equal to

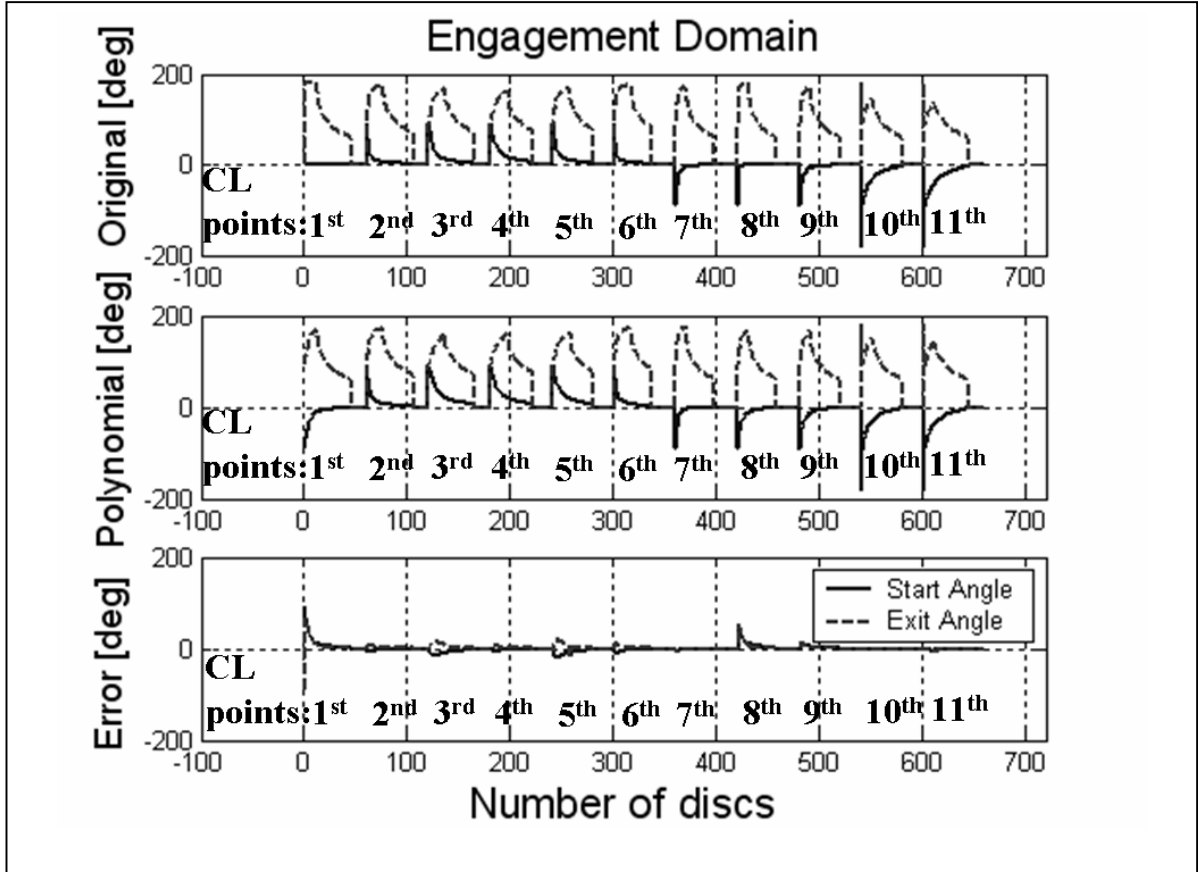


Figure 4.8: Difference between polynomial fitting and exact surface function on engagement domain

cutter and along a guide of $f(y)$. In order to find the boundary – 3 exactly, one has to find the each intersection point of a circle with sphere along $f(y)$ in $-\frac{c}{2} \leq x_c \leq x_{eci}$. On the other hand, excluding

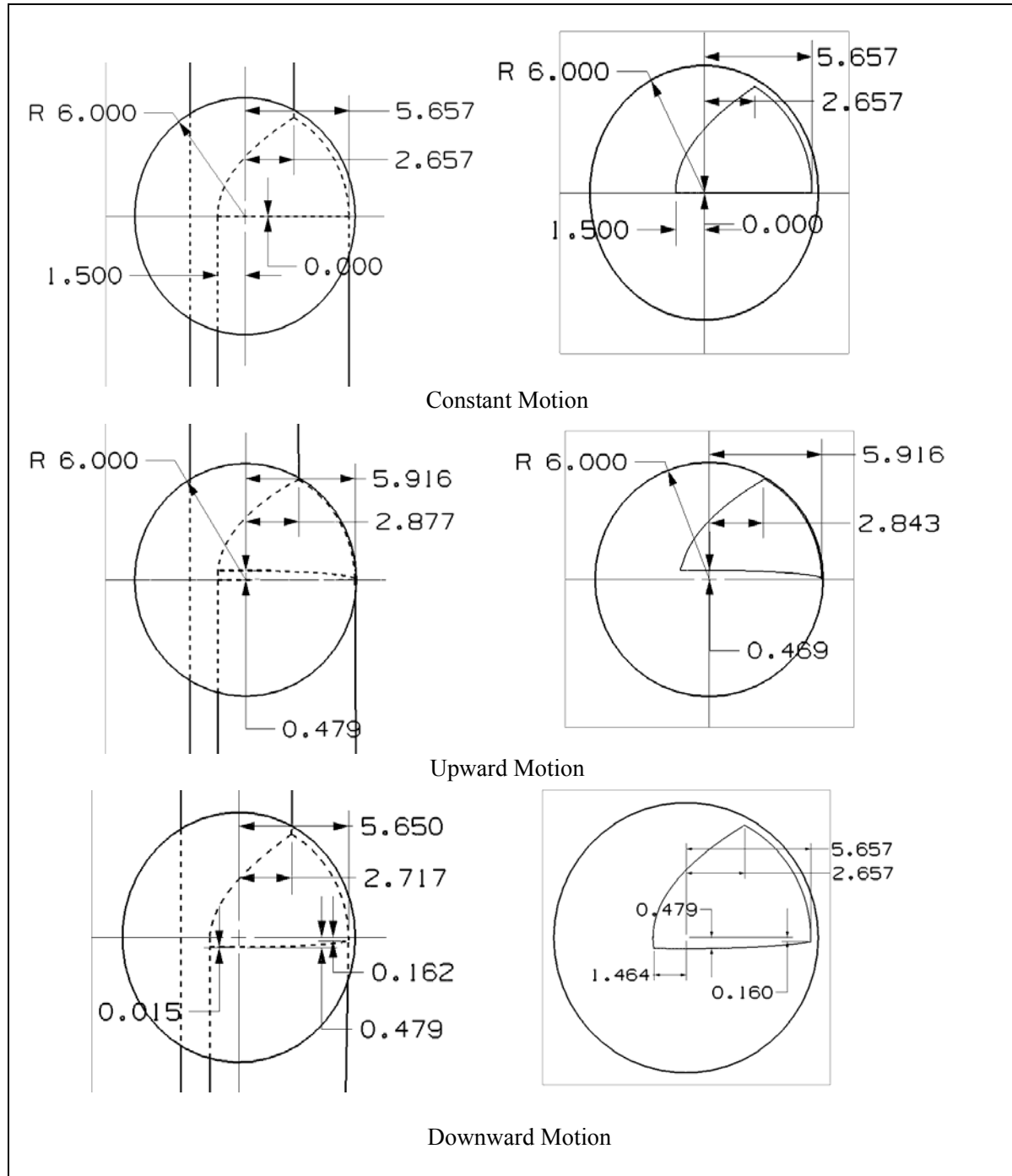


Figure 4.9: All motion regions CAD drawing vs. output of the ACRsim respectively.

this fact does not change the engagement domain abruptly, if the functions first derivative does not change widely in the neighborhood $[y_o(i) - R_1, y_o(i) + R_1]$. Moreover validations of the ACRsim with machining of different Free-Form surfaces are presented in chapter 7.

4.7 Comparison of the ACRsim with z-mapping

Z-mapping is a powerful technique to find the engagement domain between cutter and the workpiece. For any kind of surface functions, monotonic or non-monotonic, it can construct the engagement domain. On the other hand z-mapping is not a fast algorithm since it operates with Boolean algebra. On contrary ACRsim is an analytical method to find the engagement domain of monotonic surfaces as shown in Figure 4.1. The ACRsim constructed the engagement domain of a CL-file with 960 points in 4 minutes on average (in MATLAB with hp nx5000 laptop Pentium M processor 1.50 GHz 512 MB RAM). Z-map method constructs the engagement domain in the order of hours. ACRsim requires only the function of the surface and the CL-file for exact calculation or only CL-file then a surface equation will be generated from CL-file by using polynomial fitting. In addition to that, since ACRsim is much faster than Z-mapping, it can be implemented as a post-processor while simulating the machining process in a CAD/CAM program, the forces and surface topography can be seen simultaneously. A feed-rate scheduling can be performed to optimize 3D free-form machining.

Chapter 5: CALIBRATION PROCESS

5.1 Determining the Calibration Coefficients

5.1.1 The Experimental Setup

In milling simulations, using orthogonal cutting parameters such as shear angle, shear stress, and friction coefficient to determine oblique milling constants is desired for modeling a variety of milling cutter geometries. However, for orthogonal calibrations a turning center is needed, that's why a quick method of calibrating the milling tools namely mechanistic approach will be used at that time.

In order to determine the cutting constants in this study, Altintas' approach [1] for identification of cutting constants in milling has been utilized with certain changes.

The experiments for calibration and validation tests were performed on Mazak FJV-200 UHS Vertical Machining Center (VMC). The cutter was carbide ball-end mill cutter from CoroMill Plura series of Sandvik with 12 mm diameter, 37 mm projection length and 30 degrees helix angle (See Chapter 3 for a detailed analysis of the cutter). The workpiece materials were aluminum blocks (Al7039) of size 250x170x38 [mm]. Kistler 3-component dynamometer (Model 9257B) and a charge amplifier have been used to measure cutting forces. The 3-component dynamometer has been fixed to the machine table using fixtures and the aluminum block was attached to the dynamometer using two 8M screws as seen in Figure 5.1.

The cutting forces are sensed by the piezoelectric transducer within the dynamometer and an electric charge is formed due to the dynamic forces generated in the cutting edge. The charge obtained from the dynamometer is the sum of all dynamic differential forces acting on both flutes. This electric charge is later taken by the charge amplifier to be converted and amplified to voltage output. The sensitivity values for the three channels (x, y and z) in the amplifier were -7.87, -7.90, and -3.69 pC/N respectively. Amplifier gain for the device was set to 200 for all channels. Subsequently, through use of a proper data acquisition card and software, the voltage output is displayed and recorded as cutting forces which have units in Newtons. To display and record the measured data, a program namely CutPro which has a data acquisition module namely MALDAQ is used (See Figure 5.2 for a sample

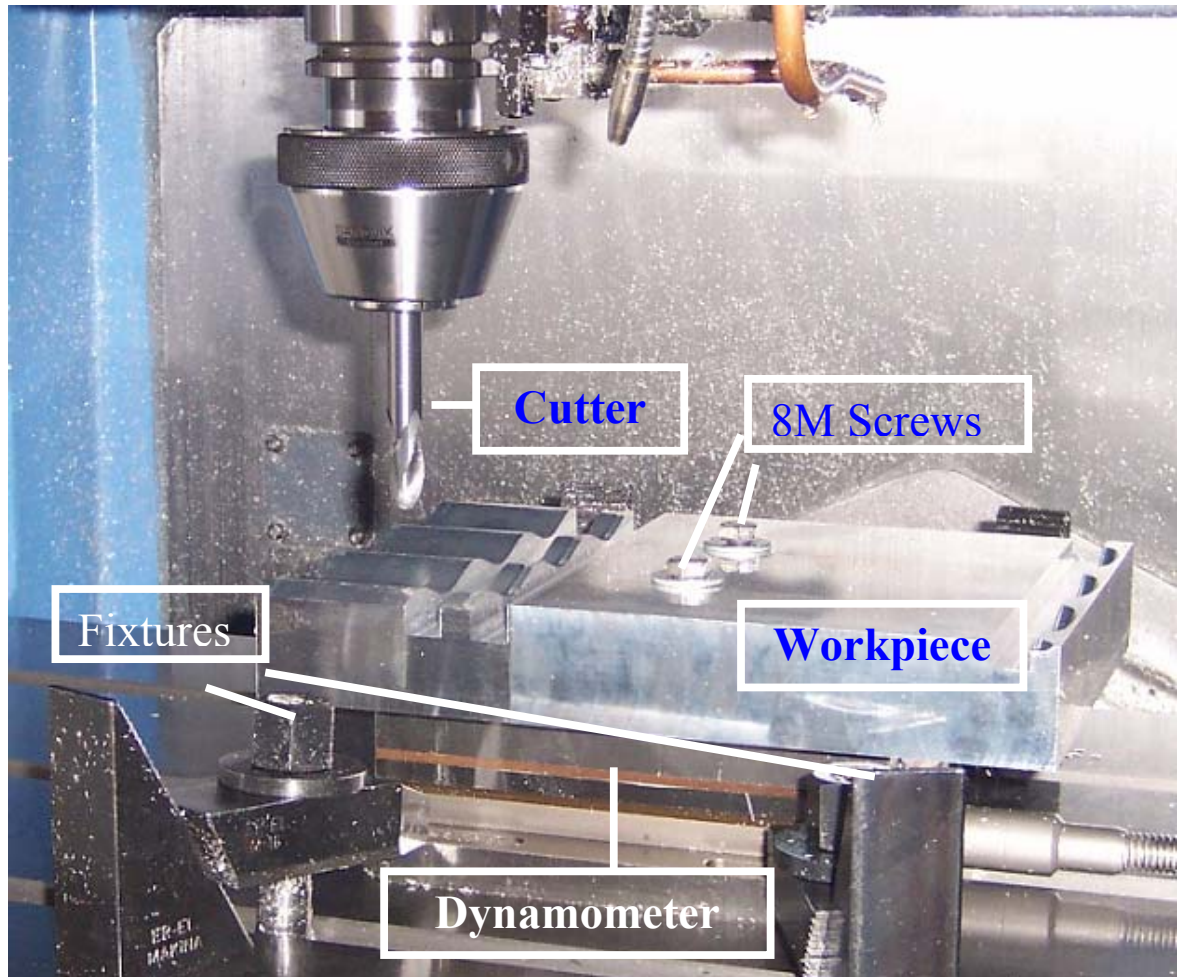


Figure 5.1: Workpiece and 3-component dynamometer fixed to VMC machine table for cutting tests

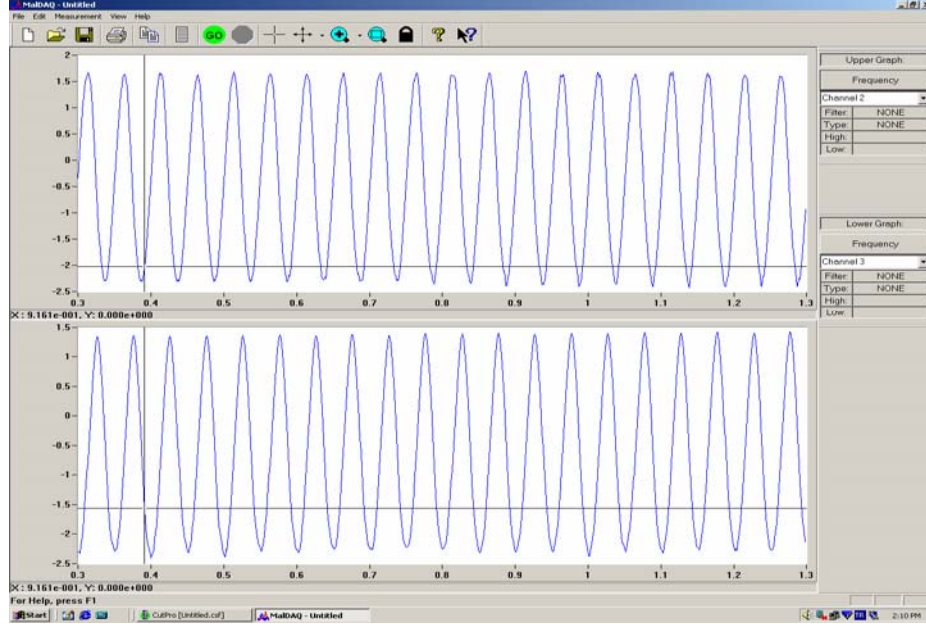


Figure 5.2: Sample output screen in CutPro for cutting forces

screenshot of CutPro in DAQ process). The testing environment is shown in Figure 5.3.

First step to be taken is to obtain the cutting forces by conducting a set of milling experiments at different feedrates. The 48-192 mm/min feedrate region has been tested in this study. The ball part of the ball-end mill has been divided into 7 regions for a detailed analysis. Assuming the tip to be at zero level, these regions were subsequently at 0-0.5, 0.5-1, 1-1.5, 1.5-2, 2-3, 3-4, 4-6 mm distance from tip and these regions are represented in Figure 5.4. Each region has been tested for four different feedrates; 48, 96, 144 and 192 mm/min. The spindle speed (ω) was set to 600 rpm. Data has been collected for full engagement region and with sampling frequency rate (f) of 1200 Hz at all tests. The collection of data is performed at every discrete rotation angle $\Delta\theta$ degrees:

$$\Delta\theta = \frac{2\pi \cdot \frac{\omega}{60}}{f} \quad (5.1)$$

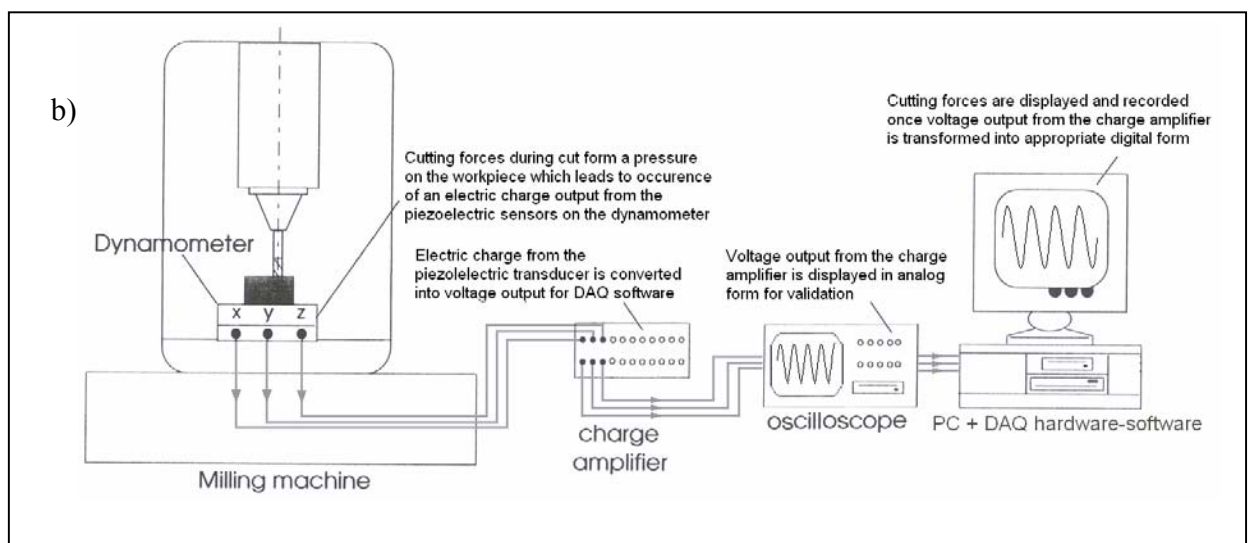


Figure 5.3: The experimental setup for measurement of cutting forces;
a) Actual testing environment, b) Detailed illustration

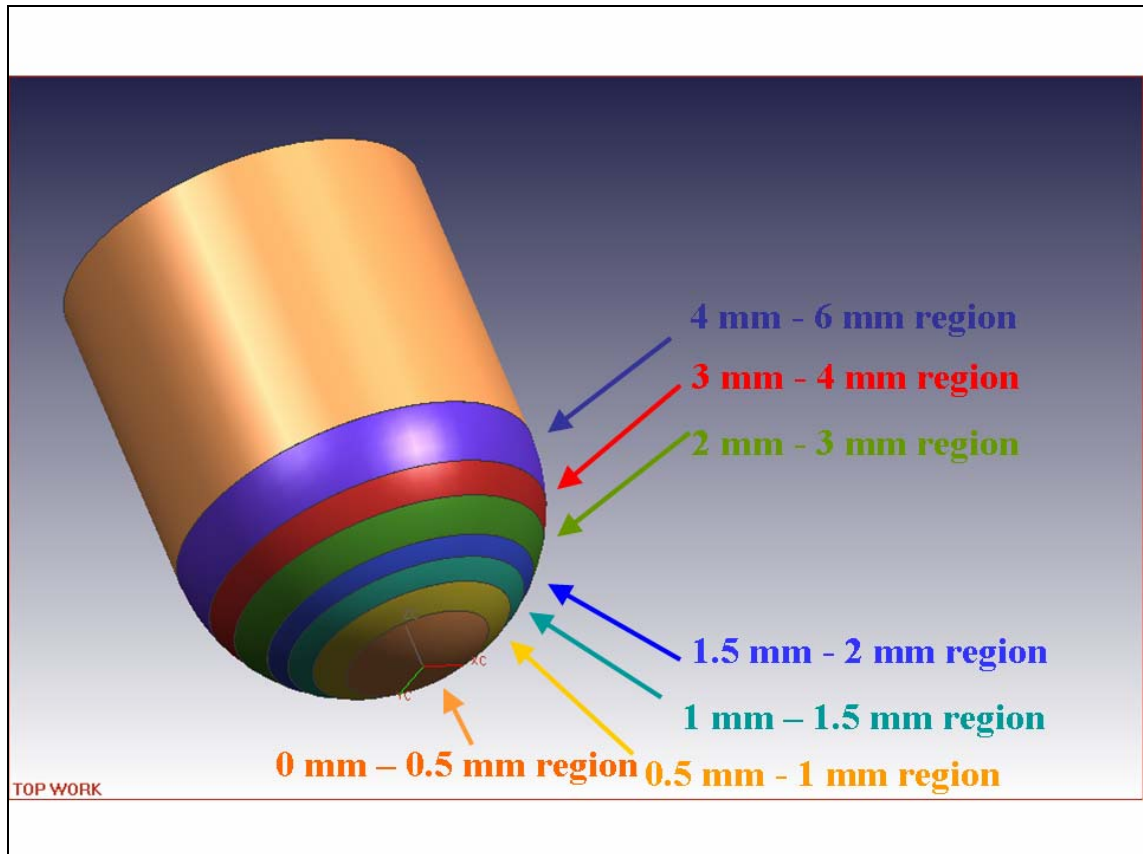


Figure 5.4 Calibration regions

5.1.2 Determination of Calibration Coefficients

In order to obtain the calibration coefficients for a certain region, the following procedure has been applied step by step:

1. All experimental force data have been equally aligned according to the angular phase differences.

2. Using the inverse of the transformation matrix given in Equation 3.10; collected force components (F_x, F_y, F_z) have been transformed into radial, zenith and tangential components (F_r, F_ψ, F_t).

3. The peak points (average of maximum values) of all three components (r - ψ - t) of the cutting force have been found for nine of the revolutions in the collected data (ten revolutions have been captured totally for spindle revolution (ω) of 600 rpm and sampling frequency (f) of 1200 Hz, two of these have been omitted in calculations to account for phase differences).

4. The differences between average of the peak points have been obtained for each increment in the depth of cut (e.g., for the 3-4 mm region, the cutting forces will be equal to the difference between 4 mm depth of cut and 3 mm depth of cut tests).

5. Once the F_r, F_ψ, F_t values have been obtained for each depth of cut, these have been plotted versus chip thickness in order to obtain the Calibration Coefficients (Figure 5.5 to 5.11). The determination of cutting constants from experimental data is summarized in Figure 5.12.

The change in Calibration Coefficients along the cutting edge is displayed in Figure 5.13. It is observed in these plots that in cutting coefficients there is an exponential decreasing behaviour and in edge coefficients, on the other hand, there is almost completely a random behaviour; no trend can be clearly identified by observing their plot.

The numerical values of the calibration coefficients determined with the calibration process can be summarized in the Table 5.1.

Region from tip Coefficients	0 - 0.5 [mm]	0.5 -1 [mm]	1 - 1.5 [mm]	1.5 -2 [mm]	2 – 3 [mm]	3–4 [mm]	4 – 6 [mm]
K_{re} [N/mm²]	2991.4	695.4	22.1	195.7	105.1	483.3	353
K_{re} [N/mm]	31.2	9.6	9.0	2.5	7.2	14.8	11.9
K_{ψc} [N/mm²]	1113.4	784.9	752.7	178.5	101.5	30.8	5.6
K_{ψe} [N/mm]	11.7	12.7	3.6	19.5	13.5	8.7	0
K_{tc} [N/mm²]	5353.9	2549	1181.8	1429.1	971.4	847.6	932.1
K_{te} [N/mm]	36.4	17.3	29.6	3.1	16.3	16.9	10.7

