INTEGRATED PLANNING, MONITORING, AND CONTROL OF MILLING OPERATIONS

by

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Date **Oct 7, 1997**
Abstract

The thesis examines the integration of high level process planning and optimization activities with the normal processes of interpolation and control. The normal approach to part manufacture starts with process planning and proceeds to the programming of tool paths and the actual cutting on the machine tool. The process planning system itself is hierarchical in nature, and many cost or process parameters must be estimated at the higher levels. It is usual, because of the lack of appropriate values for all process parameters, to be somewhat conservative in the planning process. It is also unfortunately usual to discard much of the technological data derived during the planning in the process of producing the standard input format required for CNC machine tools.

The work carried out has shown that there are considerable advantages to the integration of these levels of the manufacturing process. Most importantly, information regarding part geometry and estimated process constants may be passed to monitoring tasks within the CNC, and calibration tasks within the CNC system can pass updated values of actual part parameters back to the planning system for further analysis. Clearly, however, such integration allows much further advantage, and in this regard the possibility of dynamic process planning appears most promising. The concept of dynamic process planning is the provision of the capacity for the machine to reprogram itself in response to feedback from the monitoring tasks. Typical examples of dynamic planning
would include the recovery from tool breakage or, more typically, the replanning of paths within a feature when more or less material than expected is found. (Recovery from tool breakage requires replanning of the remaining feature without the current tool).

The integration of activities on a machine tool requires the presence of an adequate monitoring system, reliable sensors, and suitable models to relate the measured parameters to part geometry and tool condition. The author has developed basic models of cutting force and methods for identifying runout or breakage in milling. The work directed toward runout has mainly considered radial runout; however, simplified models have also been proposed to identify axial runouts. Finally, in order to allow the measurement of force on practical workpieces, the author has designed a simple robust and low cost table dynamometer.
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$F_R$ Resultant force [N]
$F_r$ Radial force [N]
$f_r$ Magnitude components of the relative radial runout series
$F_s$ Shear force [N]
$F_t$ Tangential force in milling [N]
$F_{thrust}$ Thrust force [N]
$F_v$ Power consuming force [N]
$F_X$ Feed force [N]
$F_x,F_y,F_z$ Forces in a Cartesian coordinate frame [N]
$F_y$ Force normal to feed direction [N]
$F_z$ Axial force [N]
$f_z$ Magnitude components of the relative axial runout series
$F_h$ Axial force on tooth $t$ [N]
$h^*$ Critical chip thickness
$h_0$ Undeformed chip thickness [rad]
$h_c$ Final chip thickness [rad]
$h_e$ Equivalent chip thickness [mm]
i Moment of inertia [m$^4$]
i Inclination angle [rad]
$J_c$ Spindle inertia [kg mm$^2$]
$J_m$ Spindle motor inertia [kg mm$^2$]
k$_1,k_2,k_3,k_4$ Cutting constants
$K_b$ Specific cutting pressure [N/mm$^2$]
$K_h$ Spring constant of spindle pulley [N/mm]
$k_h$ Stiffness of the velocity hoop [N/mm]
$K_m$ Motor torque constant [N m s/rad]
k$_{spring}$ Spring stiffness of table dynamometer [N/m]
$I$ Plastic contact length [mm]
$J$ Spring height of table dynamometer [mm]
$l_1,l_2$ Length of primary and secondary cutting edges [mm]
$l_c$ Length of cut [mm]
$l_e$ Length of the active cutting edge [mm]
$L_r$ Rotor coil inductance
$M$ Bending moment [Nm]
$m_{plate}$ Mass of the top plate of the table dynamometer [kg]
$m_c$ Magnitude component of torque due to radial runout

$m_{rad}$ Identification parameter for magnitude components of radial runout
$m_x$ Magnitude component of torque due to axial runout
$N$ Number of cutting edges of a cutter
$n$ Number of pole pairs per phase of stator windings
$n_p,q$ Constants in the tool life equation
$O$ Center of coordinate frame
$p$ Number of supply phases
$m_2$ Cutting constants
$r$ Nose radius [mm]
$R$ Tool radius [mm]
$r_1$ Force ratio for face forces
$r_2$ Force ratio for parasitic forces
$r_c$ Radius of spindle pulley [mm]
$r_h$ Original radius of the velocity hoop [mm]
$r_m$ Radius of drive pulley [mm]
$\text{RPM}$ Spindle speed [rev/min]
$R_t$ Rotor coil resistance
$R_t$ Radial runout of flute $t$
$s$ Spindle actuator slip
$s_{1,s_2}$ Feed per revolution [mm/rev]
$s_{req}$ Shear area identities
$t_1$ Equivalent feed per tooth
$st$ Feed per tooth [mm]
$T$ Tool life [min]
$t_2$ Thickness of cross spring of the table dynamometer
$t_2$ Thickness of longitudinal spring of the table dynamometer
$T_c$ Torque on spindle [N mm]
$t_c$ Cutting torque [N mm]
$t_{co}$ Cutting time per unit length [min/mm]
$t_h$ Heating time [msec]
$T_m$ Torque on spindle actuator [N mm]
$T_{min cost}$ Tool life for minimum cost [min]
$T_{min time}$ Tool life for minimum time [min]
$T_r$ Cutting Torque [N mm]
$T_{r_t}$ Torque of the cutter due to radial runout
$T_{s_t}$ Torque of flute, $t$, due to radial runout
$t_s$ Time to replace a worn tool [min]
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<td>$V_B$</td>
<td>Wear land</td>
<td>[mm]</td>
</tr>
<tr>
<td>$V_{B^*}$</td>
<td>Critical wear land</td>
<td>[mm]</td>
</tr>
<tr>
<td>$V_f$</td>
<td>Supply voltage</td>
<td></td>
</tr>
<tr>
<td>$V_{in}$</td>
<td>Chip entering velocity</td>
<td>[mm/s]</td>
</tr>
<tr>
<td>$V_{out}$</td>
<td>Chip exiting velocity</td>
<td>[mm/s]</td>
</tr>
<tr>
<td>$V_s$</td>
<td>Shear velocity</td>
<td>[mm/s]</td>
</tr>
<tr>
<td>$w$</td>
<td>Width of cut</td>
<td>[mm]</td>
</tr>
<tr>
<td>$W$</td>
<td>Work rate</td>
<td>[N mm/s]</td>
</tr>
<tr>
<td>$W_f$</td>
<td>Friction work rate</td>
<td>[N mm/s]</td>
</tr>
<tr>
<td>$W_s$</td>
<td>Shear work rate</td>
<td>[N mm/s]</td>
</tr>
<tr>
<td>$X$</td>
<td>Machine cost rate</td>
<td>[$/min]</td>
</tr>
<tr>
<td>$x$</td>
<td>Displacement of spindle pulley</td>
<td>[mm]</td>
</tr>
<tr>
<td>$X$</td>
<td>Thermal fatigue parameter</td>
<td></td>
</tr>
<tr>
<td>$x,y,z$</td>
<td>Deflections/positions in a Cartesian coordinate frame</td>
<td>[mm]</td>
</tr>
<tr>
<td>$y$</td>
<td>Ratio of total cycling time to time in cut</td>
<td></td>
</tr>
<tr>
<td>$z_t$</td>
<td>Axial runout of flute t</td>
<td></td>
</tr>
</tbody>
</table>
Acknowledgments

First and foremost, I would like to extend my sincere gratitude to my supervisor Dr. Ian Yellowley. Throughout the course of this thesis, he provided me with valuable technical, moral, and financial support that made my stay at the University of British Columbia a very enjoyable experience.

I would also like to thank my colleagues Ramin Ardekani, Longxiang Yang, Raymond Yeung, Allan Mertin, and Henry Reiser for sharing a wonderful work atmosphere with me.

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Chapter 1

Introduction
The process of part production spans a wide range of activities and usually involves a number of different skills and, perhaps, departments. The process begins with the high level design and progresses through detailed design to the actual production. It has long been realized that the degree of integration between the various activities has great influence on the efficiency, and hence the cost and timeliness of the overall process. Considerable effort has been directed towards the development of integrated computer aided design (CAD), computer aided manufacturing (CAM), computer aided process planning (CAPP), the link between CAD and CAM, and, indeed, computer integrated manufacturing (CIM) which spans the overall production system activities. The thesis presented here is an investigation of potential gains that can be attained from the integration of process planning with manufacturing.

The conceptual product design stage precedes process planning. This stage has been revolutionized by computer aids for drafting and by complementary design tools such as finite element modellers (FEM) which are used to perform stress, heat flow, and fatigue calculations. Various other tools have been added to examine issues ranging from dynamic performance to component packaging. It is now relatively common for part descriptions to be transferred in electronic formats from the design department to the process planning department.
Process planning is a more difficult area to automate. Differences in machines, work shop schedules, and the local cost structure make process planning unique for every company. The first CAPP systems gave experienced process planners access to well organized and categorized data bases of process plans for parts that had been previously produced by the same company. These could be altered and used as guidelines for new parts. The foregoing type of process planning is termed a variant approach and may be contrasted to the generative approach which generates process plans from mathematical models and decision-logic without human interference. Significant advances have occurred in the physical and economic models underlying the generative approach to CAPP. If they are provided with correct descriptions of the properties of the tools, the machines, and the raw and finished part, these models are able to provide good, and possibly optimal, process plans. It is important to note that the quality of the process plan is very much dependent on the quality of the process descriptions. Unfortunately there is always a discrepancy between the parameters describing the process and the actual process. This discrepancy must be compensated for by conservative and less cost effective process plans in order to ensure that the ensuing manufacturing stage can operate safely.

The manufacturing stage of the production process has been computerized for many years. The first computer numerically controlled (CNC) machine tools were introduced 30 years ago, and little has changed in the interface between them and the process planning department. However, recent advances in real
time monitoring, process identification techniques and control could be used to operate the machines more safely and economically. Unfortunately, these techniques can be used to their full potential only if they are provided with geometric and technological parameters that are calculated in the process planning phase. These parameters cannot normally be passed to the CNC machine through the traditional G-code interface between CAPP and CAM. Consequently, the interface needs to be extended to allow the passing of geometric and technological parameters between the planning system and the manufacturing system.

The extension of the interface will reap benefits on two levels. First, as mentioned in the previous paragraph, the machines can use sensory data more efficiently. This use allows them to adapt their control strategy in real time, which in turn leads to a safer and more economical operation of the machine. The second benefit is at the process planning level of the production process where the quality of process plans is dependent on reliable estimates of process parameters. The identification algorithm running on the machine tool during the manufacture of a part can provide improved estimates of the process parameters. These parameters could also be used concurrently to update and execute the same process plan. A system incorporating both these levels of integration is referred to as a dynamic process planning system.
1.1 Objectives of the Research
The main objective of this thesis is the removal of barriers to the complete implementation of a dynamic process optimization system. Such a system should be able to integrate identified process parameters into the real time machine tool control system and the process planning system. The main beneficiaries of a dynamic process planning system are not large batch production industries, but rather the small batch size operations that cannot afford long planning times. Such companies are usually smaller in size and, hence, need to avoid expensive hardware and software. A dynamic process planning system must therefore be able to run on inexpensive machines with low cost robust sensor systems and reliable controls. However, whereas computer hardware is decreasing in price, sensors are still quite costly. Furthermore, few sensor technologies are robust enough to withstand the day-to-day abuse that they would face in an industry environment. This thesis, then, attempts to improve the feasibility of dynamic process planning systems for such companies by removing some of the obstacles to a reliable low cost system.

1.2 Approach of the Research
The development of a prototype dynamic process planning system has been approached in three stages. During the first stage, the relationship between sensory cutting data and some of the required identification parameters was investigated; this investigation lead to the development of new models and algorithms for the identification task. These models were tested using specially
selected cutting procedures that demonstrated the accuracy and the robustness of the techniques. Realizing that these algorithms are useful only if the required sensors are economically justifiable, a new sensor design was developed in the second stage of this research that combines robustness with low cost. A prototype of this sensor was built and tested in real machining conditions. Finally, real time implementations of the identification techniques combined with the new sensor were integrated with a simple CAPP system. This system, even though it is quite limited in its application, is still able to show, even for small companies, the feasibility of a dynamic process planning system.

1.3 Contributions
A dynamic process planning system for general milling processes still poses challenges in many areas. The work described in this thesis attempts to pave the road towards such a system by contributing in four areas.

First, a fundamental model relating sensory information to cutting geometry has been developed. More specifically, an upper bound model for relating cutting geometry of oblique round-nosed inserts to cutting forces is proposed. Secondly, real time identification algorithms for tool condition and product quality have been developed and enhanced. In particular, radial and axial runout have been related to Fourier series coefficients of the cutting forces through analytical equations. Thirdly, effort has been spent on the development of more economical sensor designs. Finally, a 2½D real time process optimization system for peripheral milling operations has been implemented. This
implementation required the development of a reliable and efficient computer architecture for the integration of planning, monitoring, identification, and control. This system is able to demonstrate the feasibility of dynamic process planning.

1.4 Outline
A review of the relevant literature describing the different components required for a dynamic process planning system is presented in Chapter 2. Basic metal cutting theory is introduced and the influence of mechanics and wear on the economics of metal cutting is explained. A careful review of the current optimization leads to the conclusion that a real time process optimization system is required to operate the machine tool safely and economically. The final step, dynamic process optimization, is discussed only in passing because there is little previous work in the area.

A functioning real-time process optimization system is dependent on reliable sensory information. Force sensors are robust and rugged sensors that can be integrated with almost any machine. In this work, an attempt has been made to enhance the understanding of the relationship between the forces and geometry of cut. Chapter 3 introduces an upper bound model that allows the description of oblique tools with a finite nose radius.

Surface finish and edge breakage are among the most important parameters to be monitored; it is likely that they can be inferred from radial and axial runout.
Chapter 4 then is devoted to a model that is designed to predict runouts from on-line force measurements.

An integrated process planning system for 2½D milling operations was developed in the final stage of the thesis. It includes a simple CAD system and performs traditional process planning as well as novel real time control functions. A detailed description of this system is given in Chapter 5.

1.5 Conclusions
This thesis is concerned with the broad area of real-time process optimization. A wide range of critical topics in this field is discussed. A dynamic process planning system is suggested which automatically adapts real-time control strategies, as well as high level process plans, with the help of identified process parameters. Such a dynamic process planning system still requires considerable effort for milling, but the contributions made in this work should demonstrate the feasibility of a low cost system that could be economically justified, even by small companies, in the near future.
Chapter 2

Manufacturing Fundamentals and Literature Review

2.1 Introduction
Process planning and process optimization require sound physical and economic models of the process itself. The various aspects of the metal cutting process have been addressed by many researchers; the most important areas include models of the basic mechanics, cutting forces, tool life, and manufacturing economics. A discussion of previous research in these areas is presented in the following sections. Later sections will examine the integration of these models into process planning and discuss the importance of real time process optimization systems in the selection of safe and economical cutting operations. Finally, the potential benefits of dynamic process planning systems are elucidated.

2.2 The Metal Removal Process

2.2.1 Introduction
Metal can be removed from a workpiece using a variety of processes. The most widely used technique is single point cutting, where a “knife” shaped tool is utilized to remove metal chips. Turning and milling are by far the most widely used metal removal processes. This thesis is focused primarily on the improvement of the economy and safety of the milling process.
2.2.2 Cutting Geometry

Since turning is a much simpler process than milling, more successful force models and process optimization techniques have been developed for this process. A review of such models is given here as background to the required modeling of forces in milling.

2.2.2.1 Turning Geometry

The workpiece in a turning operation is mounted in the chuck and rotates while the tool is fed into the workpiece. The geometry and the cutting forces resulting from the turning process are shown in Figure 1.

![Figure 1. Turning Geometry](image)

The symbols in Figure 1 are defined here:

- $F_v$: Power consuming force [N]
- $F_{\text{thrust}}$: Thrust force [N]
- $F_{r,\text{turning}}$: Radial force [N]
- $F_{z,\text{turning}}$: Axial force [N]
- $a$: Depth of cut [mm]
- $s$: Feed per revolution [mm/rev]
- $l_e$: Active cutting edge length [mm]
- $r$: Nose radius [mm]
- $\psi$: Approach angle [rad]
- $\psi_e$: Chip flow angle [rad]
2.2.2.2 Milling Geometry

In milling the workpiece is fixed while the cutter rotates as it is driven through the workpiece. This leads to intermittent, periodic forces that are more difficult to model than turning forces. Figure 2 shows the cutting forces and the cutting geometry for milling:

Two coordinate systems are commonly used in milling. A rotating coordinate system describes the cutting forces in a cylindrical coordinate frame, and a stationary coordinate system describes the forces in a Cartesian coordinate frame. The symbols shown in Figure 2 are described here:

- $F_t$: Tangential force [N]
- $F_{thrust}$: Thrust force [N]
- $F_{r,milling}$: Radial force [N]
- $F_{z,milling}$: Axial force [N]
- $F_x$: Feed force [N]
- $F_y$: Normal Force [N]
- $T_c$: Cutting Torque [Nmm]
- $R$: Tool radius [mm]
- $a$: Axial depth [mm]
- $s_t$: Feed per tooth [mm]
- $\phi$: Cutter rotation angle [rad]
- $\phi_1$: Insert entry angle [rad]
- $\phi_2$: Insert exit angle [rad]
- $\phi_s$: Swept angle of cut [rad]
2.2.3 Cutting Force Models

Models relating cutting forces to cutting geometry have been developed starting with simple orthogonal geometries that were extended to the general turning geometry and finally applied to the periodic milling process. The following sections provide background on a number of successful cutting force models.

2.2.3.1 Forces in Orthogonal Cutting

Merchant \(^1\) coined the term orthogonal cutting; it applies to the situation where "the cutting tool generates a plane surface parallel to the original plane surface, of the material being cut and its cutting edge is perpendicular to the direction of relative motion of tool and workpiece."

Orthogonal cutting, (see Figure 3) provides the simplest geometry for the study of metal cutting mechanics and many models of the process have been advanced--usually in the form of shear angle solutions.

---

**Figure 3. Orthogonal Cutting Geometry**

- \(F_v\) ... Power consuming force
- \(F_s\) ... Shear force
- \(F_{thrust}\) ... Thrust force
- \(F_p\) ... Normal force
- \(F_n\) ... Normal force
- \(R\) ... Resultant force
- \(F_f\) ... Friction Force

\[
\vec{R} = \vec{F}_v + \vec{F}_{thrust} = \vec{F}_t + \vec{F}_n = -(F_s + F_p)
\]

- \(\alpha\) ... Resultant angle
- \(\beta\) ... Friction angle
- \(\phi\) ... Shear angle
- \(\gamma\) ... Rake angle
- \(h_o\) ... Undeformed chip thickness
- \(h_c\) ... Final chip thickness
- \(l\) ... Plastic contact length
Perhaps the best known approach is that of Merchant \(^2\) which regards the rake face contact as elastic with a constant coefficient of friction. The shear angle resulting from that model is given by

\[ \phi_{\text{Merchant}} = \frac{\pi}{4} + \gamma - \frac{\beta}{2} \]  

(1)

Lee and Shaffer \(^3\) attempted to find the shear angle relationship using a slip line field approach and arrived at the following relationship:

\[ \phi_{\text{Lee\&Shaffer}} = \frac{\pi}{4} + \gamma - \beta \]  

(2)

The validity of these two expressions has been evaluated \(^4\), and improved models based on upper bounds \(^5\), \(^6\), \(^7\) or boundary equilibrium \(^8\), \(^9\), \(^10\) have been formulated.

Assuming that there is only sliding friction on the rake face and that there is a thin primary shear deformation area, the above models lead to the following expressions for the magnitude of the power consuming force and the thrust force:

\[ F_v = h_0 \, w \left( \tau \frac{\cos(\beta - \gamma)}{\sin(\Phi)\cos(\Phi + \beta - \gamma)} \right) \]  

(3)

\[ F_{\text{thrust}} = h_0 \, w \left( \tau \frac{\sin(\beta - \gamma)}{\sin(\Phi)\cos(\Phi + \beta - \gamma)} \right) \]
where \( w \) width of cut
\( \tau \) Shear yield stress of the workpiece.

Given the difficulties that arise in predicting the shear angle and the friction angle, a simplified mechanistic model of cutting forces is often resorted to. The normal approach in practice is to combine the effect of shear angle, rake angle and friction angle under a parameter termed the specific cutting pressure:

\[
F_v = h_o w K
\]
\[
F_{\text{thrust}} = h_o w K r_1
\]

where \( K \) specific cutting pressure
\( r_1 \) force ratio.

This formulation is often replaced by a power relationship between the two orthogonal cutting forces and the undeformed chip thickness in an effort to account for both shear angle variations and edge forces:

\[
F_v = P_1 w h_o^{m_1}
\]
\[
F_{\text{thrust}} = P_2 w h_o^{m_2}
\]

where \( w \) width of cut
\( P_1, P_2, m_1, m_2 \) are constants.

It is possible to account for edge forces while also linearizing this expression. This linearizing operation leads to a formulation of total cutting forces that is composed of cutting forces that are proportional to the undeformed chip cross
sectional area and parasitic forces that are proportional to the length of the active cutting edge:

\[ F_v = k_1 (w h_o) + k_2 w \]
\[ F_{thrust} = k_1 r_1 (w h_o) + k_2 r_2 w \]

where \( k_1, k_2, r_1, r_2 \) are constants.

### 2.2.3.2 Forces in Turning

Most turning tools do not provide a reasonable approximation to orthogonal cutting. The tools typically have a nose radius, \( r \), an approach angle (see Figure 1), \( \psi \), and an angle of obliquity (see Figure 4), \( \phi \). Nevertheless, it is possible to extend the orthogonal cutting model by introducing the concept of equivalent chip thickness \(^{11, 12}\) (which accounts for the non straight cutting edge) and by accounting for obliquity. The equivalent chip thickness is given by

\[ h_e = \frac{A}{l_e} = \frac{s a}{l_e} \]  

where, \( A \) is the area of cut, and \( l_e \) is the length of the active cutting edge (see Figure 1).

The length of the active cutting edge is a function of the tool geometry and the cutting geometry. For a round nosed turning insert it has the following form:
\[
I_e \approx \frac{(a - r(1 - \sin \psi))}{\cos \psi} + R\left(\frac{\pi}{2} - \psi\right) + \frac{s}{2}
\]  

(8)

The equivalent chip thickness combines the effects of nose radius and approach angle on the cutting forces and the cutting temperature. At constant velocity, it has been found that the cutting forces can be expressed as a function of the equivalent chip thickness \(^{12, 13}\):

\[
F_v = k_1 (I_e h_e) + k_2 I_e = k_1 (s a) + k_2 I_e = k_1 s a \left(1 + \frac{h^*}{h_e}\right)
\]

\[
F_{\text{thrust}} = k_1 r_1 (I_e h_e) + k_2 r_2 I_e = k_1 r_1 (s a) + k_2 r_2 I_e = k_1 s a \left(r_1 + r_2 \frac{h^*}{h_e}\right)
\]  

(9)

where: \( h^* = \frac{k_2}{k_1} \) defines a critical equivalent chip thickness that will yield a total cutting force which is comprised of equal components of cutting and parasitic force.

The direction of the in-plane thrust force, which is described by the flow angle \( \psi_e \), completes this force model:

\[
F_{r, \text{turning}} = F_{\text{thrust}} \sin \psi_e
\]

\[
F_{z, \text{turning}} = F_{\text{thrust}} \cos \psi_e
\]  

(10)

The flow angle has been modeled by many researchers. Most of these investigations attempt an empirical force equilibrium with no consideration of kinematic feasibility. Nevertheless, these models are reasonably successful in predicting flow angles, provided they are used at reasonable cutting conditions.
Colwell \textsuperscript{14} has suggested that, in the absence of obliquity, the thrust force is perpendicular to the line connecting the two end points of the active cutting edge. A later paper by Okushima and Minato \textsuperscript{15} suggested that the chip flow is the summation of elemental flow angles over the entire length of the cutting edge. For the case of a straight oblique edge, Stabler \textsuperscript{16} proposed that the chip flow angle is proportional to the inclination angle. Most recently, Young \textit{et al.} \textsuperscript{17} have published a combined approach which assumed Stabler's flow rule was valid for infinitesimal chip widths and summed the directions of the elemental friction forces in order to obtain the direction of chip flow.

The above methods for obtaining the direction of the thrust force are empirical in nature and cannot guarantee kinematic feasibility. Usui \textit{et al.} \textsuperscript{18,19} have proposed an interesting upper bound model for non-orthogonal cutting with a non-straight cutting edge. The model required orthogonal cutting force data for predictions of chip flow, but certainly provided a sound basis for further development.

Given the lack of a scientifically sound model of cutting forces for practical cutting operations, the first step in the research for this thesis was to attempt to develop a simplified but complete model for oblique non-straight cutting edges. The model contains an upper bound solution with force equilibrium between shear forces and friction forces on the rake face. This model is described in Chapter 3.
2.2.3.3 Forces in Milling

The milling process differs from the turning process because the cutting forces on the cutting flutes, or inserts, are discontinuous, and the chip thickness is variable. The actual path taken by a single tooth is described by a trochoidal function. For practical cutting applications, where the feed is much smaller than the radius of the cutter, the chip thickness variation can be approximated quite accurately with a simple sine function \(^{20,21}\):

\[
h_0 = s_1 \sin(\phi)
\]

(11)

The torque acting on a single tooth milling cutter is the sum of a cutting component and a parasitic edge component. The cutting component is approximately proportional to the undeformed chip thickness which, in turn, varies as a function of the angular position of the tooth. The parasitic component is proportional to the length of the active cutting edge:

\[
T_c = RK a s_1 \left[ \sin(\phi) + \frac{h^*}{h_e} \right]
\]

\[\text{for } \phi_1 \leq \phi \leq \phi_2\]

\[
T_c = 0
\]

\[\text{for } 0 < \phi \leq \phi_1 \text{ and } \phi_2 < \phi \leq 2\pi\]

(12)

In this expression \(K\) is the specific cutting pressure, and \(h^*\) is the critical value of equivalent chip thickness for which metal cutting and parasitic force components are equal \(^{13}\).
The tangential force component is related to the torque through the radius of the cutter:

$$F_t = \frac{T_c}{R} \quad (13)$$

The radial thrust force is approximated, using a force ratio for the metal cutting force $r_1$, and a second force ratio for the parasitic force component $r_2$:

$$F_{thrust} = K_a s \left[ r_1 \sin \phi + r_2 \frac{h^*}{h_e} \right] \quad (14)$$

The thrust force is split into an axial component and a radial component:

$$F_{x, milling} = F_{thrust} \cos(\psi_e)$$
$$F_{y, milling} = F_{thrust} \sin(\psi_e) \quad (15)$$

where $\psi_e$ is the direction of chip flow.

These forces can be resolved into the feed and normal directions:

$$F_x = F_t \cos \phi + F_{x, milling} \sin \phi$$
$$F_y = F_t \sin \phi - F_{x, milling} \cos \phi \quad (16)$$

In this model, the cutting constants $K$, $h^*$, $r_1$, and $r_2$ are found by fitting experimental data. The flow angle $\psi_e$, however, is a function of the cutting
geometry and the friction characteristic of the chip on the rake face. A model describing this relationship is shown in Chapter 3.

The periodic, intermittent nature of the cutting forces in milling lends itself to frequency domain analysis. The convention for representing cutting torques and cutting forces for milling operations in this thesis is as follows:

\[
T_\phi = NRK a s_t \left[ \frac{a_n}{2} + \sum_{k=1}^{\infty} a_k \cos(k\phi) + b_k \sin(k\phi) \right] \\
F_x = NK a s_t \left[ \frac{a_{x_s}}{2} + \sum_{k=1}^{\infty} a_{x_k} \cos(k\phi) + b_{x_k} \sin(k\phi) \right] \\
F_y = NK a s_t \left[ \frac{a_{y_s}}{2} + \sum_{k=1}^{\infty} a_{y_k} \cos(k\phi) + b_{y_k} \sin(k\phi) \right] \\
F_z = NK a s_t \left[ \frac{a_{z_s}}{2} + \sum_{k=1}^{\infty} a_{z_k} \cos(k\phi) + b_{z_k} \sin(k\phi) \right]
\] (17)

It should be pointed out that the period of the fundamental frequency of the Fourier series is at the spindle rotation frequency.

Gygax \textsuperscript{22} was the first to suggest that in the frequency domain, multi-tooth milling forces can be described in terms of the single-tooth response through the use of a convolution operator.

Yellowley \textit{et al.} \textsuperscript{23} have used frequency domain analysis of milling forces in order to arrive at efficient identification techniques for swept angles of cut and wear from in-situ measured cutting forces. More recently, work by Wang and
Liang$^{24,25}$ has extended the frequency domain expressions to include radial spindle axis offsets for helical endmills.

Given the elegance of the frequency domain analysis and the inevitable axial and radial runout of individual inserts, an identification algorithm for these phenomena is shown in Chapter 4. The algorithm is able to detect edge breakage of individual inserts in the presence of significant original runout, and it is also able to predict the runout of individual inserts, a parameter which is important given the influence it has on part finish.

### 2.2.4 Modeling of Tool Life

The replacement cost of cutting tools has a strong influence on the cutting economics. The tool life is influenced to a varying degree by a number of factors. For turning these include

<table>
<thead>
<tr>
<th>Work material</th>
<th>tool material</th>
<th>tool geometry</th>
</tr>
</thead>
<tbody>
<tr>
<td>cutting speed</td>
<td>feed/tooth</td>
<td>depth of cut</td>
</tr>
</tbody>
</table>

For milling this list is extended by another six parameters:

<table>
<thead>
<tr>
<th>width of cut</th>
<th>time in cut</th>
<th>time out of cut</th>
</tr>
</thead>
<tbody>
<tr>
<td>cutter diameter</td>
<td>entry conditions</td>
<td>exit conditions</td>
</tr>
</tbody>
</table>
Tool wear is caused by a wide variety of physical mechanisms; it occurs in two major regions of the tool as follows:

- Flank wear - abrasion on the tool flank and is usually measured in terms of a wear land width, VB (see Figure 5), and may be strongly influenced by cutting temperature;

- Crater wear - (see Figure 5) caused by high temperature and pressure between chip and rake face, a mechanism due primarily to mass diffusion between chip and tool.

Given the predominance of flank wear in determining tool life, tool life models concentrate on the first wear mechanism. It is found that there are three regions of wear which occur during the life of a tool (see Figure 6). Initially the tool wears quite rapidly; the wear rate then flattens out in the secondary zone. At the end of the useful tool life, when the wear land has reached a critical value VB*, the wear rate starts increasing again, and finally the tool fails.
Because of the large cost that is associated with tool breakage during a cutting operation, tools should be exchanged before they reach the critical wear land size.

2.2.4.1 Tool Life in Turning
A number of models that relate tool life to cutting parameters have been developed; most of them are extensions of a model originally put forward by Taylor in the form:

\[ V T^\alpha = \text{constant} \]  

(18)

where: 
- \( T \) tool life [min]
- \( V \) cutting velocity [mm/min]
- \( \alpha \) a constant.

This model was eventually extended to include the influence of feed \( s \) [mm/rev], and the cutting depth \( a \) [mm]:

\[ V T^\alpha s^\beta a^\gamma = \text{constant} \]  

(19)

where: 
- \( \beta, \gamma \) are constants.

Typically \( \alpha \) takes on values between 0.1 and 0.5, whereas \( \beta \) is larger than \( \gamma \), but smaller than one. These ranges imply that velocity has the greatest influence on tool life and after that, feed and depth of cut. As was the case for the original equation, the extended tool life equation is valid when cutting with no built-up edge.
It is possible to reduce the number of experimental constants by replacing feed and depth with the equivalent chip thickness. This simplification leads to a new tool life equation that is also independent of changes in approach angle and nose radius:

\[ VT^\alpha h_0^\delta = \text{constant} \]  

(20)

where: \( \delta \) is a constant.

### 2.2.4.2 Tool Life in Milling

Since milling is an intermittent process, the tool life is dependent on more factors than in turning (see section 2.2.4). Taylor's original tool life equation has been extended by many researchers to account for these parameters. Yellowley and Barrow suggested an equation that includes all factors except tool entry and tool exit condition. Furthermore, it is only valid in cutting conditions with no built-up edge and when the lag angle between the leading and the trailing edges of the cutter is small:

\[ T = \frac{\phi_s}{2\pi} \frac{\text{constant}}{X^n s_{eq}^{1/\beta} V^p a^q} \]  

(21)

where:  
- \( s_{eq} \) equivalent feed per tooth  
- \( X \) thermal fatigue parameter  
- \( n, p, q \) constants.

The concept of equivalent feed has been introduced to cope with the discontinuous nature of the chip and the approximately sinusoidal variation in
chip thickness. It is defined as the feed rate which, when maintained constant, will yield the same average wear rate as the variable chip thickness in the actual milling process. Assuming a Taylor-like relationship between feed and tool life, the following equation for equivalent feed is obtained:

\[ s_{eq} = \left( \frac{1}{\phi_s} \int (s \sin \phi) \, d\phi \right)^{\beta} \]  

(22)

The thermal fatigue parameter, \( X \), is intended to characterize the influence of the in cut time ratio, which is the ratio between the actual cutting time and the total cycle time. It affects the magnitude of the temperature cycle and the attendant range of compressive strain in the tool surface:

\[ X = E_r \sqrt{\text{RPM} \, y} \]

\[ E_r = 39 \log(t_c) - 23 \log(t_h) + 37.5 \]  

(23)

where: \( E_r \) range of thermal strain parameter  
RPM spindle speed [rev/min]  
\( t_c \) cooling time [msec]  
\( t_h \) heating time [msec]  
y ratio of total cycling time to time in cut

### 2.3 Process Economics

#### 2.3.1 Introduction

The overall consideration of component cost requires one to examine economic optimization at three decision levels. At the lowest level the selection of the
cutting parameters of feed and speed determines how much metal is removed in a given time and how fast the tool wears. The next level in the cost hierarchy is the selection of radial depth in turning, and of radial width and axial depth in milling. This process is referred to as path subdivision. At the highest level, economic gains depend on the manner in which the major operations are combined and sequenced.

There are two approaches to minimizing the overall process cost. Most research has been directed towards modeling the process mathematically and using nonlinear optimization techniques to arrive at minimum cost solutions for the cutting parameters\textsuperscript{30,31}. These techniques have been applied to single pass turning\textsuperscript{32}, multi-pass turning\textsuperscript{33}, as well as to single-pass\textsuperscript{34} and multiple-pass milling\textsuperscript{35}. Such approaches usually assume an exact knowledge of the parameters influencing the cost equations of the cutting conditions. The possession of this knowledge is an unlikely scenario, and a second group of researchers has attempted to circumvent the time-consuming exact optimization strategies by replacing them with good heuristic techniques with a limited sensitivity to estimated process parameters in order to select close to optimum machining parameters\textsuperscript{36,37,38}.

The following sections will follow the second approach, since it is believed that obtaining true optimum solutions to the minimum cost cutting conditions is not realistic in most real time machine tool environments.
2.3.2 Selection of Machining Conditions in a Single Pass Optimization

Feed and spindle speed are selected during the optimization of a single pass. These two variables can be optimized to obtain a minimum production time or minimum cost. Both alternatives require an economic model of the process. Time is spent on three tasks during the machining process:

- Tool manipulation, setting times of feed and speed, loading and unloading the workpiece,
- The machining operation,
- Tool changes.

The first set of operations cannot be influenced by the selection of feed and speed; they are fixed and do not enter the optimization procedure. The two remaining times can be added to obtain the variable time of machining a unit length of material $t_v$ [min/mm]:

$$ t_v = \frac{1}{\text{RPM s}} + \frac{1}{\text{RPM s T}} t_s $$

(24)

where: $t_s$ time to replace a worn tool [min].

The variable cost of machining a unit length of material $C_v$ [$/\text{mm}$] is a result of the machine overhead rate and the tool cost:

$$ C_v = \frac{x}{\text{RPM s}} + \frac{(x t_s + C_t)}{\text{RPM s T}} $$

(25)
where: \( C_t \) tool cost [$],
\( x \) machine cost rate [$/min].

The spindle speed, \( \text{RPM} \) is related to the cutting velocity through the radius of the cutter:

\[
V = 2\pi \ R \ \text{RPM}
\]  \hspace{1cm} (26)

The tool life plays an integral part in both optimization strategies. If the depth of cut in turning and both the radial width of cut and the axial depth of cut in milling are constant, then the tool life can be expressed in the following form:

\[
V \ T^a s^b = \text{constant}
\]  \hspace{1cm} (27)

The variables that need to be optimized are feed and speed. Differentiating the variable time of machining with respect to the velocity \( V \), and setting the result to zero yields an expression of the required tool life for maximum production rate:

\[
T_{\text{min time}} = \left( \frac{1}{\alpha} - 1 \right) t_s
\]  \hspace{1cm} (28)

This expression is constant for a given machine and material; when it is substituted into the Taylor equation, the optimum velocity for minimum machining time is obtained:

\[
V_{\text{min time}} = \frac{\text{constant}}{T_{\text{min time}} s^b}
\]  \hspace{1cm} (29)
Substituting this velocity into the expression for the variable time of machining, yields the following result:

\[
 t_v = \frac{2\pi R}{\text{constant} \cdot \tan t} T_{\text{min \ time}}^{\alpha} \left( 1 + \frac{t_s}{T_{\text{min \ time}}} \right) s^{\beta-1} = c_2 \cdot s^{\beta-1}
\]  

(30)

Clearly, since \( \beta \) is smaller than one, the minimum time solution requires that the feed is maximized, whereas the spindle speed is selected according to equation (29). The maximum feed rate is usually limited by a tool edge breakage or a surface finish constraint.

Differentiating the variable cost of machining (equation 25) with respect to the velocity \( V \) results in a different optimum tool life:

\[
 T_{\text{min \ cost}} = \left( \frac{1}{\alpha} - 1 \right) \left( t_s + \frac{C_t}{x} \right)
\]

(31)

This expression is constant for a given material, tool, and machine combination. Consequently the following variable cost of machining can be obtained:

\[
 C_v = \frac{2\pi R}{\text{constant} \cdot \tan t} T_{\text{min \ cost}}^{\alpha} \left( x \cdot t_s + \frac{C_t}{T_{\text{min \ cost}}} \right) s^{\beta-1} = c_3 \cdot s^{\beta-1}
\]

(32)

Hence, the minimum cost solution requires that the feed be maximized and the cutting velocity be selected to yield the tool life of equation (31).
Edge breakage control in turning requires that the equivalent chip thickness stay below a critical value. For milling this requirement is more complex, because the equivalent chip thickness changes during the rotation of the cutter. Consequently, the maximum equivalent chip thickness throughout the contact of the cutting edge with the workpiece must stay below the critical value.

If the rated torque of the machine tool is exceeded, then the feed needs to be reduced. If the rated power is exceeded, then the spindle speed should be reduced.

2.3.3 Selection of Width/Depth Subdivision in Multi Pass Machining

The decision process as to how many passes the machine tool should use to remove a volume has been studied by a number of researchers for turning and milling. These approaches usually assume a complete knowledge of the tool life and constraint equations (torque or power). As a result, width (depth) of cut, feed, and velocity were optimized as variables in each pass. Since tool life and constraints are not known to a great degree of accuracy in real machining environments, and since width (depth) of cut is the only variable that cannot be changed easily on the shop floor, it seems much more appropriate to attempt to optimize the width (depth) of cut separately and have the operator or a real-time process optimization controller select feed and speed.
Yellowley and Gunn\textsuperscript{36} have investigated the optimization of width (depth) of cut for milling and turning. They found that if the tool life is chosen in an optimal manner (see equation (31)) then the variable cost per unit length of a single pass is directly proportional to the optimum cutting time per unit length, $t_{co}$:

$$C_v = \frac{x t_{co}}{(1 - \alpha)} \quad (33)$$

It is then possible to minimize the cost of multiple passes by minimizing the sum of the individual cutting times per unit length of each pass, $i$:

$$\min \left( \sum_{i=1}^{n} (t_{co})_i \right) = \min \left( \sum_{i=1}^{n} \frac{1}{RPM_s_i} \right) \quad (34)$$

This objective function requires the substitution of a tool life equation. In turning, an extended Taylor equation was utilized (see equation (19)) and in milling equation (21) was used. Yellowley and Gunn also considered tool breakage, chatter, torque and power constraints. They were able to show that the optimal solution in the absence of torque and power constraints requires that all but one pass are taken at maximum allowable width or depth of cut, with the remaining cut removing the rest. The maximum allowable width or depth of cut is determined by the chatter limit or by the physical dimensions of the tool.

In the case of torque and power constraints, the cost function is determined by the concave tool breakage constraint up to a critical value of depth $d_{crit}$. 


Thereafter, the torque constraint and the power constraint take over. This is shown in Figure 7.

There are two possible scenarios. The critical width or depth can be larger or smaller than the even width or depth. This relationship influences the optimal cost strategy.

In the case where the critical width or depth is smaller than the even depth or width, the optimal strategy is to take one pass at $d_{\text{crit}}$ and the rest at equal values, or to take all passes but one at $d_{\text{max}}$, with the last pass taking the remainder.

In the case where the critical width or depth is larger than the even width or depth, the optimal strategy is to take all but one pass at $d_{\text{crit}}$, with the last pass taking the remainder, or to take all but one pass at $d_{\text{max}}$, with the last pass taking the remainder. The better choice between the two alternatives is obtained by calculating the cost of each alternative.
2.3.4 Volume Subdivision or Operation Sequencing

The material that needs to be machined away from the raw part is subdivided into elemental volumes (these are defined as non-overlapping volumes). In the case of the component in Figure 8 for instance, there are three elemental milling components and one final drilling component. Figure 9 demonstrates this subdivision. These elemental volumes are combined into groups (compound volumes) that can be machined together.

In the example in Figure 9, volume one could either be machined together with volume two or with volume three. The optimum combination is the one that minimizes cost. Since real parts generally involve a large number of elemental volumes, enumeration is very inefficient for finding the best solution. A number of different authors have approached this problem. Kishinimani et al.\textsuperscript{39} proposed one of the earliest methods for choosing the optimum compound volume combination. They based their solution on dynamic programming. It requires a priori knowledge of cost of all the compound volumes. This requirement is computationally difficult for real parts that have a large number of compound volumes. Yellowley and Kusiak\textsuperscript{40} proposed a method that is based on seed volumes, where a seed volume is an elemental volume with a machined surface as one of its boundaries. The algorithm combines seed volumes into compound feasible volumes. The cost per unit volume of each of the feasible
volumes is calculated, and the volume with the lowest cost per unit volume is selected. Then all compound volumes that contain elements of the selected volume are taken out of the list and the compound volume with the next smallest cost per unit volume is selected. This process is repeated until no volume elements are left. Imaginary costs of the part shown in Figure 8 are given in Table 1 in order to demonstrate the procedure.

<table>
<thead>
<tr>
<th></th>
<th>mill (v_1)</th>
<th>mill (v_2)</th>
<th>mill (v_3)</th>
<th>Total cost</th>
<th>Cost/vol</th>
</tr>
</thead>
<tbody>
<tr>
<td>(s_1)</td>
<td>X</td>
<td></td>
<td></td>
<td>30</td>
<td>0.6</td>
</tr>
<tr>
<td>(s_2)</td>
<td></td>
<td>X</td>
<td></td>
<td>40</td>
<td>0.5</td>
</tr>
<tr>
<td>(s_3)</td>
<td></td>
<td></td>
<td>X</td>
<td>10</td>
<td>0.9</td>
</tr>
<tr>
<td>(s_4)</td>
<td>X</td>
<td></td>
<td>X</td>
<td>50</td>
<td>0.4</td>
</tr>
<tr>
<td>(s_5)</td>
<td>X</td>
<td></td>
<td>X</td>
<td>45</td>
<td>0.7</td>
</tr>
</tbody>
</table>

Table 1. Compound Volumes and Cost

The algorithm by Yellowley and Kusiak would select cases \(s_4\) and \(s_3\). This algorithm still requires the cost calculation for a large number of machined volumes. Yellowley and Fisher \(^{41}\) showed that for single pass operations with a reasonable cutter radius selection, all volumes have the characteristic that their cost per unit volume decreases as their width and depth are increased. Building on this property, they proposed that all seed volumes be extended until there are no further volumes that can be cut together with the original seed volumes. Then the cost of each of these new compound volumes is determined. The compound volume with the lowest cost per unit volume is assigned to a machining operation and taken from the list of volumes to be machined. The method is then applied recursively to the rest of the volumes until only seed
volumes are left. In this case only cost calculations for s3, s4, and s5 would have been performed.

The number of composite volumes whose cost needs to be evaluated is still not linearly related to the number of elemental volumes, but the improvement is quite dramatic compared to the previous methods.

2.3.5 Conclusion on Process Economics
Three main levels of the process economics problem have been introduced in this section. The detailed problem of minimizing the low level process cost, in this case, has been addressed from two sides. One group of researchers attempts to guarantee optimality by relying on correct process parameters. Since process parameters are never known exactly, these time consuming nonlinear optimization techniques can never be better than the quality of the estimates of the process parameters. Hence, a second approach of more computationally efficient heuristic solutions has been emphasized here; that approach will usually arrive at results of the same quality as other approaches but with considerably less computation.

2.4 Current Sensor Technology

2.4.1 Introduction
A variety of sensor types and designs can be used to infer cutting geometry and tool condition. Surveys of such sensors have been conducted by Michelletti et
al. 42 and Tlusty and Andrews 43. The latter cross-referenced sensor technologies with identification tasks. Table 2 shows their findings.

<table>
<thead>
<tr>
<th>Sensor</th>
<th>Dimensional</th>
<th>Cutting Force</th>
<th>Feed Force</th>
<th>Spindle Motor</th>
<th>Acoustic emission</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Touch Trigger</td>
<td>Non Contact</td>
<td>Dynamo Meters</td>
<td>Promess</td>
<td>Torque Power</td>
</tr>
<tr>
<td>Dimensional check of the blank, machined surface, or thermal drift</td>
<td>***</td>
<td>***</td>
<td>***</td>
<td>*</td>
<td>*</td>
</tr>
<tr>
<td>Tool wear</td>
<td>*</td>
<td>**</td>
<td>***</td>
<td>*</td>
<td>**</td>
</tr>
<tr>
<td>Tool breakage</td>
<td>*</td>
<td>***</td>
<td>*</td>
<td>**</td>
<td>***</td>
</tr>
<tr>
<td>in drilling, in turning, in milling</td>
<td>*</td>
<td>**</td>
<td>***</td>
<td>*</td>
<td>**</td>
</tr>
<tr>
<td>Preload in rolling bearings</td>
<td>***</td>
<td>***</td>
<td>***</td>
<td>***</td>
<td>***</td>
</tr>
<tr>
<td>Friction in guideways, resistance to feed</td>
<td>***</td>
<td>***</td>
<td>***</td>
<td>***</td>
<td>***</td>
</tr>
<tr>
<td>Adaptive control</td>
<td>***</td>
<td>**</td>
<td>***</td>
<td>**</td>
<td>***</td>
</tr>
</tbody>
</table>

Table 2. Comparison of Sensor Type (after Tlusty and Andrews)

Dynamometers are the most useful sensors for real time control applications, but they are expensive, and hence, alternatives are needed. The following sections provide background on a broad range of sensing techniques found in the literature.