Table 5.1 Calibration coefficients for selected depth of cuts

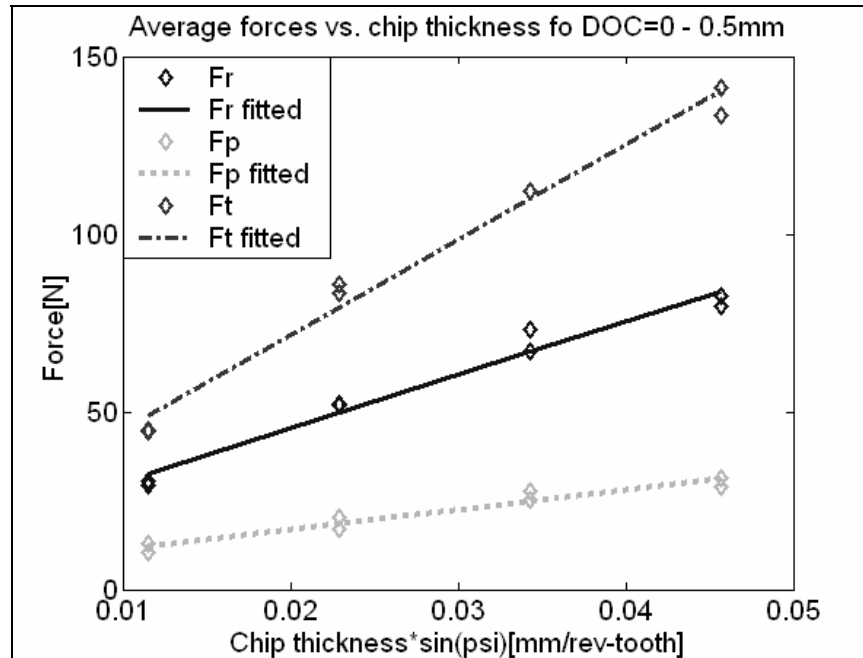


Figure 5.5: Cutting forces vs. chip thickness for 0 – 0.5 mm

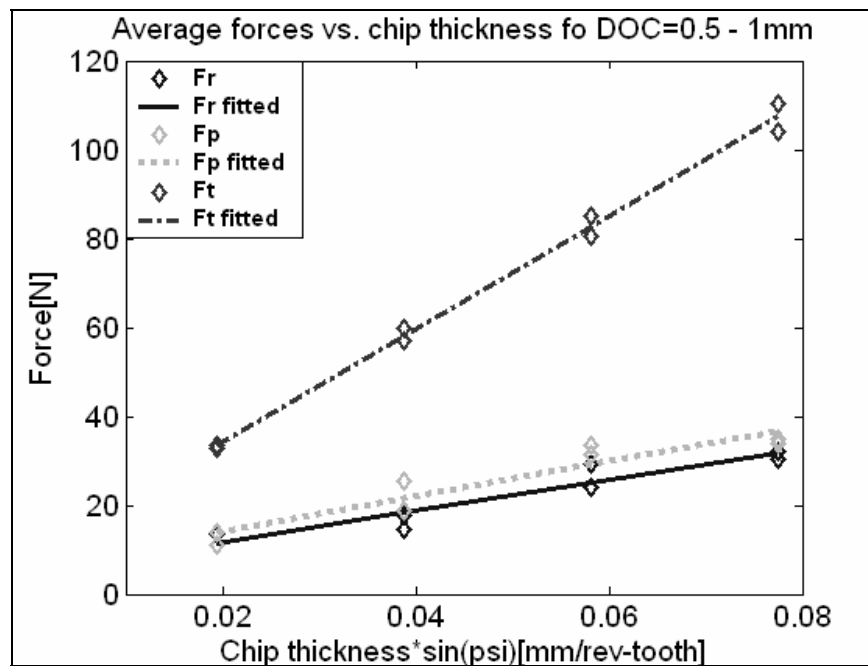


Figure 5.6: Cutting forces vs. chip thickness for 0.5 – 1 mm

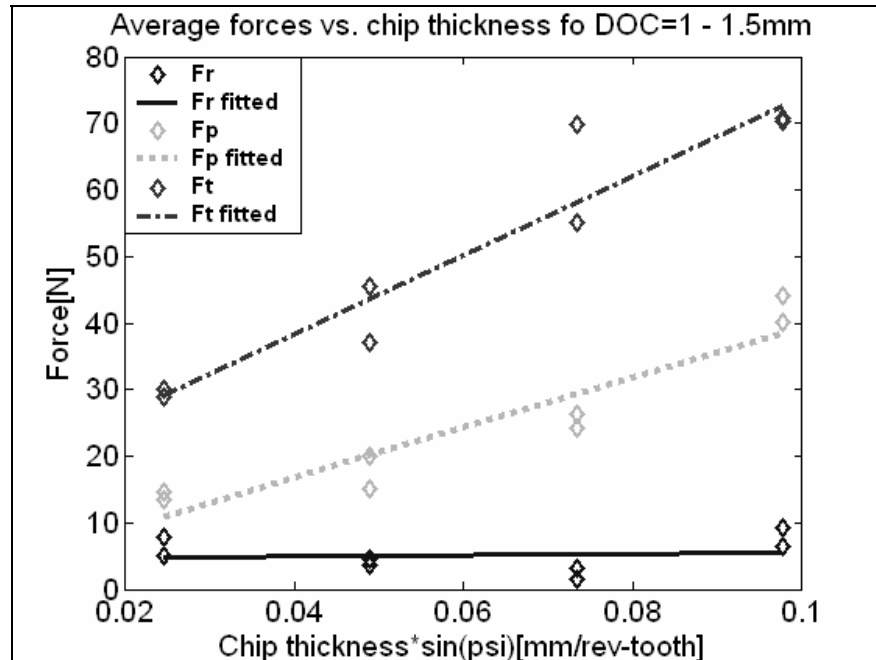


Figure 5.7: Cutting forces vs. chip thickness for 1 – 1.5 mm

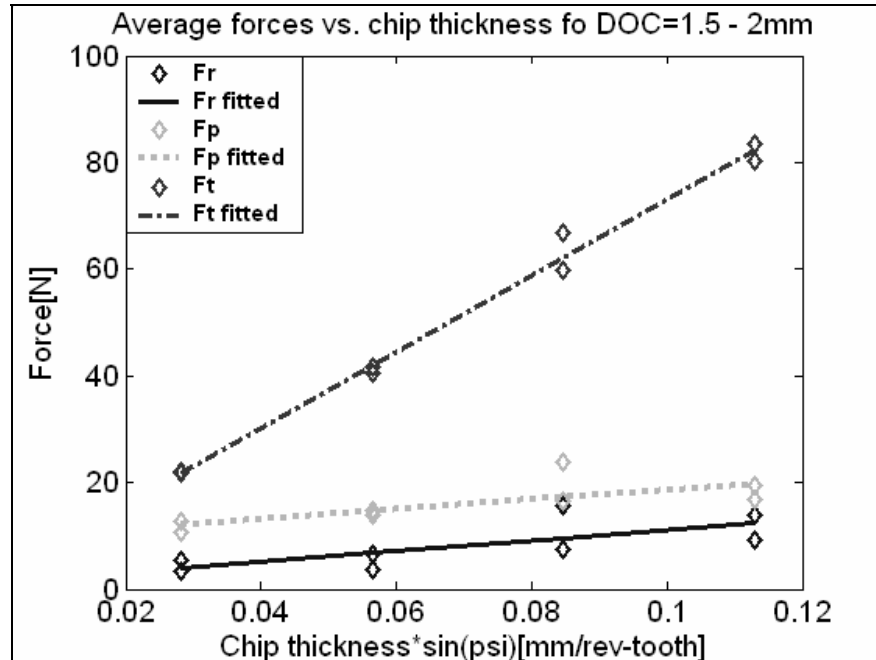


Figure 5.8: Cutting forces vs. chip thickness for 1.5 – 2 mm

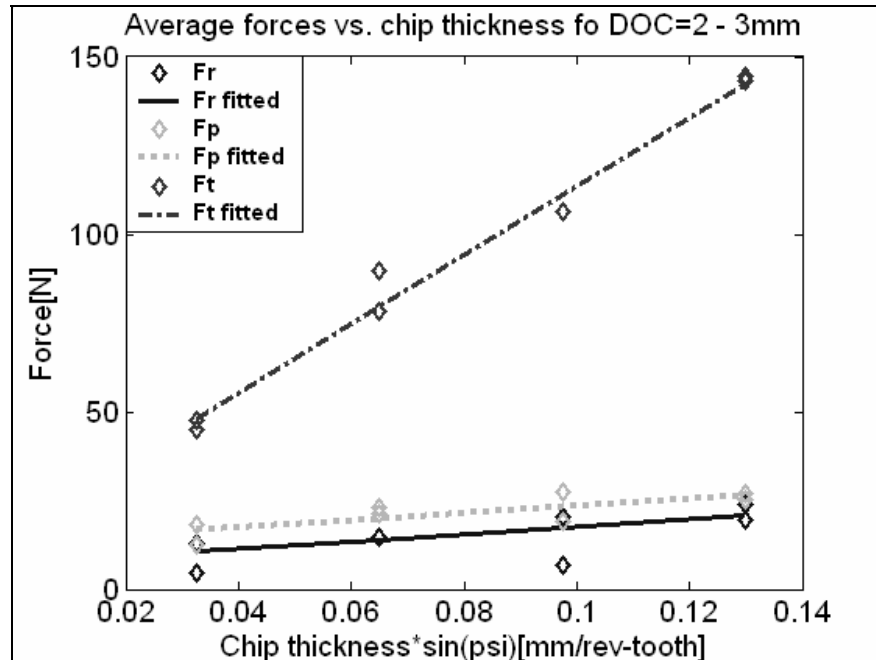


Figure 5.9: Cutting forces vs. chip thickness for 2 – 3 mm

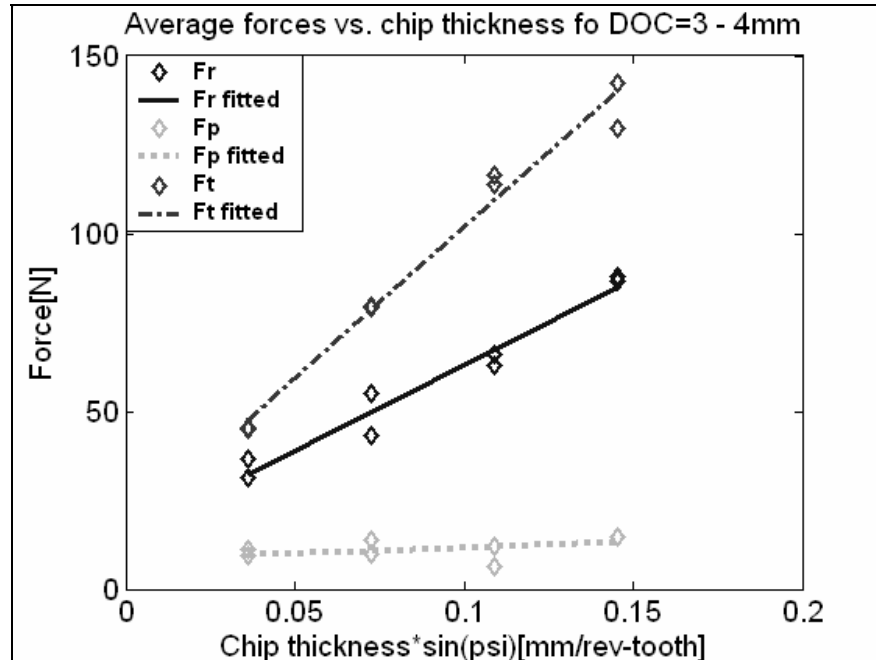


Figure 5.10: Cutting forces vs. chip thickness for 3 – 4 mm

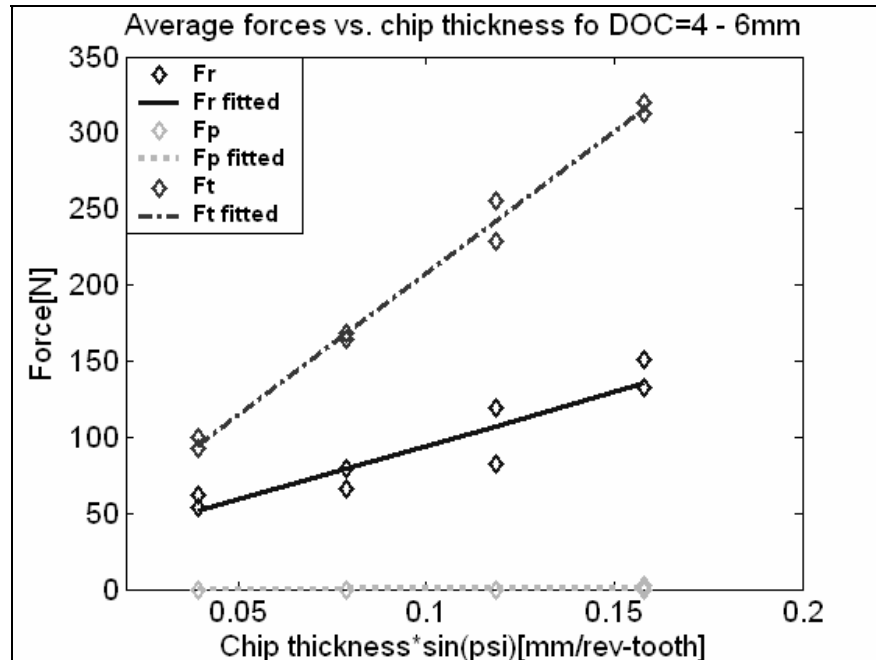


Figure 5.11: Cutting forces vs. chip thickness for 4 – 6 mm

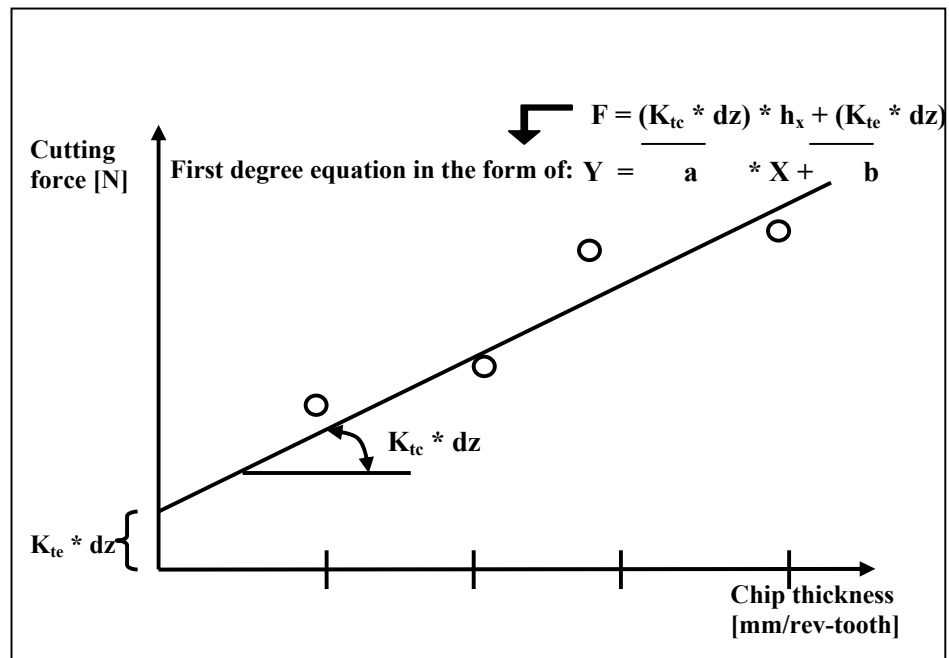


Figure 5.12: Determination of the cutting coefficients from experimental data

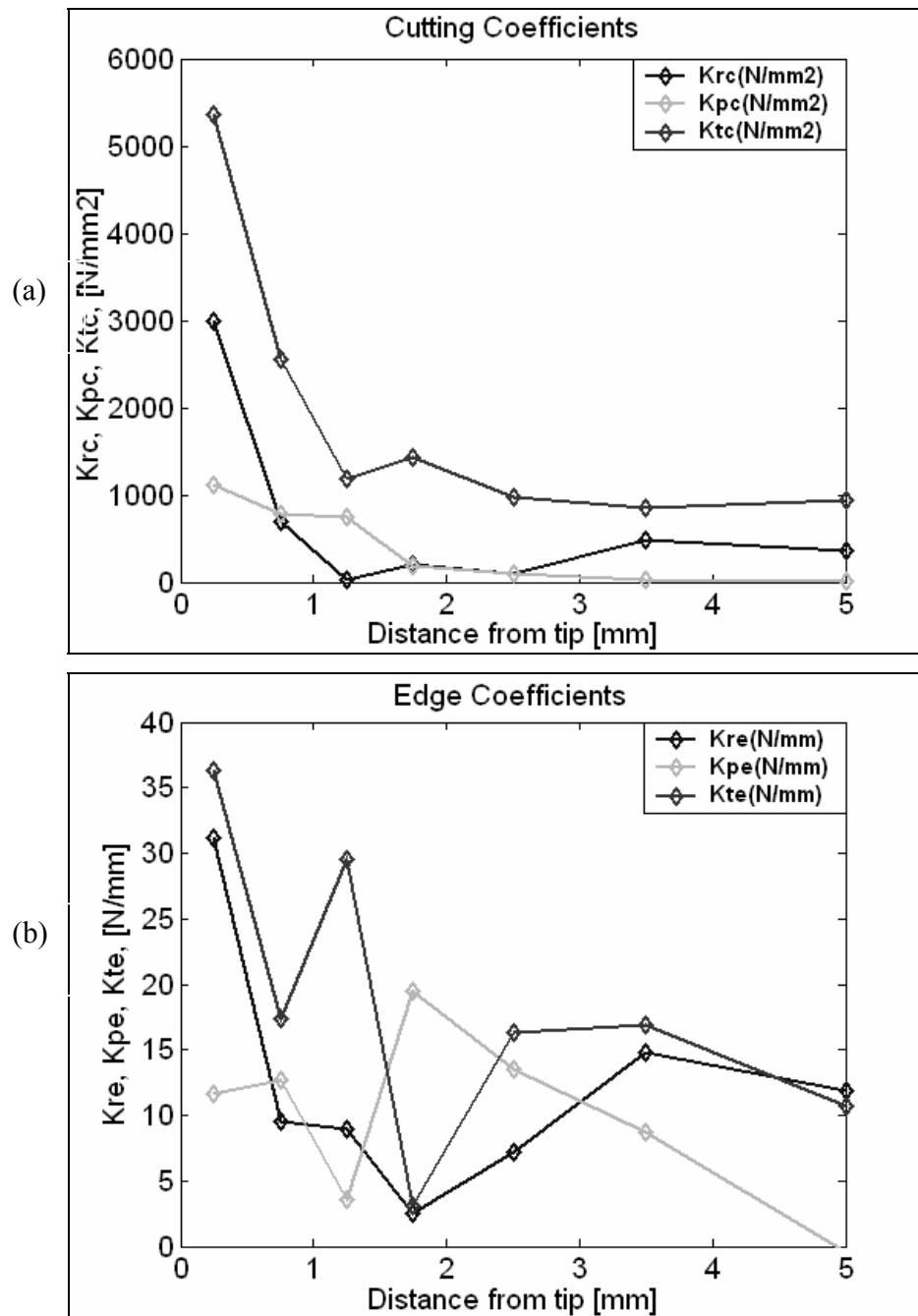


Figure 5.13: The change of coefficients along the cutting edge;
a) Cutting Coefficients, b) Edge coefficients

Chapter 6: MODIFICATION OF CALIBRATION COEFFICIENTS MODEL

6.1 Introduction

In the machining simulations of 3D free-form surfaces by ball-end milling, calibration coefficients play very critical role in force predictions. In other words, obtaining the calibration coefficients from calibration tests is a very influential process in the prediction of cutting forces. Accurately obtained calibration coefficients lead to better force predictions. In the literature the calibration coefficients are assumed to be independent of start and exit angles of the engagement region, thus they are assumed to be identical for any engagement region. Calibration coefficients are obtained from the horizontal slot cutting tests and these tests are repeated for different depth of cuts. In this chapter, in order to achieve more precise force predictions in free-form machining simulations, a new modification algorithm for calibration coefficients is presented. Most of the force models in the literature are for 2 ½D machining not for 3D free-form surfaces. In the analysis of literature, it is seen that force models, which have deficiency of excluding inclination angle dependency of the calibration coefficients, can handle the 3D free-form surface machining simulations by imposing the method stated in this chapter.

In this chapter, a new algorithm is presented for the calibration coefficients which are evaluated from the mechanistic approach to simulate the force in free-form machining. Most of the researches about ball-end milling are verified for 2½ D operations like half immersion cutting; bisection cutting and slot cutting. Free-form surface cutting are not performed very commonly in the literature due to difficulties in contact region determination, the chip thickness determination and in calibration process. In chapter 4, engagement region determination is analyzed for monotonic free-form surfaces. In this part of study, a new modification algorithm is presented for precise prediction of calibration coefficients in machining of free-form surfaces. Inclination dependency of calibration coefficients is stated mathematically, validation tests are performed for inclined surfaces and for 3D free-form surfaces. By the help of theoretical analysis and experimental force signals, it is shown that the inclination angle has a great importance in calibration and in force simulation of 3D free-form machining.

6.2 Determination of Calibration coefficients

In order to determine the cutting force constants in this study, Guzel and Lazoglu's mechanistic approach [35] is used. However, some modifications on calibration coefficients were performed as detailed in the following sections for better force predictions. The experiments for validation were performed on Mazak FJV-200 UHS Vertical Machining Center (VMC). The cutter was carbide ball-end mill cutter from CoroMill Plura series of Sandvik with 12 mm diameter, 37 mm projection length and 30 degrees helix angle. The workpiece materials were aluminum blocks (A17039) of size 250x170x38 [mm]. Kistler 3-component dynamometer (Model 9257B) was used for force data acquisition. Figure 6.1 is the combination of forces at the selected depth of cuts which were later to be used for obtaining the calibration coefficients for a feedrate of 48mm/min. The calibration algorithm of Guzel, Lazoglu [35] is used after this point for 7 depths of cut: 0-0.5, 0.5-1, 1-1.5, 1.5-2, 2-3, 3-4, 4-6 mm for feedrates of 48, 96, 144 and 192 at a spindle speed of 600 rpm and force calibration coefficients are presented in Table-5.1. The details of calibration process are given in Chapter 5.

6.3 Modification of Calibration Coefficients

The calibration coefficients obtained from horizontal slot cutting tests give accurate results for slot cutting conditions, on the other hand in free-form surface machining, the measured and simulated results do not match very precisely if one tries to use the modified Martelotti's [1] chip thickness given in Equation (6.1) where modified, not modified and experimental force signals are given in Figure A.6.

$$c_{kn} = h_x \sin(\theta) \sin(\psi) \quad (6.1)$$

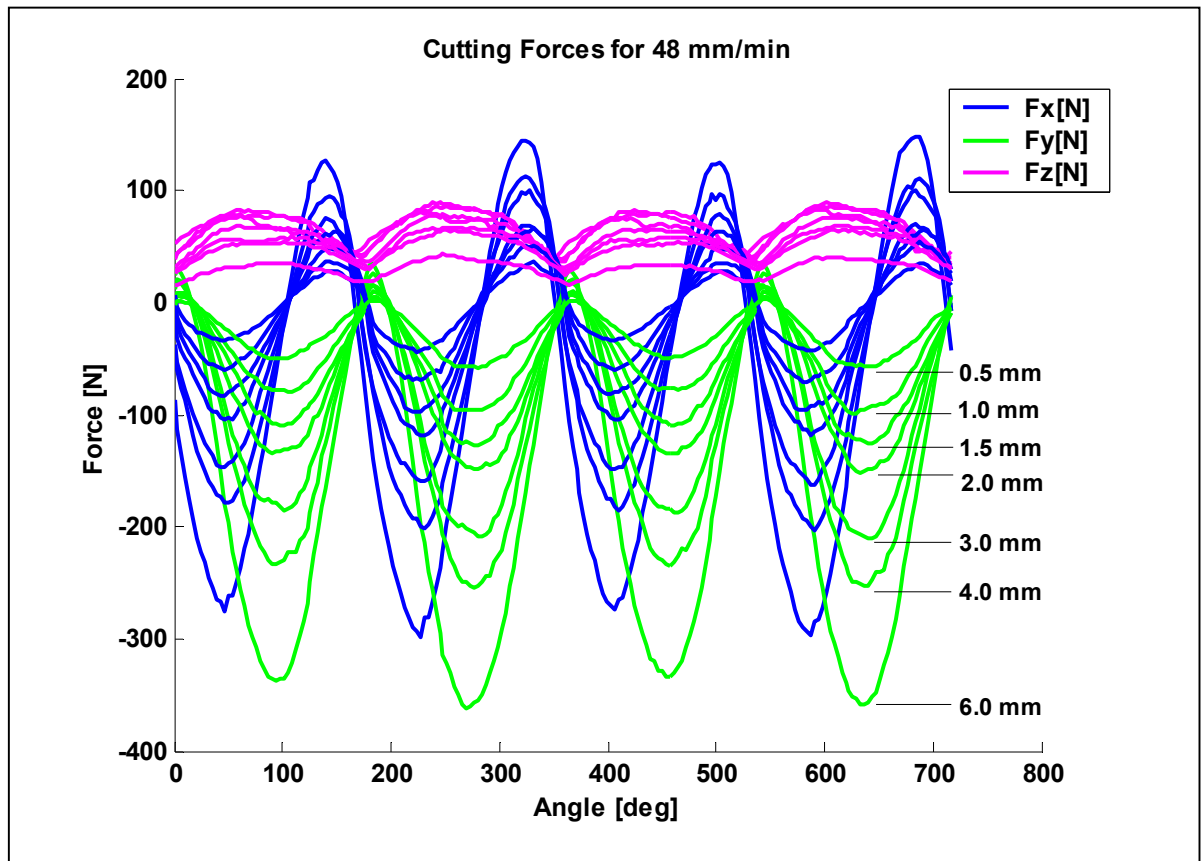


Figure 6.1 Combination of forces at selected depth of cuts for a feedrate of 48mm/min

where h is feed per tooth per revolution, θ is the rotation angle and ψ is the zenith angle (disc position angle) [35]. Martelotti's approach is simply a kinematic analysis of a point on the cutting edge. It includes rotational and translational motion and is valid if rotation vector and translational vectors are perpendicular. On the other hand, in free-form surfaces these two vectors are not always perpendicular as shown in Figure 6.2, therefore the calibration coefficients for different inclination angles are not valid. They need to be modified by including the inclination angles.

In this chapter, factors to modify the calibration coefficients which are obtained from horizontal slot cutting tests are evaluated from the end mill analytical equations. This is acceptable since the ball-end mill can be geometrically represented by end mills at different depths. In addition to that, if the terms which include zenith angle are evaluated at ninety degree, the ball-end mill force equations

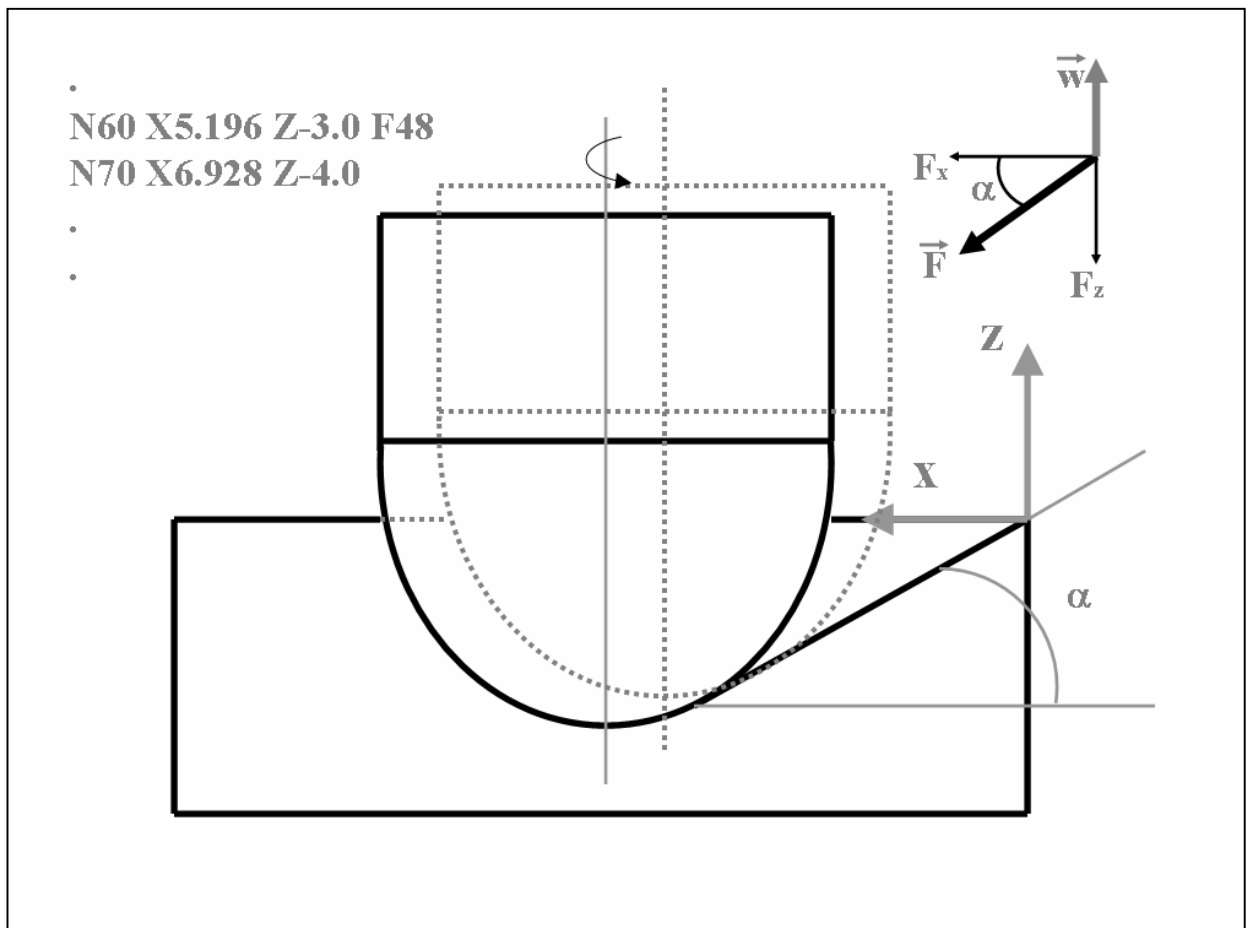


Figure 6.2 Feed direction in downward motion, rotational vector and inclination angle

[35] simplify to the end mill equations. Evaluation of zenith angle (ψ) at ninety degrees is simply a geometrical conversion of a ball-end mill to an end mill.

$$\begin{aligned}
 \bar{F}_x &= \left[\frac{Na c_{kn}}{8\pi} (K_{tc} \cos(2\theta) - K_{rc} (2\theta - \sin(2\theta))) + \frac{Na}{2\pi} (-K_{te} \sin(\theta) + K_{re} \cos(\theta)) \right]_{\phi_{st}}^{\phi_{ex}} \\
 \bar{F}_y &= \left[\frac{Na c_{kn}}{8\pi} (K_{tc} (2\theta - \sin(2\theta)) + K_{rc} \cos(2\theta)) - \frac{Na}{2\pi} (K_{te} \cos(\theta) + K_{re} \sin(\theta)) \right]_{\phi_{st}}^{\phi_{ex}} \\
 \bar{F}_z &= \left[\frac{Na}{2\pi} (-K_{ac} c_{kn} \cos(\theta) + K_{ae} \theta) \right]_{\phi_{st}}^{\phi_{ex}}
 \end{aligned} \tag{6.2}$$

Equation (6.2) is the analytical equation of average end mill forces stated by Altintas [9]. The calibration coefficients can be obtained by considering horizontal slot cutting tests which means start angle is 0° and exit angle is 180° .

$$\left. \begin{aligned}
 \bar{F}_x &= \left[-\frac{Na}{4} (K_{rc} c_{kn}) - \frac{Na}{\pi} (K_{re}) \right] \\
 \bar{F}_y &= \left[\frac{Na}{4} (K_{tc} c_{kn}) + \frac{Na}{\pi} (K_{te}) \right] \\
 \bar{F}_z &= \left[\frac{Na}{\pi} (K_{ac} c_{kn}) + \frac{Na}{2} (K_{ae}) \right]
 \end{aligned} \right\} \bar{F}_q = \bar{F}_{qc} c_{kn} + \bar{F}_{qe} \tag{6.3}$$

From equation (6.3), calibration coefficients can be written as;

$$\begin{aligned}
 K_{tc} &= \frac{4\bar{F}_{yc}}{Na} & K_{te} &= \frac{4\bar{F}_{ye}}{Na} \\
 K_{rc} &= -\frac{4\bar{F}_{xc}}{Na} & K_{re} &= -\frac{4\bar{F}_{xc}}{Na} \\
 K_{\psi c} &= \frac{\pi\bar{F}_{zc}}{Na} & K_{\psi e} &= \frac{2\bar{F}_{ze}}{Na}
 \end{aligned} \tag{6.4}$$

This slot cutting calibration coefficients can be used effectively in end milling processes. On the other hand, since calibration coefficients are dependent on the engagement angles they need to be modified according to the inclination angle of the surface in ball-end milling. In order to analyze this situation, the calibration coefficients which can be derived from equation (6.2) need to be used instead of the calibration coefficients which are obtained from equation (6.3). However, the conflict to use the equation (6.2) is that which start and exit angles are going to be taken since they are not constant throughout the engagement domains unlike horizontal slot cutting.

$$\begin{aligned}
 K_{rc}^{new} &= \frac{\bar{F}_{xc} + \frac{A_1}{A_2} \bar{F}_{yc}}{\left(A_2 - \frac{A_1^2}{A_2}\right)}, K_{tc}^{new} = \frac{\bar{F}_{xc} + \frac{A_2}{A_1} \bar{F}_{yc}}{\left(A_1 - \frac{A_2^2}{A_1}\right)}, K_{\psi c}^{new} = -\frac{\bar{F}_{zc}}{A_4} \\
 K_{re}^{new} &= \frac{\bar{F}_{xe} + \frac{A_3}{A_4} \bar{F}_{ye}}{\left(A_4 + \frac{A_3^2}{A_4}\right)}, K_{te}^{new} = \frac{\bar{F}_{xe} - \frac{A_4}{A_3} \bar{F}_{ye}}{\left(A_3 + \frac{A_4^2}{A_3}\right)}, K_{\psi e}^{new} = \frac{\bar{F}_{ze}}{A_5}
 \end{aligned} \tag{6.5}$$

where

$$\begin{aligned}
 A_1 &= \left[\frac{Na}{8\pi} \cos(2\theta) \right]_{\phi_{st}}^{\phi_{ex}} \\
 A_2 &= \left[-\frac{Na}{8\pi} (2\theta - \sin(2\theta)) \right]_{\phi_{st}}^{\phi_{ex}} \\
 A_3 &= \left[-\frac{Na}{2\pi} \sin(\theta) \right]_{\phi_{st}}^{\phi_{ex}} \\
 A_4 &= \left[\frac{Na}{2\pi} (\cos(\theta)) \right]_{\phi_{st}}^{\phi_{ex}} \\
 A_5 &= \left[\frac{Na}{2\pi} (\theta) \right]_{\phi_{st}}^{\phi_{ex}}
 \end{aligned}$$

Second disadvantage is that the calibration process must be repeated for different inclination angles, thus the time and work spent for calibration increase. For this reason, a modification of

calibration coefficient factor is introduced by considering equations (6.2) and (6.3). This factor is going to be used to modify calibration coefficients which are obtained by slot cutting calibration tests. The equations for new calibration coefficients are as given in equation (6.5).

In order to handle the problem about the values of start and exit angles that are going to be put into equation (6.2) or equation (6.5), one can take the average of these angles, in whole spectrum respect to all disc elements which are in contact at that depth of cut. More conveniently average of start and exit angles can be taken in the intervals of which calibration coefficients are valid (inclined slot cutting is assumed). In this chapter, since calibration coefficients are taken for seven depths of cut, the average of start and exit angles are taken at these seven intervals. In chapter 4, the start and exit points are presented, in addition to that for an inclined slot cutting test shown in Figure 6.3, start and exit angles are as follows;

$$\begin{aligned}
 \bar{\phi}_{st} &= \frac{\sum_{i=1}^F \tan^{-1}\left(\frac{y}{x}\right)}{F} \\
 \bar{\phi}_{ex} &= \frac{\sum_{i=1}^F \tan^{-1}\left(-\frac{y}{x}\right)}{F} \\
 x &= \frac{r_i^2 - R_2^2}{1 - \left(\frac{R_2}{R}\right)^2} \\
 y &= \sqrt{r_i^2 - x^2} \\
 r_i &= \sqrt{R^2 - (R - z_i)^2} \\
 z_i &= \frac{R}{F} i \\
 R_2 &= R \sin(\lambda) \\
 i &= 1 \dots N_d
 \end{aligned} \tag{6.6}$$

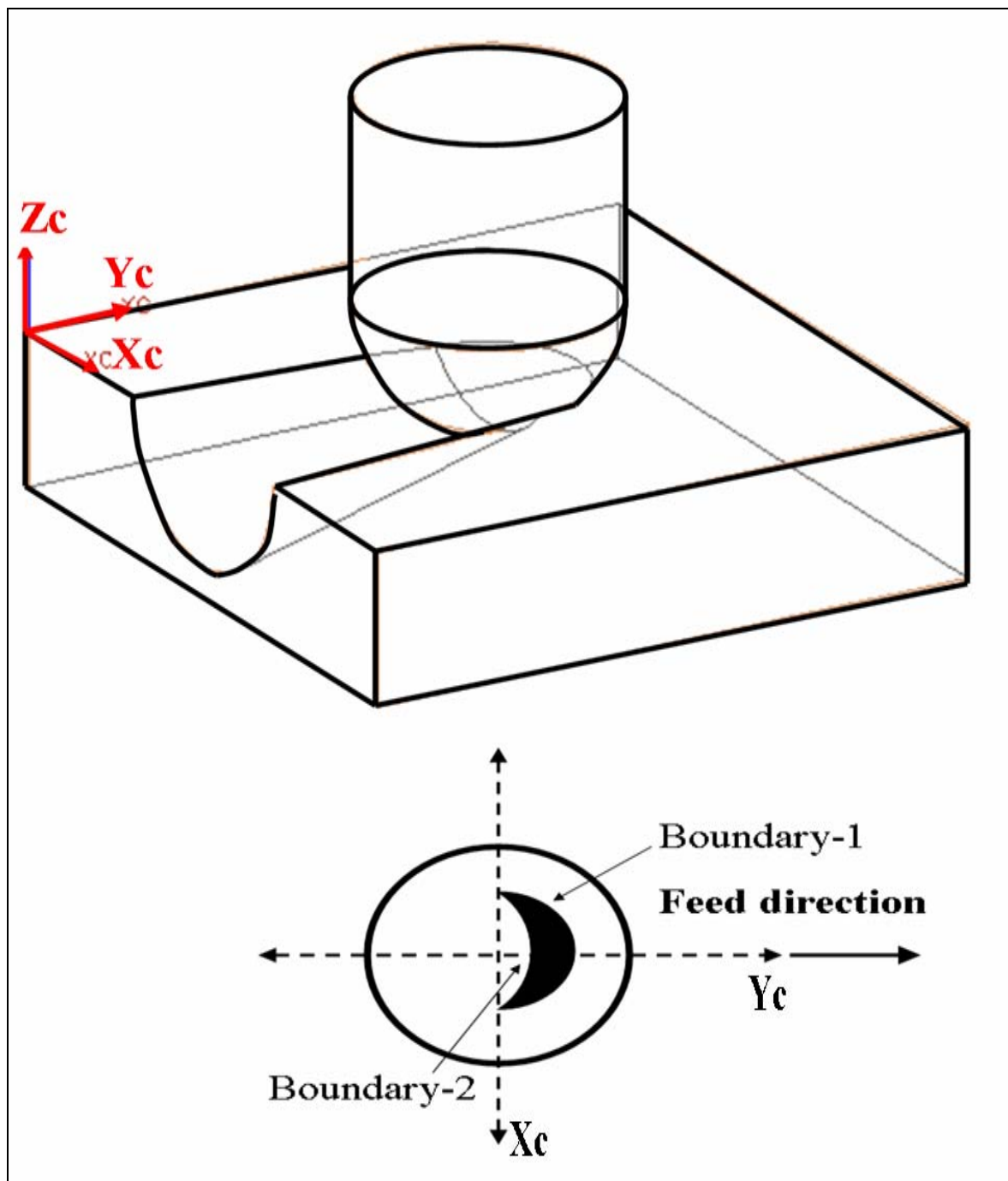


Figure 6.3 Inclined slot cutting test

where λ is the inclination angle, x, y are the intersection points of the boundaries with each disc element that are going to be used to define $\bar{\phi}_{st}$ (average start angle) and $\bar{\phi}_{ex}$ (average exit angle) and N_d is the total disc number in contact at that depth of cut.

If calibration tests are proceeded with inclined slot cutting at different inclination angles then; $\phi_{ex} = \pi - \phi_{st}$ and thus makes some simplification in the terms given in equation (6.5). $A_1 = 0$ and $A_3 = 0$ at these test conditions, which means they are not inclination dependent terms for inclined slot cutting tests.

From equation (6.5) evaluating the terms at the given condition leads to;

$$\begin{aligned} A_2 &= -\frac{Na}{4\pi}((\pi - 2\phi_{st}) + \sin(2\phi_{st})) \\ A_4 &= -\frac{Na}{\pi} \cos(\phi_{st}) \\ A_5 &= \frac{Na}{2\pi}(\pi - 2\phi_{st}) \end{aligned} \quad (6.7)$$

If one defines the modification factors as the ratio of inclined cutting calibration coefficients to the slot cutting calibration coefficients, then below equations will state the modification of calibration coefficient factors.

$$\begin{aligned} K_{tc}^{old} &= \frac{4\bar{F}_{yc}^{old}}{Na} \rightarrow K_{tc}^{new} = -\frac{\bar{F}_{yc}^{new}}{A_2} \\ K_{tc}^c &= \frac{K_{tc}^{new}}{K_{tc}^{old}}, K_{rc}^c = \frac{K_{rc}^{new}}{K_{rc}^{old}}, K_{\psi c}^c = \frac{K_{\psi c}^{new}}{K_{\psi c}^{old}} \text{ where } K_{rc}^{old} = -\frac{4\bar{F}_{xc}^{old}}{Na} \rightarrow K_{rc}^{new} = \frac{\bar{F}_{xc}^{new}}{A_2} \\ K_{\psi c}^{old} &= \frac{\pi\bar{F}_{zc}^{old}}{Na} \rightarrow K_{\psi c}^{new} = -\frac{\bar{F}_{zc}^{new}}{A_4} \\ K_{te}^{old} &= \frac{4\bar{F}_{ye}^{old}}{Na} \rightarrow K_{te}^{new} = -\frac{\bar{F}_{ye}^{new}}{A_4} \\ K_{te}^c &= \frac{K_{te}^{new}}{K_{te}^{old}}, K_{re}^c = \frac{K_{re}^{new}}{K_{re}^{old}}, K_{\psi e}^c = \frac{K_{\psi e}^{new}}{K_{\psi e}^{old}} \text{ where } K_{re}^{old} = -\frac{4\bar{F}_{xc}^{old}}{Na} \rightarrow K_{re}^{new} = \frac{\bar{F}_{xe}^{new}}{A_4} \\ K_{\psi e}^{old} &= \frac{2\bar{F}_{ze}^{old}}{Na} \rightarrow K_{\psi e}^{new} = \frac{\bar{F}_{ze}^{new}}{A_5} \end{aligned} \quad (6.8)$$

Equation (6.7) simplifies the equation (6.8) and one can show that;

$$\begin{aligned}
 K_{tc}^c &= \frac{\pi}{((\pi - 2\phi_{st}) + \sin(2\phi_{st}))} \frac{\bar{F}_{yc}^{new}}{\bar{F}_{yc}^{old}}, & K_{rc}^c &= \frac{\pi}{((\pi - 2\phi_{st}) + \sin(2\phi_{st}))} \frac{\bar{F}_{xc}^{new}}{\bar{F}_{xc}^{old}} \\
 K_{\psi c}^c &= \frac{1}{\cos(\phi_{st})} \frac{\bar{F}_{zc}^{new}}{\bar{F}_{zc}^{old}}, & K_{te}^c &= \frac{\pi}{4\cos(\phi_{st})} \frac{\bar{F}_{ye}^{new}}{\bar{F}_{ye}^{old}} \\
 K_{re}^c &= \frac{\pi}{4\cos(\phi_{st})} \frac{\bar{F}_{xe}^{new}}{\bar{F}_{xe}^{old}}, & K_{\psi e}^c &= \frac{\pi}{(\pi - 2\phi_{st})} \frac{\bar{F}_{ze}^{new}}{\bar{F}_{ze}^{old}}
 \end{aligned} \tag{6.9}$$

6.4 Validations with Inclined Slot Cutting Tests

In the validation of modification of calibration coefficient method and analytical engagement region, slot cutting experiments are performed for different inclination angles which are $-20^\circ, -10^\circ, -5^\circ, 5^\circ, 10^\circ, 20^\circ$. Figures 7.1-7.6 include measured force signals from slot cutting tests and modification algorithm force predictions. All slot validation tests and free-form tests are performed with the same workpiece and same cutter stated in determination of calibration coefficients part. The engagement domains for slot cutting tests are taken from ACRsim program [10] and simulations are performed with these domains. ACRsim gives an output in the form of number of CL-points, start angle, exit angle and inclination angle. Denotation for start and exit angles are given in Figure 6.4 and a sample output of ACRsim is given in Figure 6.5. Therefore inclination angle information comes from the domain and force simulation code uses this information to select correct coefficient modification factor matrix, it modifies calibration matrix accordingly.

Figures 7.1-7.6 are the slot cutting results for positively and negatively inclined surfaces for a feedrate of 48 mm/min at a spindle speed of 600 rpm. With the increase of the inclination angle simulated results with two methods are getting away from each other and from experimental data, however by using the modification of calibration method the experimental data is predicted with an

error below 10%. In Figures 7.1-7.6, there are dashed lines which show boundaries of the approach and retract regions of the cutter in the toolpath where the analytical solutions stated in chapter 4 are not valid outside. Chapter 4 is the analytical solution of full contact cases. Thus the error percentage values are evaluated for the values between these dashed lines. In addition to that, if the factors of modification of calibration coefficient method are independent of the workpiece material, one experiment will be sufficient to predict the change in calibration coefficients for different types of materials.

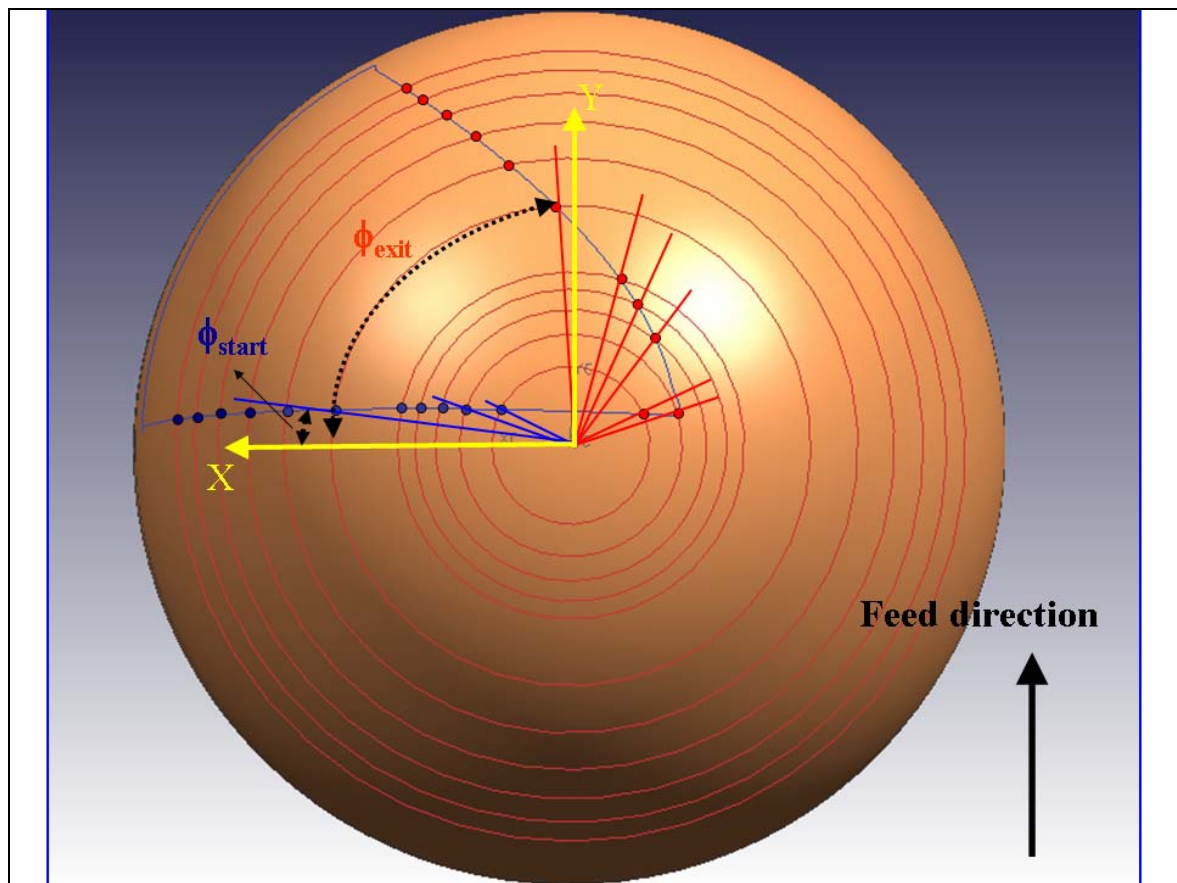


Figure 6.4 Denotations of start and exit angles

In Figure 6.6, U is the calibration coefficient matrix where U_{λ} is the coefficient matrix modified according to the inclination angle λ . U_{slot} is the calibration coefficient matrix obtained from horizontal slot cutting tests. In addition to that, as shown in Figure 6.6 there is an identical linear trend observed for the modified calibration coefficient matrices for all calibration feedrates. Thus, there is no need for a discretization of inclination angles, a linear behavior can be assumed between the angles $-20^\circ, -10^\circ, -5^\circ, 5^\circ, 10^\circ, 20^\circ$.

In the validation of the method for free-form surfaces, two tests are performed for a feedrate of 48 mm/min at a spindle speed of 600 rpm. The material used in these tests is the same material with the calibration tests and tests are performed with the same cutter of the same projection length and helix angle. The predictions in these figures are again valid for the portion between two dashed lines. In free-form tests, errors are below 10% at maximum. Calculation time of engagement domains for airfoil and for sinusoidal surface were 9.1 seconds and 26.9 seconds, respectively, with MATLAB (CPU time) on a Pentium M 1.50 GHz -512 MB RAM. Therefore, the algorithm developed here in this chapter is quite fast, and computation time is very acceptable for industrial implementations.

6.5 Discussions

In this chapter, in order to achieve more precise force predictions in 3D free-form machining processes, a new modification algorithm for calibration coefficients were developed. With the theoretical analysis and force measurements, it was shown that the inclination angle has a great importance in calibration and in force simulation of 3D free-form machining. With the new calibration algorithm including the local inclination angle at every cutter location point along the tool path in CNC code, the cutting force predictions and 3D free-form process simulations were enhanced.

	1	2	3	4	5	6	7
56	1	5.6	0	0	0	0	-0.17453
57	1	5.7	0	0	0	0	-0.17453
58	1	5.8	0	0	0	0	-0.17453
59	1	5.9	0	0	0	0	-0.17453
60	1	6	0	0	0	0	-0.17453
61	2	0.1	-1.0714	4.213	1.5708	1.5708	-0.17453
62	2	0.2	-0.6596	3.8012	1.5708	1.5708	-0.17453
63	2	0.3	-0.5165	3.6581	1.5708	1.5708	-0.17453
64	2	0.4	-0.4356	3.5772	1.5708	1.5708	-0.17453
65	2	0.5	-0.3815	3.5231	1.5708	1.5708	-0.17453
66	2	0.6	-0.3418	3.4833	0.6391	2.5025	-0.17453
67	2	0.7	-0.3109	3.4524	0.4411	2.7005	-0.17453
68	2	0.8	-0.2858	3.4274	0.2381	2.9035	-0.17453
69	2	0.9	-0.265	3.4066	0.0425	3.0991	-0.17453
70	2	1	-0.2472	3.3888	0.1352	3.0064	-0.17453
71	2	1.1	0	0	0	0	-0.17453
72	2	1.2	0	0	0	0	-0.17453
73	2	1.3	0	0	0	0	-0.17453
74	2	1.4	0	0	0	0	-0.17453
75	2	1.5	0	0	0	0	-0.17453
76	2	1.6	0	0	0	0	-0.17453
77	2	1.7	0	0	0	0	-0.17453
78	2	1.8	0	0	0	0	-0.17453
79	2	1.9	0	0	0	0	-0.17453
80	2	2	0	0	0	0	-0.17453
81	2	2.1	0	0	0	0	-0.17453
82	2	2.2	0	0	0	0	-0.17453
83	2	2.3	0	0	0	0	-0.17453

Figure 6.5 Sample output of ACRsim

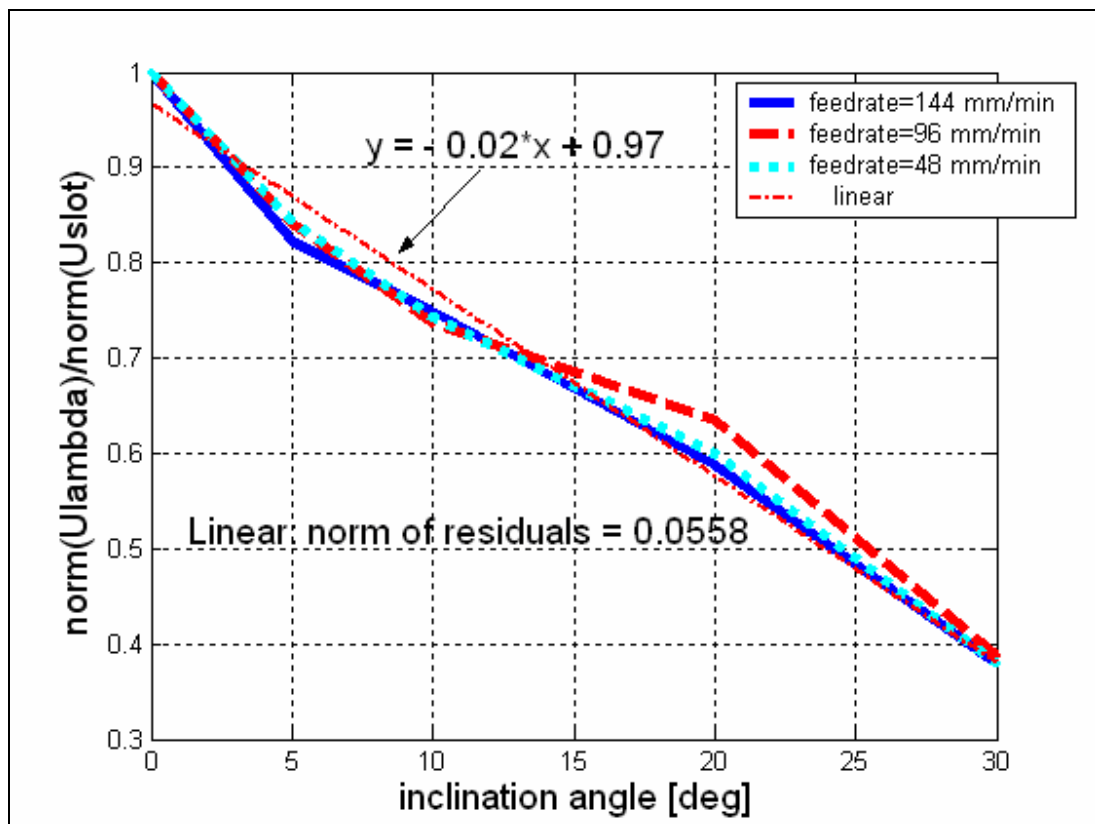


Figure 6.6: Linear trend in the modified calibration coefficient matrices

Chapter 7: VALIDATION TESTS

7.1 Introduction

Various tests have been performed for validation of the proposed engagement model and modification model. Inclined slot cutting tests are performed initially and more complicated free-form cases have been investigated once satisfactory results have been obtained in slot cutting tests.

The same experimental setup in calibration has been used in validation tests. The feed rate of the cutter has been chosen to be 48 mm/min in the tests. Cutting has been realized only by ball part of the cutter in all cases. The disc thickness in simulations was selected to be 0.1 mm which adequately resembled the actual cutting conditions for the mathematical force model.

The applied conditions and results of the performed cutting tests are described in this chapter. The predicted workpiece topography is given in all cases together with the predicted and measured cutting forces.

7.2 Inclined Slot Cutting Tests

7.2.1 5° Upward Slot Cutting Test

Inclined slot cutting tests are the simplest type of cut rather than $2\frac{1}{2}$ D cuts. These tests are important and helpful to understand the free-form machining with ball-end mill.

Slots of various inclination angles have been cut in order to test the models. Cutting has been realized by the ball part of the cutter only. Cutting conditions are summarized in Table 7.1. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.1.

Table 7.1 Cutting conditions for 5° upward slot cutting test

Test no.	1
Feed rate	48 mm/min
Depth of cuts	Starting from 5.9887 mm to 0 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	68.58 mm along y-direction 5° upward motion

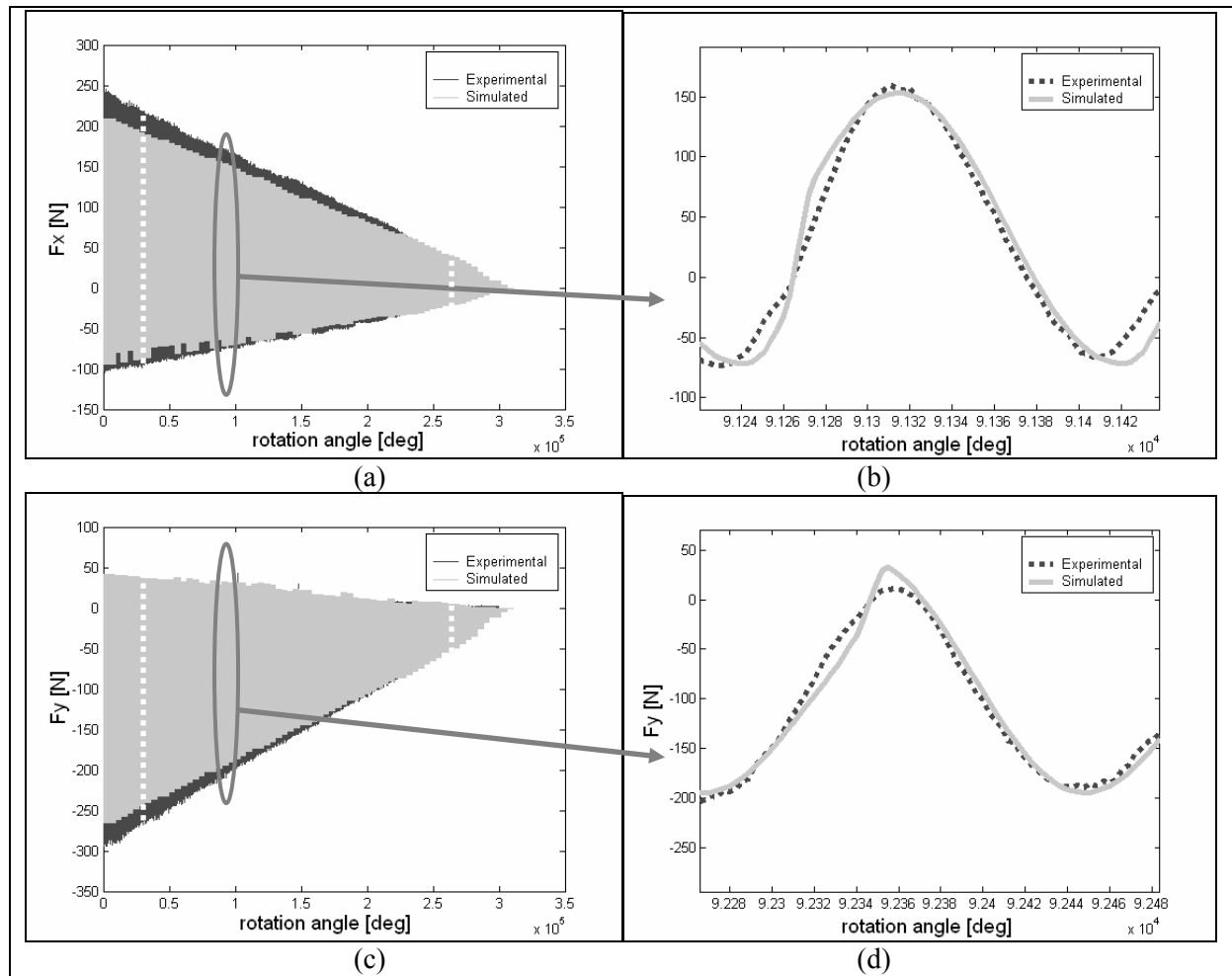


Figure 7.1: Forces in 5° upward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

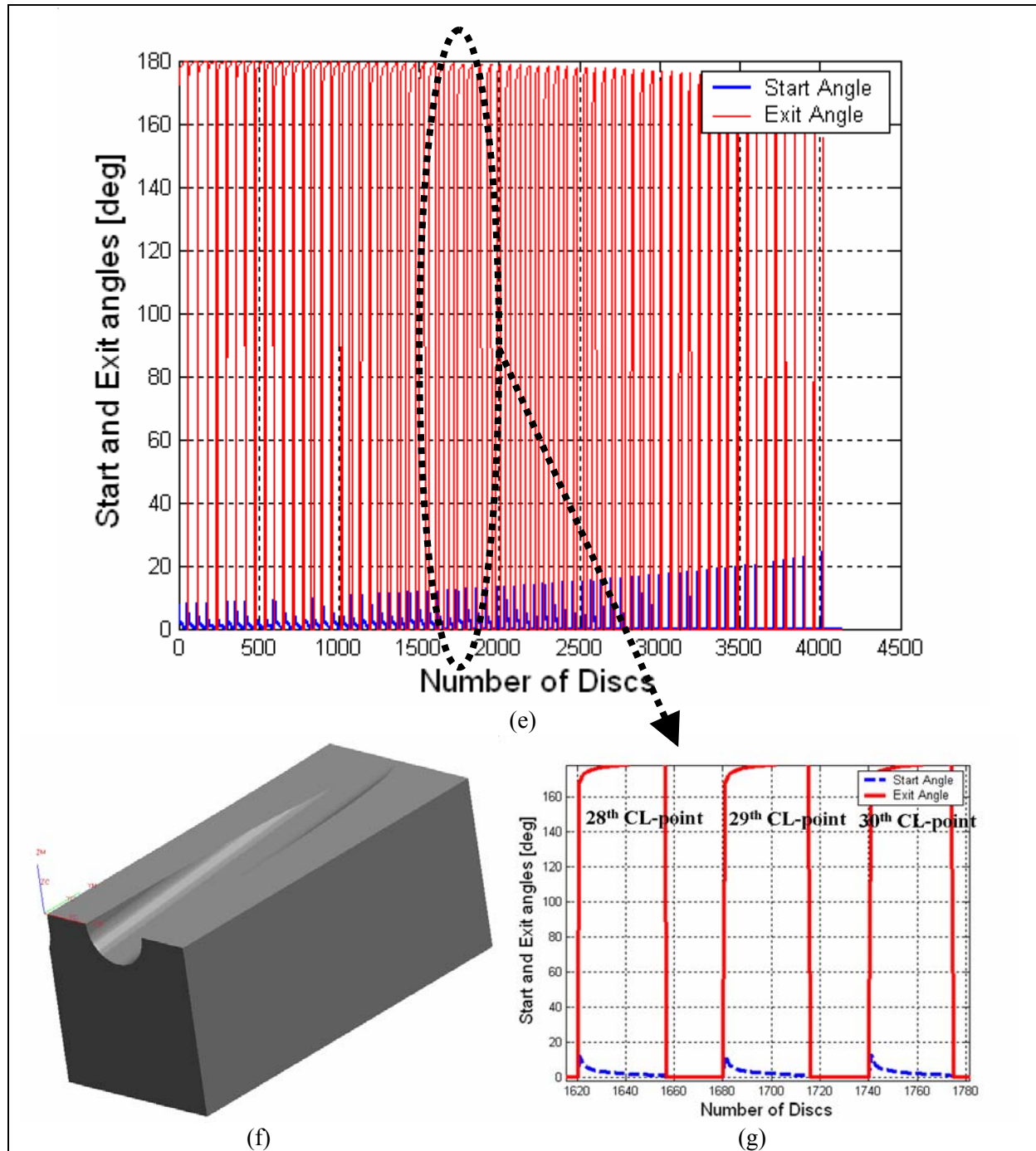


Figure 7.1: Forces in 5° upward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.1 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees. The white dash lines are the boundaries of the region that the simulation is valid inside and not valid at outside. The boundaries separate the complete cut into three regions

which are namely approaching, full contact and retracting regions. In approaching region the ball-end mill starts to enter into the workpiece in the direction of feedrate. The full contact region is the valid region of engagement model and lastly the retracting region is the region where the ball-end mill leaves the workpiece. If all the toolpath is denoted as L and the radius of the cutter is denoted as R , approaching, full contact and retracting regions sweep a domain of $[0, R]$, $[R, L-R]$, $[L-R, L]$ in the principal axis of feedrate direction at the coarse case. Since the engagement model is valid for full contact region, the simulated forces and experimental forces are compared in this region.

7.2.2 10° Upward Slot Cutting Test

After 5° upward slot cutting test, 10° upward slot cutting test is performed. Cutting conditions are summarized in Table 7.2. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.2.

Table 7.2 Cutting conditions for 10° upward slot cutting test

Test no.	2
Feed rate	48 mm/min
Depth of cuts	Starting from 5.9074 mm to 0 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	34.03 mm along y-direction 10° upward motion

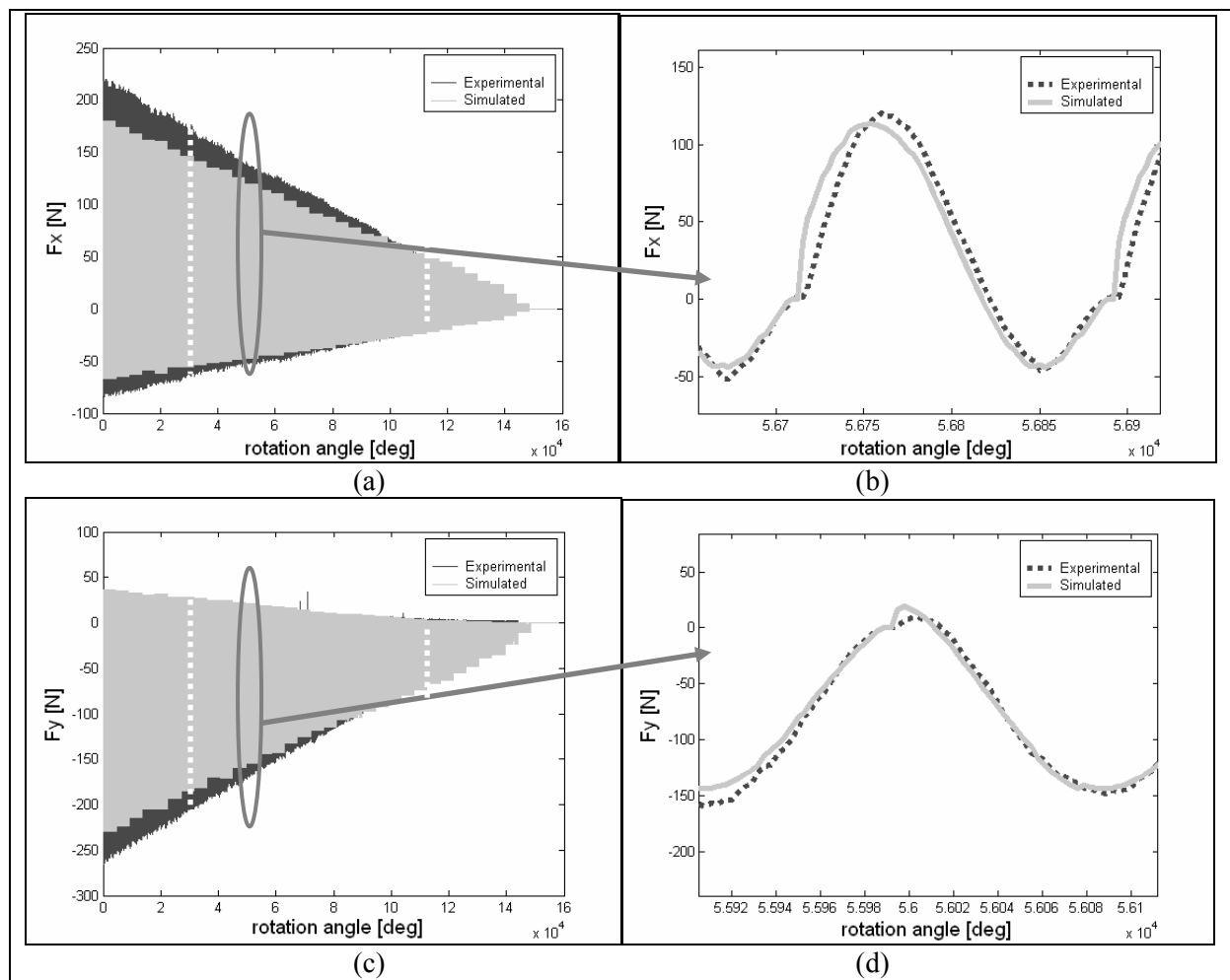


Figure 7.2: Forces in 10° upward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

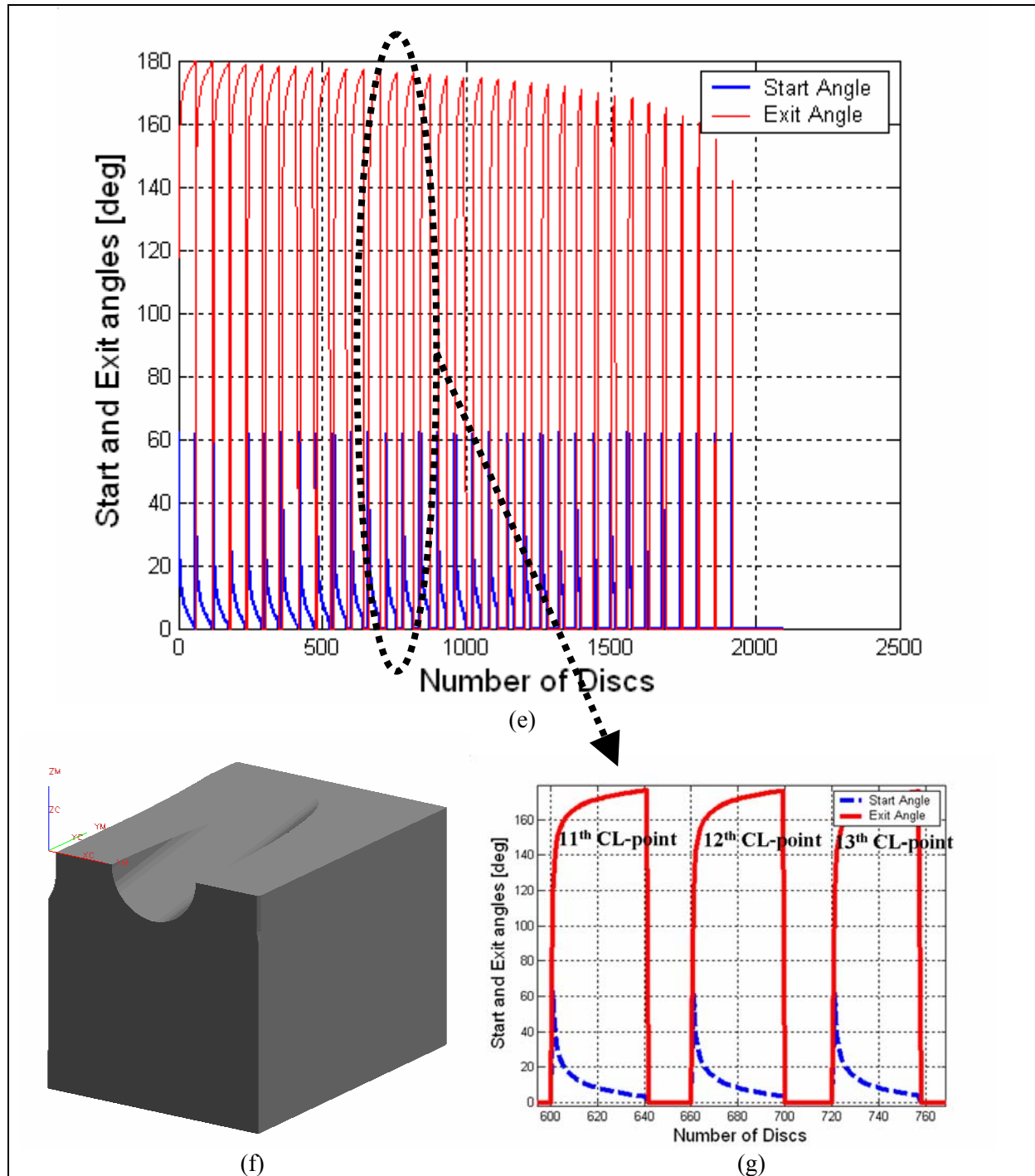


Figure 7.2: Forces in 10° upward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.2 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.2.3 20° Upward Slot Cutting Test

A more steep case of inclined slot cutting tests is performed by setting the inclination angle as 20°. Cutting conditions are summarized in Table 7.3. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.3.

Table 7.3 Cutting conditions for 20° upward slot cutting test

Test no.	3
Feed rate	48 mm/min
Depth of cuts	Starting from 5.6149 mm to 0 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	16.49 mm along y-direction 20° upward motion

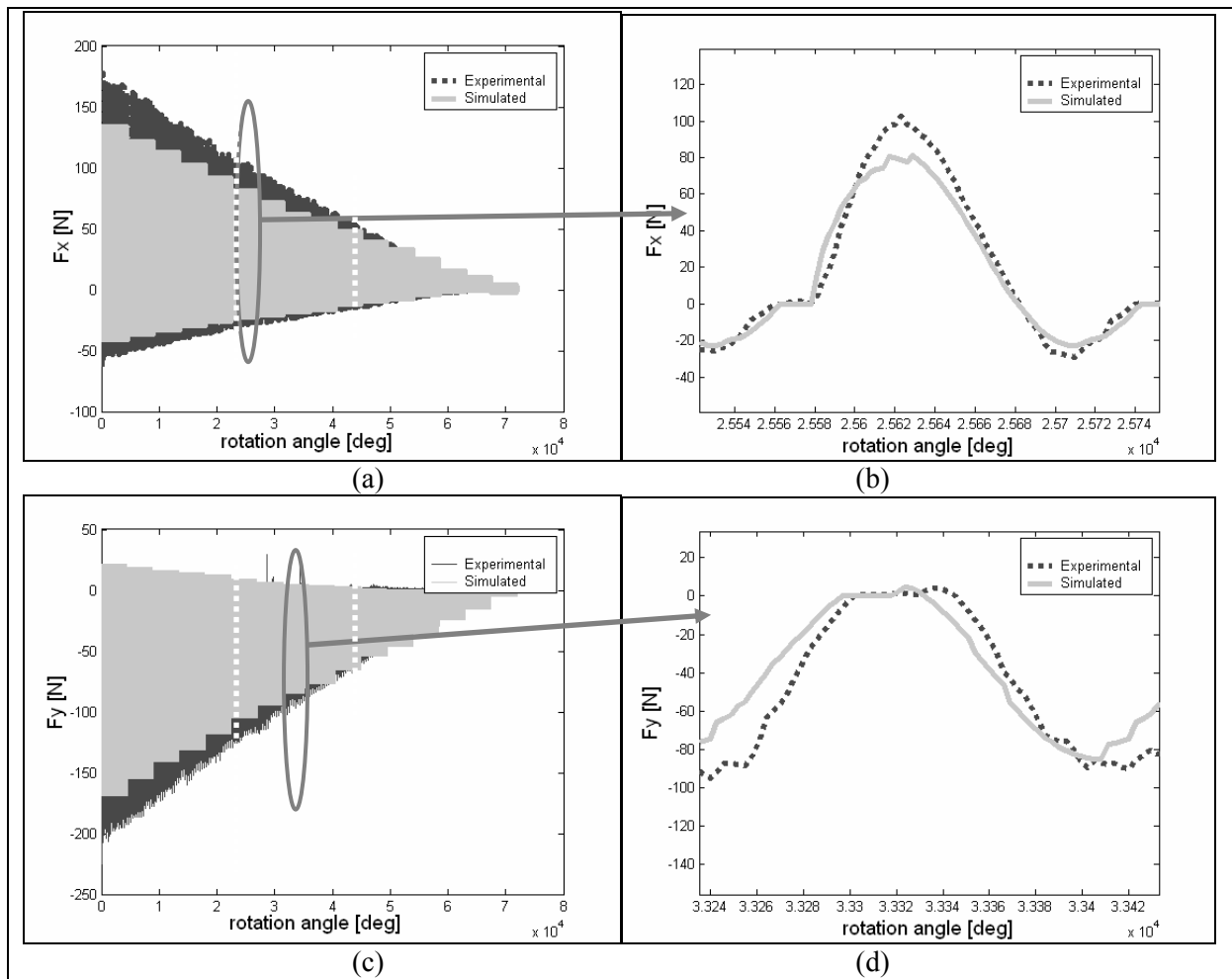


Figure 7.3: Forces in 20° upward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

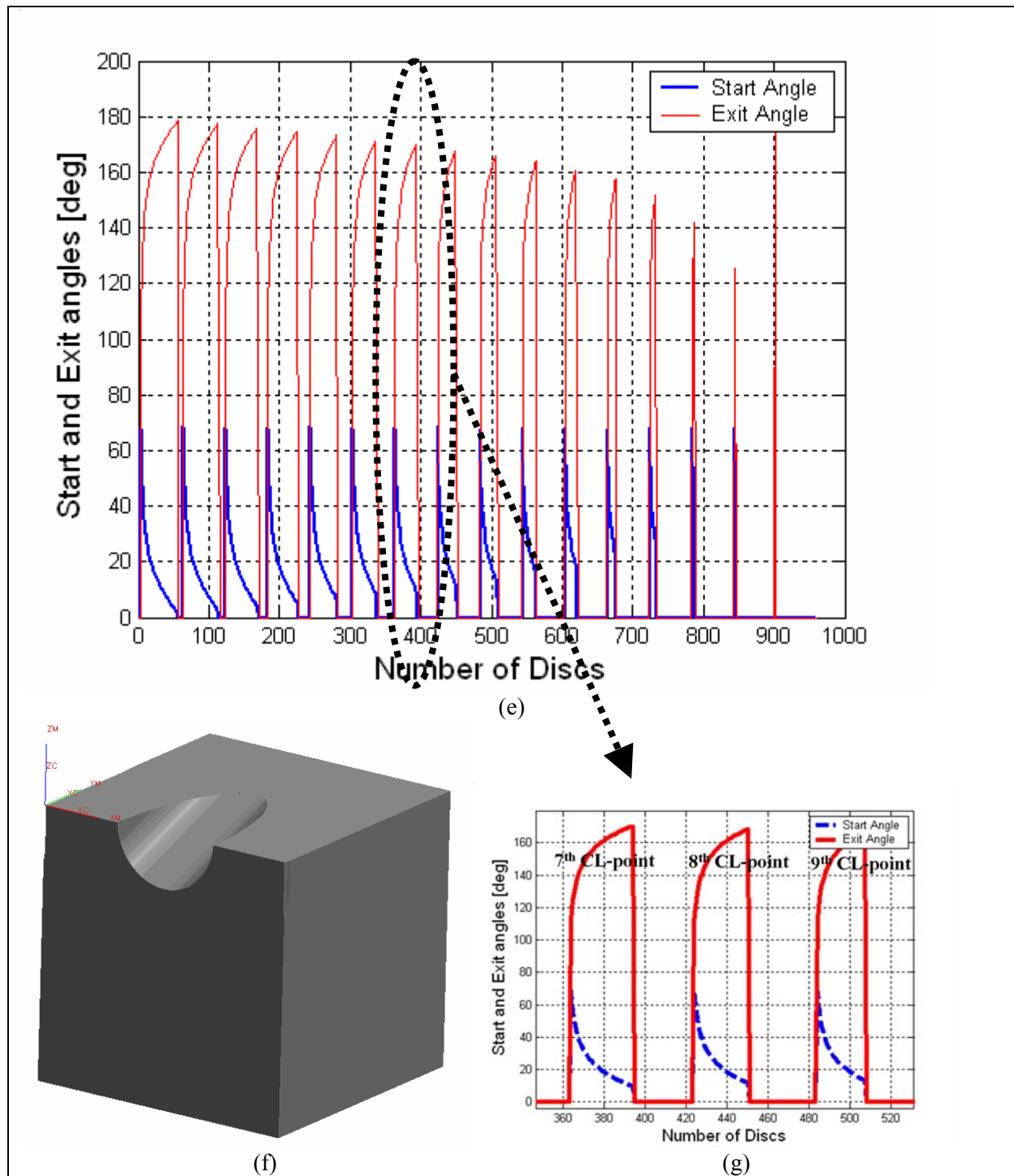


Figure 7.3: Forces in 20° upward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.3 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.2.4 5° Downward Slot Cutting Test

After finishing the upward cases, starting from 5°, downward slot cutting tests are performed. Cutting conditions are summarized in Table 7.4. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.4.