### 2.4.2 Torque Sensors

Cutting torque can be related to many different cutting parameters. Yellowley 44 has shown how to infer the swept angle of cut from the fundamental and the first harmonic of the torque signal in milling. Other researchers have used torque to infer wear and tool breakage.
2.4.2.1 Spindle Current
The current supplied to the spindle drive is proportional to the torque that the electric field provides to the rotor. Stein et al. ⁴⁵ have investigated torque monitoring on a J&L lathe with a 150 HP DC spindle. They concluded that the bandwidth of that system is approximately 2 Hz, which can only provide mean forces for most cutting applications. Furthermore, the friction in the particular system led to a complex function between armature current and disturbance torque. They also concluded that the friction component of the measurement would naturally decrease with larger cutting forces, which would improve the sensor accuracy at higher loads. Arsecularante et al. ⁴⁶ were able to calibrate the torque current characteristic of the DC spindle drive of a CNC turning center and used this system to determine cutting conditions. Liu et al. ⁴⁷ have used the current signature of a three phase induction motor to detect drill fracture. The bandwidth of their system was close to 1 Hz, and they successfully detected drill failure.

2.4.2.2 Spindle Speed
Cutting torques lead to variations in spindle speed which can be measured with a tachometer if the transfer function between spindle speed and cutting torque is known. This concept has been studied on a Holke milling machine, and the results are shown in Appendix A. This system is able to provide mean values of cutting torque, but since the belt drive system of the Holke machine was not able
to provide consistent enough results, it was not further used in this work. Kaye et al. 48 have successfully used a very similar model to track tool flank wear.

2.4.2.3 Drive Belt Tension

The tension in a spindle drive belt is directly proportional to the cutting torque. It is possible to measure the mean and possibly the first few harmonics of torque by measuring the force in the belt. This could be achieved by measuring the force in a belt tensioner (see Figure 10).

2.4.3 Force Sensors

Force sensors can be used to identify cutting parameters such as swept angle of cut in milling, the amount of tool wear, and tool breakage 23. Radial and axial runout can also be inferred from an orthogonal force triplet. These ideas will be expanded upon in Chapter 4.

2.4.3.1 Conventional Dynamometers

Force dynamometers have been employed for many years in a wide range of applications that have different physical requirements. Most are based on the measurement of the deflection of a spring-like element in response to cutting forces. Wind tunnel testing traditionally uses strain gauge dynamometers that are based on bending beam elements 49,50. Ski binding research has adopted a shear-panel-element based design 51 that utilizes strain gauges to indicate
forces. Another strain gauge type of dynamometer was proposed for measuring draft forces in trailer hitches. All of these designs are able to measure multiple components of force, and some of them are able to also measure moments. Unfortunately, they are not designed for high bandwidths and low deflections.

The deflections of dynamometers for machining need to be very small, because the accuracy of the workpiece should not be compromised, and the bandwidth of the sensor needs to be large compared to the exciting frequency of the cutting operation. Consequently there are few transducers that can be used to measure deflection. Pneumatic transducers, differential transformers, and magnetic strain gauges are all possible candidates but have not been used widely because of their low sensitivity. Zorev used dial gauges, hydraulic gauges, and the change in resistance of carbon stacks under pressure in a number of lathe dynamometers. He designed single, two, and three component dynamometers. The single component systems had a much higher bandwidth than the multi-component ones, and naturally, they also had little cross sensitivity; thus, they were used to validate results that were obtained from the multi-component dynamometers. Shaw showed designs of lathe dynamometers that used electronic transducer tubes and strain gauges. Other strain gauge designs for lathes include the ones demonstrated by Oraby et al. and by Singh et al.
The mechanical design of these lathe dynamometers usually takes advantage of the fact that the dynamometer is mounted between the tool and the turret; thus the point of force application is limited to a single point, the tool tip. Milling dynamometers, on the other hand, can either be mounted in the rotating tool holder or between the table of the machine and the workpiece (see Figure 11). The first approach suffers from difficulties in connecting sensors on the rotating tool to a stationary data collection system, a reduced stiffness of the spindle, low bandwidth, and a very high price. Consequently the second approach has been implemented more commonly, but it leads to a varying point of force application as the cut progresses.
Traditional stationary table dynamometers for milling are either based on strain gauges or piezoelectric load cells. The strain gauge types of dynamometers are usually based on strain rings that can measure a vertical component of force and a horizontal component of force (see Figure 12). A three component dynamometer is then achieved by using four such strain rings and ordering them in the way shown in Figure 13. Even with finite element optimization techniques, the bandwidth that can be achieved with this design is only around 100 Hz to 200 Hz, since the sensitivity is linearly related to the stiffness of the structure. Furthermore, strain gauges are susceptible to drift due to temperature.
Three component piezoelectric load cells are often used in more modern dynamometer designs, as in that by Lai. The load cells themselves are sensitive to force rather than deflection; thus there is no compromise between sensitivity and stiffness. Furthermore, temperature compensation is usually not required. Unfortunately these benefits are somewhat offset by the high cost of the piezoelectric load cells and the required charge amplifiers. Lai's three component dynamometer design that utilizes three 3-component piezoelectric load cells is shown in Figure 14. This design requires that the channels of the transducers are connected in parallel and fed to charge amplifiers. This connection results in cancellation of moments that are applied to the dynamometer during the cutting operation.

Commercially available piezoelectric dynamometers (e.g., Kistler) are able to provide high bandwidth, high linearity, low cross sensitivity, and low drift. Large devices that cover the complete table of a milling machine are very expensive and not economically feasible in industry.
2.4.3.2 Dynamometers Built into the Spindle Bearing

Axial and radial force can be measured by instrumenting the spindle bearing. Promess is marketing a sensor that measures both force components, but the system design limits the bandwidth of the sensor to 20 to 200 Hz. Sandvick makes a thrust sensor that can be installed in the spindle to measure axial forces or alternatively in the feed drive system to measure feed forces.

2.4.3.3 Motor Currents

Measurement of the feed motor currents is able to provide the x and y components of the cutting force. If the friction on the ways is very small, then this method is able to provide the mean and the first few harmonics of the cutting force. Unfortunately, many machines have a great amount of friction in their way systems. An exact dynamic model of friction is often difficult to attain, and a friction map of the machine must be derived and used to compensate for these frictional forces. Unfortunately, this method makes high bandwidth measurements difficult.

Arseularante et al. have implemented a system that determines feed forces from the current signals on a dc-servo driven CNC turning center. They were able to show that this system performs well for cutting condition identification. Stein et al. were able to achieve a bandwidth of 80 Hz from dc-servo motor currents, but they found that the feed system would have to be redesigned in order to improve the signal to noise ratio.
2.4.3.4 Optical Fiber Sensor

Jin et al. 61 have developed a sensor to measure main cutting forces on a lathe. This sensor measures tool shank deflections with an optical light emitter detector pair. The linearity and cross sensitivity of this device are close to 1%, and the bandwidth was determined at 950 Hz. Considering that the sensor is quite simple, that it requires little extra space for installation, and that it hardly decreases the structural stiffness of the tool holder, it appears to offer a very feasible approach.

2.4.3.5 Strain Gauge Based On-insert Force Sensor

Adolfsson and Ståhl 62 have developed a strain gauge based sensor that measures main cutting force and feed force directly on four of the eight cutting edges of a modified standard face mill. Electronic filtering, amplification and digitizing is performed on the insert holder, and the forces are transferred by IR to a data collection system. This sensor is quite complicated, but it provides very interesting force data of individual inserts that would be difficult to attain otherwise.

2.4.4 Conclusion on Sensor Technology

Conventional dynamometers are able to provide measurements with little cross sensitivity between three orthogonal force components. Moments are usually canceled because of the inherent design of the dynamometer, and they usually provide a sufficient bandwidth. Unfortunately, such dynamometers are quite
expensive, a fact which makes them unattractive for the majority of low cost machines.

Alternatives to dynamometers for measuring force and torque have been outlined in this section. A large number of them are based on measuring current in the actuators of the machine tool. These sensors benefit from low cost, but they provide a limited bandwidth. The sensors based on changes in spindle speed show similar trends. The Promess sensor also suffers from low bandwidth, and the sensor from Sandvick only measures thrust forces. The optical sensor is quite promising, but it measures only the main cutting force in a turning operation. The on-insert strain gauge type of sensor provides very valuable data, but the sensor itself appears too fragile for industrial applications.

Since commercial dynamometers are too expensive for most low cost machines and alternative sensing techniques are unsuitable for many practical milling applications, a new dynamometer was built for the work reported in this thesis. The dynamometer has relatively low bandwidth but its design considerably reduces cost. Details of the design and performance are presented in Appendix B.

2.5 Process Identification

2.5.1 Introduction

Process parameters can be identified from many different sensor types. In the manufacturing environment, force measurements have proven to be a reliable
and robust source of cutting information. The following sections will outline different aspects of the process that can be identified and used for process control. Particular emphasis is placed on identification techniques that are based on force sensors.

### 2.5.2 Geometry of Cut

The geometry in the milling process was described in section 2.2.2.2, and a model of cutting forces in milling that is based on Fourier series was introduced in section 2.2.3.3. This frequency domain description of cutting forces and cutting torques is used to identify radial width and axial depth. The swept angle of cut and the axial depth (see Figure 15) can be determined independently for non-helical cutters. In the case of helical endmills and large axial depths, these two parameters must be identified simultaneously. The identification techniques shown here assume zero helix angle.

Yellowley suggested the use of fundamental torque over the mean torque to estimate swept angle of cut or width of cut. Since the fundamental torque is difficult to measure, he proposed that a parameter termed the quasi-mean resultant force could be used to replace it. This parameter combines the mean
value of the forces in X and Y into a directionally independent parameter which is directly related to the magnitude of the fundamental component of the torque:

\[
F_{qm} = \sqrt{F_x^2 + F_y^2} = NK a \, s_1 \, \sqrt{1 + (r_1 \cos \psi_e)^2} \, \sqrt{\alpha_1^2 + b_1^2}
\]  

(35)

because,

\[
\begin{align*}
\bar{F}_x &= NK a \, s_1 \left( a_1 + r_1 \cos(\psi_e) b_1 \right), \\
\bar{F}_y &= NK a \, s_1 \left( b_1 - r_1 \cos(\psi_e) a_1 \right)
\end{align*}
\]

where

\[
\alpha_1 = \frac{\cos(2\phi_1) - \cos(2\phi_2)}{4\pi}
\]

(36)

\[
b_1 = \frac{2\phi_e + \sin(2\phi_1) - \sin(2\phi_2)}{4\pi}
\]

The drawback of this approach is that it is sensitive to changes in force due to tool wear. Elbestawi et al. \textsuperscript{63} suggested a method that requires the measurement of feed force at two similar feeds for determining the swept angle of cut. This method is directionally dependent, and it also requires the artificial variation of feed.
Yellowley et al. \textsuperscript{23} suggested that the swept angle of cut should be obtained from the ratio of the sum of the squared magnitudes of the fundamental components of feed and normal forces over the squared quasi mean resultant force. The advantage of this parameter is that it is largely independent of wear and cutting direction, and it is also fairly easy to obtain:

\[
Q = \frac{a_{x_n}^2 + b_{x_n}^2 + a_{y_n}^2 + b_{y_n}^2}{F_{qm}^2} \tag{37}
\]

A plot of this immersion parameter against immersion angle for up to four teeth is shown in Figure 16. Since the relationship between quasi mean resultant force and swept angle of cut is a cosine function, swept angles of cut of less than 30\textdegree{} and more than 150\textdegree{} will be difficult to determine accurately. The problem of inaccurate small swept angles of cut is further increased because of the dominance of rubbing forces in the tool flank in this scenario. Fortunately, this problem is not extremely detrimental, since other parameters whose identification themselves rely on the knowledge of the swept angle of cut rely on
a similar cosine function. Consequently, it is not required to have a very accurate knowledge of swept angle of cut at very low and very high immersions.

Once the swept angle of cut has been determined, the axial depth can be obtained from the magnitude of the mean torque or mean forces. This will be demonstrated in Chapter 4.

2.5.3 Cutter Wear

A measurement of tool wear is required for ensuring optimum economic cutting conditions (see section 2.3.2). On-line tool wear measurement systems can be divided into two groups. The first group attempts to measure wear directly by employing touch probes, or optical methods. The drawback of these methods is that they can only be employed when the tool is out of cut. Although direct, on-insert sensors for wear have been developed for ceramic inserts on lathes, it is not yet possible to mount the sensing technology on carbide inserts. The second group attempts to identify wear indirectly from measuring a different physical phenomenon. Usui et al. attempted to derive an analytical relationship between wear and chip surface temperature. Similarly, Groover et al. studied the relationship between remote thermocouple temperatures of the rake face and tool wear in machining. Dornfeld attempted to use statistical analysis of acoustic emissions to identify wear. Bhattacharyya et al. attempted to find a relationship between wear and cutting forces. Colwell et al. also used cutting force to identify wear in turning. Rao showed that the tool holder
vibrations can be used as an indicator of wear in stable turning. In a similar approach Pandit et al. 72 attempted to find the spectral content of wear in the tool holder acceleration signal for a turning process. Kaye 48 attempted to identify wear from changes in the spindle speed. These attempts showed that it is indeed possible to measure wear in steady state cutting conditions, and it was assumed that these conditions were known. Koren et al. 64 proposed a wear estimation algorithm for turning that is able to identify step changes in depth feed or speed. Yellowley et al. 23 and Elbestawi et al. 63 presented techniques for measuring immersion and wear in milling from Fourier series coefficients of force.

2.5.4 Cutter Breakage

There are two modes of tool breakage which must necessarily be considered. The first is shank breakage where high radial components of force cause the shank of the tool to fail. This type of failure is expected only in high speed steel endmills, because facemills are usually stronger than the feed drive of the machine tool. A second type of failure is caused by breakage of the cutting edges of face mill inserts and high speed steel endmills. This type of failure is more difficult to detect, because the cutter will continue to cut, often with similar average forces but different force patterns. Nevertheless, it is important to detect this type of failure, for it has the potential to cause unacceptable surface finish and part accuracy.
Several authors have examined the problem of breakage detection. Lan et al. \cite{73} filtered cutting forces with high order adaptive autoregressive filters to detect tool breakage and edge chipping in milling. Altintas et al. \cite{74,75} showed how a good model of cutting forces can lead to a much simpler breakage detection technique. The same author \cite{76} showed later how tool breakage can be detected from measurements of feed drive currents, which in turn are indicators of feed forces. Liu et al. \cite{47} attempted to monitor drill fractures through the current of the spindle motor.

An identification technique for runout, described later in Chapter 4, can also be used to identify breakage from force measurements. The advantage of this system is that it allows considerable runout before breakage occurs.

### 2.5.5 Cutter Runout

Cutter runout in itself is of no concern to the finished part. It is, however, an indicator of surface finish and of the tool condition. Considerable research effort has gone into developing models that describe the geometry and the force patterns that are associated with runout. Fu et al. \cite{77} proposed an analytical model of cutting forces in milling with runout and axial tilt of the spindle. Kim et al. \cite{78} developed a model to predict forces in the presence of runout and insert placing. Kline et al. \cite{79} also developed a model of the influence of runout on geometry and cutting forces in end milling. Liang et al. \cite{24} developed a Fourier series based model of cutting forces with radial cutter offset. Later, Wang et
al.\textsuperscript{25} showed a model that describes radial runout for milling. These models, even though they are able to describe well the physical phenomena related to runout, are difficult to use for real time runout identification. Chapter 4 introduces a simplified model of runout that can be used to identify radial and axial runout on-line.

### 2.5.6 Chatter

Chatter is the occurrence of self-excited vibrations in machining. Chatter creates a poor machined surface with chatter marks, and it can also lead to tool breakage, if the amplitude of the chatter vibrations become too large. Tobias\textsuperscript{80} has developed a dynamic model to describe chatter in modern machine tool operations. This model relates cutting conditions and the dynamic stiffness of tools, machines, and workpieces to cutting forces. Tlusty\textsuperscript{81} has shown how the dynamic structural modes of cutting tools can be determined experimentally. This process, in turn, allows the prediction of chatter for different cutting conditions. Tlusty\textsuperscript{82} has also related chatter to the particular case of high speed end milling. He demonstrated the influence of process damping, and he also suggested that varying the spindle speed could be used to control chatter. Smith et al.\textsuperscript{83} gave an overview of modeling and simulation of chatter, and they then proceeded to controlling chatter on-line by regulating the spindle speed\textsuperscript{84}. Weck et al.\textsuperscript{85} showed a system that avoids chatter during the generation of the tool paths by using a dynamic model of the tool, the machine, and the workpiece.
Chatter has been thoroughly investigated over many years. Although it is clearly a complex phenomenon, the problem of chatter can be alleviated through good process planning, improved tool design, and on-line control.

2.5.7 Part Accuracy
The type of operation (roughing, semi finishing, or finishing) puts different constraints on the accuracy of the finished part. Part accuracy itself is influenced by deflections of the tool, the workpiece, and the machine, as well as by tool wear.

Tool deflections can be estimated from in-plane resultant force measurements in combination with the identified geometry of cut. Part deflections have to rely on maximum allowable force levels determined in conjunction with the CAPP system, since the monitoring system is not alone able to determine the structural stiffness of the part. The machine tool can contribute to part inaccuracy through a large number of factors, eg., ball screw pitch errors, temperature, alignment, weight, etc. Tabular data of position-dependent machine errors can be used to modify the position commands that are prescribed by the path planning system.

In the usual case, part accuracy of finished parts is measured with a coordinate measuring machine (CMM) that is programmed by the process planning system. These measurements can then be used to update tool radius compensations and tool offsets during the manufacture of subsequent parts.
Touch probes on the machine tool itself have also been used to measure part accuracy. This approach turns the machine tool into a CMM that is limited by the accuracy of the drive system. It can compensate only for deflections of the part and the cutter, since all other contributing factors will also be present in the touch probe measurements.

Some special applications have been found where on line part accuracy measurements are promising. Pfeifer et al. used a fiber optic reflex sensor to measure the machining accuracy of internal threads. Other researchers developed optical and tactile systems to measure the diameter of holes. Zhang et al. proposed a system to measure machine and workpiece accuracy and straightness using distance triangulation and a laser interferometer.

Another related area of research is reverse engineering, where three dimensional geometric representations of a part are created with no information except the part itself. Taleuchi et al. tried to utilize photographic images, but the limited vision angle often restricted its use. Yamazaki demonstrated a system that applied computer tomography and achieved resolutions between 0.25-1 mm. The results of this technique are not accurate enough for characterizing the accuracy of a part, but they can be used to program a CMM for more accurate results.
2.5.8 Surface Finish

The surface that is generated by the milling operation is influenced by the tool geometry, the cutting geometry, the dynamic interaction of machine, tool and workpiece, the workpiece properties, and wear. Models and simulations of these effects have been suggested by many researchers.

Martellotti 20 provided the basic understanding of the kinematics and the mechanisms in surface generation for milling. Kline et al. 93 and Sutherland et al. 94 developed a simulation model to predict surface accuracy in endmilling with a statically deflecting cutter. Montgomery et al. 95 considered the effect of the tool workpiece dynamics on the surface generation in milling. Zhang et al. 96 included random vibrations due to variation in microhardness of the workpiece in a turning simulation. Ismail et al. 97 developed a simulation to predict surface generation in milling that includes dynamics and cutter wear.

These models can be used to predict surface finish during the planning phase of the production process. Measurements of surface finish have traditionally been obtained through tactile sensors 98,99. Non-contact optical devices are also available now. They range from simple laser interferometer adapters for existing tactile sensors 100 to real time laser measurement devices that can be mounted to manufacturing equipment 101,102. Others have also attempted to use acoustic sensors for determining the surface roughness 103; these sensors are not affected by the optical reflectivity of the surface.
Since runout of the tool is a major contributor to surface finish, on-line measurements of runout are important to ensure reasonable surface roughness. Algorithms for measuring runout from on-line cutting force measurements are described in Chapter 4.

2.5.9 Conclusion on Process Identification
Identification techniques for many process parameters have been discussed in this section. Most parameters can be obtained on line with a varying degree of accuracy. Force sensors are able to provide the required data for most of the identification algorithms that are essential for a rudimentary process optimization system. Such a system needs to be able to ensure machine and workpiece integrity, as well as to provide economic cutting conditions.

2.6 Computer Aided Process Planning

2.6.1 Introduction
The normal sequence of production planning is hierarchical in nature; it begins with the decisions of broad issues and proceeds to specific planning and implementation issues. At the highest levels, the initial product design concept is decided upon, and this decision is followed by detailed analysis and design, and finally by manufacturing plans and production. Each of these areas has received considerable improvement as a result of the use of computer aids. However, it may be argued that the gains resulting from the integration of these activities are far greater than those from improving the efficiency of individual
areas. The following paragraphs will elucidate the efforts that have gone into the different areas of process planning.

2.6.2 Approaches to Process Planning

Process planning transfers part drawings from the design and analysis stages to production plans for the manufacturing process. It consists of several phases which are hierarchical in nature, starting with relatively high level decisions, and proceeding to detailed planning. The typical requirements of the phases are shown here:

High Level Planning Phase:
- Selection of basic processes and sequence
- Selection of specific machines and order
- Design of holding devices, approximate ordering of main operations at each machine
- Subdivision of operations, detailed operation order selection of tool types

Low level Planning Phase:
- Optimization of cutting conditions and tools

NC Programming Phase:
- Detailed evaluation of tool paths
- Cost estimating

There is a likelihood of encountering infeasibility during the planning process as it proceeds from its highest to the lowest decision levels; such an event will require backtracking. Moreover, the very large number of feasible solutions makes finding the best solution almost impossible, and finding even good
solutions rather difficult. This difficulty makes process planning a promising area for computer automation.

Computer aided process planning (CAPP), even though it was first envisaged 30 years ago, is still in its childhood. The first systems gave experienced process planners access to well organized and categorized data bases of parts that had been produced in the past. These data bases could be altered and used as guidelines for new parts. This approach is generally termed the variant approach of CAPP. It has its roots in a much older manual process planning technique called Group Technology (GT) which, in essence, provided guidelines for process plans of parts that are grouped together by geometric, or manufacturing features. The lack of experienced human process planners led to the generative approach to CAPP. This approach attempts to generate process plans from the CAD drawings of the parts by relying solely on models of the manufacturing process without human interaction. Some of these techniques that are applicable to milling have been discussed in section 2.3. Generative CAPP has met with several challenges. Again, there is an integration challenge between CAD and CAPP. The required geometrical and technological information of the part is very difficult to extract from the data format of traditional CAD systems, because they were originally designed primarily as drawing programs. This challenge has been addressed by many in the research community, and some solutions have been reported. The next challenge lies within the algorithms which are used to arrive at economical
process plans. Progress has been made using a variety of avenues for this task, but uncertainties in the estimation of process parameters for the models that are used to describe the manufacturing processes lead to safety issues in the succeeding manufacturing stage. A truly optimal process plan leaves little room for error in estimates of process parameters. Such errors can lead to the failure of tools, machines and workpieces during the machining process. As a result, computer generated process plans have to use conservative estimates of process parameters that will lead to the safe operation of the machine tool and a suboptimal cost of the finished part.