Table 7.4 Cutting conditions for 5° downward slot cutting test

Test no.	4
Feed rate	48 mm/min
Depth of cuts	Starting from 0 mm to 5.9887 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	68.58 mm along y-direction 5° downward motion

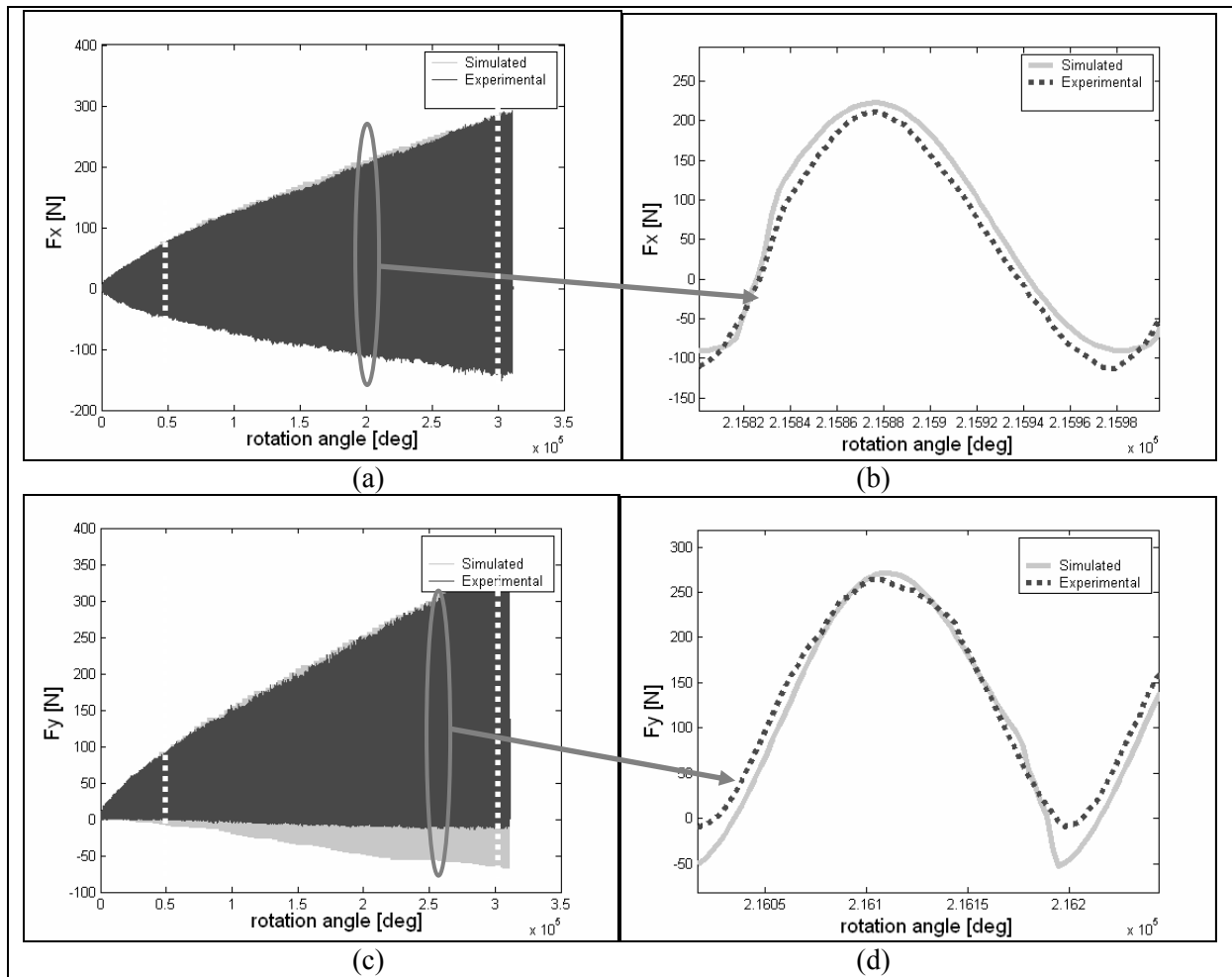


Figure 7.4: Forces in 5° downward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

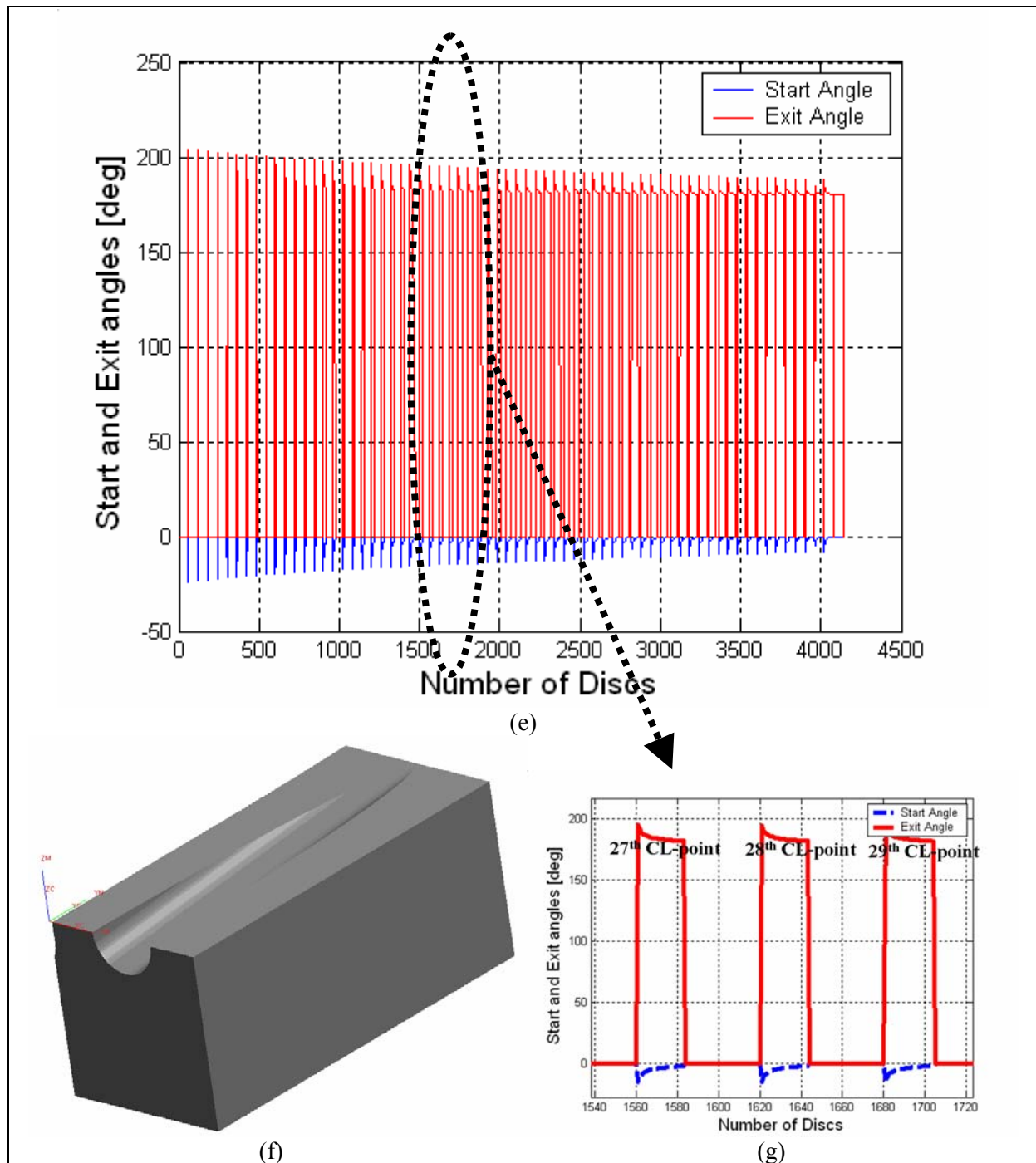


Figure 7.4: Forces in 5° downward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.4 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.2.5 10° Downward Slot Cutting Test

After 5° downward slot cutting test, 10° downward slot cutting test is performed. Cutting conditions are summarized in Table 7.5. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.5.

Table 7.5 Cutting conditions for 10° downward slot cutting test

Test no.	5
Feed rate	48 mm/min
Depth of cuts	Starting from 0 mm to 5.9074 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	34.03 mm along y-direction 10° downward motion

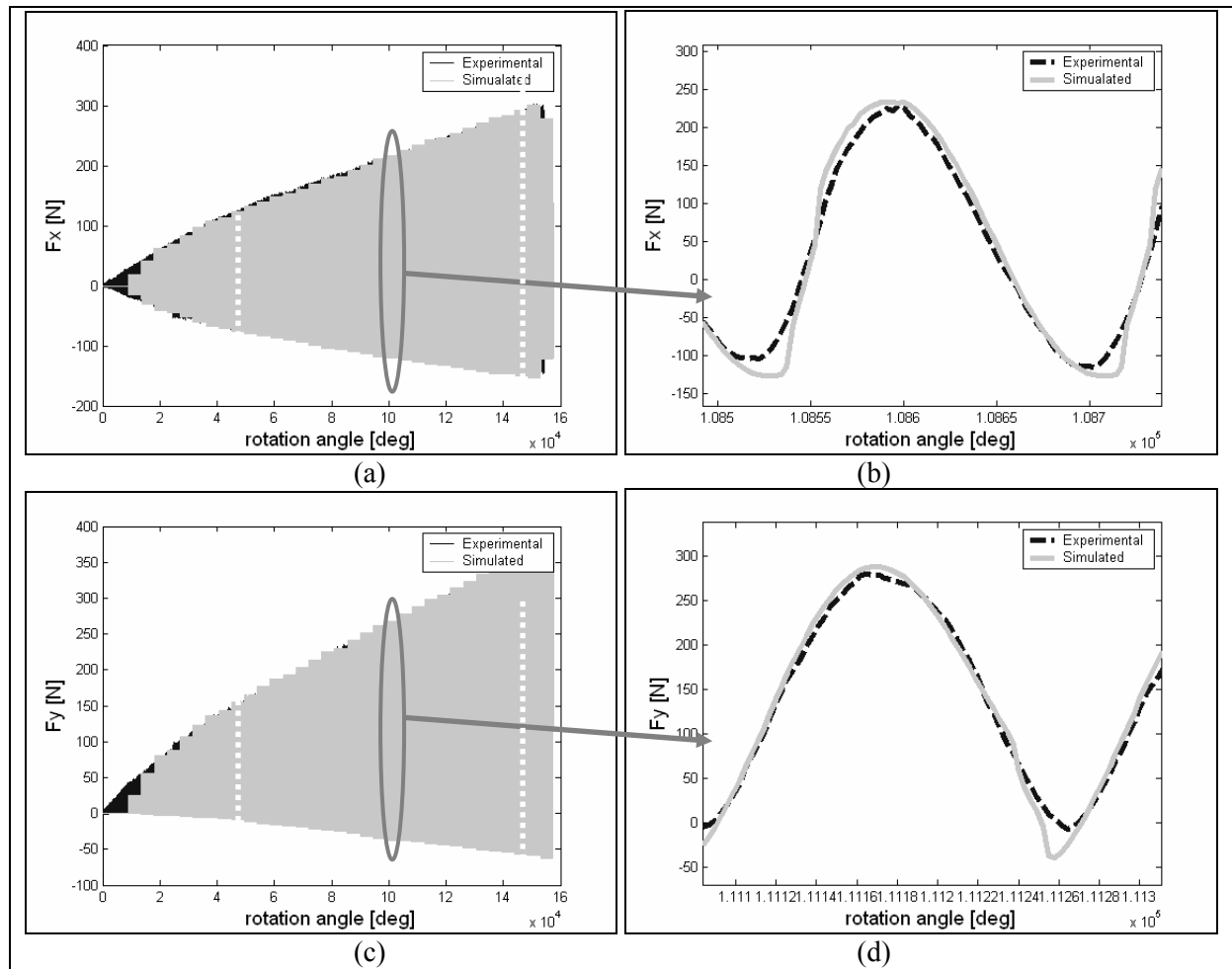


Figure 7.5: Forces in 10° downward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

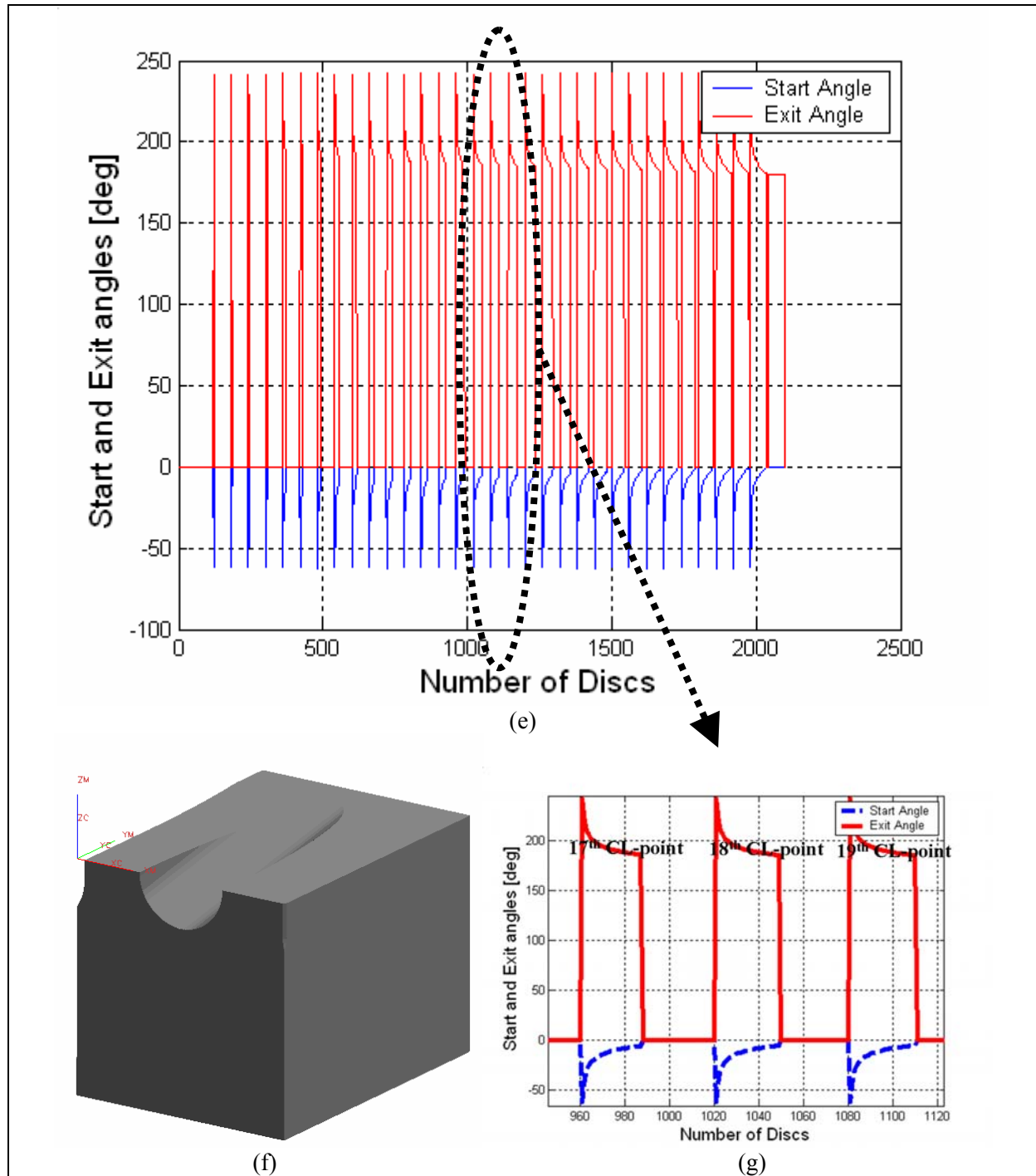


Figure 7.5: Forces in 10° downward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement. Predicted and measured forces are plotted versus rotation angle in Figure 7.5 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.2.6 20° Downward Slot Cutting Test

A more steep case of negatively inclined slot cutting tests is performed by setting the inclination as 20°. Cutting conditions are summarized in Table 7.6. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.6.

Table 7.6 Cutting conditions for 20° downward slot cutting test

Test no.	6
Feed rate	48 mm/min
Depth of cuts	Starting from 0 mm to 5.6149 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1200
Sampling time	Complete cut
Tool path	16.49 mm along y-direction 20° downward motion

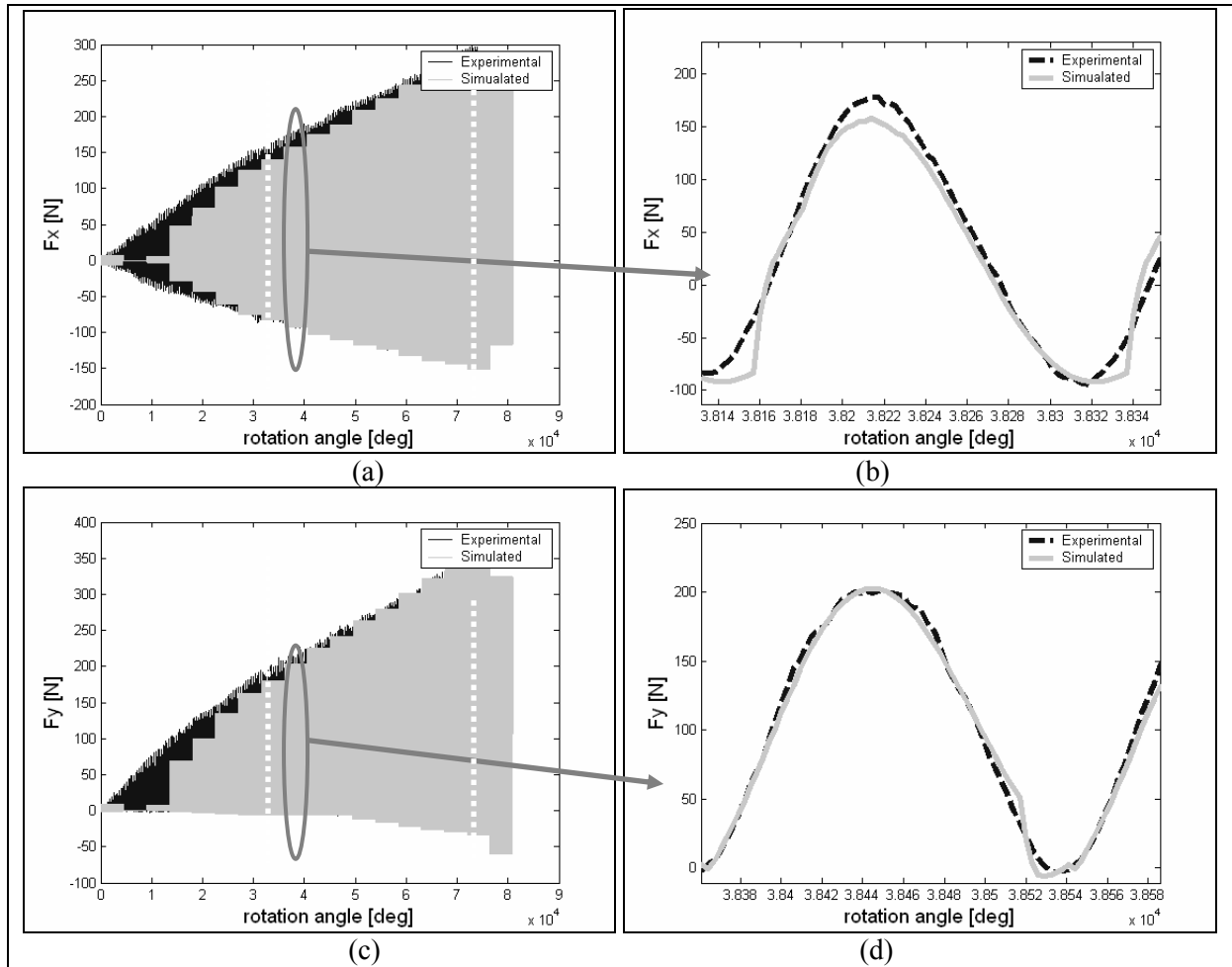


Figure 7.6: Forces in 20° downward slot cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

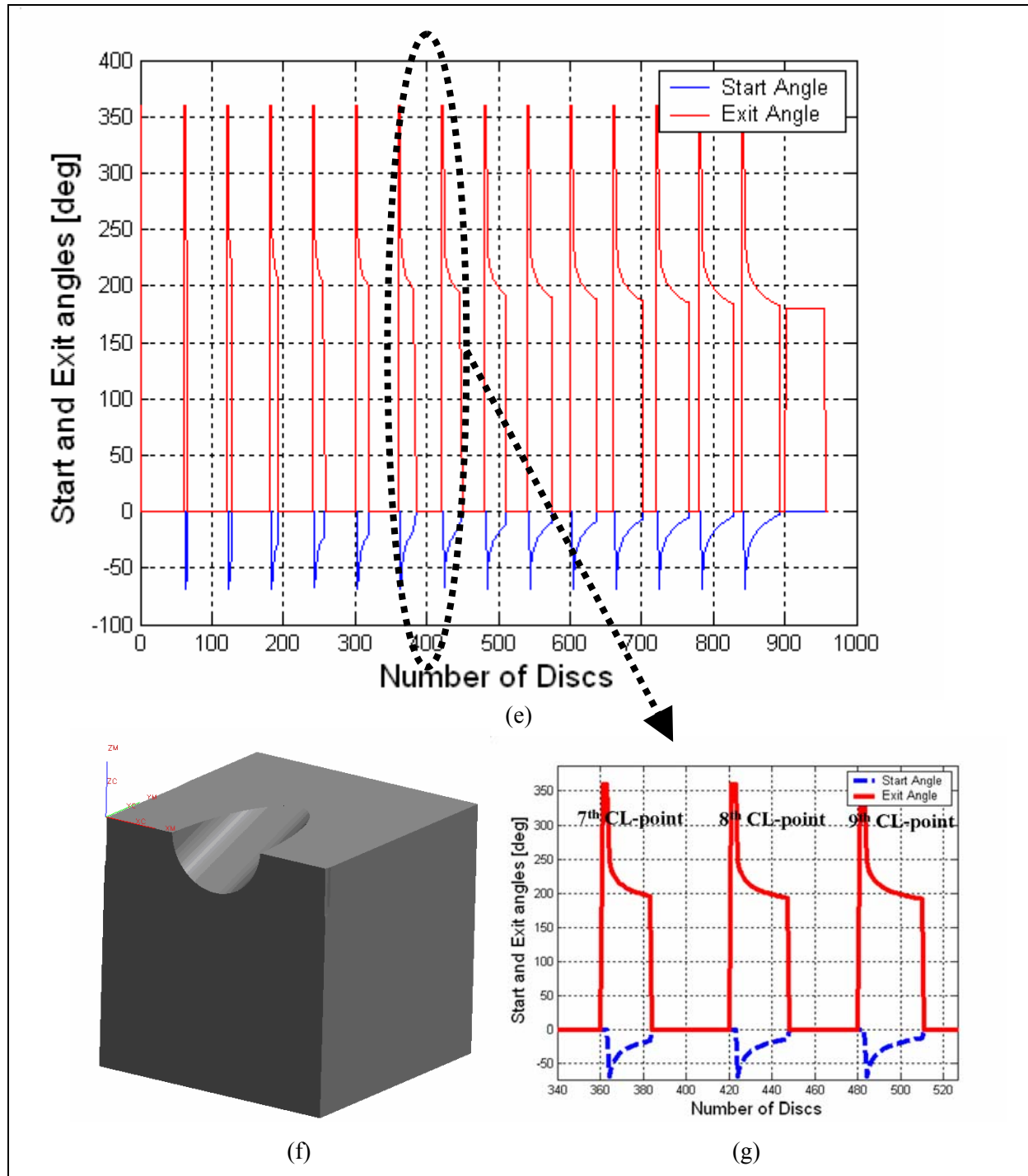


Figure 7.6: Forces in 20° downward slot cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.6 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.3 Free-form Surface Cutting Tests

7.3.1 Airfoil Cutting Test

Most complicated cuttings are free-form surface cutting tests. There are two tests performed for free-form cutting force validations. Airfoil is chosen to be cut since it is a hard and commonly used application of milling process. Cutting conditions are summarized in Table 7.7. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.7.

Table 7.7 Cutting conditions for airfoil cutting test

Test no.	7
Feed rate	48 mm/min
Depth of cuts	Changing between 6 mm to 1 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	50 mm along y-direction

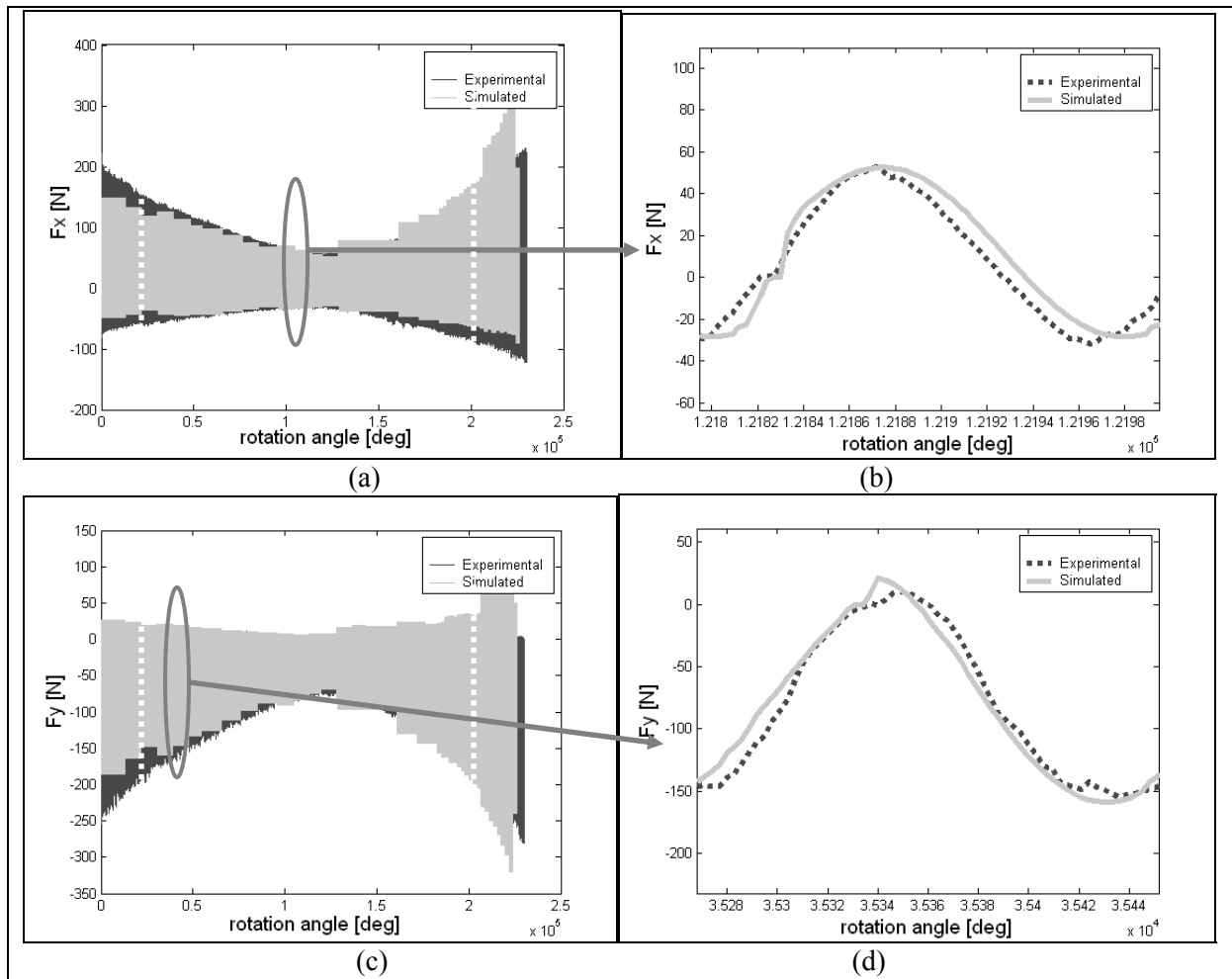


Figure 7.7: Simulated and experimental forces of the airfoil cutting test. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

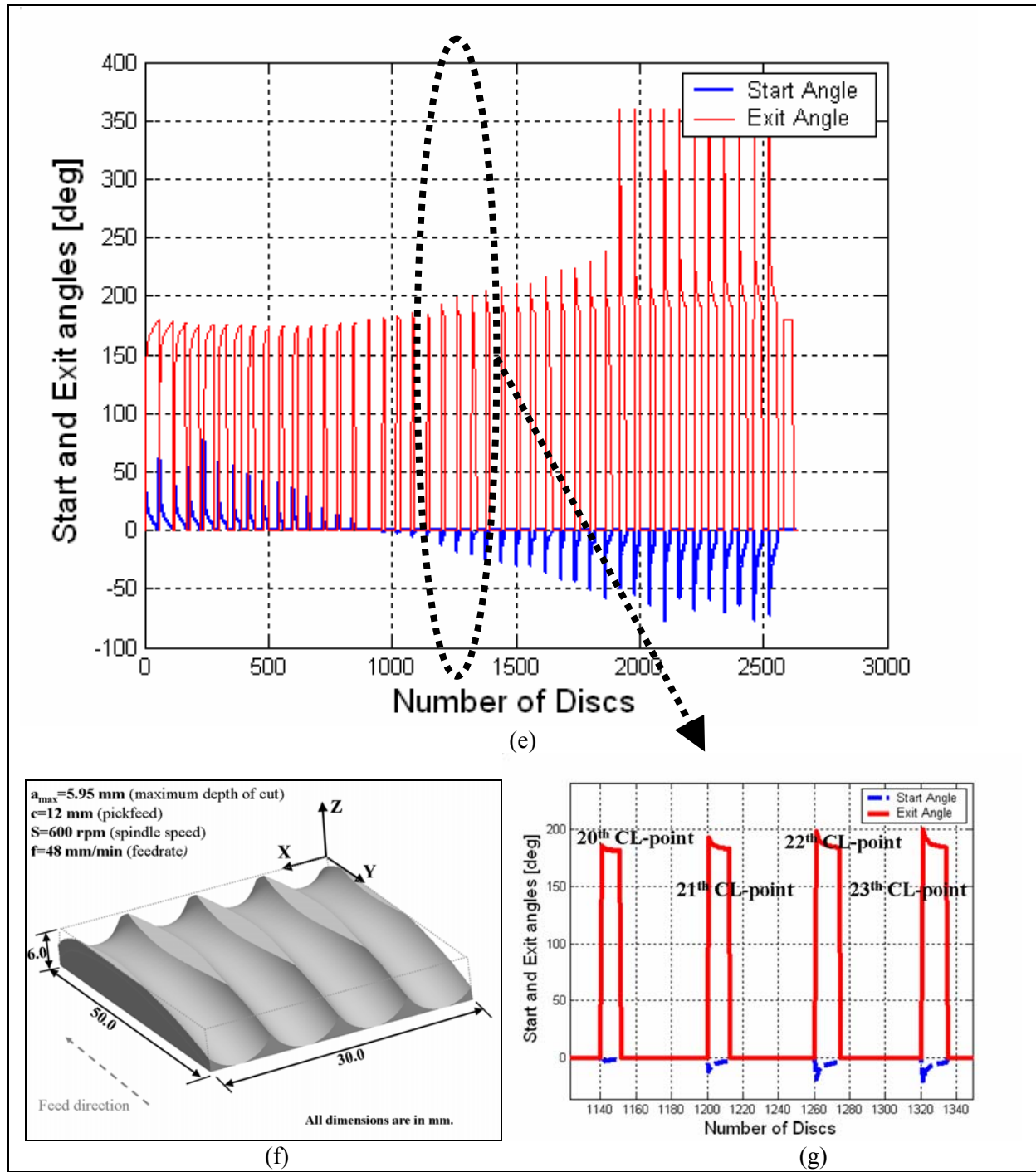


Figure 7.7: Simulated and experimental forces of the airfoil cutting test. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.7 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.3.2 Sinusoidal Surface Cutting Test

Another free-form surface cut is sinusoidal shape surface cutting test. In this test the upward and downward motion of ball-end mill and its consequences can be seen more clearly than airfoil case.

Cutting conditions are summarized in Table 7.8. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.8.