2.7 Computer Aided Manufacturing

2.7.1 Introduction
At the lowest level of the production process is the manufacture of the part on a machine tool. A great variety of different machine tools are used in industry for changing diverse part requirements. Traditionally these machines receive tool trajectory commands as well as feed and speed guidelines from the process planning system. The following sections will outline traditional manufacturing controls and the UBC open architecture controller that was used in this project to allow a CNC milling machine to utilize sensory feedback for process identification and adaptive optimization of process parameters.
2.7.2 Standard Manufacturing Controls

Machine tools have been automated with numerical controls for many years, and the interface between the planning system and the control system now dates back more than 30 years. At that point little intelligence could be put onto machine tools, and consequently the G-code interface between the machine tool and the programming system was limited. It was possible to pass simple tool path trajectories to the CNC machine, but not to transfer geometric and technological information to or from the machine tool.

Advances in sensor technology and process identification techniques could be utilized to gain economic savings during the manufacturing process. These process identification techniques, however, require geometric and technological information that is available in the process planning system, but cannot be passed to the CNC system through the traditional interface between the two. In this thesis the UBC Open Architecture controller was used to extend the interface between CAPP and CAM.

2.7.3 The UBC Open Architecture Controller

The UBC controller architecture \(^{110,111}\) (see Figure 17) was selected in this research effort because it was designed for integrated planning and control and is based on either the 8...
bit STD bus or the 32 bit STD32 bus. In either case, multiple master and slave processor boards can be connected to the bus with communication between the boards being achieved by two parallel channels. The STD bus provides data transfer via biport ram between masters and from the masters to the slaves. A front plane bus allows synchronization and communication between the slave axis control boards and the process monitoring boards. (There is usually little need for the master CPUs to access the front plane bus directly.)

The master processor boards perform separate process planning tasks and the CNC operator interface task. In this way one process planning master can run a CAD system, while another master is responsible for volume subdivision, path subdivision, and generation of motion commands that are then shared with the CNC master.

Every axis of motion has its own slave controller card; these cards obtain motion commands from the CNC master over the back plane STD bus. A synchronizing line synchronizes the slave boards, and a state line allows communication between slave boards. The synchronizing line and the state line are referred to as the front plane bus.

The real time process monitoring boards exchange process data with the masters over the back plane STD bus. Because they perform the real time control of feed (ACO), they utilize the front plane communication lines to
override the default values of feed that the slave motion control boards receive from the CNC master.

The UBC controller architecture allows a clean subdivision of tasks between processors while still maintaining the possibility of access to pertinent parameters for all processors.

2.8 Process Control

2.8.1 Introduction

Tool wear rate and tool breakage cannot be predicted accurately during the process planning stage because there is variation in tools, work material properties and dimensions, as well as in machine tools. In order to minimize cost, cutting speed and feed rate must be optimized in real time. It is also possible to use a varying feedrate to meet secondary constraints such as force, power, and torque limits, or surface finish constraints. Given an appropriate control architecture, it should also be possible to modify the tool path itself in order to cut excess material, or to ensure the maintenance of tolerances in the presence of wear and machine inaccuracies.

There are two approaches to the real time optimization stage of metal cutting. The first approach is called adaptive control optimization (ACO); it attempts to optimize a performance index based on an economic model. This approach has not been pursued strongly by researchers because it is thought that a realistic economic index is difficult to define, and the required sensory information is not
adequate at present. The second approach is called adaptive control constraint (ACC); it typically attempts to optimize the process conditions by maximizing the metal removal rate. This maximization is achieved by maintaining a single constraint such as force or torque at its maximum value. Since this approach will dramatically change the control system parameters with changing process parameters such as depth of cut, system stability has been of great interest to many researchers. The solution to this problem is usually a parameter adaptive controller where controller parameters such as gain are varied in order to ensure system stability over a wide range of cutting conditions. This approach is, however, only promising if the optimum conditions are really governed by this single constraint.

An early system that integrates some of the process planning tasks with control was proposed by Weck et al. This system is able dynamically to plan cutting depths, feed and velocity. It is an ACC system which varies cutting depth, feed, and spindle speed in order to ensure constant metal removal rate and avoid chatter. This system cannot ensure minimum cost.

Even though ACO received considerable research effort in the early stages of adaptive control development in the metal removal process, ACO systems have been implemented in practice only in turning.
2.8.2 ACO in Turning

Maintaining the equivalent chip thickness in turning at its maximum value (which is governed by edge breakage) and keeping the cutting speed constant will guarantee a constant tool wear rate in turning (see equation (20)). Minimum cost is ensured when the feed is maintained at a level which results in maximum equivalent chip thickness, and the cutting speed is set to a value that leads to a tool life described by equation (31).

Yellowley and Adey have presented an algorithm that varies the feed in turning operations in order to keep the equivalent chip thickness at a constant maximum value. Their algorithm was limited by the rate at which the CNC could respond to commands from the process monitoring CPU. Ardekani has utilized the UBC controller to implement a similar algorithm for turning. In this latter case the UBC controller was able to provide the necessary communication speed between the axis controllers and the process monitoring CPU to ensure effective control of both equivalent chip thickness and an upper force level.

2.8.3 ACO in Milling

In the early 1960s the Bendix Research Laboratories attempted to develop ACO controls for metal removal processes. Cincinnati Milacron started work on a similar system at the same time. They both concluded that it was very difficult to develop practical systems that could measure true performance of the machining process. Tool wear rate in particular could not be determined
satisfactorily. As a result ACO systems were not investigated any further; the main research effort since has been concentrated on ACC systems.

2.8.4 Conclusion on Process Control

Process control is used to ensure operational safety of the machine tool while ensuring either maximum metal removal rate or minimum production cost. Most research has gone into ensuring maximum metal removal rate because this parameter can be characterized much more easily by a simple force, power, or torque constraint. Minimum cost requires the ability to measure wear on the tool. There are few reliable sensors and identification techniques for performing this task. Most process identification tasks require process information that is available in the CAPP system, but not on the machine tool. An extension of the interface between CAPP and the machine tool controller is required, as well as the creation of the ability within the controller to synchronize traditional position control tasks with monitoring, identification, and adaptive control tasks. Some of this work has been accomplished for turning operations, but in milling few results have been reported.

2.9 Dynamic Process Planning

The process control system identifies technological cutting parameters that the process planning system may find useful in generating updates of the process plan while the part is being machined. Replanning can be performed at the lowest stage, where feed and speed are selected to ensure constant tool life. It
is also possible to regenerate higher levels of the process plan, which include path planning and volume subdivision, when new tool life data becomes available.

Even though there is great potential economic benefit in dynamic real time process optimization, no functioning system has been presented in the literature to date. This is due to difficulties in measuring both process parameters and tool wear.

2.10 Discussions and Conclusions
Much progress has been made in physical and economic models of the cutting process. This understanding allows for the production of very efficient process plans in those cases where the process parameters are known with a high degree of confidence. Because that is not often the case, sensors for identifying the process parameters during the manufacturing stage of the part need to be utilized to improve the estimates of these parameters. On the one hand, this identification permits updating of cutting conditions in real time for improved operating safety and economics, and on the other, it allows for more accurate process plan updates. This vision of a dynamically changing process control and process planning system still requires work in many areas. In this thesis high level issues relating to integration of process planning and control, as well as low level process identification algorithms, force models and sensor design, are discussed, and new approaches are introduced. The work is aimed at making dynamic process planning feasible in the near future.
Chapter 3
An Upper Bound Cutting Model for Oblique Cutting Tools with a Nose Radius

3.1 Introduction
The prediction of chip flow direction in practical cutting operations is of great importance both to the practical tool design process and to the calculation of force components. A significant amount of effort has been expended in both the development of models and the proposal of empirical rules based upon experimental observation. The work described in this chapter presents a simple upper bound approach to the problem. The motivation behind the work in this thesis is to derive force models which will allow the inverse problem to be tackled. In real time monitoring, one is interested in using force information to identify cutting conditions and tool condition. Clearly, one must have a good basic model for these forces which is extendible to many practical geometries.

* The work presented in this chapter is based on a previous publication by Seethaler and Yellowley.
3.2 The Application of the Upper Bound Method to Orthogonal Cutting

Orthogonal cutting, (see Figure 18) provides the simplest geometry for the study of metal cutting, and many models of the process have been advanced. It is worthwhile to conduct a detailed examination of the application of the upper bound method to orthogonal cutting before examining the more complex issues of obliquity and multiple edges. Upper bound solutions start with a kinematically admissible velocity field since any such solution is known to overestimate power, then one attempts to minimize the total work rate with respect to the geometry of the deformation zone. In metal cutting there is a shear work rate due to the shear deformation in the primary zone and additional shear or friction work is expended on the rake face.

3.2.1 Previous Upper Bound Models for Orthogonal Cutting

Perhaps the best known approach which results in an upper bound is that due to Merchant\(^2\). The formulation used by Merchant regards the rake face contact as elastic with a constant coefficient of friction; the process of minimizing work with
the assumption of force equilibrium leads, however, to the same result as a straightforward application of the Upper Bound method:

$$\phi_{\text{Merchant}} = \frac{\pi}{4} + \frac{\gamma}{2} - \frac{\beta}{2}$$  \hspace{1cm} (38)

Rowe and Spick\(^7\) avoid the introduction of a friction coefficient by examining shearing stresses on the rake face. They arrive at the following shear angle relationship by applying the minimum energy principle on a simple upper bound field:

$$\cos \gamma \cos(2\phi - \gamma) - m \chi \sin^2 \phi = 0$$  \hspace{1cm} (39)

where

- \( m \) ratio of shear stress at rake face over the shear yield stress of the chip material during cutting,
- \( \chi \) multiple of undeformed chip thickness, defining the length of the contact on the rake face.

DeChiffre\(^6\) attempted to find a simpler expression by replacing the shear angle with the chip compression ratio and the friction coefficient by the chip contact length. This replacement yielded an expression that is equivalent to the one obtained by Rowe and Spick:

$$\lambda = \sqrt{1 + f m n \cos(\gamma)}$$  \hspace{1cm} (40)

where

- \( \lambda = \frac{h_a}{h_c} \) the chip compression factor,
- \( m = \frac{\tau}{k_i} \) friction factor,
- \( \tau \) shear yield stress on the rake face,
The work presented here attempts to obtain the plastic contact length by balancing forces in the direction of the shear plane (i.e., the solution, while still an upper bound, is forced to conform to a simple force equilibrium). A detailed description of this approach for orthogonal cutting is shown in the following section since it will elucidate the derivation of the more complex case of oblique cutting with several cutting edges.

3.2.2 A Combined Upper Bound and Force Equilibrium Solution for Orthogonal Cutting

The upper bound solution requires that the sum of the work rate due to shear deformation of the chip and the workrate due to friction on the rake face are minimized:

Minimize: \[ W = W_{\text{Shear}} + W_{\text{Friction}} = F_s V_s + F_f V_{\text{out}} \] \hspace{1cm} (41)

The shear work rate is defined as the product of shear force and the shear velocity, whereas the friction work rate is defined as the product of the friction force and the chip velocity on the rake face:

\[ W_{\text{Shear}} = F_s V_s \]

\[ W_{\text{Friction}} = F_f V_{\text{out}} \] \hspace{1cm} (42)
Considering the force magnitudes first, the shear force for a unit width of cut is equal to the product of the shear yield stress and the shear area:

\[ F_s = \tau A_s = \tau \frac{h_0}{\sin \phi} \] (43)

where

- \( \tau \) shear yield stress of the material during cutting,
- \( A_s \) shear area,
- \( h_0 \) undeformed chip thickness,
- \( \phi \) shear angle.

The friction force is assumed simply equal to the product of shear yield stress and the plastic contact area:

\[ F_f = \tau I \] (44)

where \( I \) plastic contact length of the chip with the rake face.

However, force equilibrium requires that one may also express the friction force in terms of the resultant force as follows:

\[ F_f = R \sin \beta = \frac{F_s \sin \beta}{\cos \alpha} = \frac{F_s \sin \beta}{\cos(\phi + \beta - \gamma)} \] (45)

Equating the two expressions of the friction force yields an expression for the plastic contact length between the chip and the rake face:

\[ I = \frac{F_s \sin \beta}{\tau \cos(\phi + \beta - \gamma)} = \frac{h_0 \sin \beta}{\sin \phi \cos(\phi + \beta - \gamma)} \] (46)
In orthogonal cutting, the ratio of the plastic contact length over the length of the shear zone is equivalent to the ratio of the friction area over the shear area. This latter ratio will be used in the section on non orthogonal cutting:

\[
\frac{A_f}{A_s} = \frac{1}{h_0} = \frac{\sin \beta}{\cos(\phi + \beta - \gamma)}
\]  

(47)

The friction coefficient \( \beta \) in this case is presumed to be the ratio of two plastic stresses and, hence, is dependent upon the extent of work-hardening present. The particular case of an ideal rigid/plastic work material and sticking friction on the rake face leads to the following estimate of friction angle (from the Hencky equations\(^{119}\)):

\[
\tan \beta = \frac{1}{1 + \frac{\pi}{2} - 2\gamma}
\]  

(48)

In the case of the velocity components, the shear velocity and the friction velocity are shown in the hodograph of Figure 19.

For a unit cutting velocity, they can be described as follows:

\[
V_s = \frac{\cos \gamma}{\cos(\phi - \gamma)}
\]  

(49)

\[
V_{out} = \frac{\sin \phi}{\cos(\phi - \gamma)}
\]  

(50)
The shear work rate is the product of the shear force and the shear velocity, whereas the friction work rate is defined as the product of friction force and friction velocity. The overall work rate is the sum of the shear work rate and the friction work rate:

\[
W = F_s V_s + F_f V_{\text{out}} = \tau \frac{h_0}{\sin \phi} \left( \frac{\cos \gamma}{\cos (\phi - \gamma)} + \frac{\sin \beta}{\cos (\phi + \beta - \gamma)} \frac{\sin \phi}{\cos (\phi - \gamma)} \right)
\]

(51)

The upper bound solution requires that the overall work rate be minimized. The requirement for a minimum value leads to the specification of a unique shear angle for each given rake angle. Because this present work was aimed at examining more complex problems, then from the start, the system has been modeled using a numerical analysis package to allow rapid determination of minimum values.

Figure 20 shows the resulting shear angle \( \phi \) and the friction angle \( \beta \) for a range of rake angles \(-15^\circ < \gamma < 50^\circ\). It should be pointed out that, as expected, the angles in this figure follow the Merchant equation. For an ideal, rigid/plastic work material and the assumptions inherent in the analysis, one may now, however, directly predict the shear
angle and friction angle for a given rake angle. It should be said that in general, however, given the inevitable and unknown extent of work-hardening, the result is a reaffirmation of the Merchant solution.

3.3. Practical Cutting Tools, the Influence of Obliquity and a Non-straight Cutting Edge

3.3.1 Geometry

Practical cutting tools most often do not have a single straight cutting edge. They commonly have several straight edges, or a straight edge combined with a round nose. A typical lathe tool is shown in Figure 21, where the tool has two straight cutting edges. This figure also demonstrates obliquity, which is measured in terms of the obliquity angle \( i \). The orientation of the tool is then defined by two angles, the rake angle and the obliquity angle. Whilst the definition of the obliquity angle is fairly straightforward (the angle is measured in the plane of the machined work surface, between the main cutting edge and the normal to the cutting velocity), there are three commonly used definitions for the rake angle:

- The normal rake angle \( \gamma_n \) is measured in a plane normal to the cutting edge;
- The velocity rake angle \( \gamma_v \) is measured in a plane perpendicular to the machined work surface and parallel to the cutting velocity;
• The effective rake angle $\gamma_e$ is measured in the plane containing the cutting velocity vector and the chip flow vector.

Even though Merchant defined the oblique cutting geometry in a concise manner some time ago, much confusion has arisen in the interpretation of rake angles. In the work shown here, the above definitions are adopted.

### 3.3.2 Review of Existing Models

A recent paper on chip control by Jawahir et al. reviewed a large number of popular theories on chip flow. The following section discusses some of these models, and augments Jawahir’s review with more recent work in chip flow prediction.

![Figure 22. Chip Flow in Oblique Cutting](image)

Early investigations concentrated on the examination of obliquity alone (see Figure 22). These investigations were concerned with identifying which of the three definitions of rake angle would best characterize the cutting forces in oblique cutting. They also demonstrated that the angle of the chip flow direction has an important role in the cutting geometry; thus, various ways of expressing this angle were proposed.

Kronenberg suggested that the most significant of the three rake angle definitions would be the velocity rake angle, since it affects not only the cutting
forces, but also the finish of the work and the tool life. He also indicated that the flow direction would be in the plane parallel to the cutting velocity and perpendicular to the machined work surface. Around the same time, Merchant developed a model for frictionless oblique cutting; he attempted to minimize the shearing strain in the chip and found that his rake angle relationship for orthogonal cutting could be applied to non-orthogonal cutting. He also found that the cutting ratio for non-orthogonal cutting must be unity for the minimum energy solution. The final consequence of these findings, that the flow angle must equal the obliquity angle, was not realized by Merchant. Stabler later suggested that the effective rake angle should be used to describe cutting forces, and he also proposed a model to predict chip flow in oblique cutting that is based largely upon experimental evidence. The so called Stabler chip flow law postulates that the chip flow angle, $\eta$, is equal to the inclination angle, $i$:

$$\eta = i$$

(52)

This rule will be derived for particular conditions using the upper bound approach in a later section. Shaw et al. supported Stabler's claim of the importance of the effective rake angle, but they suggested that Stabler's flow rule was an approximation only. They related the flow angle to the inclination angle, the cutting width, and the width of the chip. Brown and Armargeo conducted an experimental study that concluded that the power consuming force is mainly affected by the normal rake angle and that the inclination angle has little effect on it. They did, however, find that even though a positive rake angle
will decrease the flow angle - they showed experimentally how accurately to relate the flow angle to cutting parameters, the normal rake angle, and the inclination angle - Stabler's flow rule is sufficiently accurate for most practical applications.

All investigators but Merchant attempted to fit cutting models to experimental data while ensuring some sort of force equilibrium, and identifying flow angles from empirical findings. A similar tendency to empiricism is found in the attempts to find cutting force models for practical tools with non straight cutting edges.

Colwell suggested that the chip flow direction is perpendicular to a line between the endpoints of the active cutting edge (see Figure 23). This model predicts the direction of edge forces only. In many cases edge forces and friction forces are well aligned. Therefore, this model provides fairly accurate results with minimal computational effort, but it is only valid for non-oblique cutting. This model has enjoyed great popularity because of its simplicity and accuracy in cutting conditions where the nose radius is significantly smaller than the depth of cut.
Okushima and Minato \cite{Okushima} proposed another model that provides very similar results to Colwell's model (see Figure 24). It is computationally more complex, and it also does not provide results for oblique cutting. This model assumes that the chip is constituted of infinitesimal elemental chips that would flow perpendicular to the elemental cutting edge. The overall flow direction is determined from the vectorial sum of the chip flows.

Young, Mathew, and Oxley \cite{Young} took a different approach again (see Figure 25). Their model is based on three assumptions. They assumed that an elemental friction force is collinear with the local chip velocity. Then the magnitude of this local friction force would be directly proportional to the local undeformed chip thickness. Finally the local chip flow direction would satisfy Stabler's flow rule. The third assumption allows them to cover oblique cutting. In their paper they derive analytic expressions for zero obliquity. It would seem that a derivation for the oblique cutting case is quite cumbersome.

Usui \textit{et al.} \cite{Usui} in a very complete investigation applied an upper bound solution, much like the one proposed in this thesis, to predict chip flow. In their model they considered obliquity and non-straight cutting tools. Their approach to calculate the shear area is identical to the one described here, but they chose to analyze the geometry numerically, rather than to replace the nose radius of the
tool with straight cutting edges. Furthermore, they used orthogonal cutting data for obtaining the shear angle, the shear yield stress, and the friction angle as functions of the rake angle. Finally, in their attempt to obtain an expression for the friction forces, they did not ensure a force equilibrium. Nevertheless, they were able to predict cutting forces, flow angles and shear angles with the aid of equivalent orthogonal cutting data. They also showed that Stabler's flow rule appeared to produce a minimum energy solution for cutting conditions with a dominant primary cutting edge, small friction, and large normal rake angles.

Finally several recent approaches have adopted approaches which appear to be in essence identical to that of Usui. Tai et al. $^{123}$ applied Usui's model to ball-end milling. Fuh et al. $^{124}$ extended Usui's model to chamfered main cutting edge tools.

### 3.3.3 Development of a Simplified Upper Bound Solution

The exercise conducted in this chapter is intended to allow the development of a model which is simple enough to allow rapid computation (including real time) and to allow easy identification of the influence of the various practical parameters. The tool edge geometry has been approximated by a series of straight edges; models using up to 6 edges have been analyzed. However, it has been found that the simple 3 edge approximation shown in Figure 26 below yields essentially similar results and trends. In this figure the actual cutting geometry is defined by the tool nose radius $r$, the approach angle $\psi_1$, the feed $s$, and the depth of cut $a$. The three edge approximation of this geometry is
defined in terms of the primary approach angle $\psi_1$, the secondary approach angle $\psi_2$, the length of the primary cutting edge $l_1$, the length of the secondary cutting edge $l_2$, and finally the feed $s$. The tertiary cutting edge is always parallel to the $x$ axis, and it is the same length as the feed. The workpiece approaches the rake face of the tool from the positive $z$ direction; it is rotated around the $x$ axis by the inclination angle $\gamma_p$ (back rake angle in milling or axial rake angle in turning), and it is pivoted around the $y$ axis by the angle $\gamma_f$ (side rake angle in milling or radial rake angle in turning). For a tool with zero approach angle, $\gamma_p$ is equal to the inclination angle, and $\gamma_f$ is equal to the normal rake angle.

As in the case of orthogonal cutting, one must formulate the work equations and obtain a minimum value. The kinematics of this situation are more complex in non-orthogonal oblique cutting than in orthogonal cutting, and the allowable shear angles normal to the individual cutting edges are constrained by the requirement for the chip to leave the zone as a rigid body. (The chip is assumed to leave with no rotation).
3.3.3.1 Velocity Components

The coordinate frame used in this chapter defines the rake face within the xy-plane, the feed direction in the negative x-direction, and the chip approach direction as the positive z-direction (see also Figure 26).