Table 7.8 Cutting conditions for sinusoidal surface cutting test

Test no.	8
Feed rate	48 mm/min
Depth of cuts	Changing between 6 mm to 0 mm
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	150 mm along y-direction

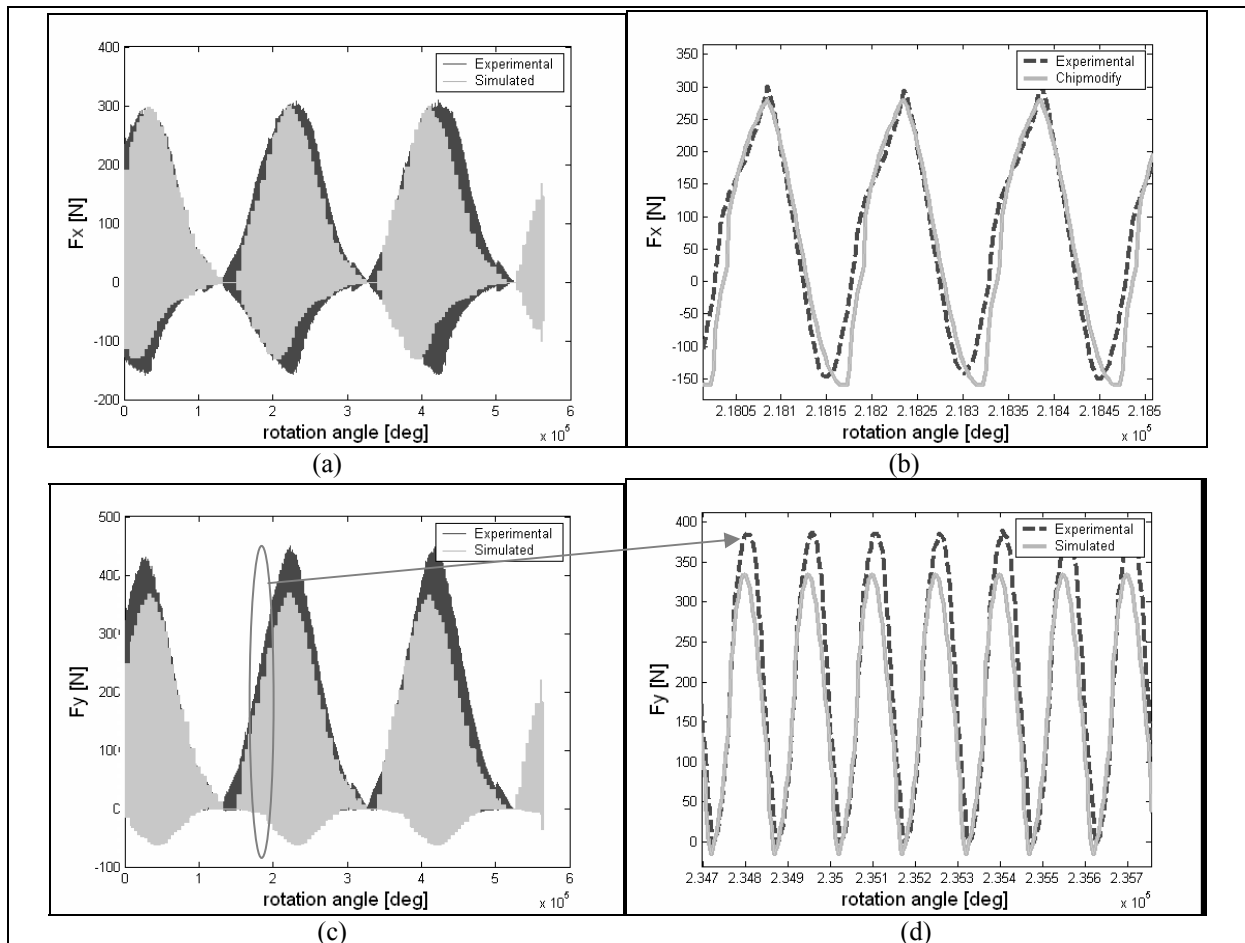


Figure 7.8 Simulated and Measured Forces in a sinusoidal surface cutting test; (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

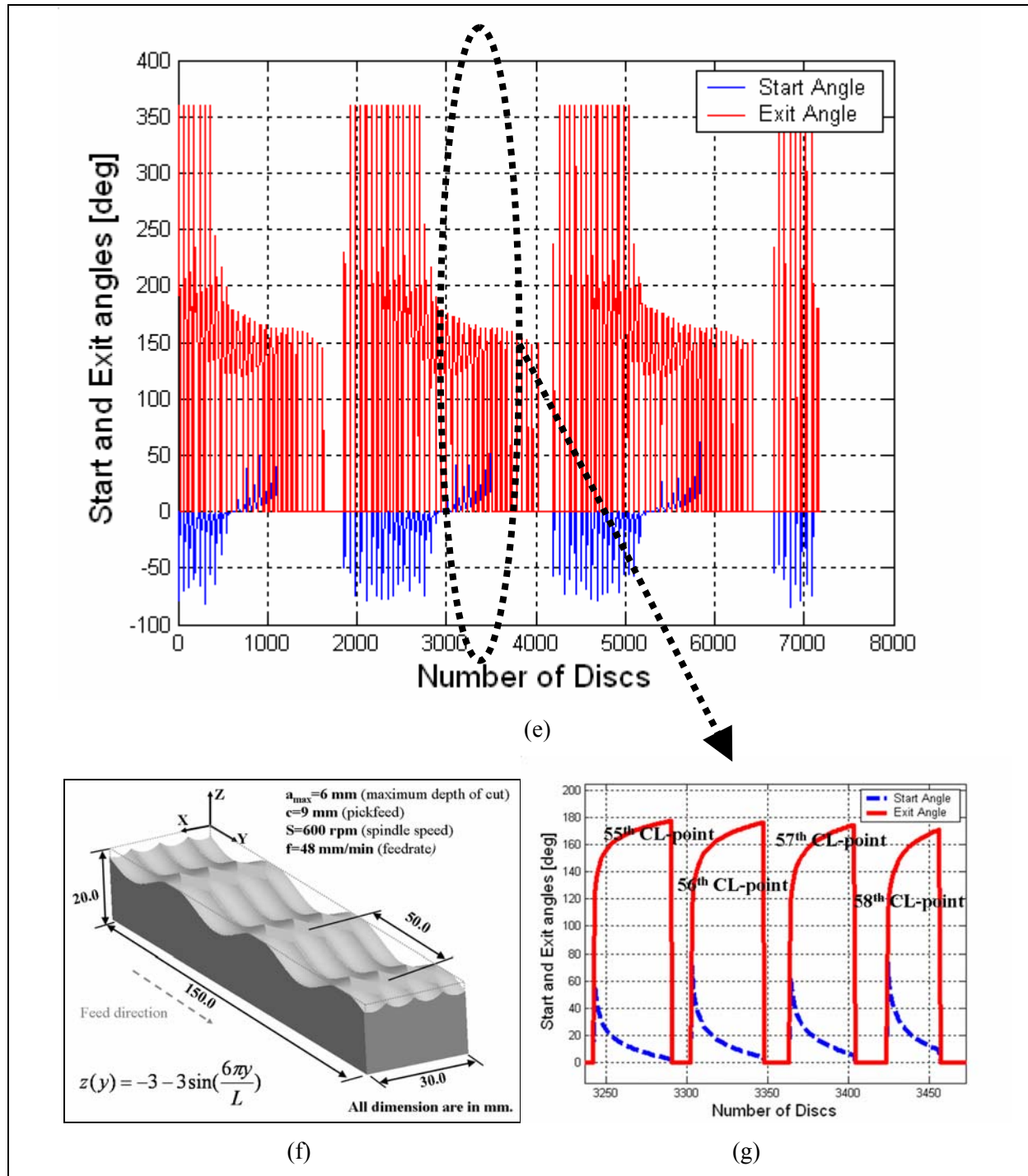


Figure 7.8 Simulated and Measured Forces in a sinusoidal surface cutting test; (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values at. Predicted and measured forces are plotted versus rotation angle in Figure 7.8 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.4 Finish Cutting Tests

7.4.1 10° Upward Finish Cutting Test

Up to this point all cutting tests are performed starting from block workpiece and trying to form the desired surface so called rough milling. From this point forward tests are performed to validate that the engagement and force model also works for finishing cases. Firstly 10° upward finish cutting test is performed and cutting conditions of the test are summarized in Table 7.9. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.9.

Table 7.9 Cutting conditions for 10° upward finish cutting test

Test no.	9
Feed rate	192 mm/min
Depth of cuts	1 mm relative to desired surface
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	34.03 mm 10° upward motion (in y direction)

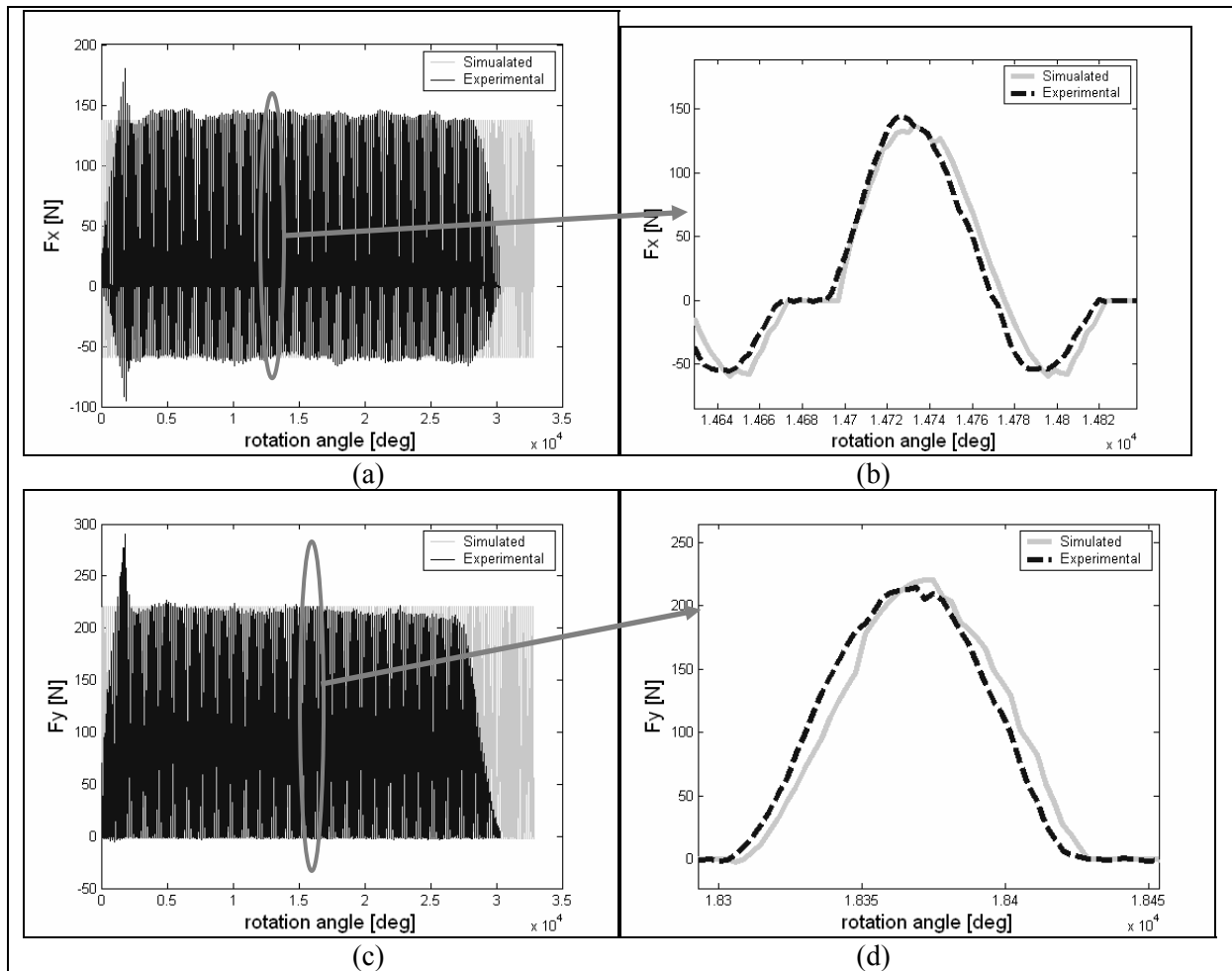


Figure 7.9: Forces in 10° upward finish cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

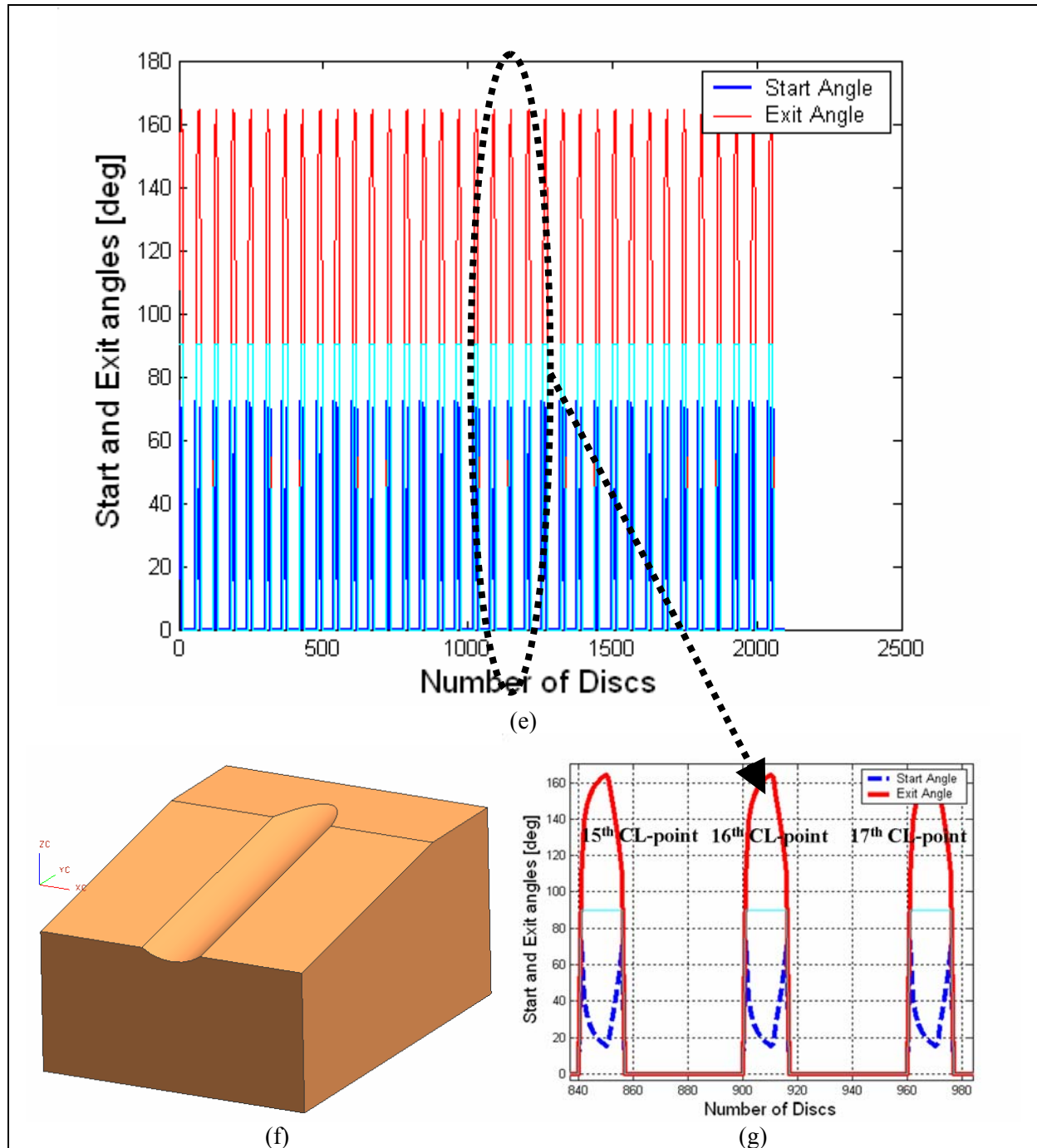


Figure 7.9: Forces in 10° upward finish cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.9 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.4.2 20° Upward Finish Cutting Test

A more steep case of inclined finish cutting tests is performed by setting the inclination angle as 20°. Cutting conditions are summarized in Table 7.10. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.10.

Table 7.10 Cutting conditions for 20° upward finish cutting test

Test no.	10
Feed rate	192 mm/min
Depth of cuts	1 mm relative to desired surface
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	16.49 mm along y-direction 20° upward motion

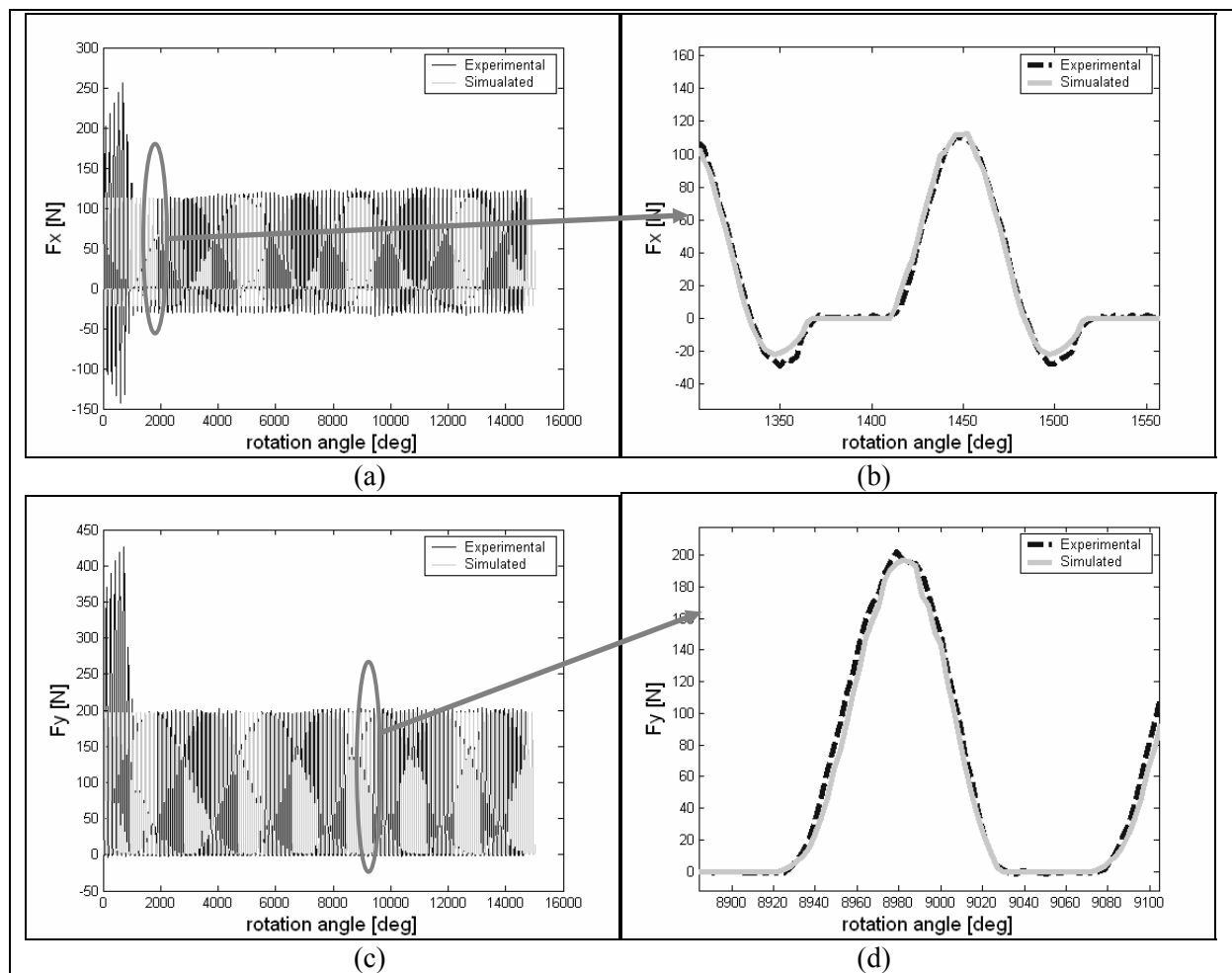


Figure 7.10: Forces in 20° upward finish cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

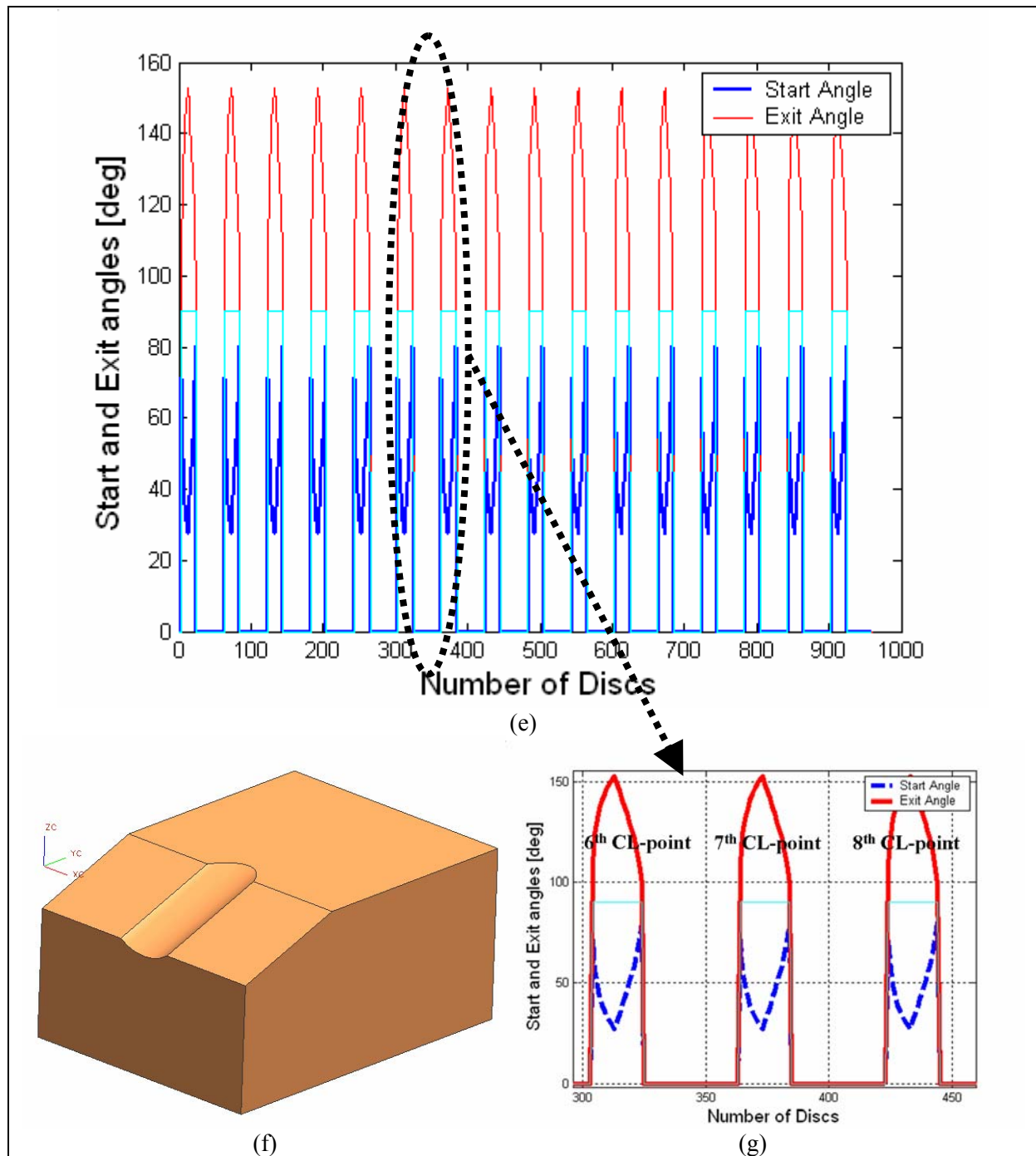


Figure 7.10: Forces in 20° upward finish cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.10 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.4.3 10° Downward Finish Cutting Test

After finishing upward cases, starting from 10° downward finish cutting tests are performed. Cutting conditions are summarized in Table 7.11. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.11.

Table 7.11 Cutting conditions for 10° downward finish cutting test

Test no.	11
Feed rate	192 mm/min
Depth of cuts	1mm relative to desired surface
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	34.03 mm along y-direction 10° downward motion

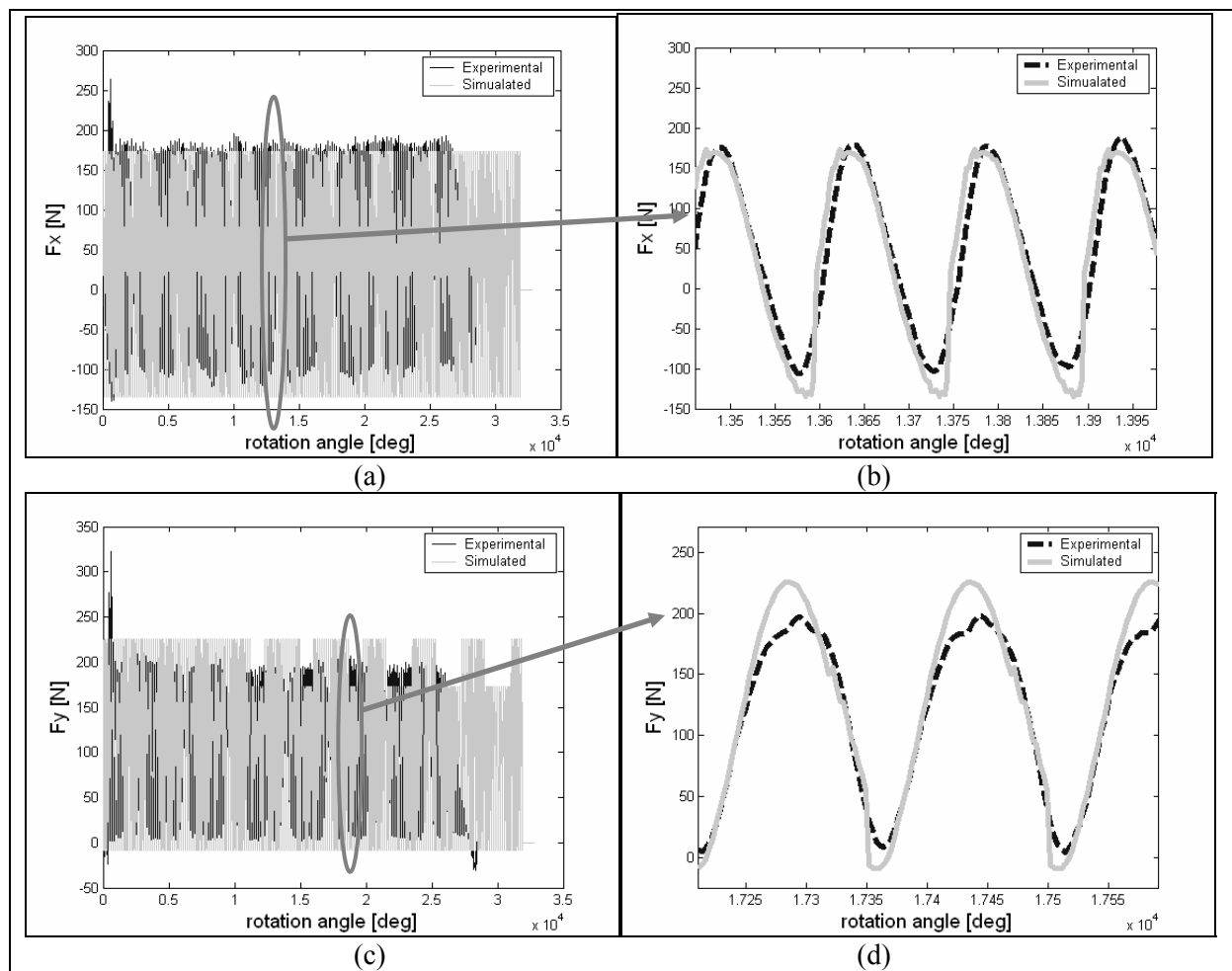


Figure 7.11: Forces in 10° downward finish cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y ,

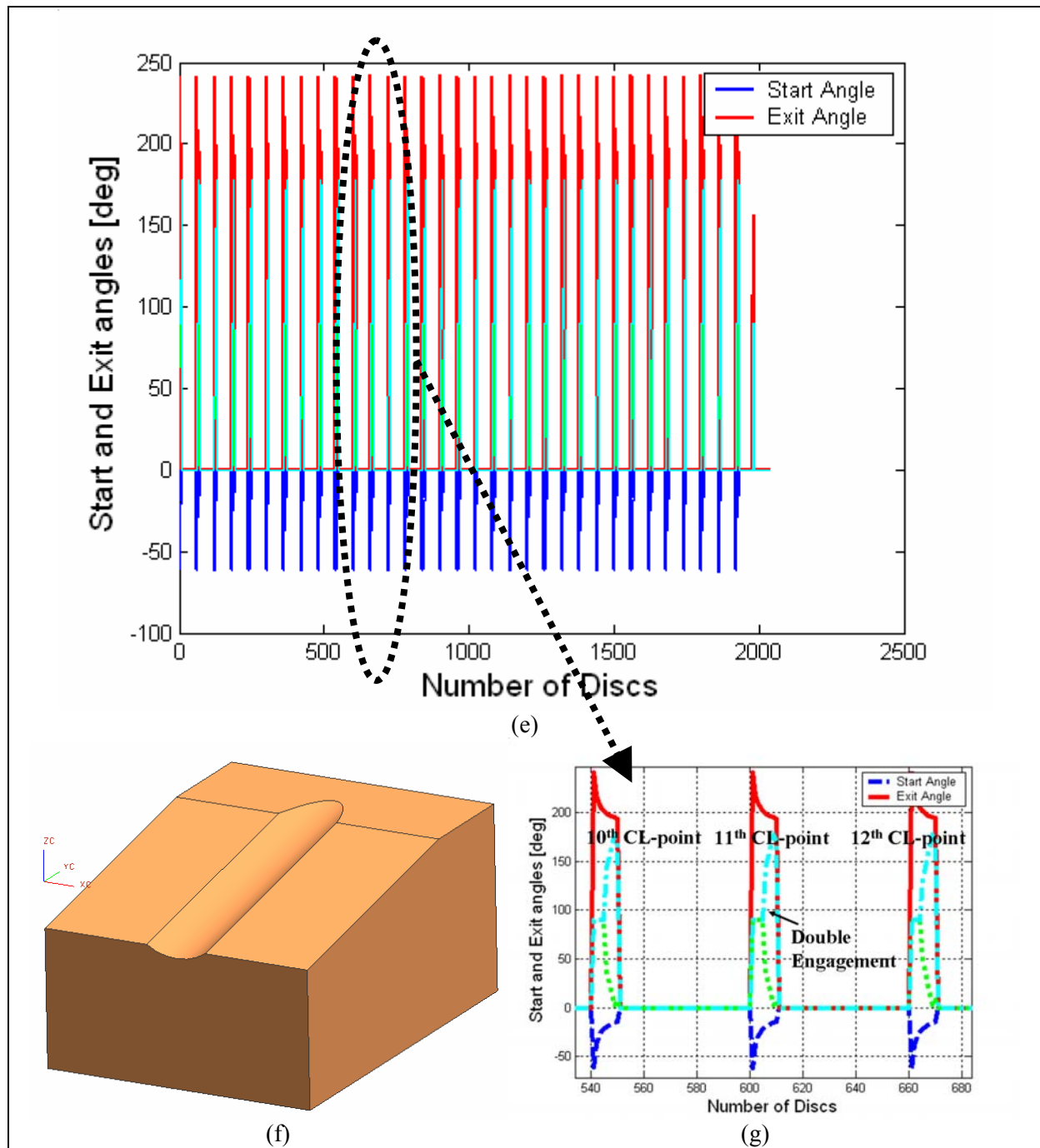


Figure 7.11: Forces in 10° downward finish cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions showed very good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.11 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

7.4.3 20° Downward Finish Cutting Test

A more steep case of negatively inclined finish cutting tests is performed by setting the inclination angle as 20°. Cutting conditions are summarized in Table 7.12. The simulated and experimental force outputs of the machined part and whole spectrum of engagement domain are given in Figure 7.12.

Table 7.12 Cutting conditions for 20° downward finish cutting test

Test no.	12
Feed rate	192 mm/min
Depth of cuts	1 mm relative to desired surface
Spindle Speed	600 rpm
Amplifier gain	200
Sampling frequency	1000
Sampling time	Complete cut
Tool path	16.49 mm along y-direction 20° downward motion

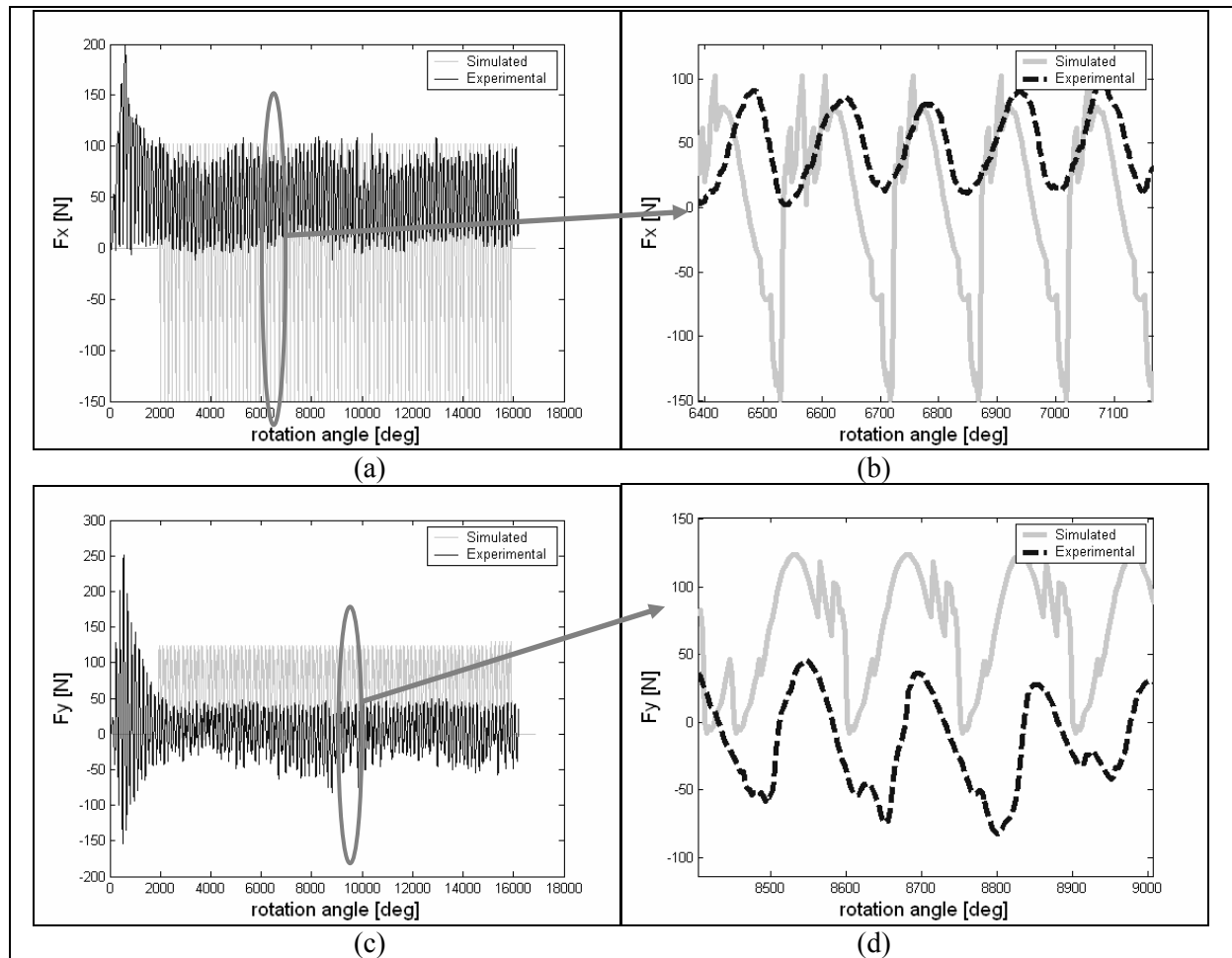


Figure 7.12: Forces in 20° downward finish cutting tests. (a) Experimental and simulated F_x forces, (b) Detailed view of F_x , (c) Experimental and simulated F_y forces, (d) Detailed view of F_y

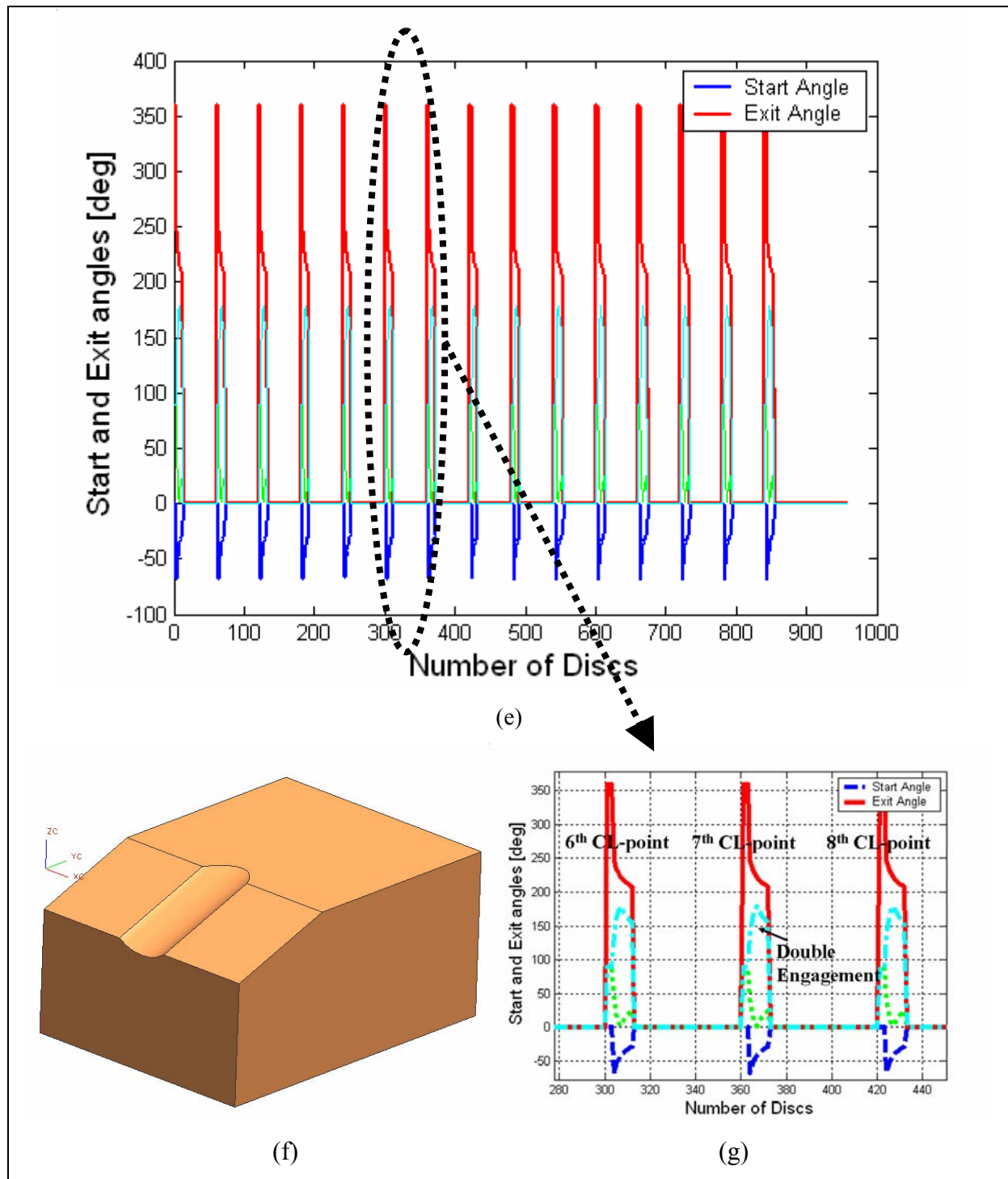


Figure 7.12: Forces in 20° downward finish cutting tests. (e) Engagement domain, (f) Predicted surface topography, (g) Closer view of engagement domain

The prediction of cutting forces in X-Y directions do not showed good agreement with actual force values. Predicted and measured forces are plotted versus rotation angle in Figure 7.12 for complete cut. The cutter has two flutes, as a result two peaks are observed in a single rotation of the cutter that corresponds to 360 degrees.