The velocity of the work material entering the shear area is assumed to have unit magnitude. Its direction, relative to the rake face, is dependent on the inclination angle and the normal rake angle:

\[
\vec{V}_{\text{in}} = \begin{bmatrix}
\sin \gamma_f \cos \gamma_p \\
-\sin \gamma_p \\
-\cos \gamma_f \cos \gamma_p 
\end{bmatrix}
\] (53)

The chip exiting the shear area has a velocity in the plane of the rake face (xy-plane), and its direction is described by the flow angle:

\[
\vec{V}_{\text{out}} = V_{\text{out}} \begin{bmatrix}
\cos \eta \\
-\sin \eta \\
0
\end{bmatrix}
\] (54)

The shear velocity is the vector difference between the exiting velocity and the entering velocity, hence,

\[
\vec{V}_s = \vec{V}_{\text{out}} - \vec{V}_{\text{in}} = \begin{bmatrix}
V_{\text{out}} \cos \eta - \sin \gamma_f \cos \gamma_p \\
-V_{\text{out}} \sin \eta + \sin \gamma_p \\
\cos \gamma_f \cos \gamma_p
\end{bmatrix}
\] (55)
3.3.3.2 Shear Areas

The shear area can be divided into two parts. These areas correspond to two parallelograms that constitute the undeformed chip areas. The first area (see Figure 26) is described by the points O-B-G-I; the second area is described by the points B-D-E-G. The overall shear area is the sum of these two areas.

\[ A_s = A_{s1} + A_{s2} = \overline{OBI} + \overline{BDEG} \]  

(56)

In order to calculate areas \( A_{s1} \) and \( A_{s2} \), each of these two areas is subdivided into three elemental areas that can be evaluated using vector cross products:

\[ A_{s1} = A_{SP}(l_1, \psi_1, \psi_2) = A_{e1}(l_1, \psi_1, \psi_2) + A_{e2}(l_1, \psi_1, \psi_2) + A_{e3}(l_1, \psi_1, \psi_2) \]

\[ A_{s2} = A_{SP}(l_2, \psi_2, \frac{\pi}{2}) = A_{e1}(l_2, \psi_2, \frac{\pi}{2}) + A_{e2}(l_2, \psi_2, \frac{\pi}{2}) + A_{e3}(l_2, \psi_2, \frac{\pi}{2}) \]  

(57)

Two identities \( s_1 \) and \( s_2 \) are required to determine these areas. Figure 27 shows the geometry of these two identities. They are related to the feed \( s \) by the approach angle and the flow angle:

\[ s_1(\psi) = \frac{s \sin \eta}{\sin \eta \sin \psi + \cos \eta \cos \psi} \]  

(58)

\[ s_2(\psi) = \frac{s \cos \psi}{\sin \eta \sin \psi + \cos \eta \cos \psi} \]  

(59)

The first elemental area corresponds to triangle OAI in \( A_{s1} \) and to triangle BCG in \( A_{s2} \) (see Figure 26):
Chapter 3  

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\[ A_{e_1}(l, \varphi_1, \varphi_2) = \frac{1}{2} \left| \begin{bmatrix} \sin \varphi_1 \\ \cos \varphi_1 \\ 0 \end{bmatrix} \cdot \begin{bmatrix} s_1(\varphi_1) \\ \cos \varphi_1 \\ 0 \end{bmatrix} \right| \times \begin{bmatrix} s_2(\varphi_1) V_{out} \end{bmatrix} \]  

(60)

The second elemental shear area elements correspond to the parallelograms ABHI and CDFG in \( A_{S_1} \) and \( A_{S_2} \) respectively, thus:

\[ A_{e_2}(l, \varphi_1, \varphi_2) = \left| \begin{bmatrix} \sin \varphi_1 \\ \cos \varphi_1 \\ 0 \end{bmatrix} \cdot \begin{bmatrix} (1 - s_1(\varphi_1)) \cos \varphi_1 \\ \cos \varphi_1 \\ 0 \end{bmatrix} \right| \times \begin{bmatrix} s_2(\varphi_1) V_{out} \end{bmatrix} \]  

(61)

The third elemental area corresponds to the triangle BGH in \( A_{S_1} \) and to triangle DEF in \( A_{S_2} \):

\[ A_{e_3}(l, \varphi_1, \varphi_2) = \frac{1}{2} \left| \begin{bmatrix} \sin \varphi_2 \\ \cos \varphi_2 \\ 0 \end{bmatrix} \cdot \begin{bmatrix} s_1(\varphi_2) \cos \varphi_2 \\ \cos \varphi_2 \\ 0 \end{bmatrix} \right| \times \begin{bmatrix} s_2(\varphi_1) V_{out} \end{bmatrix} \]  

(62)

The sum of the three elemental areas constitutes the overall areas \( A_{S_1} \) and \( A_{S_2} \). At this point one should realize that the sum of \( A_{e_1} \) and \( A_{e_2} \) can be simplified because the cross product terms result in parallel vectors:

\[ A_{e_12}(l, \varphi_1, \varphi_2) = A_{e_1}(l, \varphi_1, \varphi_2) + A_{e_2}(l, \varphi_1, \varphi_2) \\
= \left| \begin{bmatrix} \sin \varphi_1 \\ \cos \varphi_1 \\ 0 \end{bmatrix} \cdot \begin{bmatrix} \frac{1}{2} s_1(\varphi_1) \cos \varphi_1 \\ \cos \varphi_1 \\ 0 \end{bmatrix} \right| \times \begin{bmatrix} s_2(\varphi_1) V_{out} \end{bmatrix} \]  

(63)

The overall sum of the three elemental areas is then defined as follows:
This definition of the shear area requires knowledge of the length of the primary and the secondary cutting edges, as well as the angles that those cutting edges take with respect to the y-axis. The approach angle of the tertiary cutting edge is defined as 90°. The approach of the secondary cutting edge is chosen as the average between the primary and the tertiary approach angle:

$$\psi_2 = \frac{\pi}{4} + \frac{\psi_1}{2}$$  \hspace{1cm} (65)$$

The length of the tertiary cutting edge is set equal to the feed, and it is centered at the vertex of the nose radius. This assumption uniquely defines the length of the primary and the secondary cutting edge:

$$l_1 = \frac{a + r(sin \psi_1 - 1)}{cos \psi_1} + \frac{s}{2}$$  \hspace{1cm} (66)$$

$$l_2 = \frac{r \cos \psi_1 - \frac{s}{2}(1 + \sin \psi_1)}{\sin \psi_2}$$  \hspace{1cm} (67)$$

The overall shear area as a function of equation (64) is obtained by substituting equations (57), (65), (66), and (67), into equation (56). This substitution leads to
Chapter 3 An Upper Bound Cutting Model

a definition of the shear area in terms of approach angle \( \psi \), nose radius \( r \), depth of cut, \( a \), and feed, \( s \).

\[
A_s = A_{s_1} + A_{s_2} = A_s \ast (l_1, \psi_1, \psi_2) + A_s \ast (l_2, \psi_2, \frac{\pi}{2})
\]

\[
A_s = A_{sp} \left( \frac{a + r (\sin \psi_1 - 1)}{\cos \psi_1} + \frac{s}{2} \psi_1 \frac{\pi}{4} + \frac{\psi_1}{2} \right) + \frac{r \cos \psi_1 - \frac{s}{2} (1 + \sin \psi_1)}{\sin \left( \frac{\pi}{4} + \frac{\psi_1}{2} \right)} \left( \frac{\pi}{4} + \frac{\psi_1}{2} \right)
\]

(68)

3.3.3.3 Friction Area

An equivalent straight shear area in the direction of the chip flow is assumed; thus, the ratio of shear area to friction area can be expressed by replacing the friction angle \( \beta \), the rake angle \( \gamma \), and shear angle \( \phi \), in equation (47) with equivalent parameters in the in the chip flow direction:

\[
A_f = A_{sp} \left( \sin \beta_e \right) \frac{\sin \beta_e}{\cos (\beta_e - \gamma_e + \phi_e)}
\]

(69)

The friction angle \( \beta_e \) depends on the material properties and on the amount of work hardening present. In the absence of work-hardening and the presence of sticking friction (from the Hencky equations 119),

\[
\tan \beta_e = \frac{1}{1 + \frac{\pi}{2} - 2\gamma_e}
\]

(70)
The effective rake angle $\gamma_e$ and the effective shear angle $\phi_e$ are measured in the plane containing the cutting direction and the chip flow direction; they are determined using the following two relationships:

\[
\sin \gamma_e = \frac{\vec{V}_{in} \cdot \vec{V}_{out}}{|\vec{V}_{in}| |\vec{V}_{out}|} = \cos \eta \sin \gamma \cos \gamma_p + \sin \eta \sin \gamma_p
\]

\[
\cos \phi_e = -\frac{\vec{V}_{in} \cdot \vec{V}_{in}}{|\vec{V}_{in}|^2} = \frac{1 - V_{out} \left( \cos \eta \sin \gamma \cos \gamma_p + \sin \eta \sin \gamma_p \right)}{|\vec{V}_{in}|}
\]

3.3.3.4 Work Rates
The sum of the shear work rate and the friction work rate must be minimized in order to obtain an upper bound solution of the flow angle. The shear work rate is defined as the product of the shear force and the shear velocity:

\[
W_s = F_s \cdot V_s = \tau A_s V_s
\]

Similarly, the friction work rate is defined as the product of the friction force and the friction velocity:

\[
W_f = F_f \cdot V_{out} = \tau A_f V_{out} = F_s \frac{A_f}{A_s} V_{out}
\]

The overall work rate is the sum of the shear and the friction work rate:

\[
W = W_s + W_f = F_s \left( V_{in} + \frac{A_f}{A_s} V_{out} \right) = \tau A_s \left( V_{in} + \frac{A_f}{A_s} V_{out} \right)
\]
This objective function can be expanded using equations (65) to (72), which results in an expression for the overall workrate whose only unknowns are the magnitude of the exiting velocity $V_{out}$, and the flow angle $\eta$. The upper bound solution then requires that the overall workrate is minimized with respect to these unknowns. The programming package Maple\textsuperscript{114} was used to minimize the overall workrate numerically in the remainder of this chapter.

**3.4 Comparison of the Upper Bound Solution with Other Models**

There are many formulations which allow the prediction of chip flow direction and inplane force components. Most of these are empirical in nature. Given the relative success of such physically based empirical formulations, the author will first compare the predictions from the upper bound with existing formulations.

**3.4.1 Comparison with Merchant's Rule**

Even though Merchant's rule (see equation (38)) was developed for orthogonal cutting, the assumptions made in the derivation of this upper bound solution will give the Merchant shear angle solution when the effective parameters $\beta_e$, $\gamma_e$, and $\phi_e$ are substituted for the orthogonal equivalents $\beta$, $\gamma$, and $\phi$. Figure 28 shows the effective shear angles as functions of effective rake angle for a ratio of nose radius over cutting
depth of 0.1 and an inclination angle of 10°. This figure was obtained by solving
equation (75) numerically with the programming package Maple for rake
angles ranging between -10° and +10°. Two separate simulations were
conducted. In the first case, zero friction was assumed and the top curve was
obtained. In the second case, the Hencky equation was utilized to obtain the
friction angle (see equation (70)) and the lower curve was generated.

3.4.2 Comparison with Stabler's Rule
Stabler proposed that the chip flow angle is equal to the inclination angle for a
single edge cutting tool. In this section Stabler's rule is derived analytically,
and then the model from the previous section is utilized further to confirm the
validity of Stabler's rule.

The shearing velocity for a single edge cutting tool is given by

$$\bar{V}_s = \begin{bmatrix} V_{out} \cos \eta - V_{in} \sin \gamma_n \cos i \\ -V_{out} \sin \eta + V_{in} \sin i \\ V_{in} \cos \gamma_n \cos i \end{bmatrix}$$

The shear area is evaluated by taking the magnitude of the crossproduct of the
sides if the shear area:

$$A_s = \begin{bmatrix} 0 \\ 1 \\ 0 \end{bmatrix} \times \left( \frac{s}{V_{out} \cos \eta} \bar{V}_s \right)$$

The work rate is defined as the product of the shear velocity and the shear area
times the shear modulus as follows:
The upper bound solution of the three edge cutting tool that was derived in section 3.3.3 was used to evaluate the flow angle of a tool with a nose radius that was three orders of magnitude smaller than the length of the main cutting edge. Flow angles were determined for a range of inclination angles and rake angles. The results of...
these simulations confirm Stabler’s rule for cutting conditions with no friction. When friction is present, the flow angle is decreased. Figure 29 shows the flow angle as a function of inclination angle for the cases with and without friction. The curve designated with $\beta=0^\circ$ represents the non-friction case. In this case, the inclination angle is independent of the rake angle, and the flow angle is equal to the inclination angle. The three other curves show the inclination angle for the case where the friction angle was determined using the Hencky equations (equation (70)). In this case the flow angle is dependent on the rake angle. It is concluded that friction and positive rake angles decrease the flow angle.

3.4.3 Comparison with Models for Tools with a Nose Radius

Most models that were discussed in section 3.3.2 are applicable to cuts with zero inclination angle. This section then compares the solutions derived from these models with that for the upper bound. The results, for three approach angles and a range of depth of cut to radius ratio are shown in Figure 30 to Figure 32.
The result of the numerical exercise shows that, in general, the value of flow angle predicted lies between those predicted by Colwell type models and the model proposed by Young et al. In practice, Colwell's flow rule is widely used because of its simplicity.

When a cutting tool has a reasonably large inclination angle, then the simplest method of predicting forces and chip flow angle would seem to be to combine Stabler and Colwell rules. Figure 33 to Figure 36, show a comparison of such an approach with results resulting from the upper bound. The flow angles predicted by Colwell and Stabler have a similar trend to those predicted by the upper bound solution. Clearly, however, the agreement is best for low friction, low normal rake angle and smaller nose radius.
Chapter 3

An Upper Bound Cutting Model

**Flow Angle for r/a=0.1, no Friction**

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<table>
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<tr>
<th>θ (deg)</th>
<th>U.B., γf = 0°</th>
<th>U.B., γf = 15°</th>
<th>U.B., γf = 30°</th>
<th>U.B., γf = 45°</th>
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Figure 33. Flow Angle for r/a=0.1, without Friction

**Flow Angle for r/a=0.1, with Friction**

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<th>θ (deg)</th>
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Figure 34. Flow Angle for r/a=0.1, with Friction

**Flow Angle for r/a=0.5, no Friction**

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Figure 35. Flow Angle for r/a=0.5, without Friction
3.5 Comparison with Experiments
The upper bound equations have also been tested against discrete experimental data. The first series of experiments considered was performed by Yellowley and Lai 126, the data here was obtained by straight diameter cutting. The second series of experiments was carried out by Adey 127 who measured all three components of force during circular interpolation. Finally, to provide some data with an alternate work material, the author performed a similar exercise to that described by Adey using an aluminum alloy workpiece rather than low alloy steel as was the case in the other two experiments.
The data gathered by Lai\textsuperscript{126}, was motivated by the need to perform identification and wear monitoring in production operations.

The flow angles obtained from the experiment and the flow angle obtained from the upper bound solution are shown in Figure 37. In this case flow angles are measured with respect to the turning axis. There is a good correlation between the measured and the predicted flow angles.

The geometry used by Adey\textsuperscript{127} allowed the generation of a wide range of effective tool approach angle (see Figure 38). The feed was varied along the contour to ensure a constant equivalent chip thickness.
Flow Angles (D. Adey’s circular interpolation data)

Figure 39. D. Adey’s Experiment

| Work Material: | AISI 1045 (HB 320) |
| Nose radius:   | 0.79 mm             |
| Approach angle:| 0°-79°               |
| Rake angle:    | 5°                   |

| Tool Material: | Kennametal K45 |
| Depth of cut:  | 1.27 mm         |
| Cutting speed: | 25 m/min        |
| Inclination angle: | 5°              |

Figure 39 shows a plot of the flow angles obtained from the experimental friction forces and the upper bound solution. The first three data points must be neglected since they contain unaccounted tool entry conditions. The flow angles are measured with respect to the turning axis.

A new set of experiments of similar nature to those reported by Adey was performed on the Hitachi Seiki lathe at UBC. In this case, however, the tangential feed was constant. The sampling rate used was 1 kHz, and an analog antialiasing filter with a break frequency of 300 Hz was utilized. The force and position data was then digitally filtered and subsampled to provide one
sample per revolution of the spindle. The cutting geometry is shown in Figure 38.

Figure 40 shows the flow angles obtained in the experiment. In this case, flow angles are measured with respect to the turning axis. The upper bound model gives a good estimate of the flow angle for low approach angles. At the higher values, the upper bound solution approaches Colwell's solution, whereas the cutting data shows slightly lower angles. This discrepancy can be attributed to some extent to the tool exit conditions. There is a slight inconsistency at approach angles of approximately 35°. This inconsistency could be due to experimental error or to the chip breaker on the rake face of the tool that is not part of the upper bound model.

Over all, the agreement between the theory and the experimental results is quite good.

3.6 Conclusions

A simplified upper bound solution for turning tools of general geometry has been described. This model provides the flow angle to the cutting models in sections
2.2.3.2 and 2.2.3.3. These cutting models relate the magnitude of the power consuming force and the thrust force to the cutting geometry and cutting constants. The flow angle which is provided by the upper bound solution presented here describes the direction of the thrust force; thus, axial and radial components of the thrust force in turning and milling can be predicted from the cutting geometry, the cutting constants, and the friction characteristic of the chip on the rake face.

The model needs no experimental data to generate flow angle; however, both the accuracy of flow angle estimation and, in particular, the accuracy of prediction of effective shear and friction parameters can be improved, given equivalent orthogonal data. The model has been used to prove the validity of the Stabler solution for oblique cutting with a straight edge and has been compared to the most commonly accepted models for chip flow direction.

The effective values of shear, friction, and rake angle are related by the Merchant expression; comparison with discrete experimental data shows good agreement for flow angle.
Chapter 4
Process Identification

4.1 Introduction
Process plans rely upon estimated values of geometrical and technological cutting parameters with which certain amount of uncertainty is always associated. Real time identification of these parameters enables the process control system to optimize the cutting conditions and the planning system to improve the process efficiency.

Numerous physical phenomena have been utilized indirectly to gain qualitative and quantitative insight into the cutting process. (Direct transducers for most of the parameters in question are not available.) Transducers for cutting forces, cutting temperature, and acoustic emission have been applied with good success for various parameters of the process. In this work, cutting forces are emphasized, since they themselves represent a direct constraint, and their interaction with the cutting process is also fairly well understood.

The identification algorithms described in this chapter rely on force measurements that contain frequency components up to the tooth passing frequency only, because high bandwidth sensors are too costly for practical milling operations. A complementary low cost dynamometer that is able to provide this data is described in Appendix B.
The process control system requires both magnitudes of force and a knowledge of cutting geometry to perform feed regulation. These parameters are also required for the subsequent identification schemes for tool condition monitoring. Techniques for identifying the peripheral milling geometry from in-plane forces have been provided in section 2.5; they are extended here to include face milling. This extension is an important step forward because transients for face and end milling can be identified with these techniques as well.

The identification of cutting force parameters and the feedback of such parameters to the planning system from the monitoring system is necessary for a totally integrated planning and control system. The data can be used to calculate the relationship between cutting forces and cutting geometry in the planning stage in real time; and changes in these parameters can also be used to track tool wear. Previous algorithms have been extended from end milling to face milling.

Tool condition monitoring is important from an operating safety standpoint and from a part quality perspective. Badly worn tools or tools with excessive runout can lead to unacceptable surface finish, poor part tolerances, and, if the tools fail catastrophically, then machines and operator safety are compromised. The major effort in this chapter is the development of efficient techniques for identifying radial and axial runout of individual inserts in real time. It is believed that a quantitative measure of these parameters can eventually be correlated with other important variables such as surface finish and edge breakage.
4.2 Identification of Machining Conditions

In face milling, the cutting geometry is uniquely defined by the insert entry angle $\phi_1$, the insert exit angle $\phi_2$, and the axial depth $a$ (see Figure 41). It is difficult to identify the two angles directly from cutting forces. However, the swept angle of cut $\phi_s$ (the difference between these two angles) and the mean immersion angle $\phi_m$ (the average between the two angles) can be identified fairly readily from Fourier series coefficients of an orthogonal in-plane force pair.

\[
\phi_s = (\phi_2 - \phi_1) \quad (79)
\]

\[
\phi_m = \frac{1}{2}(\phi_1 + \phi_2) \quad (80)
\]

The swept angle of cut and the mean immersion angle can of course be related to the insert entry and exit angles:

\[
\phi_1 = \phi_m - \frac{\phi_s}{2} \quad (81)
\]

\[
\phi_2 = \phi_m + \frac{\phi_s}{2} \quad (82)
\]

The radial width can then be determined in terms of these two angles.

4.2.1 Swept Angle of Cut

The swept angle of cut is obtained from the ratio of the sum of the squared magnitudes of the fundamental components of feed and normal forces over the
squared quasi mean resultant force. A more detailed description of this algorithm is shown in section 2.5.2.

\[ \phi_s = f \left( \frac{a_{x_o}^2 + b_{x_o}^2 + a_{y_n}^2 + b_{y_n}^2}{F_{q_m}^2} \right) = f(Q) \]  

(83)

Computing the parameter Q requires the fundamental force components in x and y. It is possible to eliminate calculating Fourier series coefficients, if the feed and normal forces are filtered with a steep low pass filter slightly above the tooth passing frequency. These force can then be represented as a Fourier series with only a fundamental term at the tooth passing frequency, if the components due to runout are neglected. (This is a good assumption unless several teeth are severely damaged.)

\[ F_x = a_{x_o} + a_{x_n} \cos(\phi) + b_{y_n} \sin(\phi) \]
\[ F_y = a_{y_o} + a_{y_n} \cos(\phi) + b_{y_n} \sin(\phi) \]  

(84)

The sum of the means of these two filtered forces squared is called the mean squared resultant force:

\[ F_{mr}^2 = F_x^2 + F_y^2 = a_{x_o}^2 + b_{x_o}^2 + 1\frac{1}{2} \left( a_{x_n}^2 + b_{x_n}^2 + a_{y_n}^2 + b_{y_n}^2 \right) \]
\[ = F_{q_m}^2 + 1\frac{1}{2} \left( a_{x_n}^2 + b_{x_n}^2 + a_{y_n}^2 + b_{y_n}^2 \right) \]  

(85)

This relationship leads to a new expression of the immersion parameter:

\[ Q = 2 \frac{F_{mr}^2 - F_{q_m}^2}{F_{q_m}^2} = 2\frac{F_x^2 + F_y^2 - F_{x}^{-2} - F_{y}^{-2}}{F_x^{-2} + F_y^{-2}} \]  

(86)

It is stressed again that this algorithm for obtaining the immersion parameter assumes that the forces are filtered close to the tooth passing frequency. It is
Chapter 4 Process Identification

also assumed that the Fourier series coefficients due to radial runout that are found at multiples of the spindle rotation frequency are small compared to the components at the tooth passing frequency.

4.2.2 Mean Immersion Angle

The mean immersion angle can be monitored in the time domain or in the frequency domain. In this work, both approaches were implemented and tested. The frequency domain algorithm was finally selected over the time domain algorithm, because the former applies for a wider range of operating conditions. This algorithm is presented in the next paragraphs, whereas the time domain algorithm is shown in Appendix C.

The frequency domain identification technique attempts to find the mean immersion angle from the ratio of the mean forces in x and in y. If edge forces are neglected, the ratio is given by the following equation:

\[
\frac{F_y}{F_x} = Q_m = \frac{b_i - r_i \cos(\psi_e) a_i}{a_i + r_i \cos(\psi_e) b_i} \left( \frac{r_i \cos(\psi_e)(\cos(2\phi_m + \phi_s) - \cos(2\phi_m - \phi_s))}{\sin(2\phi_m - \phi_s) - \sin(2\phi_m + \phi_s) + 2\phi_s} \right) \tag{87}
\]

This relationship can be solved analytically for the mean immersion angle:
The analytical solution is quadratic and is quite complex. A very good approximation for this equation is, however, found by inspection:

\[
\phi_m \approx \phi_m^* = \tan^{-1}(Q_m) + \tan^{-1}(r_i \cos(\psi_e))
\]  

A comparison of these two formulae is shown in Figure 42 and Figure 43, where the theoretical value of mean swept angle of cut is plotted against the identified value from the simplified approximate relationship. The approximation performs better for low values of swept angle of cut and values of \( \phi_m \) close to 90°. For edge breakage control the approximation will always yield conservative values of \( \phi_m \) that are closer to the half immersion value than the exact solution.
4.2.3 Axial Depth
The axial depth is obtained from the magnitude of the quasi mean resultant force and knowledge of the immersion angles. If edge forces are neglected, then the quasi mean resultant force can be expressed in terms of the fundamental of the torque series:

\[
F_{qm} = \sqrt{F_x^2 + F_y^2} = \frac{NK a s_t}{2} \sqrt{1 + (r_1 \cos(\psi_1))^2} \sqrt{a_1^2 + b_1^2}
\]

\[
= \frac{NK a s_t}{4\pi} \sqrt{1 + (r_1 \cos(\psi_1))^2} \sqrt{\phi_s (\sin 2\phi_1 - \sin 2\phi_2) + \sin^2 \phi_2 + \phi_s^2}
\]

This relationship is inverted to find the axial depth:

\[
a = \frac{4\pi F_{qm}}{NK s_t \sqrt{1 + (r_1 \cos(\psi_1))^2} \sqrt{\phi_s (\sin 2\phi_1 - \sin 2\phi_2) + \sin^2 \phi_2 + \phi_s^2}}
\]

4.3 Identification of Cutting Parameters
When the cutting geometry is known exactly, calibration cuts can be used to identify the cutting parameters. The mean forces in x and y give a good estimate
of the cutting pressure and the force ratio. Again, if we neglect the edge forces, the cutting forces are expressed in terms of the fundamental of the cutting torque:

\[
\begin{align*}
F_x &= \frac{NK a_s}{2} \left[a_i + (r_i \cos(\psi_e)) b_i \right] \\
F_y &= \frac{NK a_s}{8\pi} \left[(\cos(2\phi_1) - \cos(2\phi_2)) + (r_i \cos(\psi_e))(2\phi_1 + \sin(2\phi_1) - \sin(2\phi_2))\right]
\end{align*}
\]

These two equations can be solved for the cutting pressure \(K\), and the force ratio \(r_i \cos(\psi_e)\):

\[
\begin{align*}
K &= \frac{8\pi}{NK a_s} \frac{a_i F_x - b_i F_y}{a_i^2 + b_i^2} \\
r_i \cos(\psi_e) &= \frac{b_i F_x + a_i F_y}{a_i F_x - b_i F_y}
\end{align*}
\]

If the cutter wears, the force ratio \(r_i\) is likely to increase; thus, it is possible to use equation (94) to track tool wear. This approach has been described by Yellowley et al.\textsuperscript{23}.

### 4.4 Identification of Runout\textsuperscript{*}

The use of Fourier series for the identification of cutting conditions and the tracking of tool condition in milling is fairly well established\textsuperscript{128,44}. Previous investigations have generally assumed that, in the first place, all teeth on a

\textsuperscript{*} The work presented on radial runout has been submitted for publication by Seethaler and Yellowley\textsuperscript{129}.
milling cutter have the same dimensions and hence the same magnitudes of cutting forces. The reality, of course, is more complex; cutters have both axial and radial inconsistencies, whether as a result of a simple eccentricity in mounting or, more generally, as a result of a combination of factors involved in the manufacture and mounting of the cutter. The case of a simple eccentricity has been analyzed for helical end mills \(^{24}\). In this section, identification of the radial and axial runout of individual teeth on a milling cutter in the general case is investigated in detail. The provision of a generalized approach for face milling has great practical importance because the characterization of a simple eccentricity will rarely be sufficient; in the specific case of inserted tooth cutters, for instance, the geometry of individual pockets and inserts are likely to be more important than spindle runout. It should also be possible to utilize the approach developed to identify breakage as a change in relative runouts as cutting progresses. Such a method is then able to cope with relatively large initial runouts and to identify the changes caused by a subsequent edge chip or breakage.

4.4.1 Radial Runout
Radial runout influences part accuracy and surface finish in end milling. It is reflected in the in plane force patterns; thus, the Fourier series coefficients of this force pair is used to identify radial runout. The technique described here is derived for tools with zero helix angle. In the case of helical end mills, this technique can be used only for cuts with small axial depths.
4.4.1.1 Radial Runout Representation as a Discrete Fourier Series

The presence of a radial runout between adjacent cutting edges leads to a change in chip thickness for the later of the two teeth. This change may be approximated as a constant amount equal to the relative runout. Figure 44 illustrates the relationship between the absolute radial size of teeth \( R_t \) and the relative runout between teeth \( A_{r_t} \). The relative runout \( A_{r_t} \) is the difference between the radial size of the current tooth \( R_t \) and the radial size of the previous tooth \( R_{t-1} \). The sum of the individual chip thickness added as a result of runout in a single revolution, in the absence of other dynamic effects, must be zero for a cutter.

A general expression describing the runout between teeth in the form of a discrete Fourier series (as shown in equation (95)) simplifies the expressions relating runouts of individual teeth to cutting torques and cutting forces. The magnitude components in this expression are designated \( f_{k} \), the phase components are labeled \( \lambda_{k} \), and the overall number of flutes of the cutter is called \( N \).

\[
A_{r_t} = R_t - R_{t-1} = \frac{f_{k_t}}{2} + \sum_{k=1}^{\text{floor}(N/2)} f_{k} \cos \left( \lambda_{k} + 2\pi k \frac{t}{N} \right)
\]

where \( \lambda_{N/2} = 0 \) or \( \pi \) for \( N = \text{even} \)
4.4.1.2 In-Plane Force Series
Radial runout is assumed to add a constant undeformed chip thickness throughout the contact of the tool with the workpiece. Given a linear relationship between chip thickness and force, the torque due to radial runout $T_r$ is a function of the tool radius $R$, the cutting pressure $K$, the axial depth $a$, the undeformed chip thickness $A_i$, the rotational angle $\phi$, and the insert entry and insert exit angles $\phi_1$ and $\phi_2$.

$$T_r = RKaA_i, \quad \text{for } \phi_1 \leq \phi \leq \phi_2$$

$$T_r = 0, \quad \text{for } 0 \leq \phi \leq \phi_1$$

$$\phi_2 \leq \phi \leq 2\pi$$

(96)

The tangential force acting on a single tooth is related to the cutting torque through the radius of the tool (see Figure 2 in Chapter 2):

$$F_t = R T_r$$

(97)

The radial force is assumed to be related to the tangential force by the cutting ratio $r_1$ and the chip flow angle $\psi_e$:

$$F_r = r_1 \cos(\psi_e)F_t$$

(98)

The forces in the direction of table and saddle, generally designated as $F_x$ and $F_y$ respectively, can be described in terms of the cutting torque $T_r$, the tool radius $R$, the force ratio $r_1$, the chip flow angle on the tool insert with respect to the axial direction of the spindle $\psi_e$, and the rotational angle of the cutter $\phi$: 
\[ F_x = \frac{T}{R} (\cos \phi + r_i \cos(\psi_e) \sin(\phi)) = \]
\[ = K a \sum_{t=1}^{N} A_t (\cos \phi + r_i \cos(\psi_e) \sin(\phi)) \left\{ \begin{array}{l}
\text{Heaviside} \left( \phi - \left( \frac{2\pi t}{N} + \phi_1 \right) \right) \\
-\text{Heaviside} \left( \phi - \left( \frac{2\pi t}{N} + \phi_2 \right) \right)
\end{array} \right. \]

\[ F_x = \frac{T}{R} (\sin \phi - r_i \cos(\psi_e) \cos(\phi)) \]
\[ = K a \sum_{t=1}^{N} A_t (\sin \phi - r_i \cos(\psi_e) \cos(\phi)) \left\{ \begin{array}{l}
\text{Heaviside} \left( \phi - \left( \frac{2\pi t}{N} + \phi_1 \right) \right) \\
-\text{Heaviside} \left( \phi - \left( \frac{2\pi t}{N} + \phi_2 \right) \right)
\end{array} \right. \] (99)

These forces can also be expressed in the form of Fourier series with Fourier series coefficients \(a_{\alpha k}\) and \(b_{\alpha k}\) for \(F_x\) and \(a_{\gamma k}\) and \(b_{\gamma k}\) for \(F_y\).
\[ F_x = \frac{a_{\alpha_k}}{2} + \sum_{k=1}^{\infty} a_{\alpha_k} \cos(k \phi) + b_{\alpha_k} \sin(k \phi) \]

\[ F_y = \frac{a_{\gamma_k}}{2} + \sum_{k=1}^{\infty} a_{\gamma_k} \cos(k \phi) + b_{\gamma_k} \sin(k \phi) \]

where:

\[ a_{\alpha_k} = K \alpha \sum_{t=1}^{N} A_{\alpha_k} \int_{\frac{2 \pi t + \phi_1}{N}}^{\frac{2 \pi t + \phi_2}{N}} \left[ \cos\left( \phi - \frac{2 \pi t}{N} \right) + r_{i} \cos(\nu_{e}) \sin\left( \phi - \frac{2 \pi t}{N} \right) \right] \cos(k \phi) \, d\phi \]

\[ b_{\alpha_k} = K \alpha \sum_{t=1}^{N} A_{\alpha_k} \int_{\frac{2 \pi t + \phi_1}{N}}^{\frac{2 \pi t + \phi_2}{N}} \left[ \cos\left( \phi - \frac{2 \pi t}{N} \right) + r_{i} \cos(\nu_{e}) \sin\left( \phi - \frac{2 \pi t}{N} \right) \right] \sin(k \phi) \, d\phi \]

\[ a_{\gamma_k} = K \alpha \sum_{t=1}^{N} A_{\gamma_k} \int_{\frac{2 \pi t + \phi_1}{N}}^{\frac{2 \pi t + \phi_2}{N}} \left[ \sin\left( \phi - \frac{2 \pi t}{N} \right) + r_{i} \cos(\nu_{e}) \cos\left( \phi - \frac{2 \pi t}{N} \right) \right] \cos(k \phi) \, d\phi \]

\[ b_{\gamma_k} = K \alpha \sum_{t=1}^{N} A_{\gamma_k} \int_{\frac{2 \pi t + \phi_1}{N}}^{\frac{2 \pi t + \phi_2}{N}} \left[ \sin\left( \phi - \frac{2 \pi t}{N} \right) + r_{i} \cos(\nu_{e}) \cos\left( \phi - \frac{2 \pi t}{N} \right) \right] \sin(k \phi) \, d\phi \]  \hspace{1cm} (100)

Equation (100) can be solved analytically for different numbers of teeth. A very convenient parameter for relating the magnitude components of the runout series \( f_k \) to the force series is the sum of the squares of the magnitude components of the \( x \) force and the \( y \) force:

\[ m_{\alpha_k} = \sqrt{(a_{\alpha_k})^2 + (b_{\alpha_k})^2} \]

\[ m_{\gamma_k} = \sqrt{(a_{\gamma_k})^2 + (b_{\gamma_k})^2} \]

Substitution from equation (100) into equation (101) leads to the relationships shown in equation (102) which can be used to infer the magnitude components of the runout series from the magnitude components of the forces in \( x \) and \( y \). The expressions have been evaluated for cutters with up to sixteen teeth.
\[ m_{xy} = K a \frac{f_r N}{2 \pi} \sqrt{2 \left( 1 + \left( r_i \cos \psi_e \right)^2 \right) \left( 1 - \cos \phi_s \right)} \]

\[ m_{oxy} = K a q_i \frac{f_r N}{4 \pi} \sqrt{2 \left( 1 + \left( r_i \cos \psi_e \right)^2 \right) \left( 1 + 2 \phi_s^2 - \cos 2 \phi_s \right)} \]

\[ m_{oxx} = K a q_k \frac{f_r N}{2(k+1) \pi} \left( 1 + \left( r_i \cos \psi_e \right)^2 \right) \left( \frac{1 - \cos \left( (k+1) \phi_s \right)}{k+1} \right) \]

\[ \text{for } 1 < k \leq \frac{N}{2} \]

where \( q_k = 1 \) for \( k < \frac{N}{2} \)

\[ q_n = 2 \text{ sign } a_{xy} \]

\[ \begin{align*}
\begin{pmatrix}
\frac{N}{N+2} \left( \cos \left( \frac{N}{2} \phi_2 \right) - \cos \left( \frac{N+1}{2} \phi_2 \right) \right) \\
\frac{N}{N-2} \left( \cos \left( \frac{N}{2} \phi_1 \right) - \cos \left( \frac{N-1}{2} \phi_1 \right) \right) \\
+ \frac{N}{N-2} \left( \sin \left( \frac{N}{2} \phi_1 \right) - \sin \left( \frac{N-1}{2} \phi_1 \right) \right)
\end{pmatrix}
\end{align*} \]  

(102)

It should be noted that for a cutter with an even number of teeth, the \( N/2 \) harmonic of the radial runout series does not have a phase angle. However, the magnitude of the harmonic can be positive or negative. This problem has been addressed in the expression of \( q_{n/2} \) in equation (102).

The accuracy of the identification results are dependent on good parameter estimates of cutting pressure \( K \), and face force ratio \( r_1 \). If the edge forces are neglected, then it is possible to normalize the identified relative runouts with the quasi mean resultant force which cancels the cutting parameter estimates:
The expressions describing the phase angles of the runout series can be obtained from a variety of different parameters. One possible parameter is the ratio of difference between the sum of the squares of the real components of the x and y forces and the sum of the squares of the imaginary components of the x and y forces over the sum of the squares of the real and imaginary components of the x and y forces. This parameter leads to a very simple quadratic expression for relating phase angles of relative radial runouts to cutting forces; unfortunately, the quadratic nature of this solution presents a strong drawback because it switches for different immersion angles. Consequently, a different solution is proposed here that relies on x forces alone (i.e., force in the direction of cutter feed). The method utilizes the ratio of the real and imaginary components of the cutting force in the x direction:
\[ \tan \lambda_{i_0} = 0 \quad \text{and} \quad \tan \lambda_{r_n} = 0 \]

\[
\tan \lambda_i = \frac{a_{r_k} \left[ \lambda_{1_k} + (r_i \cos \psi_e) \lambda_{2_k} \right] + [\lambda_{3_k} + (r_i \cos \psi_e) \lambda_{4_k}] - a_{r_k} \left[ \lambda_{1_k} + (r_i \cos \psi_e) \lambda_{2_k} \right] + [\lambda_{3_k} + (r_i \cos \psi_e) \lambda_{4_k}]}{b_{r_k} \left[ \lambda_{1_k} + (r_i \cos \psi_e) \lambda_{2_k} \right] + [\lambda_{3_k} + (r_i \cos \psi_e) \lambda_{4_k}]} \quad \text{for } 1 \leq k < \frac{N}{2}
\]

where

\[
\lambda_{1_k} = \cos(2\phi_2) - \cos(2\phi_1) \quad , \quad \lambda_{2_k} = 2\phi_a + \sin(2\phi_1) - \sin(2\phi_2)
\]

\[
\lambda_{3_k} = -2\phi_a + \sin(2\phi_1) - \sin(2\phi_2) \quad , \quad \lambda_{4_k} = \cos(2\phi_1) - \cos(2\phi_2)
\]

and

\[
\lambda_{1_k} = -(k-1) \left( \cos((k+1)\phi_1) - \cos((k+1)\phi_2) \right) - (k+1) \left( \cos((k-1)\phi_1) - \cos((k-1)\phi_2) \right)
\]

\[
\lambda_{2_k} = (k-1) \left( \sin((k+1)\phi_1) - \sin((k+1)\phi_2) \right) - (k+1) \left( \sin((k-1)\phi_1) - \sin((k-1)\phi_2) \right)
\]

\[
\lambda_{3_k} = (k-1) \left( \sin((k+1)\phi_1) - \sin((k+1)\phi_2) \right) + (k+1) \left( \sin((k+1)\phi_1) - \sin((k+1)\phi_2) \right)
\]

\[
\lambda_{4_k} = (k-1) \left( \cos((k+1)\phi_1) - \cos((k+1)\phi_2) \right) - (k+1) \left( \cos((k+1)\phi_1) - \cos((k+1)\phi_2) \right)
\]

Equation (104) requires that the forces be aligned with the cutting direction. In the case where the force transducer is not aligned with the cutting direction, the measured forces need to be rotated into the cutting direction.

4.4.1.3. Simulation of Radial Runout

Before an experimental investigation was conducted, the equations and the expected accuracy of prediction were tested by simulation. The main goal of this simulation was to show the affect that different sampling rates have on the accuracy of the identified values of runout.
The cutter used in the simulation had five teeth, and the initial runout series had the coefficients shown in Table 3. The simulation program constructed a time series of the x and y forces in a down milling operation resulting from a cutter with the prescribed runout. A fast Fourier transform was then applied to sets of data obtained by sampling the force traces. The actual runout was then predicted from the equations of section 4.4.1.2 using the magnitude and phase of the mean, fundamental and required harmonics.

During the simulation, the immersion angle was varied between $5^\circ$ and $180^\circ$, and two sampling systems were used, one with 30 samples per revolution ($12^\circ$ per sample), the other with 256 samples per revolution ($1.4^\circ$ per sample). The cutting model used in this simulation was based on equation (99). It was deemed reasonable to model the forces due to runout alone because they appear at different harmonics than the main cutting forces which, consequently, have no influence on the runout identification procedure. The specific cutting pressure $K$ and the axial depth $a$, were set to unity arbitrary units, the force ratio $r$, was set to 0.3, and the chip flow angle $\psi_e$ was set to $0^\circ$.

<table>
<thead>
<tr>
<th>$A_{r_1}$</th>
<th>$A_{r_2}$</th>
<th>$A_{r_3}$</th>
<th>$A_{r_4}$</th>
<th>$A_{r_5}$</th>
<th>$f_{r_1}$</th>
<th>$f_{r_2}$</th>
<th>$f_{r_3}$</th>
<th>$\lambda_{r_1}$</th>
<th>$\lambda_{r_2}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.66 mm</td>
<td>0.07 mm</td>
<td>-2.99 mm</td>
<td>-1.17 mm</td>
<td>2.52 mm</td>
<td>0 mm</td>
<td>2 mm</td>
<td>1 mm</td>
<td>35°</td>
<td>167°</td>
</tr>
</tbody>
</table>

Table 3. Runout Series Coefficients (see equation(95))

The result of the simulated procedure is shown in Figure 45 where it is seen that the system works well except for the cases where both the immersion angle is a multiple of the tooth spacing angle and the sampling frequency is low. These inconsistencies are attributed to discretization errors of the sampling system.
The cutting forces were represented as a continuous Fourier transform in the model shown in the previous section. In the simulation a fast Fourier transform was utilized to represent the cutting forces. If there are less than two samples over the time that a tooth is in contact with the workpiece, then there is a large difference between the fit of the cutting forces made by the fast Fourier transform and the actual continuous forces.

![Graph showing Delta tooth size vs. Immersion][1]

Figure 45. Simulation of Radial Runout of a Five Tooth Cutter with 30 Samples per Revolution and 256 Samples per Revolution

### 4.4.2 Axial Runout

Axial runout contributes to poor surface finish in face milling. Axial cutting forces are good indicators of this problem. In the following section a model relating axial runout to axial edge forces will be presented. Originally, a model to describe the axial runout forces for round nosed inserts was examined. There are, as will be seen, many problems with this geometry. Fortunately, for the commonly applied chamfered insert geometry, the process is much more straightforward.
4.4.2.1 Tools with a Nose Radius

The vertical forces on an insert with a large nose radius are largely dependent on the amount of contact length of the nose radius with the workpiece. (It is assumed that the vertical forces are primarily edge forces.) If a cutter has no axial runout at all, then the edge forces in the vertical direction are proportional to the sum of the nose radius and half the feed per tooth. If, on the other hand, there is an axial runout of the same order of magnitude as the feed per tooth, then only half the nose radius will ever be in cut. This can be seen in Figure 46.

\[ F_z = K h \times r \times (1 - \cos(\theta)) \]  (105)

Assuming that the feed per tooth \( s_t \) and the axial offset \( z \) are much smaller than the tool nose radius \( r \), then the angle \( \theta \) is defined as follows:

In a slotting operation, the feed of an insert continuously changes from zero to the cutter feed per tooth and back to zero. In this case, the contact length and consequently the axial force vary throughout the rotation of the cutter. The exact expression of the edge forces in the \( z \) direction shows that this force is proportional to the length of the active cutting edge projected onto a normal to the axial direction:

\[ F_{z1} = K h \times r \times (1 - \cos(\theta)) \]
\[
\frac{s_t}{z_t} = -\tan \theta \\
\cos \theta = \pm \frac{1}{\sqrt{\left(\frac{s_t}{z_t}\right)^2 + 1}}
\]

This expression can be substituted into the expression for the axial force. It is noted that the solution depends on the sign of the axial run out, \(z_t\):

\[
F_{z_t} = K h r_r \left(1 + \text{sign}(z_t) \sqrt{\frac{1}{\left(\frac{s_t \sin(\phi_s)}{z_t}\right)^2 + 1}}\right)
\]

Transforming this expression into the frequency domain analytically is impossible, since the integrals for the Fourier series coefficients cannot be evaluated in close form. Two types of simplifications have been attempted. First, an equivalent average feed was substituted for the instantaneous feed:

\[
s_t \sin \phi = \frac{s_t (1 - \cos \phi_s)}{\phi_s}
\]

This substitution leads to a very simple representation of the axial force due to axial runout where the axial cutting force is constant throughout the contact of the insert with the workpiece:

\[
F_{z_t}^1 = K h r_r \left(1 + \text{sign}(z_t) \sqrt{\frac{1}{\left(\frac{s_t \sin(\phi_s)}{z_t}\phi_s\right)^2 + 1}}\right)
\]
This expression is easily transformed to the frequency domain, but unfortunately it is not very accurate at low angles of immersion and low axial runouts.

The second attempt at simplifying equation (107) approximates the nose radius by two straight lines. The intersection of these two lines is at the very bottom of the tool, and each line also intersects the nose radius a second time at a distance $s/2$ away from the first intersection in the direction of the feed. This is geometry shown in Figure 47.

There are three cutting edges in this representation of a round nosed tool. The primary cutting edge is vertical. It has no influence on the axial force:

$$F_z^I = 0 \quad (110)$$

The secondary cutting edge always has the same length. It results in an edge force in the axial direction that is directly proportional to the nose radius:

$$F_z^{II} = K_h r^2 r \quad (111)$$

The tertiary cutting edge has a variable length. If there is no run out at all, then the tertiary cutting edge generates an axial force that is proportional to half the...
feed. A close up of the linearized tertiary cutting edge with axial runout is shown in Figure 48.

The following expression for the tertiary axial force assumes that the feed and the axial runout are much smaller than the nose radius:

$$F_z^{\text{III}} = K h * r_2 \left( \frac{s_t \sin \phi}{2} + \frac{2z_t}{s_t \sin \phi} \right)$$  \hspace{1cm} (112)

This expression is further approximated by replacing the feed in the runout term with an equivalent feed from equation 108:

$$F_z^{\text{III}} = K h * r_2 \left( \frac{s_t (1 - \cos \phi_s)}{2 \phi_s} + z_t \frac{2 \phi_s}{s_t (1 - \cos \phi_s)} r \right)$$  \hspace{1cm} (113)

The overall edge force in the axial direction is the sum of the three edge components:

$$F_z = K h * r_2 \left[ r \left( 1 + \frac{2z_t \phi_s}{s_t (1 - \cos \phi_s)} \right) + \frac{s_t \sin \phi}{2} \right]$$  \hspace{1cm} (114)
Even though this linear model is quite simple, it still is not useful in practice because it is not able to cope with the variation of force from zero to a maximum at half immersion and back to zero at full immersion, a circumstance which is encountered with a cutter that has axial runout and inserts with a finite nose radius.

A number of other approximations have been examined and abandoned as being no better in practice than the one given by equation (109). Fortunately, a large proportion of face milling is carried out with chamfered inserts (especially when surface finish is important). A development of axial forces and a method of identifying axial runout for this type of insert is shown in the next section.