In 20° downward finish cutting test, the drilling action of ball-end mill is well seen. The proposed force model does not handle the drilling action of ball-end mill. If one looks up the engagement domain in Figure 7.12, s/he will see that the number of disc which are at full contact are dominant which means that drilling is more dominant than milling which can be seen from Figure 7.13. The dash lines represent full contact discs dominancy. Figure 7.13 represents the numerical values of engagement domain in 20° downward finish cutting test (see Figure A.7 for engagement domain determination of 20° downward finish cutting case).

	1	2	3	4	5	6	7
174	3	5.4	0	0	0	0	-0.34907
175	3	5.5	0	0	0	0	-0.34907
176	3	5.6	0	0	0	0	-0.34907
177	3	5.7	0	0	0	0	-0.34907
178	3	5.8	0	0	0	0	-0.34907
179	3	Drilling effect			0	0	-0.34907
180	3	0	0	0	0	0	-0.34907
181	4	0.1	0	6.2832	1.5708	1.5708	-0.34907
182	4	0.2	0	6.2832	1.5708	1.5708	-0.34907
183	4	0.3	0	6.2832	1.5708	1.5708	-0.34907
184	4	0.4	-1.1856	4.3272	0.7585	2.3831	-0.34907
185	4	0.5	-0.9573	4.0989	0.3424	2.7992	-0.34907
186	4	0.6	-0.8272	3.9688	0.1222	3.0194	-0.34907
187	4	0.7	-0.7366	3.8782	0.0275	3.1141	-0.34907
188	4	0.8	-0.6678	3.8094	0.1404	3.0012	-0.34907
189	4	0.9	-0.6128	3.7544	0.2305	2.911	-0.34907
190	4	1	0.5674	3.7089	0.3053	2.8363	-0.34907
191	4	1.1	0.5287	3.6703	0.369	2.7725	-0.34907
192	4	1.2	0.4953	3.6369	0.4244	2.7172	-0.34907
193	4	1.3	0	0	0	0	-0.34907
194	4	1.4	0	0	0	0	-0.34907
195	4	1.5	0	0	0	0	-0.34907
196	4	1.6	0	0	0	0	-0.34907
197	4	1.7	0	0	0	0	-0.34907
198	4	1.8	0	0	0	0	-0.34907
199	4	1.9	0	0	0	0	-0.34907
200	4	2	0	0	0	0	-0.34907
201	4	2.1	0	0	0	0	-0.34907

Figure 7.13 Numerical values of engagement domain in 20° downward finish cutting

Table 7.13 is the combination of all tests and the results are summarized in this table. Maximum values of forces in x and y direction and absolute error with relative error is given in this table.

Table 7.13 Results of cutting tests

Test Type	Maximum Simulated Fx Experimental Fx [N]	Maximum Simulated Fy Experimental Fy [N]	Absolute Maximum Error [N]	Relative Maximum Error %
5° upward (rough)	213.33 203.61	260.52 243.90	9.71 16.62	4.77 6.82
10° upward (rough)	183.88 180.18	223.45 218.26	3.70 5.19	2.05 2.38
20° upward (rough)	139.93 137.70	166.34 166.99	2.23 0.65	1.62 0.39
5° downward (rough)	261.74 246.09	318.82 303.22	15.65 15.59	6.36 5.14
10° downward (rough)	278.03 257.08	342.11 312.74	20.95 29.36	8.15 9.39
20° downward (rough)	224.01 254.88	279.93 301.03	30.88 21.09	12.11 7.01
Airfoil (rough)	166.26 147.22	187.50 178.71	19.05 8.79	12.94 4.92
Sinusoidal (rough)	298.37 350.10	369.38 413.09	51.73 43.71	14.78 10.58
10° upward (finish)	153.78 147.22	233.47 224.85	6.56 8.61	4.46 3.83
20° upward (finish)	112.28 130.37	196.51 207.28	18.09 10.77	13.87 5.20
10° downward (finish)	173.10 195.56	225.97 207.28	22.46 18.69	11.48 9.02
20° downward (finish)	149.57 87.16	124.24 117.92	62.41 6.32	71.60 5.36

Chapter 8: FUTURE WORK

DYNAMICS OF BALL-END MILLING

8.1 Introduction

The instability of the machining systems becomes dominant with the advancement in machining speeds and accelerations of high-speed machining. The instable conditions produce excitation of oscillations and cause dynamic problems. These problems affect the tool life (tool wear and tool failure), produce poor surface finish, reduce productivity, produces scrap parts and affect the environment. Chatter's analytical prediction method was combined with analytical engagement regions by assuming 2 degree of freedom system to obtain the stability lobes. This chapter describes way of this new method which obtains the stability information for some vibration modes that can be used to graph the stability lobes for high-speed milling, and these to help in the selection of parameters for chatter free operations. No tests could be carried out to demonstrate the quality of this method since without averaging chip load the equations become explicit and thus could not be solved analytically. This work presented in this chapter is a future work in obtaining the exact analytical – experimental stability lobes for free-form surface machining operations.

8.2 Determination of Stability Lobes

Stability lobes in milling process are dependent to the magnitude and frequency of the forces generated at the cutting edge in machining. Force model presented in chapter 3 and engagement model presented in chapter 4 will be used in this work to obtain the stability lobes in machining simulations. Equation (3.8) can be rearranged as follows;

$$\begin{bmatrix} dF_x \\ dF_y \end{bmatrix} = \begin{bmatrix} \sin(\psi)\sin(\theta) & \cos(\psi)\sin(\theta) & \cos(\theta) \\ \sin(\psi)\cos(\theta) & \cos(\psi)\cos(\theta) & -\sin(\theta) \end{bmatrix} \begin{bmatrix} K_{rc}h + K_{re} \\ K_{\psi c}h + K_{\psi e} \\ K_{tc}h + K_{te} \end{bmatrix} dz \quad (8.1)$$

Milling cutters can be considered to have two orthogonal degrees of freedom as shown. The excitation of the forces in the feed and normal directions causes dynamic displacements as $x(t)$ and $y(t)$. Since spindle rotates at an angular velocity, the immersion angle is a function of time. The resultant chip thickness consists of static part and a dynamic component caused by the vibrations of the tool at the present and previous tooth periods. The total chip thickness can be expressed as;

$$h_j(t) = c_{kn} \sin(\theta(t)) \sin(\psi) - [-x(t) \sin(\theta(t)) - y(t) \cos(\theta(t))] + [-x(t-T) \sin(\theta(t)) - y(t-T) \cos(\theta(t))] \quad (8.2)$$

The static component of the Equation (8.2) is dropped from the expressions because it does not contribute to the dynamic chip load regeneration mechanism. Equation (8.2) simplifies to;

$$h(\theta) = [\Delta x \sin(\theta) + \Delta y \cos(\theta)] g(\theta) \quad (8.3)$$

where $\Delta x = x - x_0$ and $\Delta y = y - y_0$. Here (x, y) and (x_0, y_0) represent the dynamic displacements of the cutter structure at the present and previous tooth periods, respectively. $g(\phi_j)$ is a unit step function that determines whether the tooth is in contact with the workpiece or not.

If one puts Equation (8.3) into Equation (8.1);

$$\begin{bmatrix} dF_x \\ dF_y \end{bmatrix} = \begin{bmatrix} a_{xx} & a_{xy} \\ a_{yx} & a_{yy} \end{bmatrix} \begin{bmatrix} \Delta x \\ \Delta y \end{bmatrix} dz + \begin{bmatrix} b_x \\ b_y \end{bmatrix} dz \quad (8.4)$$

where

$$\begin{aligned}
a_{xx} &= \sin(\psi) \sin^2(\theta) K_{rc} + \cos(\psi) \sin^2(\theta) K_{\psi c} + \sin(\theta) \cos(\theta) K_{tc} \\
a_{xy} &= \sin(\psi) \sin(\theta) \cos(\theta) K_{rc} + \cos(\psi) \sin(\theta) \cos(\theta) K_{\psi c} + \cos^2(\theta) K_{tc} \\
a_{yx} &= \sin(\psi) \sin(\theta) \cos(\theta) K_{rc} + \cos(\psi) \sin(\theta) \cos(\theta) K_{\psi c} - \sin^2(\theta) K_{tc} \\
a_{yy} &= \sin(\psi) \cos^2(\theta) K_{rc} + \cos(\psi) \cos^2(\theta) K_{\psi c} - \sin(\theta) \cos(\theta) K_{tc}
\end{aligned} \tag{8.5}$$

Equation (8.4) can be stated as;

$$\begin{bmatrix} dF_x \\ dF_y \end{bmatrix} = A_0 [\Delta t] dz + \begin{bmatrix} b_x \\ b_y \end{bmatrix} dz \tag{8.6}$$

Then by using the simplistic approximation, the average component of the Fourier series expansion is considered then;

$$[A(0)] = \frac{1}{\phi_p} \int_{\phi_{st}}^{\phi_{ex}} [A(\phi)] d\phi \tag{8.7}$$

where $\phi_p = \frac{2\pi}{N}$.

The integrated functions are given as follows;

$$\begin{aligned}
\alpha_{xx} &= \left[\left(\frac{2\theta - \sin(2\theta)}{4} \right) \sin(\psi) K_{rc} + \left(\frac{2\theta - \sin(2\theta)}{4} \right) \cos(\psi) K_{\psi c} - \frac{\cos(2\theta)}{4} K_{tc} \right]_{\phi_{st}}^{\phi_{ex}} \\
\alpha_{xy} &= \left[- \left(\frac{\cos(2\theta)}{4} \right) \sin(\psi) K_{rc} - \left(\frac{\cos(2\theta)}{4} \right) \cos(\psi) K_{\psi c} + \left(\frac{\sin(2\theta) + 2\theta}{4} \right) K_{tc} \right]_{\phi_{st}}^{\phi_{ex}} \\
\alpha_{yx} &= \left[- \left(\frac{\cos(2\theta)}{4} \right) \sin(\psi) K_{rc} - \left(\frac{\cos(2\theta)}{4} \right) \cos(\psi) K_{\psi c} + \left(\frac{\sin(2\theta) - 2\theta}{4} \right) K_{tc} \right]_{\phi_{st}}^{\phi_{ex}} \\
\alpha_{yy} &= \left[\left(\frac{2\theta + \sin(2\theta)}{4} \right) \sin(\psi) K_{rc} + \left(\frac{2\theta + \sin(2\theta)}{4} \right) \cos(\psi) K_{\psi c} + \frac{\cos(2\theta)}{4} K_{tc} \right]_{\phi_{st}}^{\phi_{ex}}
\end{aligned} \tag{8.8}$$

Thus time-invariant but immersion-dependent directional cutting coefficient matrix A_0 takes the form;

$$A_0 = \frac{N}{2\pi} \begin{bmatrix} \alpha_{xx} & \alpha_{xy} \\ \alpha_{yx} & \alpha_{yy} \end{bmatrix} \quad (8.9)$$

The real part of the oriented transfer function between the displacement in the y direction and cutting force in F_y is;

$$\Phi(w) = \frac{1 - r_1^2}{k_1 \left[(1 - r_1^2)^2 + (2\zeta_1 r_1)^2 \right]} + \frac{1 - r_2^2}{k_2 \left[(1 - r_2^2)^2 + (2\zeta_2 r_2)^2 \right]} \quad (8.10)$$

where $r_1 = \frac{w}{w_{n1}}$, $r_2 = \frac{w}{w_{n2}}$. ω_n is the natural frequency selected in one direction and k is the stiffness coefficient selected in the same direction with natural frequency.

The transfer function matrix ($\Phi(iw)$) identified at the cutter-workpiece engagement zone is;

$$\Phi(iw) = \begin{bmatrix} \Phi_{xx}(iw) & \Phi_{xy}(iw) \\ \Phi_{yx}(iw) & \Phi_{yy}(iw) \end{bmatrix} \quad (8.11)$$

where $\Phi_{xx}(iw)$ and $\Phi_{yy}(iw)$ are the direct transfer functions in the x and y directions, and where $\Phi_{xy}(iw)$ and $\Phi_{yx}(iw)$ are the cross transfer functions. The vibration vectors at the present time (t) and the previous tooth period (t-T), which are defined at the chatter frequency w_c in the frequency domain using harmonic functions, are as follows;

$$\begin{aligned} r(iw_c) &= \Phi(iw_c) dF e^{iw_c t} \\ r_0(iw_c) &= e^{-iw_c T} r(iw_c) \\ \Delta(iw_c) &= r(iw_c) - r_0(iw_c) \end{aligned} \quad (8.12)$$

where T denotes the successive tooth periods. The transfer function of the system is obtained for three cases, by an experimental method stated below;

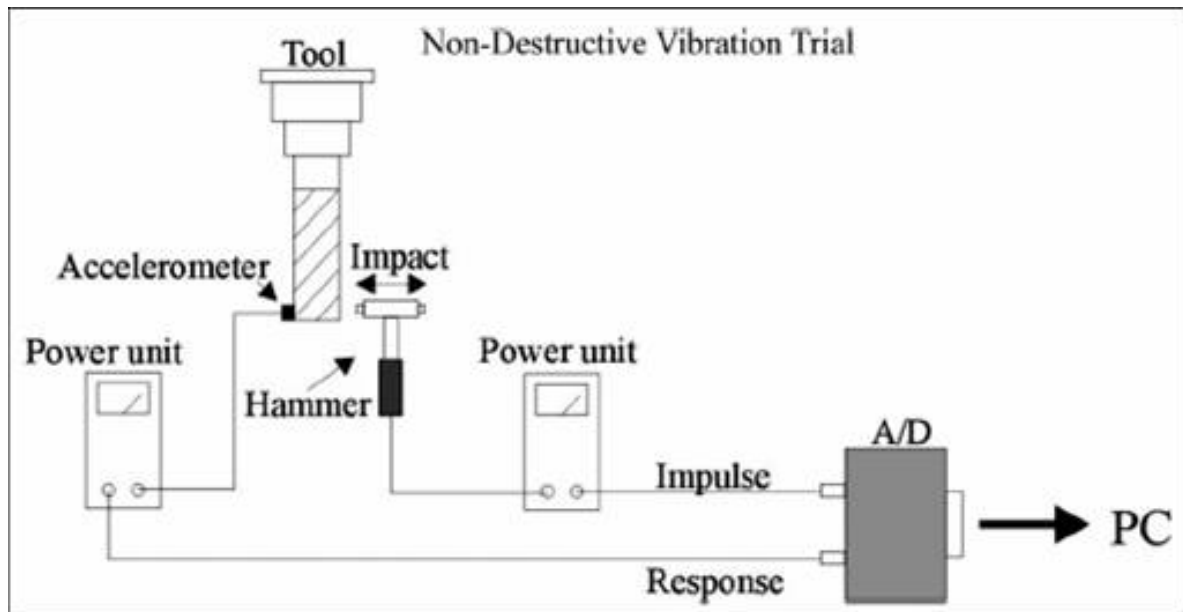


Figure 8.1: Experimental setup

There are six sets of data are collected for the cases given in Table 8.1 and also parameters of the system is given in Table 8.1. The cases are repeated till collecting a noiseless clean data. The data collection is performed by using the MALDAQ module of a commercial product namely CutPro 6.0. Plotted data are given in Figures 8.2-8.7.



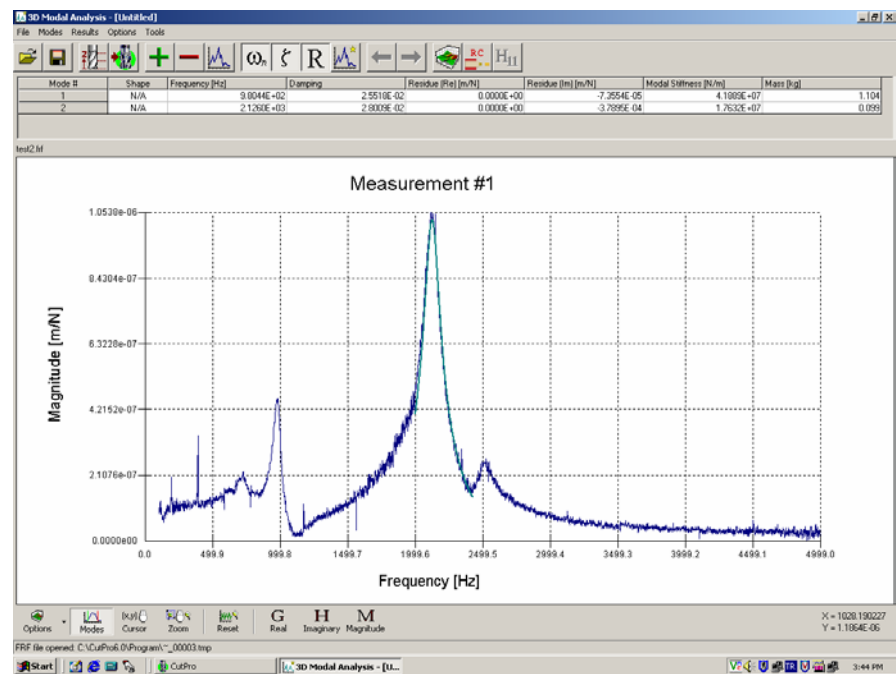


Figure 8.4 Plotted data of test 3

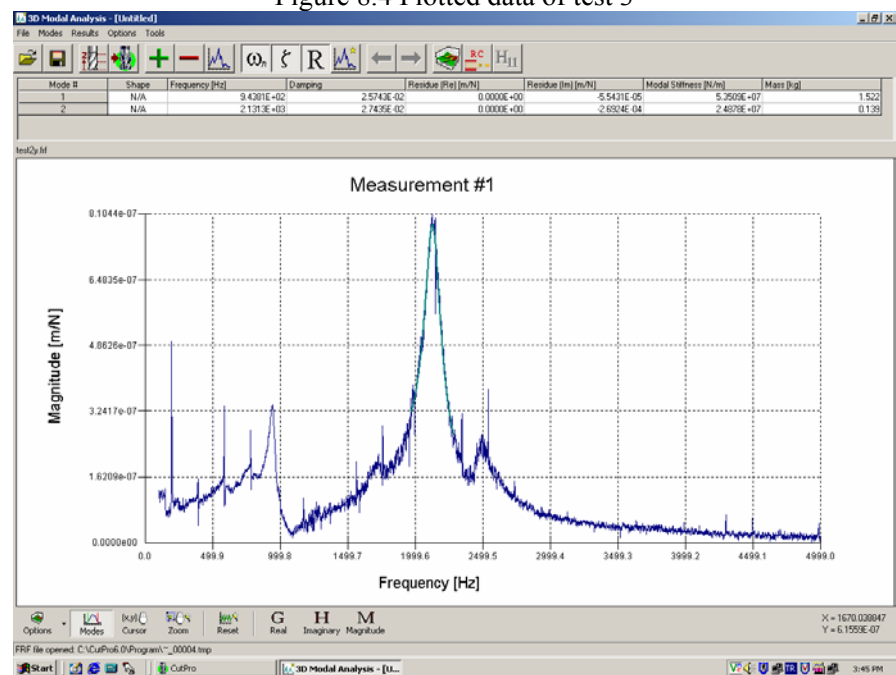


Figure 8.5 Plotted data of test 4

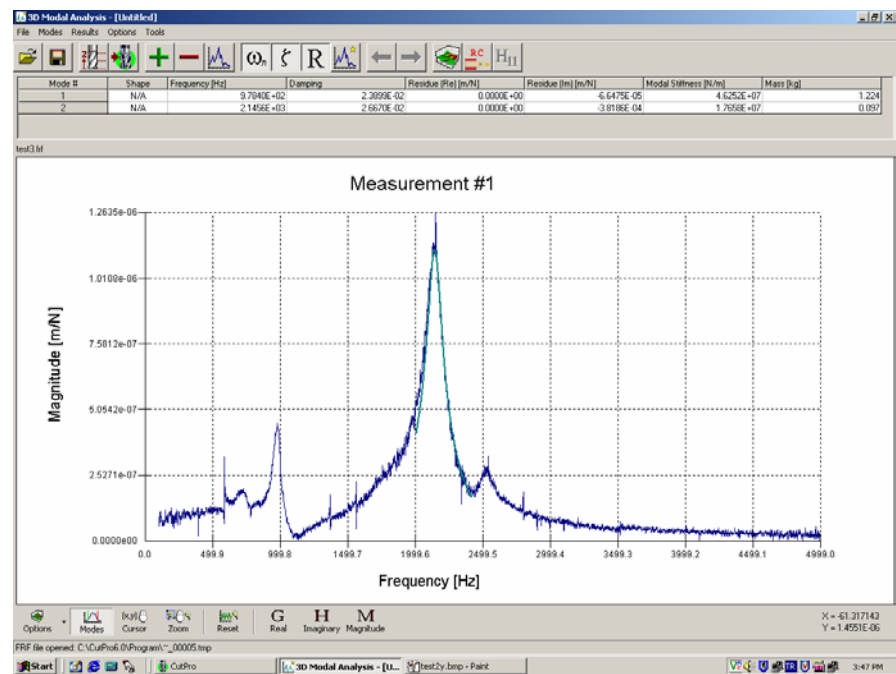


Figure 8.6 Plotted data of test 5

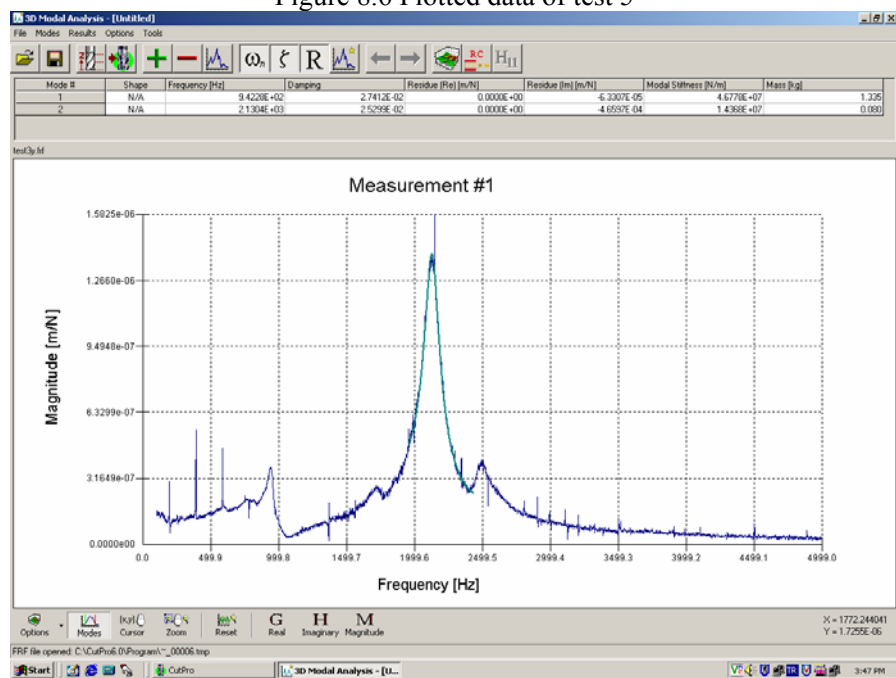


Figure 8.7 Plotted data of test 6

Substituting Equation (8.12) into the dynamic milling equation (8.6) yields;

$$dF e^{-i\omega_c t} = (1 - e^{-w_c T}) e^{-w_c t} [A_0 \mathbb{I}(\Phi(i\omega_c))] dF dz \quad (8.13)$$

Thus characteristic equation can be defined as;

$$[I] - (1 - e^{-i\omega_c T}) [A_0 \mathbb{I}(\Phi(i\omega_c))] dz = 0 \quad (8.14)$$

Table 8.1 Cases of data collection and parameters of the system

Location	Laser	Hammer
Test 1 (x direction)	shank	shank
Test 2 (y direction)	shank	shank
Test 3 (x direction)	middle	shank
Test 4 (y direction)	middle	shank
Test 5 (x direction)	Near to tip	shank
Test 6 (y direction)	Near to tip	shank

	w_1 [Hz]	w_2 [Hz]	ξ_1	ξ_2	$r_1(10^{-5})$ $\left[\frac{m}{N}\right]$	$r_2(10^{-5})$ $\left[\frac{m}{N}\right]$	$k_1(10^7)$ $\left[\frac{N}{m}\right]$	$k_2(10^7)$ $\left[\frac{N}{m}\right]$
Test 1	970	2103	0.035	0.01607	-8.914	-12.626	3.421	5.233
Test 2	943.5	2125	0.0244	0.0261	-5.275	-18.34	5.62	3.64
Test 3	980.44	2126	0.0255	0.02801	-7.3554	-37.895	4.189	1.763
Test 4	943.8	2131	0.02574	0.027435	-5.543	-26.924	5.351	2.4878
Test 5	978.4	2145.6	0.023899	0.02667	-6.6475	-38.186	4.625	1.765
Test 6	942.28	2130.4	0.027412	0.025299	-6.3307	-46.597	4.6778	1.4368

Equation (8.14) has a non-trivial solution if its determinant is zero;

$$\det[I - (1 - e^{-i\omega_c T})dz[A_0 \Phi(i\omega_c)]] = 0 \quad (8.15)$$

Equation (8.15) can be written as;

$$\det[I + \Lambda A] = 0 \quad (8.16)$$

$$A = (e^{-i\omega_c T} - 1)\Phi_0(i\omega_c)dz$$

where oriented transfer function is;

$$\Phi_0(i\omega_c) = \begin{bmatrix} \alpha_{xx}\Phi_{xx}(i\omega_c) + \alpha_{xy}\Phi_{yx}(i\omega_c) & \alpha_{xx}\Phi_{xy}(i\omega_c) + \alpha_{xy}\Phi_{yy}(i\omega_c) \\ \alpha_{yx}\Phi_{xx}(i\omega_c) + \alpha_{yy}\Phi_{yx}(i\omega_c) & \alpha_{yx}\Phi_{xy}(i\omega_c) + \alpha_{yy}\Phi_{yy}(i\omega_c) \end{bmatrix} \quad (8.17)$$

If two orthogonal degrees of freedom in feed (X) and normal (Y) directions are considered ($\Phi_{xy} = \Phi_{yx} = 0$), the characteristic equation becomes just a quadratic function;

$$a_0\Lambda^2 + a_1\Lambda + 1 = 0 \quad (8.18)$$

$$a_0 = \Phi_{xx}(i\omega_c)\Phi_{yy}(i\omega_c)(\alpha_{xx}\alpha_{yy} - \alpha_{xy}\alpha_{yx})(e^{-i\omega_c T} - 1)dz^2$$

$$a_1 = \alpha_{xx}\Phi_{xx}(i\omega_c) + \alpha_{yy}\Phi_{yy}(i\omega_c)(e^{-i\omega_c T} - 1)dz$$

The eigenvalues of the system can be obtained by finding the roots of Equation (8.18);

$$\Lambda = -\frac{1}{2a_0} \left(a_1 \pm \sqrt{a_1^2 - 4a_0} \right) \quad (8.19)$$

Since the transfer functions are complex, the eigenvalues has a real and imaginary parts, $\Lambda = \Lambda_R + i\Lambda_I$. Putting Equation (8.18) into (8.19) yields;

In addition to that $\Lambda = \Lambda(T, \alpha, w, z)$ where tooth passing is defined as $T = \frac{2\pi}{w}$.

8.3 Discussions

It is clear that all the equations up to now are valid for only one disc element on the cutter. Integration needs to be performed to sum all the disc elements. On the other hand, there are some problems in integration process. First, if the limits of the integral are defined as the vertical distance between the intersection points of the cutting edge with the boundaries of engagement region, they are not constant through the toolpath even within the CL-point. Each CL-point has different engagement region and thus different depth of cuts. For instance, in upward motion region, the depth of cut is a variable throughout the rotation of the cutter. In other words, at some rotation angles the cutting edge intersects with boundary-1 and boundary-2, however for some cases it intersects with boundary-2 and boundary-3. Thus, it is unclear to define a depth of cut for upward motion cases. In addition to that, in downward motion region the back face of the cutter is also in engagement, that's another problem to define the depth of cut in these cases.

Secondly, a unique analytical representation of the cutting edge is required to find the intersection points of the cutting edge and a polynomial fitting to the calibration coefficients is required. Third order polynomials fitted to calibration coefficients is given in Appendix (A.1). In addition to that, if Equation (8.1) is integrated with the points found from the intersection of the cutting edge with the engagement boundaries at an angle θ , the analytical equation of the forces at a rotation angle θ is obtained. Equation (E.2) given in appendix is the analytical force equation at a rotation angle.

All the equations up to now are valid for only one disc element on the cutter. Integration needs to be performed to sum all the disc elements. There are some problems in integration process. First, if the limits of the integral are defined as the vertical distance between the intersection points of the cutting edge with the boundaries of engagement region, they are not constant through the toolpath even within the CL-point. Secondly, Each CL-point has different engagement region and thus different depth of cuts. The maximum difference of the two contact points with the boundaries and the cutter may be chosen as the maximum depth of cut and stability lobes can be performed for each CL-point. Then the minimum stability lobe may be chosen as the optimum one.

Chapter 9: FUTURE WORK
AN EXPERIMENTAL ANALYSIS OF FIVE-AXIS
BALL-END MILLING

9.1 Introduction

The development of the aeronautic and automobile industries brought new technological challenges, related to the growing complexity of the products and the new geometries modeled in CAD systems. These more complex geometries impose new challenging manufacturing situations for the milling of moulds and stamping tools. This is an important reason for the development of new milling technology, namely 5 axis milling.

9.2 Cutting Tests

A set of tests were performed at a feedrate of 48 mm/min and a spindle speed of 600 rpm. A Universal Deckel MAHO DMU 60P 5-D CNC machine with rotating table is used. The tool holder is ISO40.DIN69871 type tool holder and CNC control unit is a standard DECKEL MAHO Millplus control unit.

Tests are performed for different lead/tilt angle combinations and tabulated in Table 9.1. The predicted surface topography after all tests is given in Figure 9.1. Figure 9.1 shows the combinations of lead/tilt angles in addition.

The milling center, where the cutting tests are performed, has degrees of freedom given in Figure 9.2. The lead angles and tilt angles are stated in Figure A.5.