4.4.2.2 Chamfered Inserts

The cutting edges of chamfered inserts are comprised of three straight portions. Figure 49 shows the geometry of such an insert. The insert shown here has a secondary cutting edge that is at an angle of 45° to the primary cutting edge and the tertiary cutting edge.

Figure 49. Geometry of a Chamfered Insert
4.4.2.3 Axial Runout Representation as a Discrete Fourier Series

The vertical forces do not relate to the axial offset of the inserts directly, but they reflect changes in the projected contact length of the insert with the workpiece. This is shown in Figure 50.

The primary cutting edge is vertical and has no influence on the vertical edge force. The entire secondary cutting edge is in contact with the workpiece, unless the radial runout is so large that the insert is not in cut at all. The length of the tertiary cutting edge is related to the feed per tooth, radial runout, and axial runout. The tertiary cutting edge will intersect the path of the secondary cutting edge of a previous tooth. The axial runout of the cutter will determine the insert, whose path of the secondary edge will be intersected. The longest insert in the axial direction intersects with itself. In the case shown in Figure 50, insert number one intersects with itself. Insert number two has an axial runout that makes it intersect with insert number one. Insert number three has less axial runout than insert number two, but more axial runout than insert number one. As a result insert number three intersects with insert number one.

Figure 50. Insert Geometry for a Three Tooth Cutter
For simplicity, it is assumed that the radial runout is smaller than the feed per tooth, and hence all the inserts are in cut. A computer pseudo-code for determining the projected length of the cutting edge that is in contact with the workpiece is shown in Figure 51.

```
\begin{align*}
  t_{i} &= t_{i} - 1 \\
  \text{while } \quad Z_{i} > Z_{i} \\
  \quad t_{i} &= t_{i} - 1 \\
  \text{end} \\
  A_{Z_{i}} &= s_{i} (t_{i} - t_{i}) + (R_{i} - R_{ii}) + (l''_{i} - z_{i})
\end{align*}
```

Figure 51. Pseudo-Code for Determining Projected Contact Length

In this code, the absolute radial runout is designated with $R_{t}$, the feed per tooth is displayed as $s_{t}$, the length of the secondary cutting edge is called $l''_{i}$, and the absolute axial runout is designated as $z_{i}$, where positive runouts are measured away from the cutter. The final projected contact length of the insert with the workpiece is shown as $A_{Z_{i}}$.

This contact length parameter can be described by a discrete Fourier series, similar to the one used for describing relative radial runout.

\[
A_{Z_{i}} = \frac{f_{z_{i}}}{2} + \sum_{k=1}^{\text{floor}(N/2)} f_{k_{i}} \cos \left( \lambda_{Z_{i}} + 2\pi k \frac{t_{i}}{N} \right)
\]  

(115)

where $\lambda_{Z_{i}} = 0$ or $\pi$ for $N = \text{even}$

This transformation simplifies the expressions relating axial runouts of individual teeth to cutting forces.

4.4.2.4 Axial Force series

Since the $z$-force is largely independent of the face forces due to radial runout, it is possible to utilize this force to determine axial runout independently of radial runout.
The vertical edge force is nearly constant over the time that the insert is in contact with the workpiece because the feed per tooth is usually small compared with the axial projection of the cutting edge. The axial edge force is a function of the cutting pressure $K$, the critical chip thickness $h^*$, the edge force ratio $r_2$, the projected length of the cutting edge $A_{z_2}$, the rotational angle $\phi$, and the immersion angles $\phi_1$ and $\phi_2$:

\[
F_{z_1} = K h^* r_2 A_{z_1}, \quad \text{for} \quad \phi_1 \leq \phi \leq \phi_2
\]

\[
F_{z_1} = 0, \quad \text{for} \quad 0 \leq \phi \leq \phi_1 \quad \text{and} \quad \phi_2 \leq \phi \leq 2\pi
\]

In the time domain, the axial forces can be defined using unit step heaviside functions, or Fourier series:

\[
F_z = K h^* r_2 \sum_{k=1}^{N} A_{z_k} \left( \text{Heaviside} \left( \phi_1 - \frac{2\pi k}{N} \right) - \text{Heaviside} \left( \phi_2 - \frac{2\pi k}{N} \right) \right)
\]  

\[
F_z = \frac{m_{z_2}}{2} + \sum_{k=1}^{\infty} \left( a_{z_k} \cos(k\phi) + b_{z_k} \sin(k\phi) \right)
\]

\[
= \frac{m_{z_2}}{2} + \sum_{k=1}^{\infty} \left( a_{z_k} \cos(k\phi + \delta_{z_k}) \right)
\]

where:

\[
a_{z_k} = K h^* r_2 \sum_{l=1}^{N} A_{z_k} \int_{\frac{2\pi l}{N} + \phi_1}^{\frac{2\pi l + \phi_2}{N}} \cos(k\phi) \, d\phi
\]

\[
b_{z_k} = K h^* r_2 \sum_{n=1}^{N} A_{z_k} \int_{\frac{2\pi n}{N} + \phi_1}^{\frac{2\pi n + \phi_2}{N}} \sin(k\phi) \, d\phi
\]

The resulting expressions for the Fourier series coefficients of the axial edge force series are as follows:
A much simpler and equivalent relation between the coefficients of the discrete axial runout series and the axial edge force series is obtained by replacing the real and imaginary components of the force series \( a_{z_k} \) and \( b_{z_k} \) with magnitude and phase components \( m_{z_k} \) and \( \delta_{z_k} \). This representation leads to relationships between the magnitude components of the runout and force series that are independent of the respective phase components:

\[
\begin{align*}
  m_{z_0} &= a_{z_0} = N K h^* r_2 f_{z_0} \frac{\phi_s}{2\pi}, \quad b_{z_0} = 0 \\
  a_{z_k} &= \frac{N K h^* r_2 f_{z_k}}{2k} \left\{ \sin(k \phi_2 - \lambda_{z_k}) - \sin(k \phi_1 - \lambda_{z_k}) \right\} + \text{ for } 0 < k < N/2 \\
  b_{z_k} &= \frac{2}{k} K h^* r_2 f_{z_k} \left\{ -\cos(k \phi_2 - \lambda_{z_k}) + \cos(k \phi_1 - \lambda_{z_k}) \right\} + \text{ for } 0 < k < N/2 \\
  a_{z_{nu2}} &= \frac{N K h^* r_2 f_{z_{nu2}}}{\pi} \left\{ \sin\left(\frac{N}{2} \phi_2 - \lambda_{z_{nu2}}\right) - \sin\left(\frac{N}{2} \phi_1 - \lambda_{z_{nu2}}\right) \right\} \\
  b_{z_{nu2}} &= \frac{N K h^* r_2 f_{z_{nu2}}}{\pi} \left\{ -\cos\left(\frac{N}{2} \phi_2 - \lambda_{z_{nu2}}\right) + \cos\left(\frac{N}{2} \phi_1 - \lambda_{z_{nu2}}\right) \right\}
\end{align*}
\]

\[
\begin{align*}
  m_{z_k} &= \sqrt{a_{z_k}^2 + b_{z_k}^2} = N K h^* r_2 f_{z_k} \frac{2(1 - \cos(k \phi_s))}{k \pi}, \quad \text{for } 0 < k < N/2 \\
  m_{z_{nu2}} &= \sqrt{a_{z_{nu2}}^2 + b_{z_{nu2}}^2} = \pm \frac{N}{2} K h^* r_2 f_{z_{nu2}} \frac{2\left(1 - \cos\left(\frac{N}{2} \phi_s\right)\right)}{\pi}
\end{align*}
\]

The phase angles of the axial runout series are also expressed in terms of the force series:
\[ \tan(\lambda_z) = 0 \]
\[ \tan(\lambda_{zw}) = 0 \text{ or } \pi \]
\[ \tan(\lambda_{zk}) = \frac{a_{zk}}{b_{zk}} \left[ \cos(k \phi_2) - \cos(k \phi_1) \right] + \left[ \sin(k \phi_2) - \sin(k \phi_1) \right] \]
\[ \frac{a_{zk}}{b_{zk}} \left[ \sin(k \phi_1) - \sin(k \phi_2) \right] + \left[ \cos(k \phi_2) - \cos(k \phi_1) \right] \]
(121)

for \( k \neq 0 \) and \( k \neq N/2 \)

### 4.4.2.5 Simulation of Axial Runout

A simulation of a five teeth cutter was programmed in the programming environment of Matlab\(^{130}\). This simulation showed again that it is necessary to have a sufficient number of samples in order to be able to identify axial runout reliably.

The expressions derived in section 4.4.2.4 assume a continuous Fourier transform of the force traces. Data acquisition systems sample the force data synchronized with the spindle rotation. This process can result in discretization errors if there are fewer than two samples acquired during the contact of the tool with the workpiece. This problem occurs when the immersion angle is close to a multiple of the tooth spacing angle.

The simulation constructs a time series of axial force in a down milling operation from the projected contact lengths of the teeth. A fast Fourier transform is applied to the force traces, and the fundamental and the harmonics of the FFTs of the forces are utilized to reconstruct the relative sizes of the teeth. In the end, the original contact lengths of the teeth are plotted against the reconstructed contact lengths of the teeth.
The immersion angle in the simulation was varied between 5° and 180°. Two sampling systems were examined, one with 30 samples per revolution (12° per sample) and another one with 256 samples per revolution (1.4° per sample). A five teeth cutter was assumed because it validates the formula for the phase angles of the harmonics of runout. Table 4 shows the runout parameters that are used in this simulation.

<table>
<thead>
<tr>
<th>A₁</th>
<th>A₂</th>
<th>A₃</th>
<th>A₄</th>
<th>A₅</th>
<th>f₁</th>
<th>f₂</th>
<th>λ₁</th>
<th>λ₂</th>
</tr>
</thead>
<tbody>
<tr>
<td>5.66mm</td>
<td>5.07mm</td>
<td>3.07mm</td>
<td>4.17mm</td>
<td>7.52mm</td>
<td>5mm</td>
<td>2mm</td>
<td>1mm</td>
<td>35°</td>
</tr>
</tbody>
</table>

Table 4. Simulation Parameters for a Five Tooth Cutter with Axial Runout

The simulation validates the expressions derived for the runout magnitudes and runout phase angles. Figure 52 shows that the aforementioned discretization errors are reduced when the sampling rate is increased from 30 to 256 samples per revolution.

Figure 52. Simulation of Axial Runout with a Five Tooth Cutter with 30 Samples per Revolution and 256 Samples per Revolution

4.5 Experimental Investigation

The identification techniques for machining conditions and cutting parameters described here are basically extensions of known end milling algorithms to the
process of face milling. The main thrust in this chapter is towards identification techniques for runout. Consequently, independent experiments for the latter algorithms are shown in this chapter, whereas the integration of all algorithms is shown in the following chapter where they are used for process control.

Two sets of runout experiments were performed. In the first set, combined axial and radial runout with chamfered inserts was identified for a number of fixed immersions. In the second set of experiments, radial runout only is identified for a set of round nosed inserts. The second set of experiments has two parts. In the first part, the immersion is varied continuously between zero and half immersion. In the second part, edge breakage for a tool with considerable initial runout is identified.

4.5.1 Combined Radial and Axial Runout Identification

Four experiments were conducted on a Bridgeport 2s milling machine with a five flute face mill. The five TPK 43P2R inserts were chamfered with a flat bottom and a secondary cutting edge that was at 45° to the main cutting edge. Figure 53 shows the geometry of this insert. The runouts of this assembly were measured with a dial gauge that has a resolution of 0.0001 inch. Two steel specimens of different hardness were used in this test series.
The cutting speed was selected at 240 RPM, and the feed was set to 85 mm/min. The axial depth of cut was 2 mm. The tool had a radius of 40 mm. A DAS20 AD board was utilized in conjunction with LabView Notebook for collecting the data. The dynamometer described in Appendix B was used to measure cutting forces. A Butterworth filter with a cutoff frequency of 100 Hz was utilized to filter high frequency noise. The sampling rate of the system was 960 Hz.

Table 5 list all material parameters, runout magnitudes, and cutting data.

<table>
<thead>
<tr>
<th>Test Number</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material Hardness [RC]</td>
<td>5</td>
<td>8</td>
<td>8</td>
<td>8</td>
<td>30</td>
<td>30</td>
<td>30</td>
</tr>
<tr>
<td>Specific Cutting Pressure, K [N/mm²]</td>
<td>3011</td>
<td>3459</td>
<td>3459</td>
<td>3459</td>
<td>2800</td>
<td>2800</td>
<td>2800</td>
</tr>
<tr>
<td>Critical Chip Thickness, h* [mm]</td>
<td>0.0291</td>
<td>0.0363</td>
<td>0.0363</td>
<td>0.0363</td>
<td>0.0294</td>
<td>0.0294</td>
<td>0.0294</td>
</tr>
<tr>
<td>Face Force ratio, r1</td>
<td>0.396</td>
<td>0.3650</td>
<td>0.3650</td>
<td>0.3650</td>
<td>0.430</td>
<td>0.430</td>
<td>0.430</td>
</tr>
<tr>
<td>Edge Force Ratio, r2</td>
<td>1.0512</td>
<td>1.0064</td>
<td>1.0064</td>
<td>1.0064</td>
<td>0.99</td>
<td>0.99</td>
<td>0.99</td>
</tr>
<tr>
<td>Rad. Width, d [mm]</td>
<td>40</td>
<td>20</td>
<td>40</td>
<td>60</td>
<td>20</td>
<td>40</td>
<td>60</td>
</tr>
<tr>
<td>Axial Depth, a [mm]</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td>Feed [mm/min]</td>
<td>85</td>
<td>85</td>
<td>85</td>
<td>85</td>
<td>85</td>
<td>85</td>
<td>85</td>
</tr>
<tr>
<td>Speed [RPM]</td>
<td>240</td>
<td>240</td>
<td>240</td>
<td>240</td>
<td>240</td>
<td>240</td>
<td>240</td>
</tr>
<tr>
<td>Number of Inserts</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td>Up/Down Milling</td>
<td>Down</td>
<td>Up</td>
<td>Up</td>
<td>Up</td>
<td>Down</td>
<td>Down</td>
<td>Down</td>
</tr>
<tr>
<td>Rad. Offset, Insert 1, [0.0001&quot;]</td>
<td>0.4</td>
<td>0.4</td>
<td>0.4</td>
<td>-2.2</td>
<td>4.85</td>
<td>4.85</td>
<td>4.85</td>
</tr>
<tr>
<td>Rad. Offset, Insert 2, [0.0001&quot;]</td>
<td>-3.0</td>
<td>-2.4</td>
<td>-2.4</td>
<td>-2.7</td>
<td>2.1</td>
<td>2.1</td>
<td>2.1</td>
</tr>
<tr>
<td>Rad. Offset, Insert 3, [0.0001&quot;]</td>
<td>-3.8</td>
<td>-3.0</td>
<td>-3.0</td>
<td>-2.4</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Rad. Offset, Insert 4, [0.0001&quot;]</td>
<td>-0.9</td>
<td>-1.8</td>
<td>-1.8</td>
<td>-0.7</td>
<td>2.21</td>
<td>2.21</td>
<td>2.21</td>
</tr>
<tr>
<td>Rad. Offset, Insert 5, [0.0001&quot;]</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>-0.9</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Axial Offset, Insert 1, [0.0001&quot;]</td>
<td>-0.1</td>
<td>-0.1</td>
<td>-0.1</td>
<td>-0.1</td>
<td>0.51</td>
<td>0.51</td>
<td>0.51</td>
</tr>
<tr>
<td>Axial Offset, Insert 2, [0.0001&quot;]</td>
<td>-0.4</td>
<td>-0.5</td>
<td>-0.5</td>
<td>0</td>
<td>0.53</td>
<td>0.53</td>
<td>0.53</td>
</tr>
<tr>
<td>Axial Offset, Insert 3, [0.0001&quot;]</td>
<td>-0.5</td>
<td>0</td>
<td>0</td>
<td>-0.2</td>
<td>0.8</td>
<td>0.8</td>
<td>0.8</td>
</tr>
<tr>
<td>Axial Offset, Insert 4, [0.0001&quot;]</td>
<td>-1.6</td>
<td>-0.6</td>
<td>-0.6</td>
<td>-0.5</td>
<td>0.18</td>
<td>0.18</td>
<td>0.18</td>
</tr>
</tbody>
</table>

Table 5. Cutting Parameters for the Runout Experiments
Tooth 2
Tooth 3
Tooth 1
Tooth 4
Tooth 5

Figure 54. Insert Spacing for Test 1

The dial gauge measurements of radial and axial offsets allow the construction of the theoretical cutting geometry for every tooth. Pictures of the insert geometries are shown in Figure 54 to Figure 56. These figures indicate that in test 1 to test 3, insert number 3 is not in cut. In practice small cutting forces on the third insert are measured that are attributed to spindle deflections.

Tooth 3
Tooth 2
Tooth 1
Tooth 5
Tooth 4

Figure 55. Insert Spacing for Test 2 and Test 3

Figure 56. Insert Spacing for Test 4

4.5.1.1 Radial Runout Identification
The formulation of radial runout that was proposed in section 4.4.1.2 assumes that the relative runout between teeth is smaller than the feed per tooth. In tests
1 to 3, the third tooth had relative runout that was larger than the feed per tooth, and hence it was not actually cutting. The runout model requires that the relative runout of the third tooth be set to the feed per tooth, and the relative runout of the fourth tooth be adjusted such that the sum of all relative runouts equals zero.

The sampling must be started at exactly zero immersion of the first tooth in order to obtain accurate phase angles from the identification technique. Since no synchronization signal of the spindle was available in this test setup, sampling was started arbitrarily, and the phase angles of the identified runout series were then shifted until the phase of its fundamental frequency component lined up with the phase component obtained from the dial gauge measurements.

Figure 57 to Figure 63 show pictures of the x & y force traces, the identified relative runouts, and the magnitude and phase components of the relative runouts. The relative runout that was obtained from the dial gauge is shown in circles, whereas the runout identified from the forces is plotted in solid. There is good correlation between the measured and the identified radial runouts.
Figure 57. Radial Runout Identification for Test 1

- Runout measured with dial gauge
- Runout predicted form forces
Figure 58. Radial Runout Identification for Test 2

- Runout measured with dial gauge
- Runout predicted form forces
Figure 59. Radial Runout Identification for Test 3

- Runout measured with dial gauge
- Runout predicted from forces
Figure 60. Radial Runout Identification for Test 4

- Runout measured with dial gauge
- Runout predicted from forces
Figure 61. Radial Runout Identification for Test 5

- Runout measured with dial gauge
- Runout predicted from forces
Figure 62. Radial Runout Identification for Test 6

- Runout measured with dial gauge
- Runout predicted from forces
Figure 63. Radial Runout Identification for Test 7

o...Runout measured with dial gauge
--- Runout predicted from forces
4.5.1.2 Axial Runout Identification
Axial runout adds or subtracts a constant amount of cutting edge length to the z force. The result is an almost constant amount of z force over one revolution. In the example chosen here, every tooth has a vertical z force due to the secondary cutting edge, and a vertical force due to the tertiary cutting edge. In test 1 to test 3, the third tooth never touches the work piece because of the large radial runout. Consequently, it shows zero vertical force.

Again sampling is supposed to start at zero immersion of tooth one. Since no spindle synchronization is available, the phase angles of the identified runout series are shifted until its fundamental lines up with the fundamental phase angle obtained from the dial gauge readings. Figure 64 to Figure 70 show the results obtained from this identification. The runouts obtained from the dial gauge readings are plotted in circles, and the runouts obtained from the force traces are shown in solid.

The correlation between the dial gauge measurements and the identification from axial cutting forces is deemed adequate.
Figure 64. Axial Runout Identification for Test 1

○...Runout measured with dial gauge

-... Runout predicted from forces
Figure 65. Axial Runout Identification for Test 2

- Runout measured with dial gauge
- Runout predicted from forces
Figure 66. Axial Runout Identification for Test 3

- Runout measured with dial gauge
- Runout predicted form forces
Figure 67. Axial Runout Identification for Test 4

- Runout measured with dial gauge
- Runout predicted form forces
Figure 68. Axial Runout Identification for Test 5

- Runout measured with dial gauge
- Runout predicted from forces
Figure 69. Axial Runout Identification for Test 6

○...Runout measured with dial gauge

--- Runout predicted form forces
Figure 70. Axial Runout Identification for Test 7

o...Runout measured with dial gauge
--- Runout predicted form forces
4.5.2 Radial Runout Identification with Round Nosed Inserts

Two sets of experiments were performed with round nosed inserts. The first experiment demonstrated that the algorithm proposed in this paper is able to identify radial runout for round nosed inserts over a wide range of immersion values. The second experiment investigated the performance of the algorithm in the detection of breakage when the cutter has significant initial run out. The same four insert carbide cutter was used in both experiments; the work material was a low alloy steel, (BHN 250).

<table>
<thead>
<tr>
<th>Cutter radius</th>
<th>Nose radius</th>
<th>Tool Inserts</th>
<th>Material Hardness</th>
<th>$K$ [$N/mm^2$]</th>
<th>$h^*$ [mm]</th>
<th>$r_1$</th>
<th>$r_2$</th>
<th>$\psi_e$ [rad]</th>
</tr>
</thead>
<tbody>
<tr>
<td>24.7015</td>
<td>0.7938</td>
<td>4</td>
<td>250</td>
<td>5384.3</td>
<td>0.0120</td>
<td>0.6336</td>
<td>1.3104</td>
<td>0.1673</td>
</tr>
</tbody>
</table>

Table 6. Cutting Parameters for the Runout Experiment

4.5.2.1 Runout Identification

A four flute face mill was used to cut the taper shown in Figure 71. The forces were sampled at uniform spindle rotation increments using an interrupt driven acquisition routine running on a slave processor within the UBC open architecture machine tool controller. In this case, 30 values were collected per revolution, and the sampling interrupt was created using a proximity probe whose output was generated from a splined feature on the machine tool spindle (see Appendix E for a Photograph). The immersion angle was varied between $0^\circ$ and $90^\circ$ (see Figure 71). The spindle speed was 1000 RPM, the feed 200 mm/min, and the axial
depth 1.905 mm. The cutting parameters are shown in Table 6 where K is the specific cutting pressure, \( h^* \) is the chip thickness which yields a total cutting force which is comprised of equal components of cutting and parasitic force \( r_1 \) and \( r_2 \) are the force ratios of the cutting and parasitic force respectively, and \( \psi_e \) is the chip flow angle with respect to the radial direction of the spindle \(^{23} \). It may be noted that the model fitted from the experimental data includes edge forces explicitly. The expression for incremental torque and force due to runout will not include any edge force dependent terms. The initial values of radial runout are measured in situ using a dial gauge. The coefficients of the runout series (equation(95)) are shown in Table 7.

<table>
<thead>
<tr>
<th>( A_{r1} ) [mm]</th>
<th>( A_{r2} ) [mm]</th>
<th>( A_{r3} ) [mm]</th>
<th>( A_{r4} ) [mm]</th>
<th>( f_{r1} ) [mm]</th>
<th>( f_{r2} ) [mm]</th>
<th>( f_{r3} ) [mm]</th>
<th>( \lambda_{r1} ) [deg]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0332</td>
<td>0.0034</td>
<td>-0.0201</td>
<td>-0.0164</td>
<td>0</td>
<td>0.0284</td>
<td>0.0130</td>
<td>-20.3862</td>
</tr>
</tbody>
</table>

Table 7. Measured Runout Series Coefficients

The immersion angle in these experiments was determined from the geometric relationship between the workpiece and the cutter.