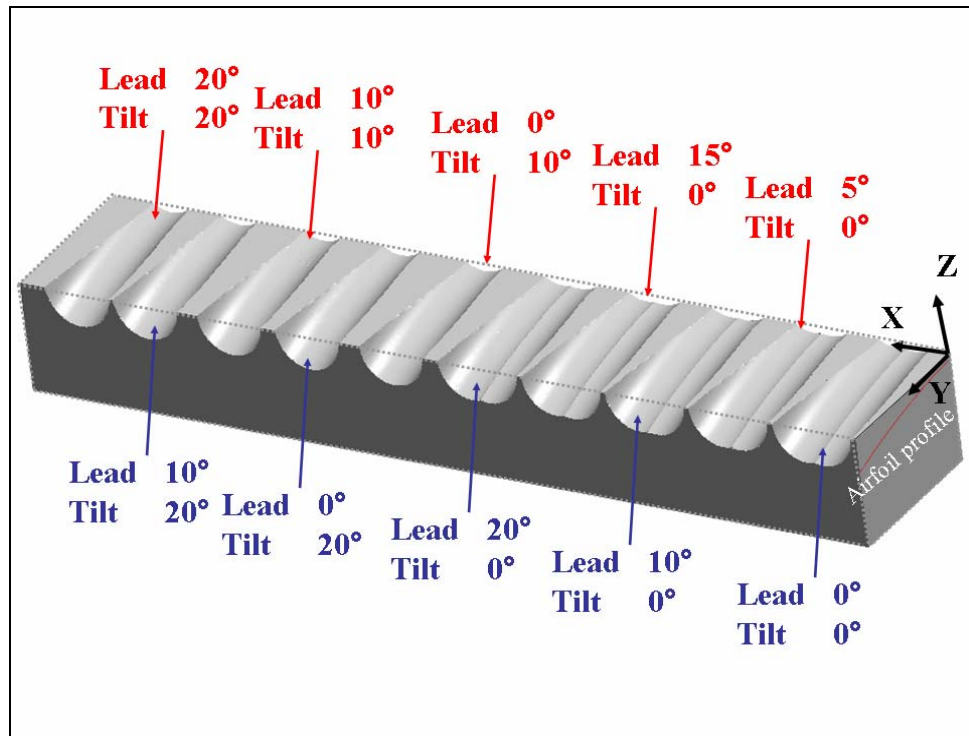


Figure 9.1 Predicted surface topography

Table 9.1 Combinations of lead/tilt angles in the cutting tests

	Lead angle [deg]	Tilt angle [deg]
Pass 1 c=12	0	0
Pass 2 c=3	0	0
Pass 3 c=12	5	0
Pass 4 c=3	5	0
Pass 5 c=12	10	0
Pass 6 c=3	10	0
Pass 7 c=12	15	0
Pass 8 c=3	15	0
Pass 9 c=12	20	0
Pass 10 c=3	20	0
Pass 11 c=12	0	10
Pass 12 c=12	0	20
Pass 13 c=12	10	10
Pass 14 c=12	10	20
Pass 15 c=12	20	20

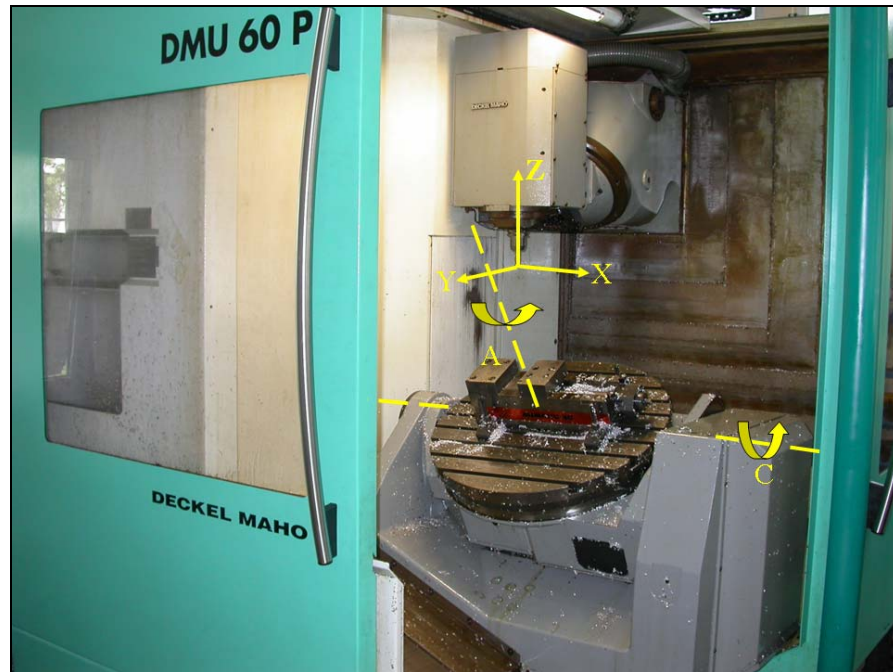


Figure 9.2 Milling center configuration

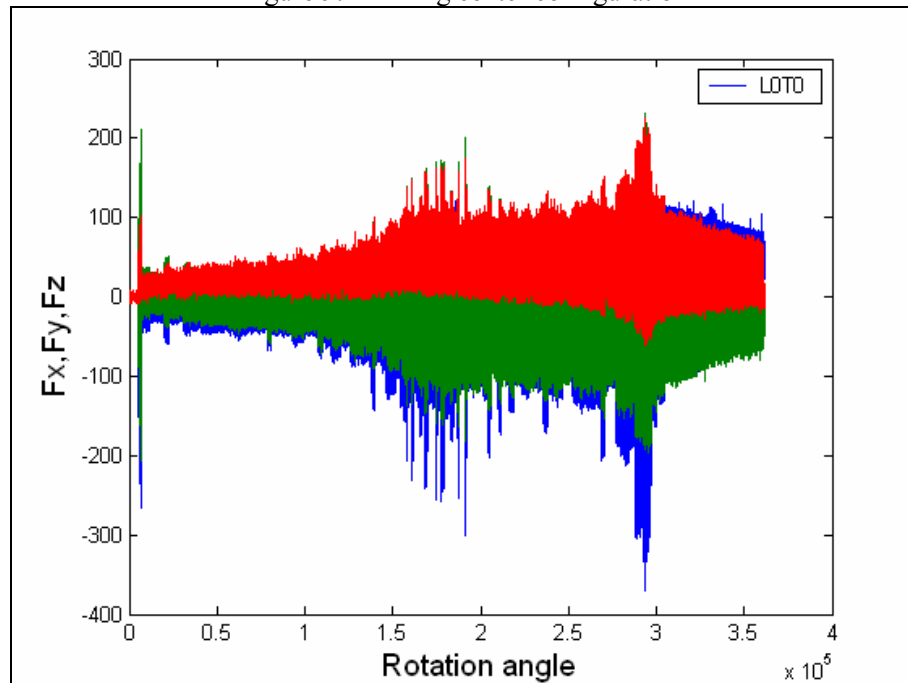


Figure 9.3 Experimental force values of pass 1

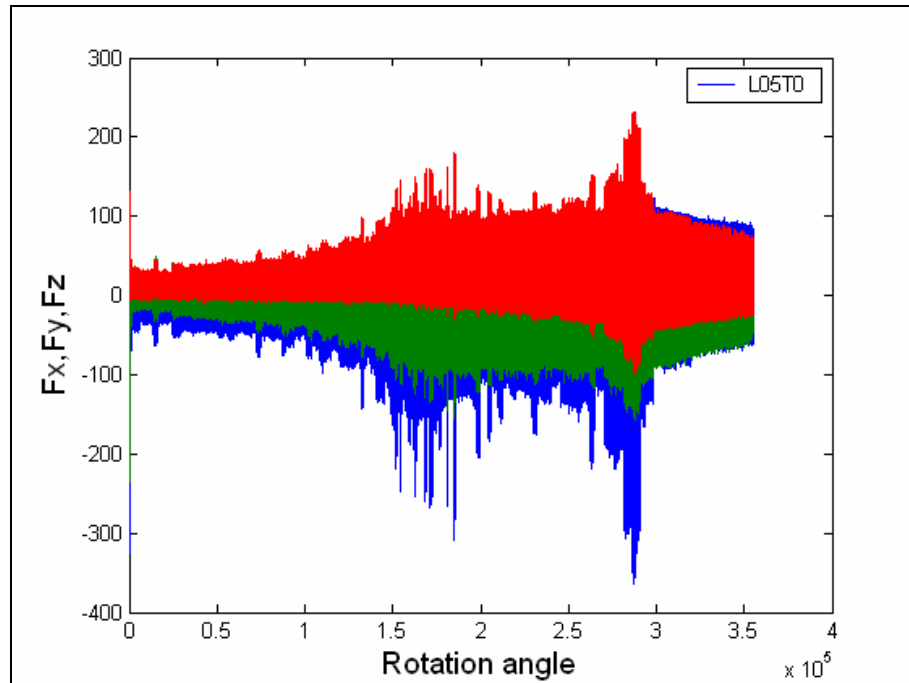


Figure 9.4 Experimental force values of pass 3

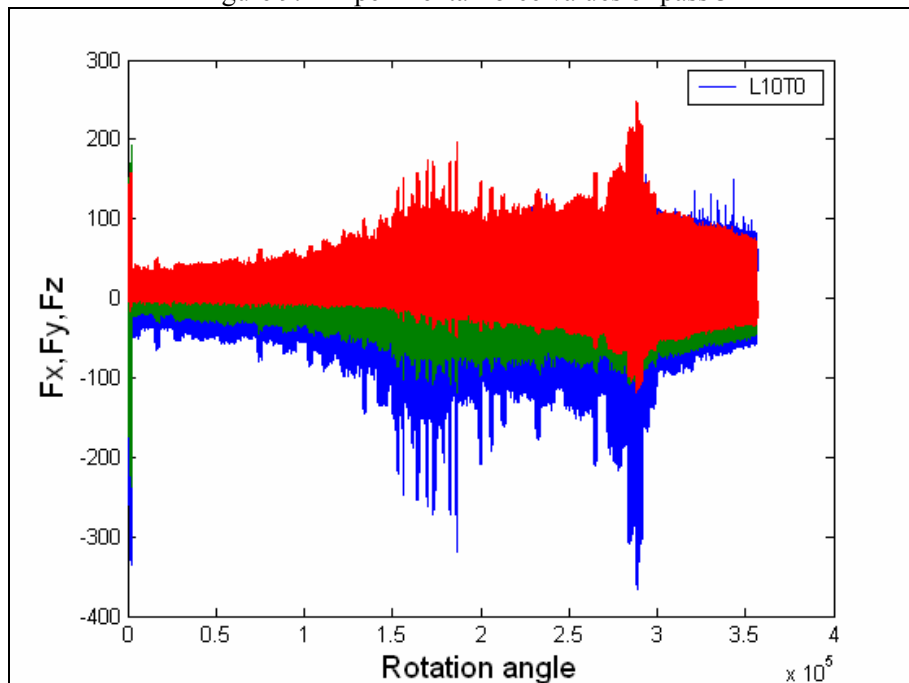


Figure 9.5 Experimental force values of pass 5

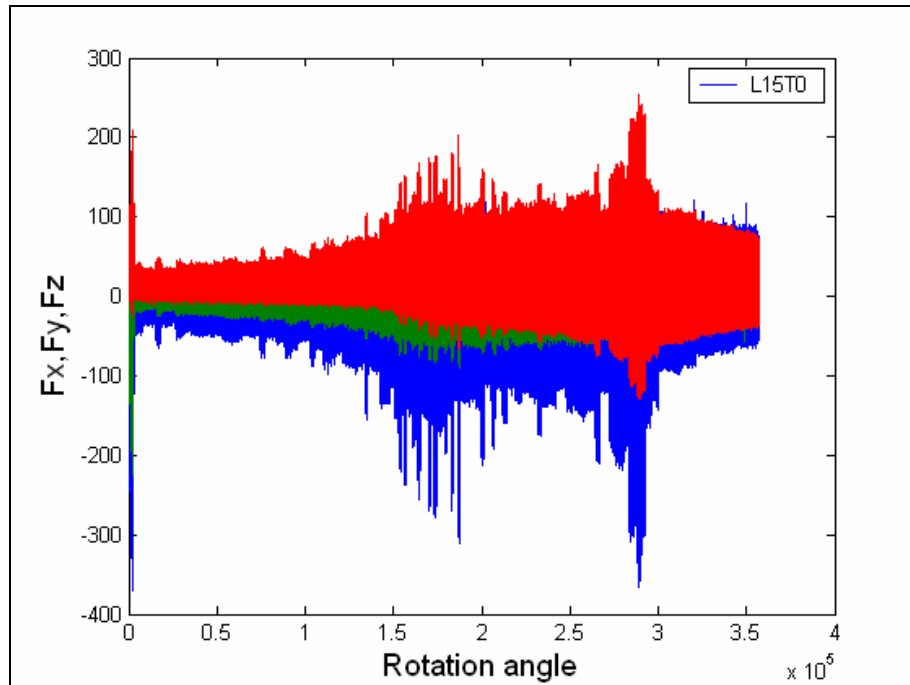


Figure 9.6 Experimental force values of pass 7

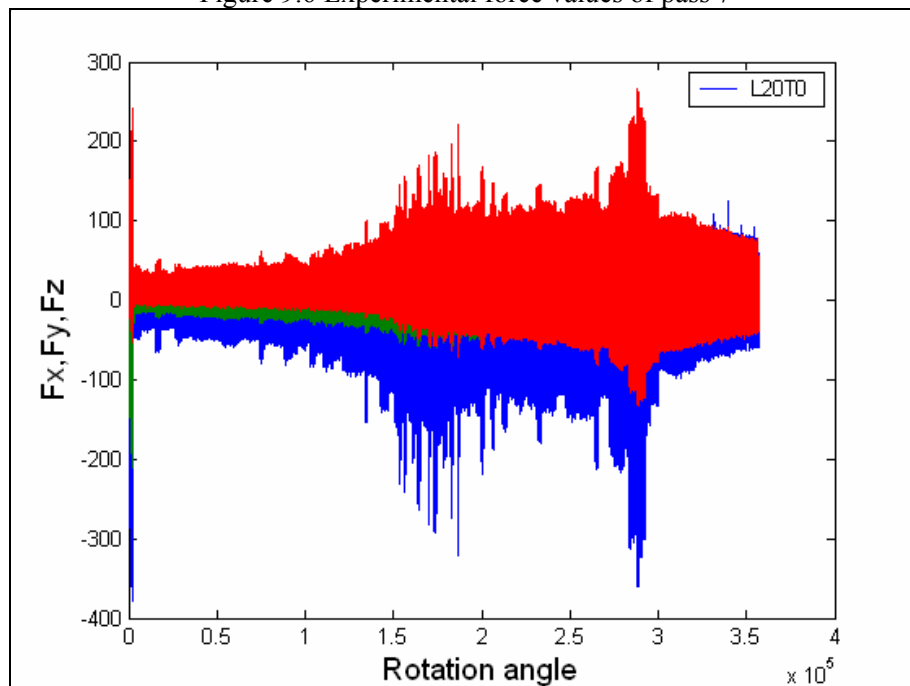


Figure 9.7 Experimental force values of pass 9

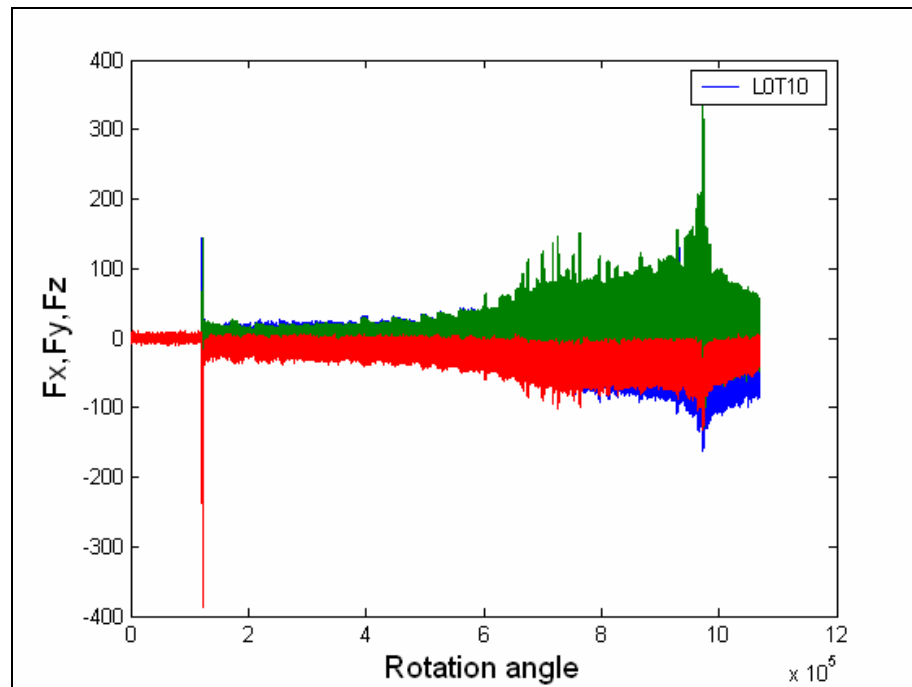


Figure 9.8 Experimental force values of pass 11

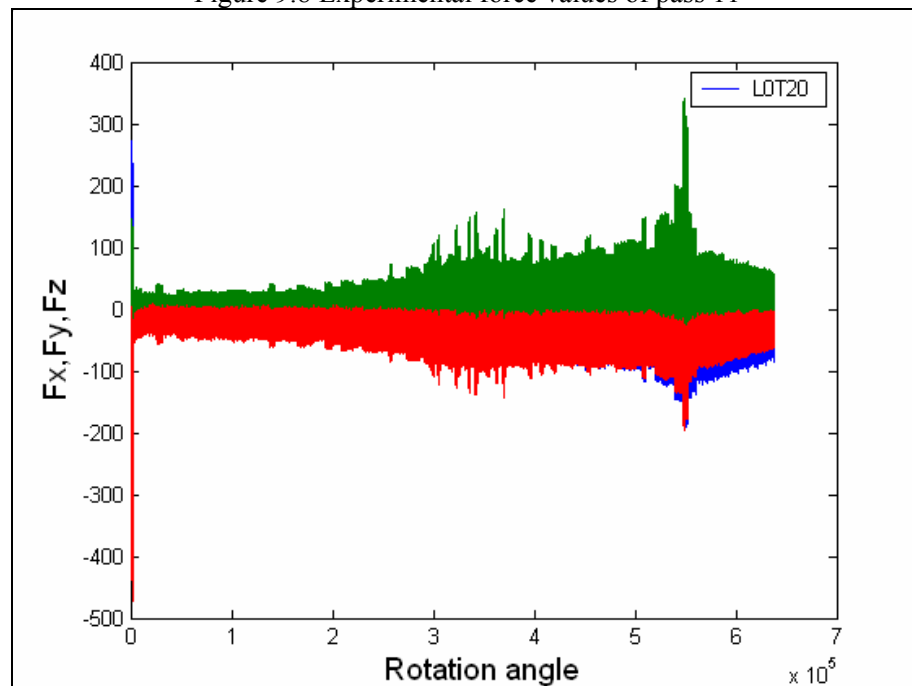


Figure 9.9 Experimental force values of pass 12

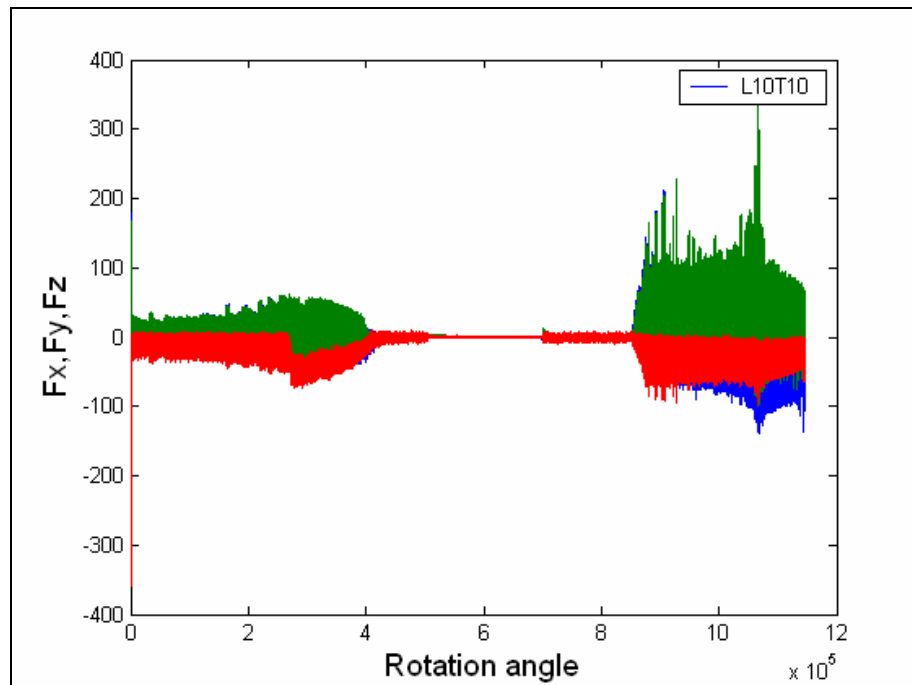


Figure 9.10 Experimental force values of pass 13

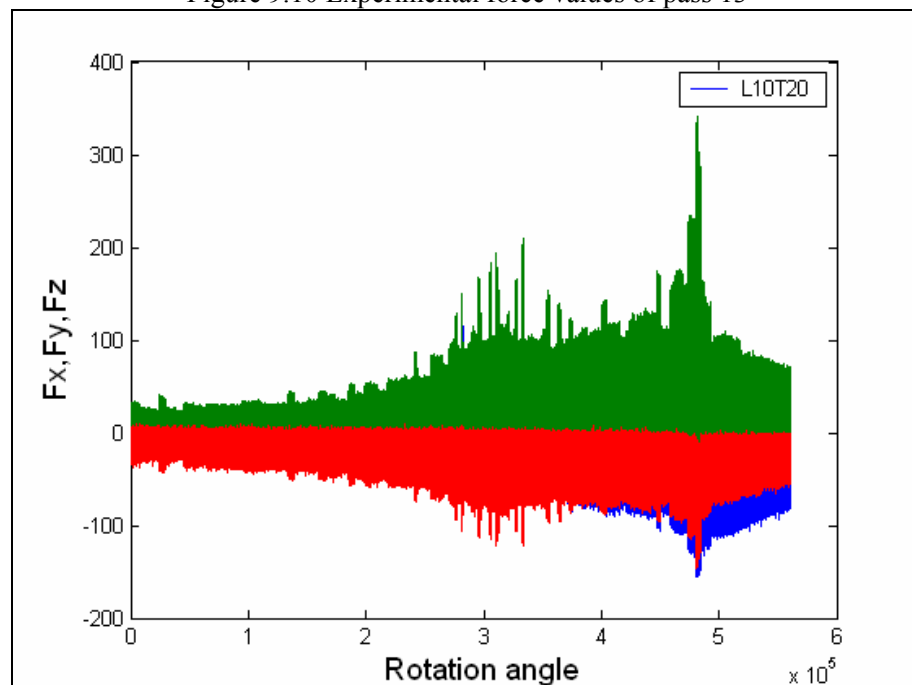


Figure 9.11 Experimental force values of pass 14

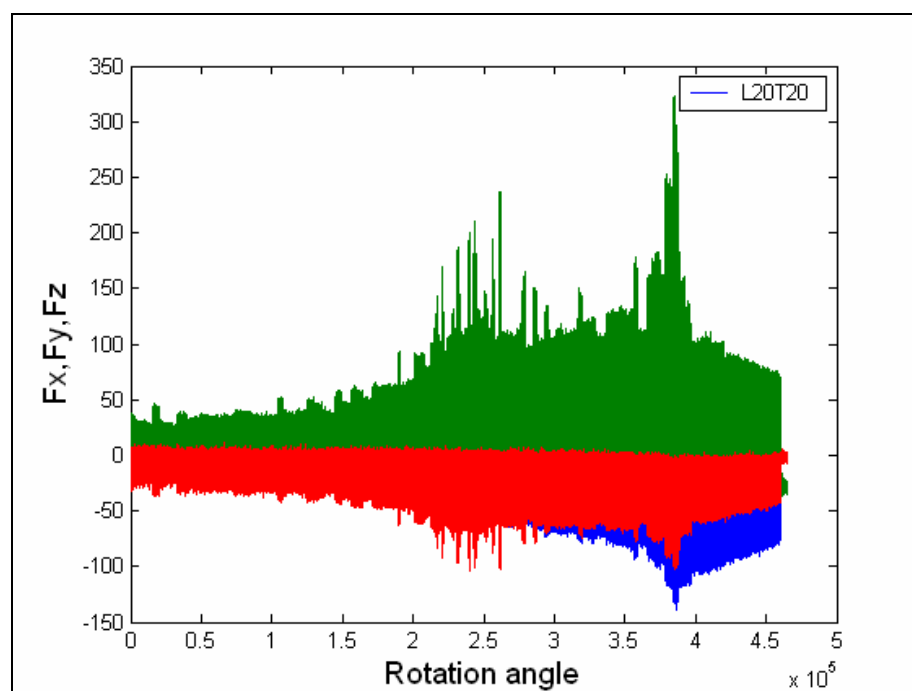


Figure 9.12 Experimental force values of pass 15

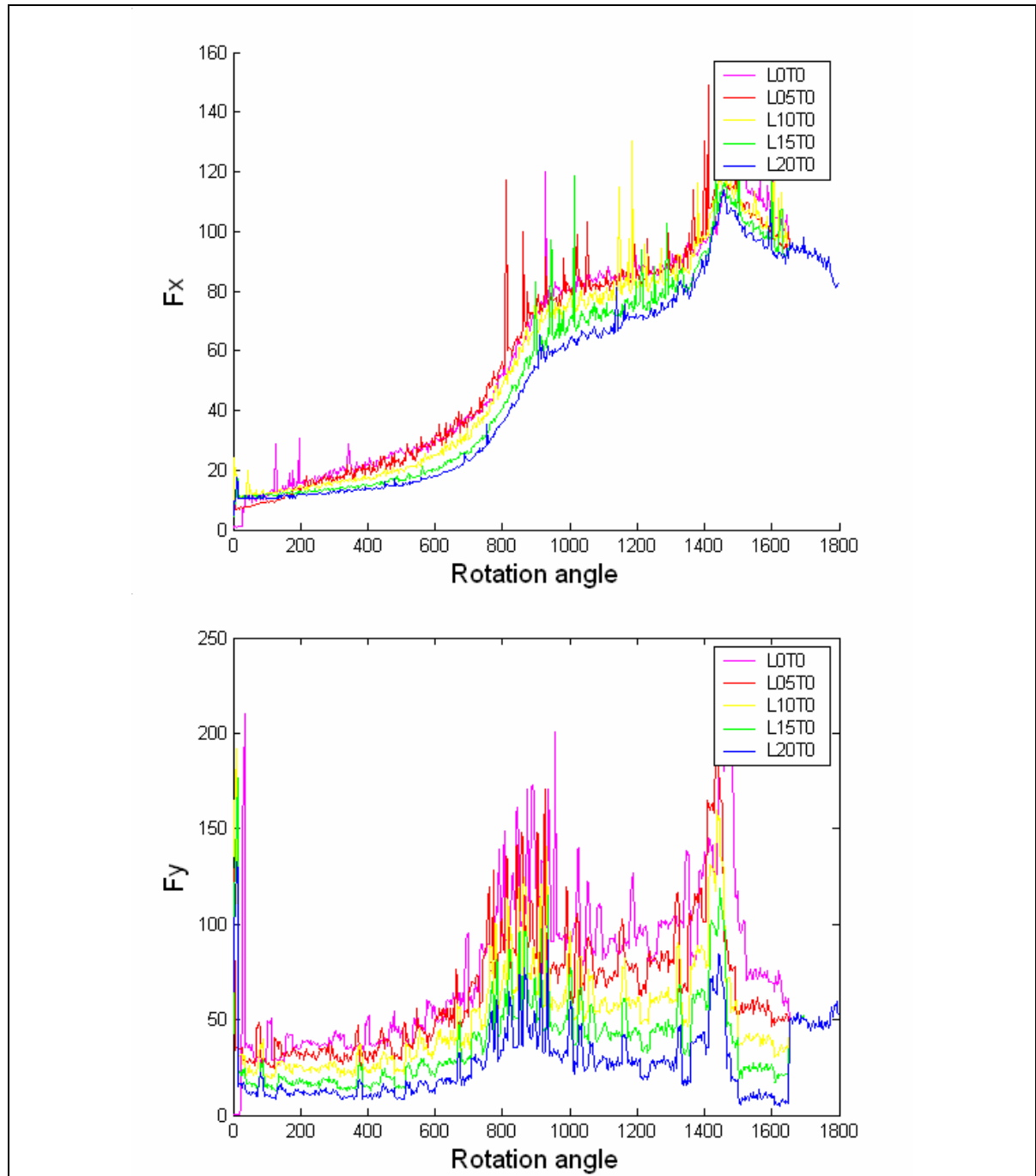


Figure 9.13 Fx and Fy force readings from four axis tests

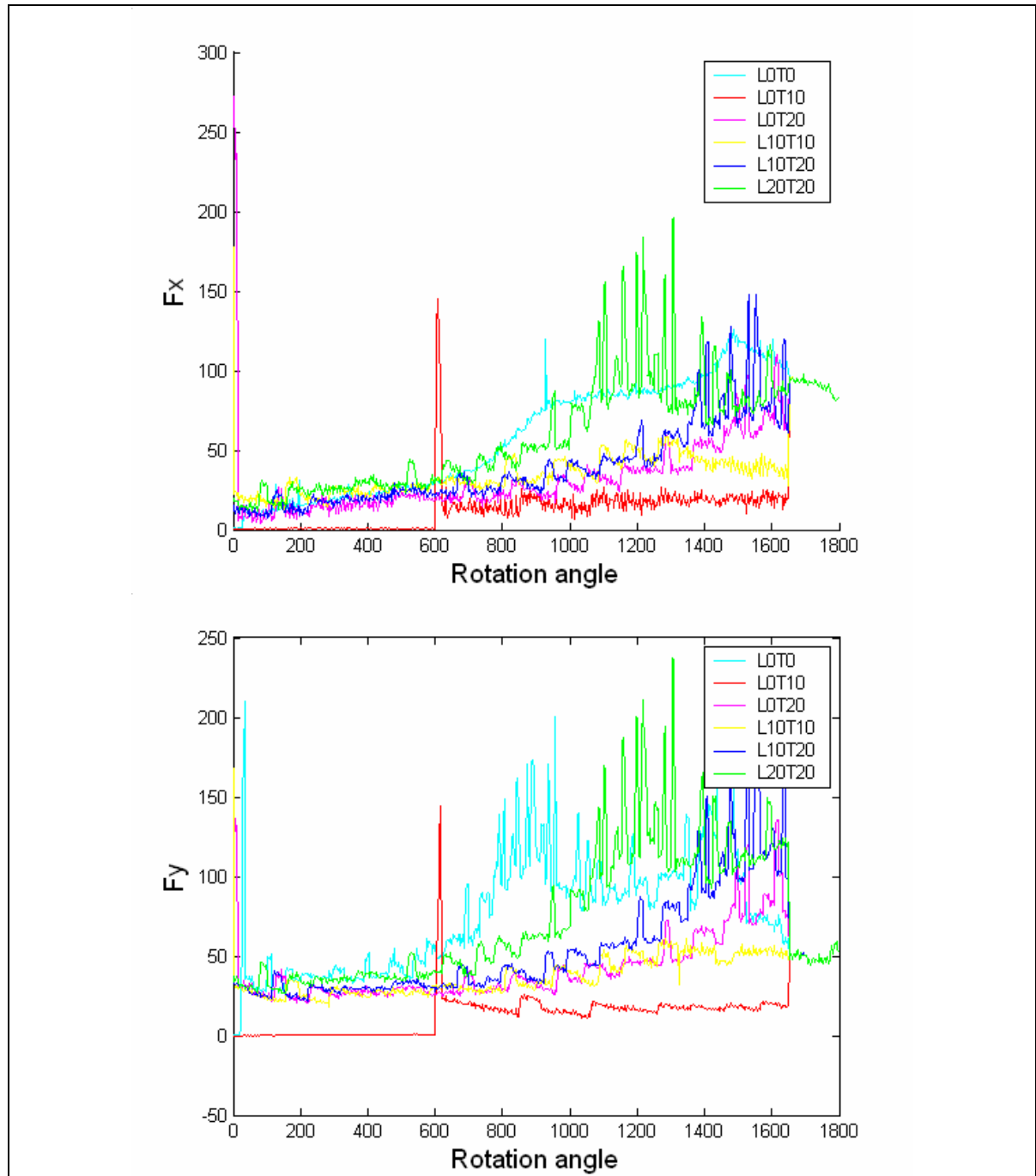


Figure 9.14 F_x and F_y force readings from five-axis tests

Figures 9.3-12 are the cutting test data measured for different lead/tilt angle combinations. Since the workpiece is attached to a rotary table, the force values obtained from the dynamometer are not the absolute force values acting on the cutter. The data are transformed to absolute force readings by using the lead (κ) and tilt (η) angles from the NC code and by using Equation 9.1. It is seen that there are some sudden jumps in the force values due to the NC code generated for these tests. It is expected to have a smooth increasing lead angle in the NC code; however there are some regions that the lead and tilt angles make oscillations. For this reason, there occur sudden jumps in the force signal.

$$A = \begin{bmatrix} \cos(\kappa)\sin(\eta) & \sin(\kappa)\sin(\eta) & -\cos(\eta) \\ \cos(\kappa)\cos(\eta) & \sin(\kappa)\cos(\eta) & \sin(\eta) \\ \sin(\kappa) & -\cos(\kappa) & 0 \end{bmatrix} \quad (9.1)$$

Figures 9.13 and 9.14 are the maximum envelopes of the forces given in Figures 9.3-12. Figure 9.13 represents the four axis cutting tests where only the lead angle changes and Figure 9.14 represents the five-axis cutting tests where tilt angle also changes test to test. Sudden jumps seen in the absolute force Figures can also be seen in Figures 9.13 and 9.14.

9.3 Simulating the Forces in Four-axis Ball-End Milling

In the prediction of forces in four-axis ball-end milling process, the force model presented in chapter 3 can be used with some modifications. If one determines the engagement model in a five-axis machining process, the domain can be used predict the rotated forces and then these forces is rotated to absolute coordinates by the help of lead and tilt angles with Equation 9.1. In addition to that five-axis machining requires an additional rotational analysis of the cutter or workpiece in the space. Since the cutter or workpiece is in rotation while linear feeding is performed, Martelotti's approach [1] needs to be modified by including the rotational phenomenon of cutter/workpiece to the translational model. The chip load presented in chapter 3 is found from the translational kinematic analysis of a point on the surface. In five-axis machining the rotation of cutter/workpiece must be taken into account and a kinematic analysis of the point on the cutter must be performed.

In five-axis machining of free-form surfaces the end mill part of a ball-end mill cutter may be in engagement with the workpiece due to lead (κ) and tilt (η) angles. To account for these cases, end mill model need to be embedded into the force model.

There have been some simulations performed for four-axis cases by using the model given in chapter 4 and are given in Figures 9.15-17. Figure 9.18 is the envelope of x and y directions of the simulated forces. The simulated forces do not agree with the experimental due to incorrect chip load analysis. As a result a chip load analysis needs to perform which combines translational and rotational of the point on the cutter relative to workpiece.

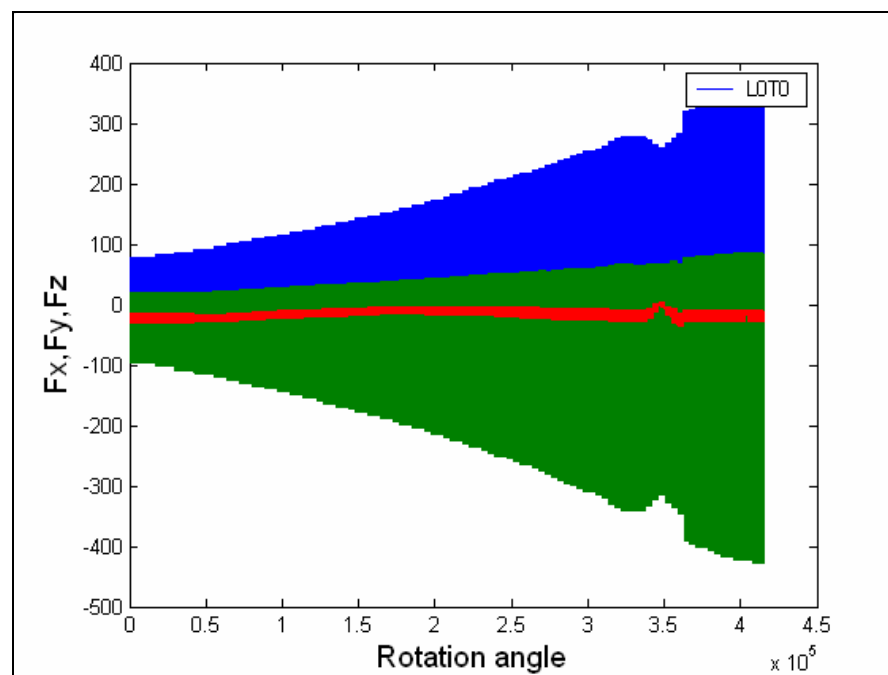


Figure 9.15 Simulated force values of pass 1

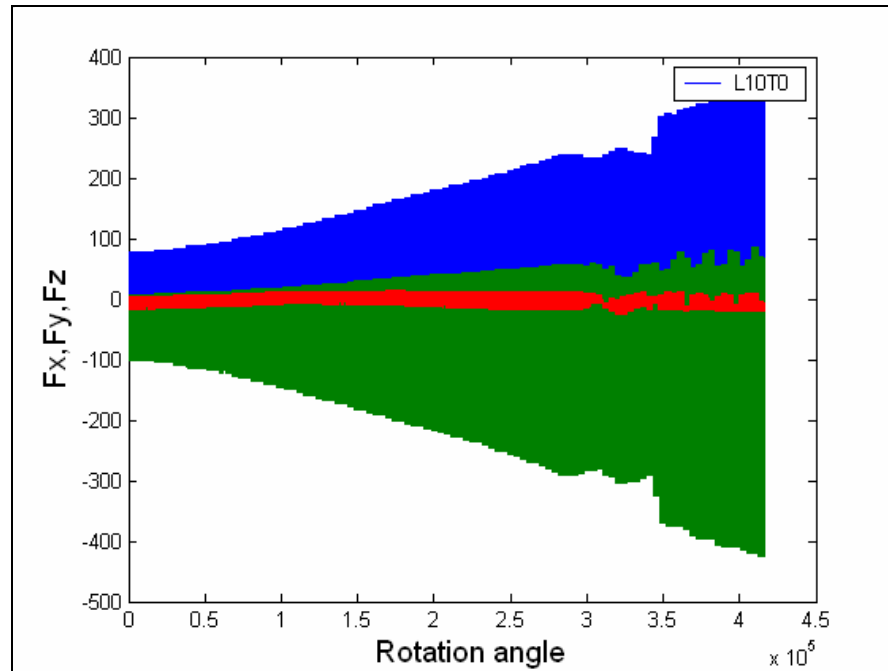


Figure 9.16 Simulated force values of pass 5

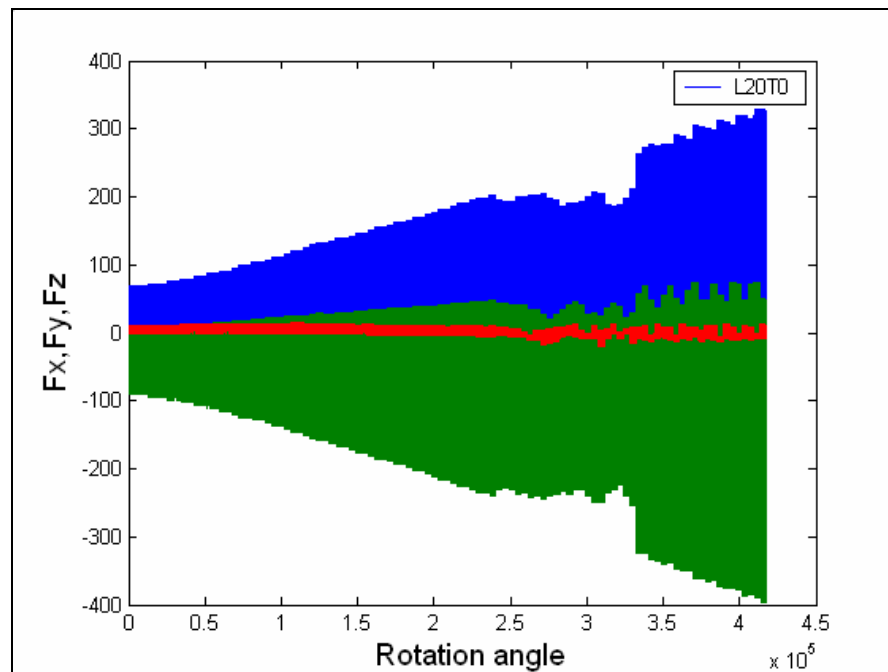


Figure 9.17 Simulated force values of pass 9

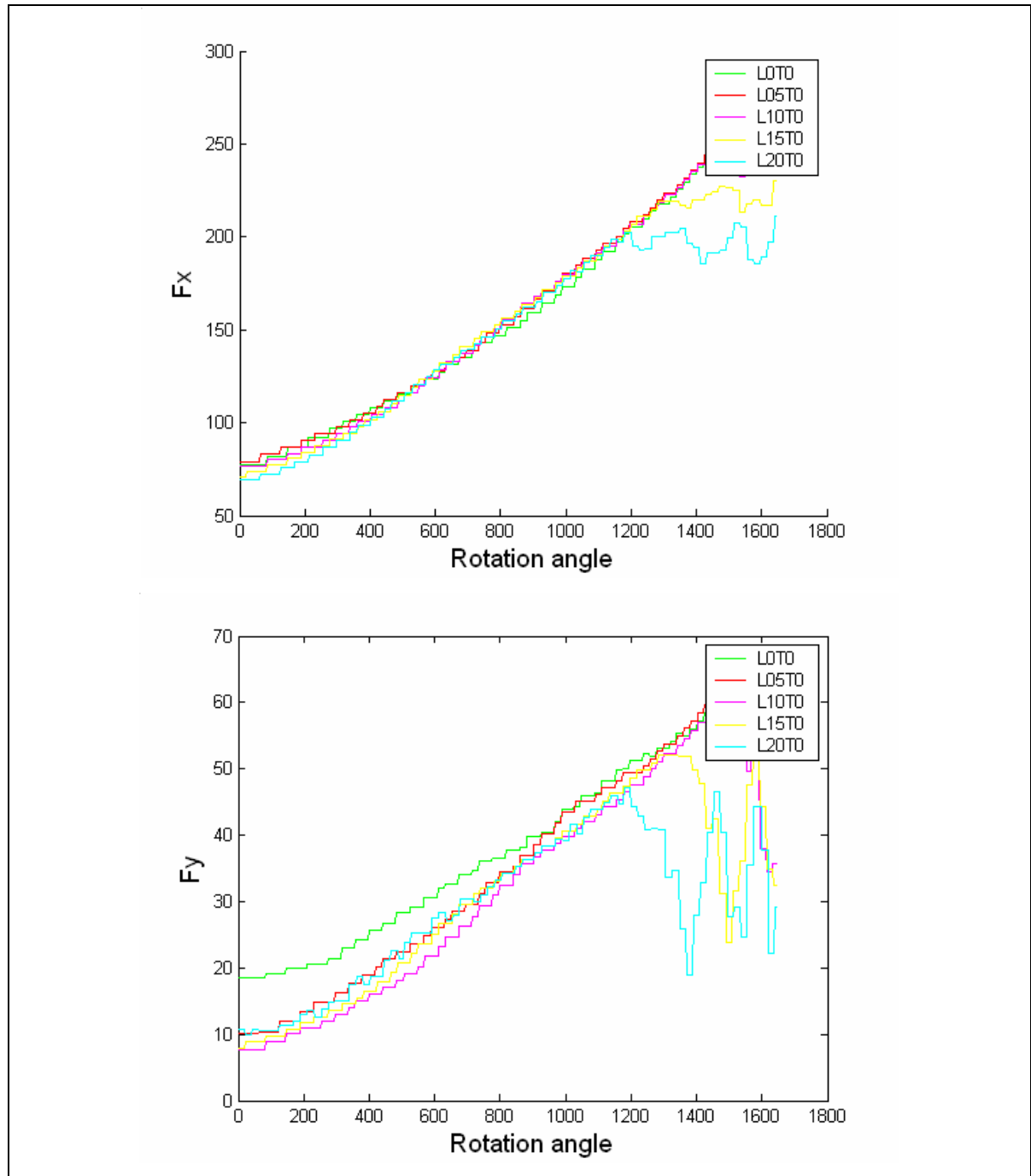


Figure 9.18 Simulated Fx and Fy forces for four axis tests

9.4 Discussions

The advantages of multi-axis machining are quite significant compared to traditional 3-axis modes. Faster machining and a nicer finish makes multi-axis machining a prime selection when 5-axis machining is feasible. Unfortunately, it is far more difficult to employ 5-axis machining compared to 3-axis modes. These difficulties arise mainly from the more complex and quite unpredictable motion that is introduced by multi-axis machining. The 5-axis toolpath introduces many difficult questions regarding the accuracy, validity, and accessibility of the generated motion commands. Five-axis machining of free-form surfaces is a challenging machining process due to uncertainties in the tool/workpiece representations and the post-processing of the machining process. In this chapter, an experimental study is performed to analyze the effect of lead and tilt angles in the force values. 10 combinations of lead/tilt angles are taken into account ranging from 0° to 20° . From the fact that given in chapter 4, for the same depth of cut upward motion results lower force magnitudes than downward motion. In upward motion the engagement region is on the front face of the cutter, however in downward motion the engagement region is on both faces thus producing more chip load thus results higher force magnitudes. That's why, only the plunge cutting cases is taken into account.

One can notice the no force area in Figure 9.10, the post-processor generated to machine this pass has some errors and due to this errors machine behaves unexpectedly. In the machining instance this unexpected behaviour cannot be observed and since the CNC is a property of another institute, a second chance to repeat the cutting test. In five-axis experiments the lowest forces are seen in the lead angle 0° and tilt angle 10° case. Maximum forces is seen for the lead angle 0° and tilt angle 0° case in which tip point is always on the desired surface and in contact.

Chapter 10: CONCLUSION

The objective of the thesis has been to develop a practical method to predict the behavior of ball-end milling operations. Special emphasis has been placed on the prediction of cutting forces in free-form surface machining operations which are widely used in machining dies, molds, automobile and aerospace parts. The results of this study provide the prediction of the waveform and amplitude of cutting forces applied on the cutter and workpiece structures. The prediction of cutting forces in advance helps the process engineers to select 'appropriate values' for the process parameters to achieve a more productive machining level. Besides, knowledge on the cutting forces helps to design better fixtures, contributing to an increase in surface qualities of parts.

In this study, the tool geometry of the ball-end mill has been defined in Cartesian coordinates initially. The ball-end mill cutter and the workpiece have been modeled using Matlab in order to obtain a mathematical frame that will be utilized by the force model. The engagement regions during cut have been detected by the engagement model. Models in previous studies commonly did not detect the engagement regions in a fast and precise manner. Accurate prediction of engagement regions is the basis for making reliable predictions using a force model.

The engagement region predictions were the inputs of the force model together with the cutting and edge constants. Whereas the previous models usually employed transformation methods to make use of orthogonal cutting constants, in this study, the cutting constants have been directly calculated using a practical calibration method. Cutting forces have been analyzed for different regions of the cutter during slot cutting tests and it is seen that these forces have a linear behavior for changing feed rate at each region. As a result cutting and edge constants have been derived through use of the linear relationship between feed rate and cutting forces. The cutting constants had substantially high values near the tip of the cutter and decreased to certain values as the distance from tip was increased.

By the time all inputs for the force model to function have been provided, validation tests have been made in order to test the system. All tests have been realized in the 48 mm/min-192 mm/min feedrate interval since the performed calibration covered this interval. Inclined slot cutting tests for rough milling and finish milling, free-form surface cutting tests and force predictions for these tests are performed. Predicted and measured forces showed good agreement in the all tests except 20°

downward finish cutting test. In the force prediction of the surfaces which have inclination angles higher than 20° , ball-end milling and drilling models need to be combined since the ball-end mill starts to drill or more conveniently the area on the ball end mill which is in full contact with the workpiece is dominant.

Possible enhancements to this model will be including the effect of cutter runout, form errors due to deflection of the cutter and drilling action of ball-end mill. More accurate results will be obtained for even extreme, undesirable cutting conditions once these additions are provided to the model.

The model that is developed in this study is supposed to directly contribute to increasing the productivity of a manufacturing system. The proposed model can be implemented to a CAM package and manufacturing simulations, mechanistic cutting force analysis, dynamic analysis, surface roughness and feedrate scheduling strategies can be performed simultaneously. A feedrate scheduling may be chosen for optimum machining. A limiting force value can be defined and feedrate values can be optimized according to these values. Model can be used to predict the regions where feedrate can be increased, thus substantially decreasing the production time for free-form surfaces.

APPENDIX

A.1 Airfoil Equation

$$\begin{aligned}
 z(y) = & \frac{3486735491899729}{4835703278458516698824704} y^7 - \frac{2628091395046615}{18889465931478580854784} y^6 \\
 & + \frac{6410612500495543}{590295810358705651712} y^5 - \frac{1016208072590567}{2305843009213693952} y^4 + \frac{1}{100} y^3 - \frac{6661}{50000} y^2 \\
 & + \frac{1077}{1000} y - \frac{281203}{50000}
 \end{aligned} \quad (A.1)$$

Norm = 0.00051785

Coordinate axis is given in Figure 7.7.

A.2 Polynomial Fits to Calibration Coefficients

$$\begin{aligned}
 K_{rc} &= -69.698z^3 + 1021.6z^2 - 4634.2z + 6603.5 \\
 K_{re} &= 12.2987 \\
 K_{psic} &= 8.2668z^3 - 71.603z^2 - 77.667z + 1240.8 \\
 K_{psie} &= 9.8839 \\
 K_{tc} &= -69.723z^3 + 1065.9z^2 - 5295.1z + 9566.8 \\
 K_{te} &= 18.6118
 \end{aligned} \quad (A.2)$$

Fitting is performed by using Least-squares method.

A.3 Closed Form Dynamic Force Equation in z-direction

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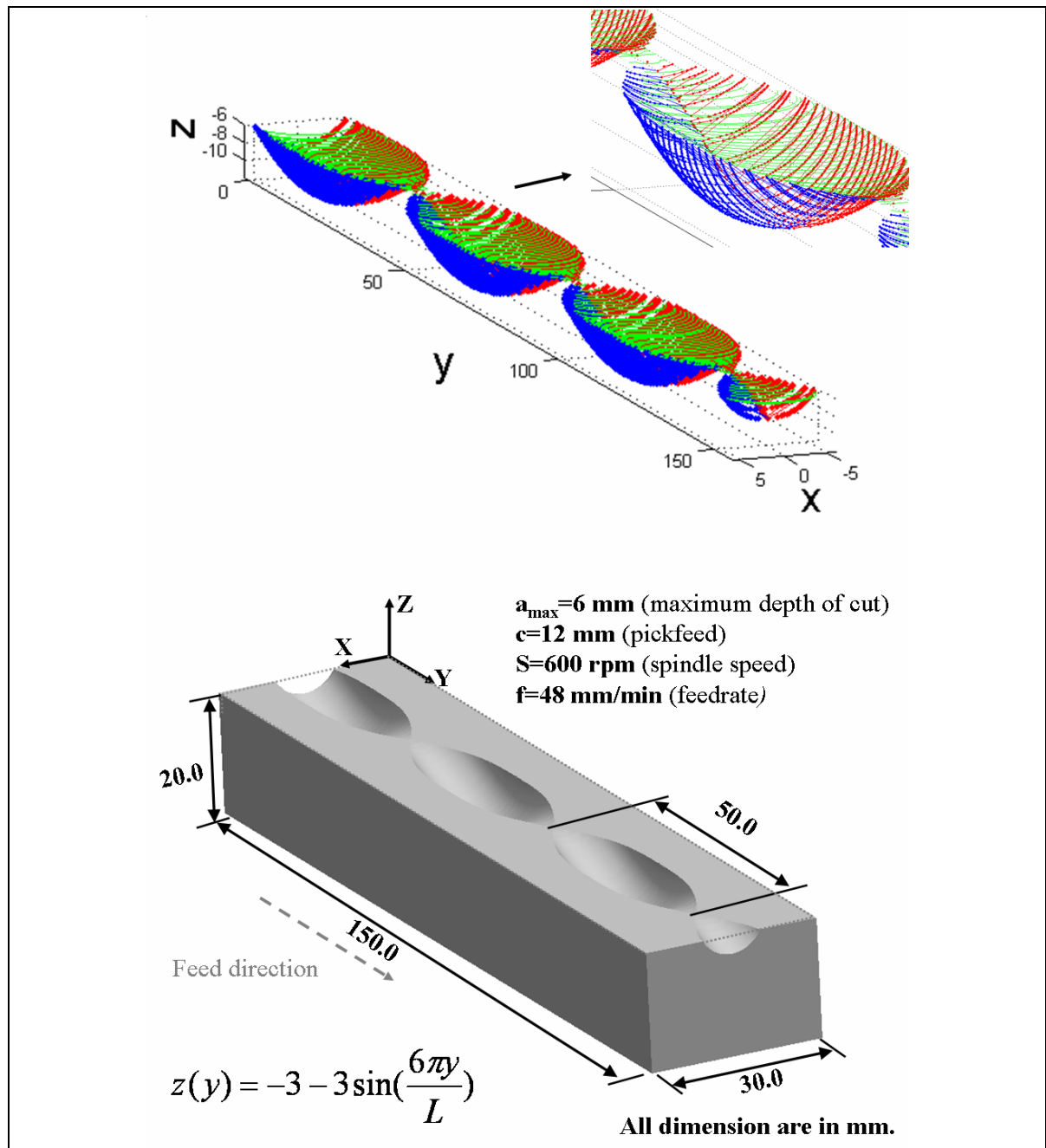
dFx_dz=sin(psi)*sin(teta)*(Krc*h+Kre)+cos(psi)*sin(teta)*(Kpsic*h+Kpsie)+cos(teta)*(Ktc*h+Kte);
dFy_dz=sin(psi)*cos(teta)*(Krc*h+Kre)+cos(psi)*cos(teta)*(Kpsic*h+Kpsie)-sin(teta)*(Ktc*h+Kte);
Fx=inline(int(dFx_dz,z));
Fy=inline(int(dFy_dz,z));
Fx=feval(Fx,teta,x,y,zexit)-feval(Fx,teta,x,y,zentry);
Fy=feval(Fy,teta,x,y,zexit)-feval(Fy,teta,x,y,zentry);

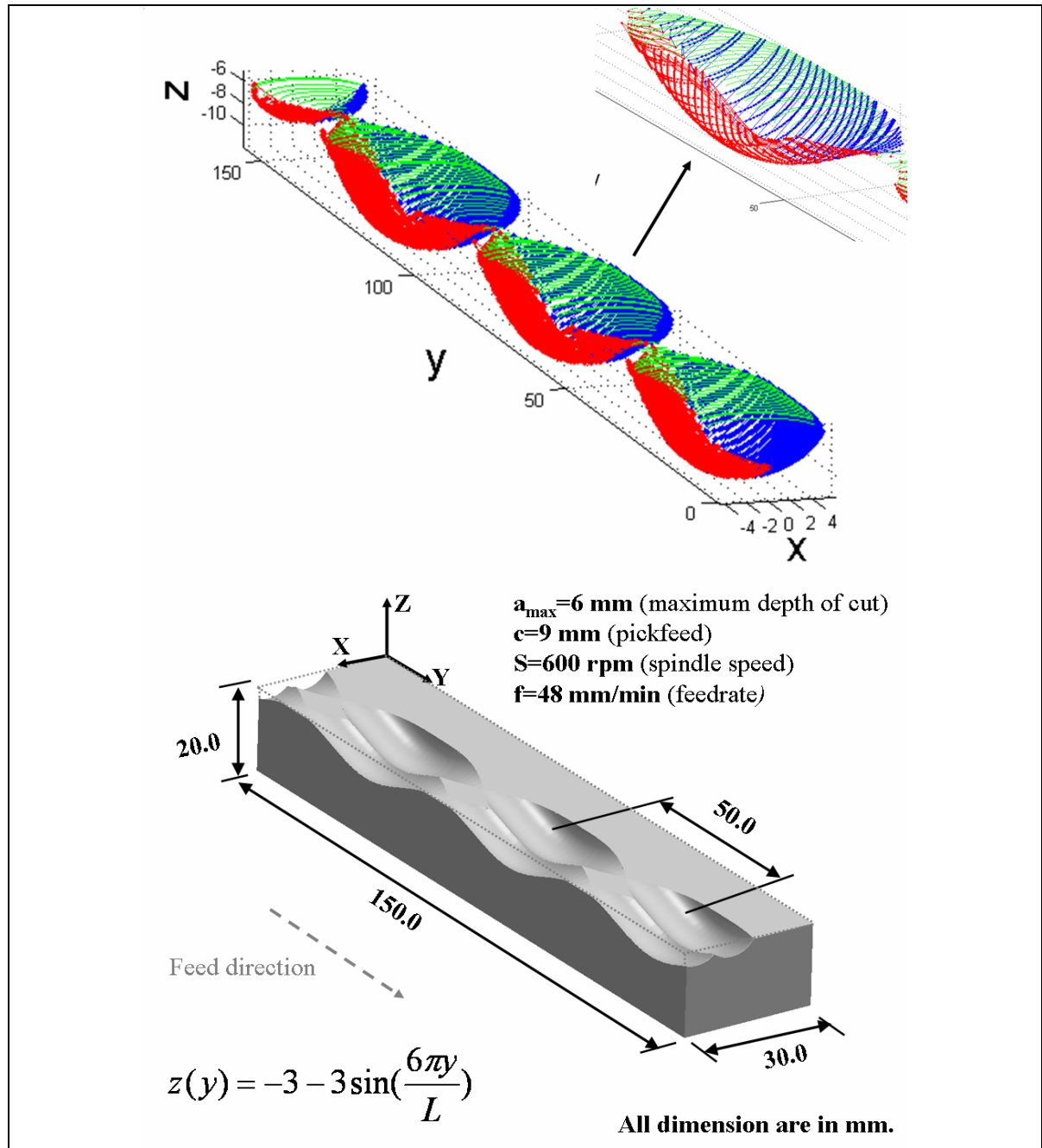
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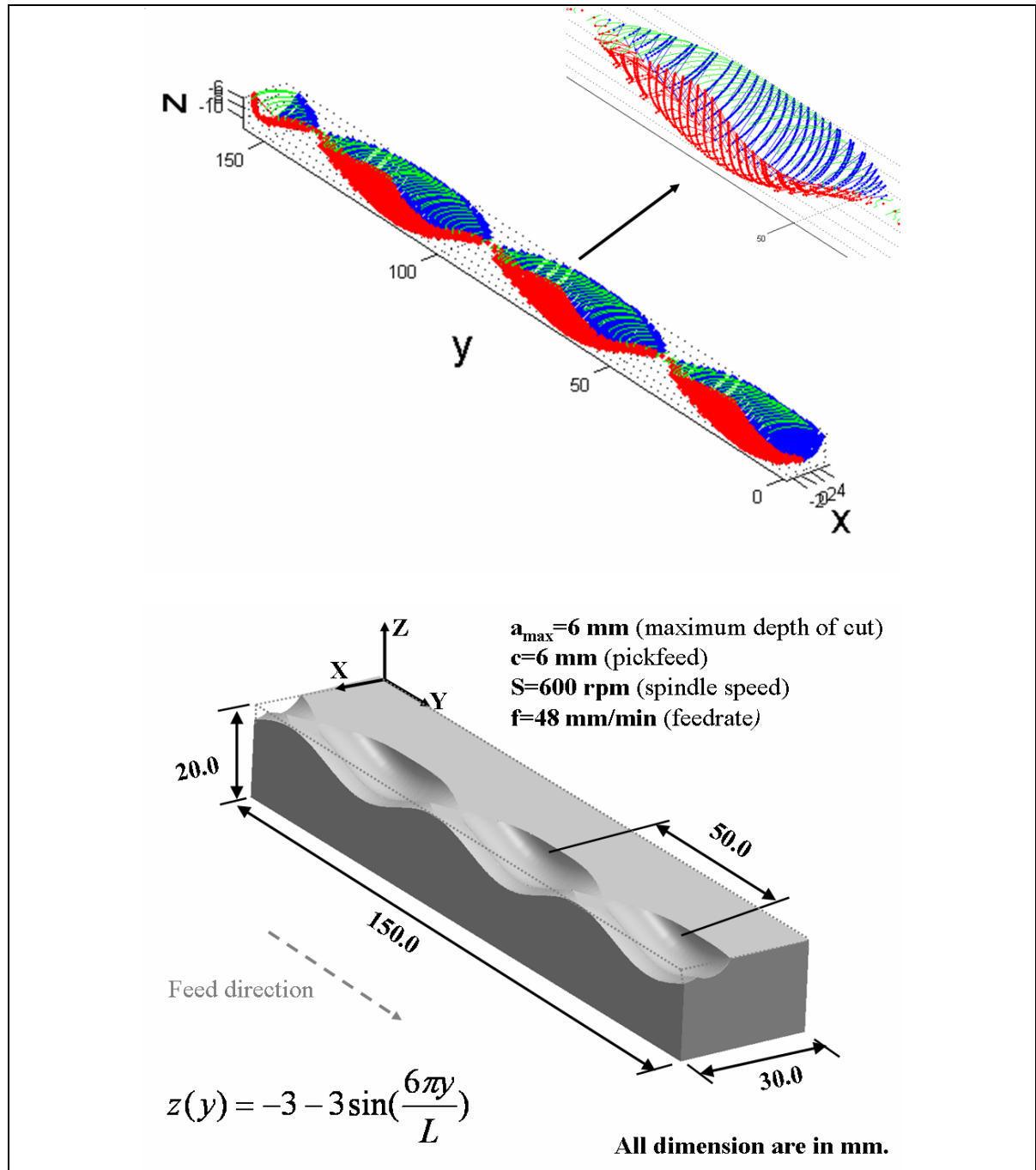
Above code can be used to obtain the closed form dynamic force equation in z-direction. z_{entry} and z_{exit} can be found from the intersection of the cutting edge and the engagement region boundaries. The equation of the projection of the cutting edge on xy plane is;

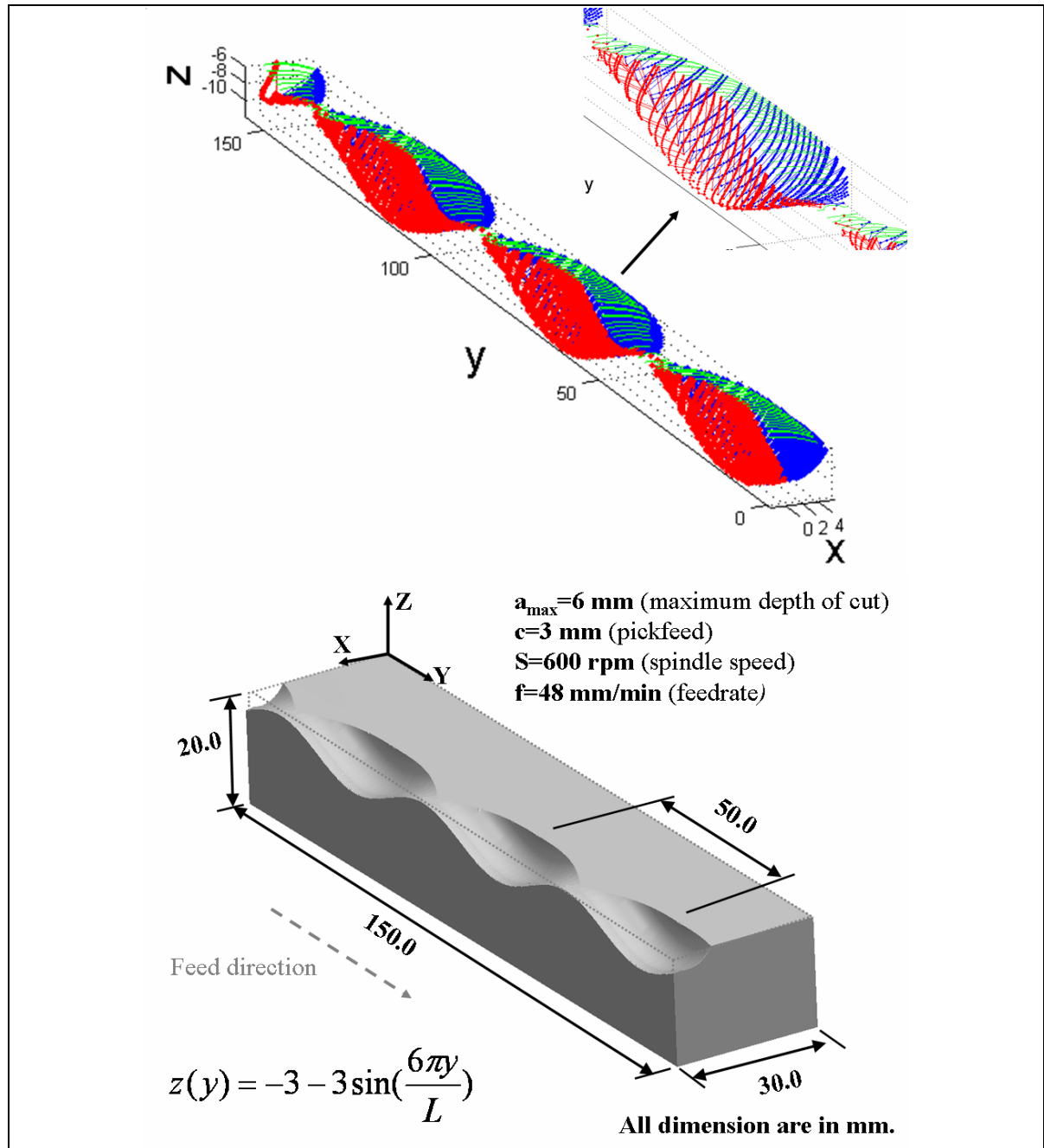
$$\begin{aligned}
 y_c = & \frac{8338850658520403}{18446744073709551616}x^5 - \frac{8134875334203685}{10384593717069655257060992658440192}x^4 \\
 & - \frac{6848007860913289}{1152921504606846976}x^3 + \frac{1058985170225861}{81129638414606681695789005144064}x^2 \\
 & + \frac{7474101883990037}{144115188075855872}x + \frac{7147845662880507}{649037107316853453566312041152512}
 \end{aligned} \quad (\text{A.3})$$

A.4 ACRsim Outputs

Figure A.1 ACRsim output vs. CAD/CAM output for $c=12 \text{ mm}$

Figure A.2 ACRsim output vs. CAD/CAM output for $c=9 \text{ mm}$

Figure A.3 ACRsim output vs. CAD/CAM output for $c=6 \text{ mm}$

Figure A.4 ACRsim output vs. CAD/CAM output for $c=3 \text{ mm}$

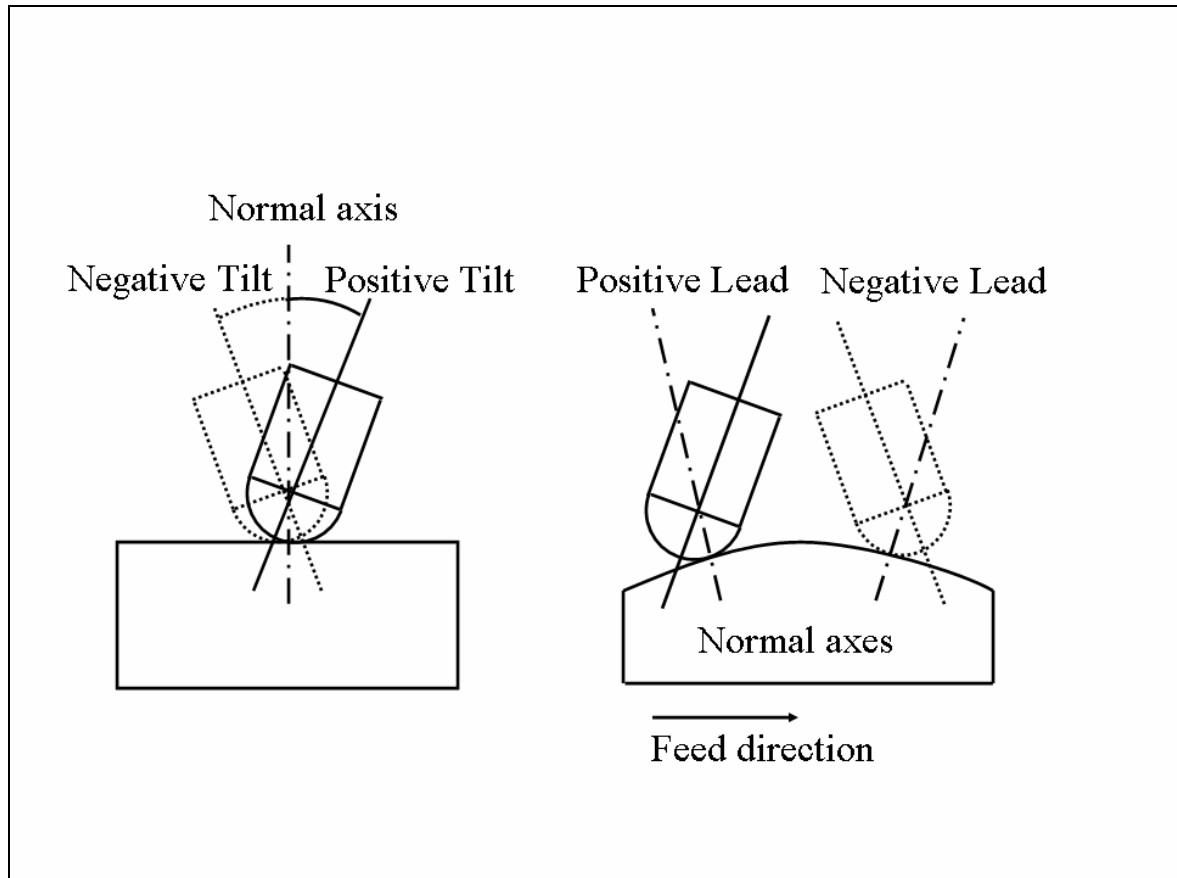
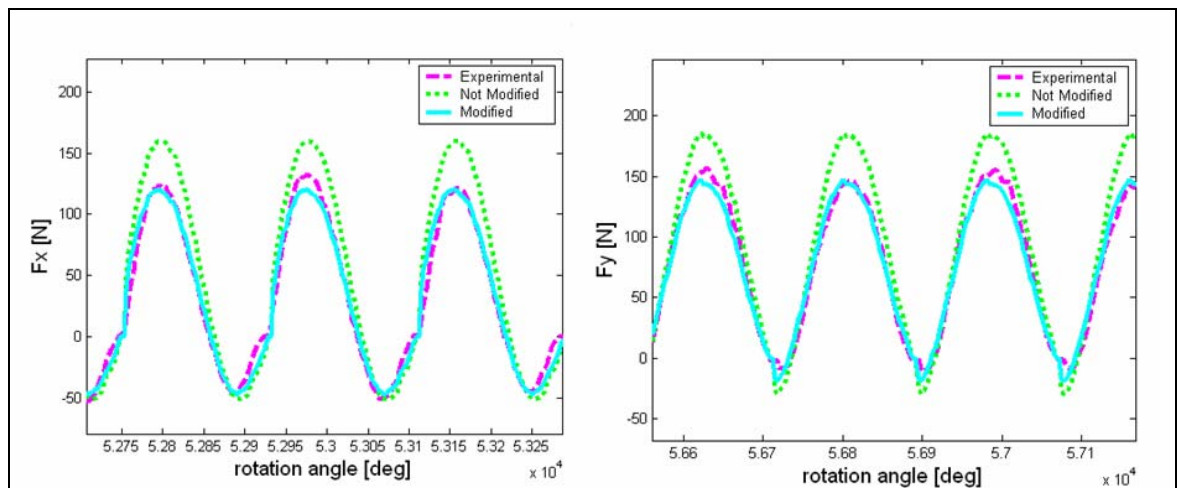


Figure A.5 Lead and Tilt angle illustration

Figure A.6 Modified, not modified and experimental force signals for upward 10° slot cutting test

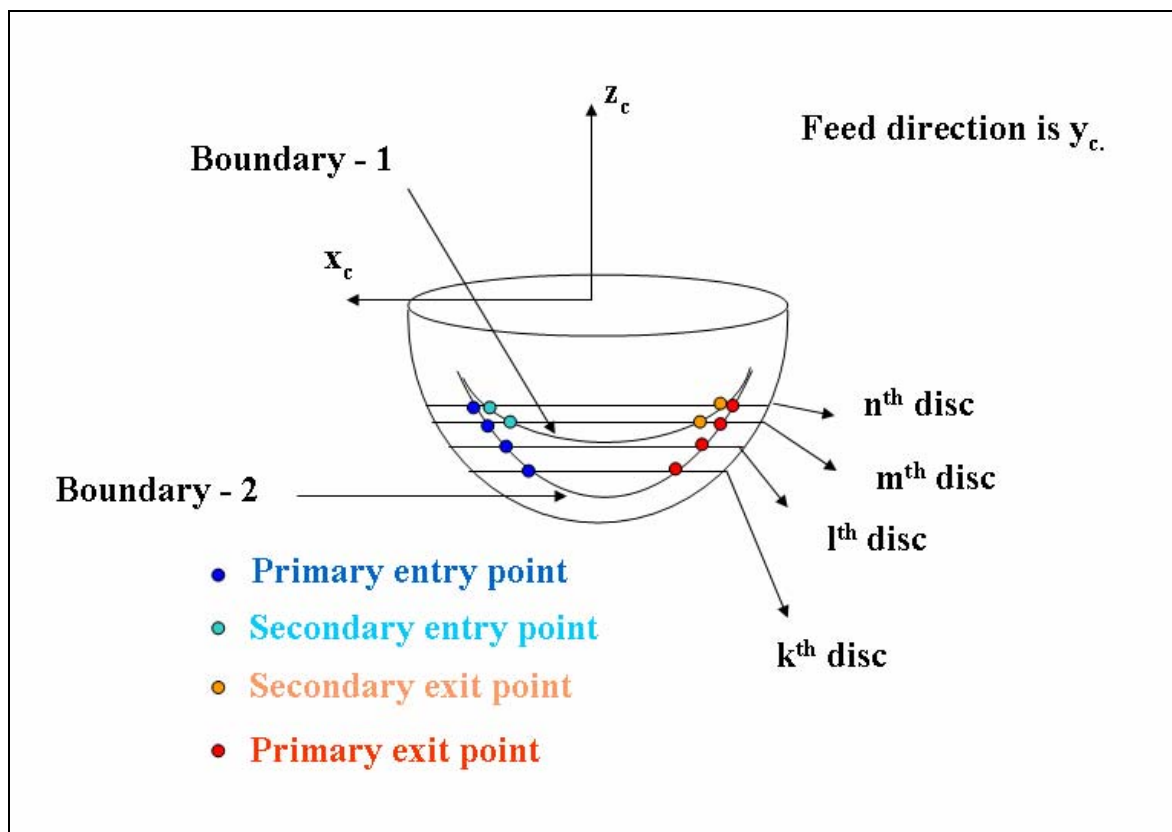


Figure A.7 Double engagement view

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