The changing spectral content due to the varying immersion angle has little influence on the results of the runout identification technique, even though the Fourier series assumes a time invariant signal, because the change in immersion at the particular feed chosen during one revolution is too small. Process noise was minimized by using a high order low pass filter with a break frequency slightly higher than the tooth passing frequency.
Figure 72 shows the results obtained with this identification technique. The results at low immersion angles are rather poor; these poor results are due both to the low sampling rate and to the assumption that the influence of runout may be expressed as a constant chip thickness change over the entire swept angle. The simulation described in the section 4.4.1.3 made the same assumptions regarding the chip thickness in the calculation of force, hence its apparently better performance at low immersions.

4.5.2.2 Edge Breakage Identification in the Presence of Initial Runout
This experiment was performed with the same four insert carbide cutter and the same workpiece material as in the previous experiment. The cutting parameters of the tool workpiece combination are shown in Table 6. The initial runout of the cutter was again measured with a dial gauge. The data acquisition system was improved in this experiment to collect 90 rather than 30 samples per revolution.

The initial runout identification was performed at a spindle speed of 530 RPM with a feed of 381 mm/min, an axial depth of 1.27 mm, and a radial width of 12.7 mm. The feed was then continuously increased by increments of 127 mm/min until one insert finally broke at a feed of 1524 mm/min. The resulting breakage
effectively removed 90% of the active cutting edge. Unfortunately, the machine used for this experiment showed severe chatter when the insert broke, a fact which prohibited identifying the runout during the actual breakage. The feed was then reduced to 381 mm/min and the resulting identified runout was compared to the initial runout measured at the same feed.

<table>
<thead>
<tr>
<th></th>
<th>( A_{r_1} )</th>
<th>( A_{r_2} )</th>
<th>( A_{r_3} )</th>
<th>( A_{r_4} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dial gauge</td>
<td>0</td>
<td>-0.0660</td>
<td>-0.0610</td>
<td>-0.0254</td>
</tr>
<tr>
<td>Initial Identif.</td>
<td>-0.0025</td>
<td>-0.0737</td>
<td>-0.0610</td>
<td>-0.0254</td>
</tr>
<tr>
<td>Final Identif.</td>
<td>-0.2134</td>
<td>-0.0762</td>
<td>-0.0356</td>
<td>-0.0254</td>
</tr>
</tbody>
</table>

Table 8. Measured Runout Series Coefficients

Figure 73 and Table 8. show the absolute runout that was obtained from the in situ dial gauge measurements and the runouts obtained from the initial and the final identification. Insert number one was reduced by 0.21 mm during the breakage. This reduction, which is almost equal to the prescribed feed per tooth, corresponds to a relative radial runout of 0.19 mm between the broken insert and its predecessor. This leads to the correct conclusion that the complete edge of insert number one has been severely removed.

4.6 Conclusions

This chapter has examined a wide range of identification problems that are required for a real time process control system in milling.
Algorithms for identifying machining conditions are necessary foundations for real time process control systems. They are required for controlling tool edge breakage and are the basis of more sophisticated identification algorithms for wear and runout. Existing algorithms for determining radial width and axial depth have been extended from end milling to face milling. This extension provides more insight into transient tool entry conditions that are important in real time process control.

A similar extension from end milling to face milling was provided for identifying cutting parameters. These algorithms are useful in calibration cuts for determining parameters of the cutting force model, but they can also be used in normal cutting to track tool wear.

Building on the machining parameter identification, the major contribution of this chapter are algorithms for identifying radial and axial runouts of single teeth. Runout is an important tool condition parameter that can also give indications of likely surface finish and tool edge condition. Analytical expression relating cutting forces to runouts are presented.

Because in-plane forces are largely independent of axial runout, radial runout can be conveniently be related to these forces. Axial forces, on the other hand, are mainly attributed to axial edge force which makes them a good candidate for identifying axial runout. This chapter has introduced new methods utilizing frequency domain models of these forces for the identification of both axial and radial runout.
An experimental investigation has been carried out which shows the robustness of the runout identification techniques and has demonstrated that the same algorithms can be used to detect edge breakage in the presence of significant initial runout.
Chapter 5

IPAC: An Integrated Process Planning and Control System

5.1 Introduction

The development of high powered distributed control systems allows, for the first time, the consideration of the integration of preprocessing and control activities. This integration leads to improvements in efficiency on two levels. At the lower level, sensor based real time process control systems can take advantage of process information that is much more readily available in an integrated planning and control system than in a traditional CNC system. At a higher level, the planning system can use process parameters that the low level process control system identifies to update process plans while the part is being manufactured.

During the work described in this thesis IPAC, an integrated process planning and process control system has been designed and implemented by the author. The system is built around a Holke vertical milling machine, which has been retrofitted with a UBC open architecture controller by Ardekani\textsuperscript{134}. The monitoring and control system components are divided into two parts located in different physical processors. A high speed component collects cutting forces synchronized with the spindle rotation of the milling machine, performs fourier

\footnote{Some of the material presented in this chapter has been published by Seethaler and Yellowley\textsuperscript{131}}
series transformations, identifies cutting geometry, and performs multiple constraint feed control. A low speed component of the monitoring system identifies runout once per spindle revolution. A planning interface was also implemented; it is able to model the geometry of 2½D milling components and develop optimized process plans for these components. The process planning system is able to perform volume subdivision, path planning and the optimum selection of feed and speed. The optimization algorithms used in the planning system are based on earlier work by Yellowley and Fisher\(^41\). A block diagram of this system is shown in Figure 74.
5.2 An Extendible Dynamically Reprogrammable Architecture

Traditional CAPP systems provided the machine tool with tool path trajectories and conservative values of feed and speed. With the development of new sensors and identification techniques, it is possible to have the CAPP system choose less conservative cutting parameters and to have an on line process optimization system compensate for variations in workmaterial and raw part geometry. Such a system requires a different architecture from traditional CAPP and CNC systems because a wider range of process parameters need to be passed between the two systems. This section will introduce an extendible process control architecture that allows for passing of process parameters between the CAPP system and a process optimization system. The architecture also ensures that process control tasks and motion tasks on the CNC system can be synchronized.

The UBC Open Architecture controller was chosen for the implementation of IPAC. The system allows for multiple masters that can share tasks such as process planning and high level CNC control tasks. Slave boards are responsible for the real time control of the machine position, as well as for identification and process control. Finally, there is a standard communication protocol between the slaves that ensures synchronization of traditional position control tasks and novel adaptive process control tasks. The basic architecture of the UBC controller is described in more detail in section 2.7.3.
There are three logical units within a process optimization system; these need to be able to transfer parameters to each other and perform tasks synchronously. First, the CAPP system generates process plans with parameter estimates of the process. Second, the traditional CNC control system positions the tool according to the process plan that it receives from the CAPP system. Third, the process control system updates cutting conditions through the CNC system according to control tasks that it receives from the CAPP system and sensory inputs that are gathered during the manufacturing process. Naturally the control tasks in the process control system need to be synchronized with the position control of the CNC system. A distributed monitoring architecture is proposed here that fulfills these requirements. A diagram of this architecture where two monitoring systems, a CNC machine, and a CAPP system are interconnected is shown in Figure 74.

The dash-dotted lines between the process planning system and the CNC machine represent the traditional information flow of position, speed, and feed information. The dashed lines show the flow of monitoring tasks that are sent by a task scheduler in the CAPP system and received by schedulers on the monitoring boards. It is also possible that monitoring tasks have requests for the process planning system. These are routed through the same schedulers, but in the opposite direction. The dotted lines represent connections to a front plane bus which has two functionalities. The "sync" lines in this bus allow for synchronization of position loop closing, spindle rotation, or other process
relevant synchronization events. The so called state lines are used to modify machine feed rate.

There are a large number of monitoring tasks that vary, depending on the type of process that is required and the type elements being processed. As a result of the time varying nature of the processing tasks, a dynamically reprogrammable and reconfigurable architecture is proposed. This architecture allows reprogramming of processing tasks on the monitoring board through a task scheduler in the CAPP system. Following startup, the process boards inform the CAPP system as to what they are and what capabilities they have, and the scheduler on the CAPP system is then able to provide specifically tailored monitoring and control tasks to these boards. (These tasks may be altered when the cutting operations change.) Extensions of the process control system with new sensors are also simplified. New control boards are "plugged" into the process control system, and the CAPP automatically identifies and integrates them into the existing control architecture. This reprogrammability also allows for simple system updates. For this system to function, a standard scheduling system on the CAPP system and the monitoring boards is required.

FORTH, an industry standard embedded control language, is an ideal task scheduler for use on the monitoring boards. It is an interpretive language with a multitasker that can run on most monitoring boards because it has small memory requirements. In this application, the CAPP system literally programs monitoring
tasks on the monitoring boards using the on-board FORTH interpreter. This type of application has long been implemented using FORTH; more recently JAVA has aroused a large amount of interest in similar applications.

The task scheduler in the CAPP system could be programmed in almost any high level language, but FORTH is chosen again, because the process planning system running on the same CPU requires Forth for its NLI implementation.

The scheduling system is the major potential bottleneck of this architecture. It is necessary to ensure that the CAPP system distributes tasks efficiently between the monitoring boards in order to minimize task reprogramming and data transfer between the monitoring boards.

At the current stage IPAC does not dynamically reprogram monitoring tasks on the monitoring boards because the monitoring and identification activities are quite limited. Monitoring tasks are loaded into the monitoring boards during startup using the onboard forth interpreter on the monitoring board. Appropriate monitoring tasks are then started in accordance with the cutting process requirements by the task scheduler in the process planning system.

5.3 Process Planning
A simple new CAPP package was written in 32 bit FORTH that is able to optimize 2½-D milling geometries. An attempt was made to provide a Natural Language Interface (NLI) that would allow the shop floor programmer to
communicate with the geometry engine and the process optimization system. The planning system is able to perform volume subdivision and single pass optimization of feed and speed (see section 2.3). The system was integrated with the UBC open architecture controller and resulted in a simple CIM system.

The description of the part geometry is the first stage in the production process; this description traditionally requires sophisticated CAD software that has to run on dedicated computers. The interpretation of these drawings by the CAPP system is a task that still attracts much research effort. In this thesis it was attempted to avoid this problem by using a shopfloor programming language that is particularly designed for the simple description of 2½D milling parts. This language runs within the FORTH interpreter of the CAPP system, and by carefully choosing CREATE> DOES defining words, a near natural language interface was achieved. Parts are defined in terms of machining features (shoulders, slots, surfaces, ..); such definition eliminates cumbersome feature recognition techniques. (On the down side, however, there is a limited potential for extending the concept to more complicated geometries.) The syntax of this interface is described in Appendix D.

The NLI feeds into a geometry engine that uses a combined cell decomposition and B-rep representation of the parts. Solids are modeled as rectangular shaped volumes, and the data structure for each volume contains information on the state of the volume (e.g.: full, empty, to be machined), the state of each face
(free, seed, or interface), and it also contains information on adjacency. The representation is very computationally efficient for the volume subdivision stage in process planning; thus, on line optimization of process plans is made possible.

The three stages of volume subdivision, path subdivision, and selection of cutting parameters were implemented using the optimization algorithms described in section 2.3. The volume subdivision algorithm recombines into a new set of features the volume elements of the features, that were used by the operator to describe the part. This recombination process utilizes lower level optimization routines for path subdivision and the selection of feed and speed. The path subdivision algorithm attempts to use the smallest cutter that is able to remove a volume in a single path. If there is not a large enough cutter available, or if no cutter fits the geometry that needs to be removed, then several cuts are taken. Feed and speed are selected by optimizing the machining cost.

Once a process plan has been established, the part is machined, and the CAPP system attempts to optimize cutting parameters during the manufacture of the actual part. Results of the optimization are displayed in real time by IPAC. A sample display is shown in Figure 75.
Figure 75. IPAC’s Monitoring Screen

The monitoring screen is divided into four windows. The top right window displays the part and the current tool path. The top left window displays predicted and identified cutting conditions. In the example shown here, the identified values are zero because the screen dump was generated off line. The bottom right corner contains the G-code that is sent to the machine tool, and the bottom left corner contains economic parameters of the volume element that is currently being machined and accumulated parameters for all volume elements that have been removed.
5.4 Process Control

As described in section 2.3.2, machining at minimum cost requires that the feed is kept at a maximum value that is governed by tool breakage and machine torque constraints, whereas the speed is varied to result in an optimal tool life. The following sections will describe the control strategies that have been adopted to select feedrate and speed.

5.4.1 Feed Control

Both cutting tools and machines can suffer damage when operating conditions are not chosen carefully by the CAPP system or by the operator. Tool breakage or spindle stalls are dangerous and can also damage the part or the machine tool. A monitoring system is required to ensure that the tool, the part, and the machine are handled safely.

The most common failure is tool edge breakage. It is normally assumed that this condition will occur if the equivalent chip thickness on an edge is larger than a known maximum value.

The equivalent chip thickness is defined as the undeformed area of the chip over the engaged cutting length of the tool insert:

\[ h_e = \frac{A}{l_e} = \frac{s_t a}{l_e} \]  

(122)
The equivalent chip thickness in milling varies during each revolution. Consequently, it is necessary to identify the maximum equivalent chip thickness during a cutter revolution; this identification, of course, requires the identification of the maximum instantaneous undeformed chip thickness (see Figure 76):

\[
\begin{align*}
\text{if } \phi_2 < \pi/2 & \rightarrow s_{i_{\text{max}}} = s_1 \sin(\phi_2) \\
\text{if } \phi_1 > \pi/2 & \rightarrow s_{i_{\text{max}}} = s_1 \sin(\phi_1) \\
\text{if } \phi_1 < \pi/2 < \phi_2 & \rightarrow s_{i_{\text{max}}} = s_1
\end{align*}
\]

The active cutting edge is related to the geometry of the cutter and the insert. For cutters with a finite nose radius the expression is approximately as follows (see Figure 1):

\[
l_{\text{nose}} = \frac{a - r(l - \sin \psi)}{\cos \psi} + r\left(\frac{\pi}{2} - \psi\right) + \frac{s_1}{2}
\]

Helical end mills have zero approach angle, and the equivalent length of the active cutting edge is equal to the axial depth of cut:
\[ l_{\text{helical}} = s_t + a \approx a \]  

(128)

The monitoring system needs to identify the maximum equivalent chip thickness over one revolution of the cutter and to reduce the feed in cases where the identified equivalent chip thickness is in danger of increasing the safe limits set by the planning system.

In the work described here, the ratio of the identified equivalent chip thickness over the allowable equivalent chip thickness is multiplied by the current feed rate override in order to arrive at a new feed rate override for edge breakage control:

\[ F_{\text{RO new, edge}} = F_{\text{RO current}} \times \frac{h_{\text{allowable}}}{h_{\text{identified}}} \]  

(129)

Shank breakage is the second tool failure mode. It arises only with end mills because in face milling, edge breakage will always precede shank breakage. The shank breakage failure mode is due to torque and in-plane resultant force. The effect of torque is usually negligible when compared to the size of the in-plane resultant force. A conservative way of estimating the maximum allowable resultant force is to assume that the force is applied at the tip of the tool, and that the tool itself behaves like a cantilever beam (see Figure 77). This assumption leads to an expression that is dependent on the ultimate yield strength of the tool \( \sigma_{\text{ult}} \), the radius of the tool \( R \),
and the shank length of the tool \( L \). All of these variables are available in the tool library of the CAPP system. If the measured resultant force exceeds its limit, then the feed needs to be reduced.

\[
F_{\text{max}} < \sigma_{\text{ult}} \frac{\pi R^3}{4 L} \tag{130}
\]

Again the ratio of the allowable resultant force over the measured resultant force is used to update the feed rate override for shank breakage:

\[
F_{\text{RO}}^{\text{new shank}} = F_{\text{RO}}^{\text{current}} \frac{F_{\text{allowable}}}{F_{\text{measured}}} \tag{131}
\]

Since the commanded feed rate overrides for edge breakage and shank breakage usually are not the same, the lower of the two is chosen as the final update for feed rate override:

\[
\begin{align*}
\text{if } & F_{\text{RO}}^{\text{new edge}} \leq F_{\text{RO}}^{\text{new shank}} & \rightarrow & F_{\text{RO}}^{\text{new}} = F_{\text{RO}}^{\text{new edge}} \\
\text{if } & F_{\text{RO}}^{\text{new edge}} > F_{\text{RO}}^{\text{new shank}} & \rightarrow & F_{\text{RO}}^{\text{new}} = F_{\text{RO}}^{\text{new shank}}
\end{align*} \tag{132}
\]

Even though the monitoring system attempts to minimize the possibility of edge or shank breakage, it is not possible to avoid it completely. The monitoring system is therefore required to detect breakage when it occurs. The runout identification routine can be used to detect a broken cutter within one revolution of the actual breakage. It is also possible to find changes in the force signatures
between teeth. Such an algorithm can detect breakage within a tooth period of the actual breakage.

The spindle will, of course, stall if an excessive torque is demanded. This stalling is counteracted in this system by reducing the feed to zero if the spindle speed drops by more than 10 RPM over one revolution. (This reduction is done using the state lines which, as mentioned, are read at the loop closing speed.)

The last safety measure to be considered applies when the cutter is not cutting but moving through air. Sudden changes in force levels or rapid accelerations measured on the spindle during air moves are indicators of collisions. If a collision is detected, the feed must immediately be dropped to zero in order to avoid cutter or machine breakage.

During the starting phase of a tool entry transient and the end phase of a tool exit transient, the measured force levels drop below the noise level. In such a case, the identification system described later cannot perform reliably, and a strategy based on conservative estimates of the cutting geometry must be employed to control the feed. IPAC assumes that the swept angle of cut is larger than 90° and that the real axial depth is equal to the nominal axial depth.

**5.4.2 Speed Control**

The spindle speed control on the machine used in this work is manual. Consequently automatic speed control by the CAPP system is not available.
This limitation, however, does not seriously impact on the performance of the system. Considering that it is currently quite difficult to measure tool wear, estimations of the tool life parameters have to be used; therefore, exact speed control cannot be achieved with current sensor technology.

Fortunately, suboptimal selections of spindle speed will not affect the overall cost by the same degree as inappropriate values of feed. The effect of feed and speed on the machining cost is demonstrated in Figure 78, where cost is plotted against variations in spindle speed for three different feed rates. Clearly, the cost is affected little by ±10% variations in spindle speed from the optimal speed. Therefore, apriori selection of speed is adequate in most circumstances.

5.5 Experimental Investigation

Two identical parts were machined, one in steel, the other one in titanium. The parts contained full immersion, three quarter immersion and quarter immersion cuts. The parts were designed to demonstrate that the system is able to operate
in a variety of cutting conditions that include entry and exit transients and to demonstrate on line identification of cutting parameters and radial runout.

5.5.1 Experimental Apparatus

IPAC consists of a separate CNC control unit and a CAPP/monitoring unit. These two functionalities were physically separated because the original CNC control unit did not provide enough physical space for the required planning and monitoring boards. This separation also improved development safety because a system crash on the CAPP/monitoring unit would not require rebooting the CNC control unit.

The following sections describe the hardware components that constitute IPAC. Pictures of the components of this system are shown in Appendix E.

5.5.1.1 Retrofitted Holke Milling Machine

A Holke vertical milling machine, retrofitted with the UBC open architecture controller, was used as the test machine. It has a three horsepower spindle with a manual, infinitely variable, belt transmission. The drive motors for the table are 1.5 kW Glentek dc-servo motors, and the actuator in the axial direction is a 0.8 kW Glentek dc-servo drive. The motors are matched with PWM amplifiers from Glentek.

The UBC open architecture controller is used to control the drives, and an in house built Pendant and PLC are used to interface to the operator.
5.5.1.2 Force Sensor

Conventional milling dynamometers are able to provide reliable high bandwidth measurements of force; Unfortunately, their price is too high for practical milling applications, and a lower cost solution is required. This goal was approached from two sides. First, the process identification algorithms that were developed in this thesis were designed to require force measurements with frequency components up to the tooth passing frequency only. Second, a novel low cost dynamometer was designed that takes advantage of the low bandwidth requirements of the process identification techniques.

This sensor attempts to decouple forces in three orthogonal directions into separate spring member deflections. This approach allows the use of single direction load cells for measuring three components of force. The challenge of the design was to provide a structure that would allow the use of a minimum number of load cells, while still guaranteeing that the force measurements are independent of the point of force application. Furthermore, the structure needed to be simple to manufacture in order to arrive at a low cost sensor.

These requirements were fulfilled by a structure that incorporates two four bar linkages type spring structures that are placed perpendicular to each other in the xy-plane (see Figure 79). Forces in x and y are identified with load cells that are attached to the spring structures, whereas the vertical force is identified from a centrally located load cell under the top plate.
The sensor was machined from a single piece of aluminum because a lack of suitable manufacturing facilities did not allow for a more practical steel prototype. The aluminum sensor showed a bandwidth of approximately 200 Hz, which will improve by a factor of 1.75 for a steel prototype. This bandwidth is adequate for a wide range of practical milling applications. Further improvements can be achieved by redesigning the dimensions of the spring elements.

A more detailed description of this Dynamometer is supplied in Appendix B. Pictures of the sensor are shown in Appendix E.

5.5.1.3 Force Collection System
The output of the force table is fed through Kistler charge amplifiers and a KRON-HITE filter that is set to a break frequency of 70 Hz in order to eliminate resonant vibration of the force table. The output from the filter is sampled using
a ROBOTROL RBX311 8-channel differential 12 Bit AD-card that is connected to the Isbx interface of the process monitoring card.

The force data collection is synchronized with the spindle rotation. A TURCK UPROX capacitive switch is used which triggers off a splined surface on the spindle. The output of this switch is fed through a buffer into the external interrupt pin of the process monitoring board. Since there are only thirty splines on the spindle, one extra sample is collected in between every triggered sample. The timing of this extra sample is determined by extrapolating from the period between the last triggered samples to the midpoint between the current triggered sample and the next triggered sample. Figure 80 shows a diagram of the force collection system.

5.5.1.4 CAPP/Monitoring Hardware

The CAPP system is located on a Ziatek 486 based computer with an STD32 BUS. The process monitoring and control cards are Universal Systems UE9001A cards that are based on the Intel 80c196 processor. They are connected to the same STD32 bus. This arrangement provides shared memory.
between the CAPP system and the process monitoring cards. Figure 81 shows a diagram of the monitoring system.

![Figure 8.1. Monitoring Architecture](image)

The CAPP system and the CNC control system are linked through a serial line that allows the CAPP system to run high level forth code on the UBC controller. The synchronization line and state lines that were originally designed for use within the CNC system have been extended to connect with the process monitoring and control system.

The two monitoring cards provide a physical division for high and low bandwidth identification and control task. The high bandwidth card collects force data, performs geometry identification, and controls feed for tool breakage avoidance. It also performs Fourier series transformations that are sent to the low bandwidth card for identification of runout. Cutting parameters can only be identified when the geometry of cut is known exactly. This condition is determined by the CAPP
system which then launches the identification of cutting parameters on the low bandwidth board.

5.5.2 Part Geometry

The part geometry is shown in Figure 82; it contains a slot, a surface, and a shoulder.

5.5.3 Machining Steel

The first piece is machined from 200 BHN steel. A single 2" diameter face mill with round nosed Sandvik TPMN 16 03 08 inserts of grade P25 is used to machine the entire part. Table 9 lists the parameters that were used to perform the process optimization:

<table>
<thead>
<tr>
<th>Tool Data</th>
<th>Tool Life Data*</th>
<th>Material Data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flutes: 4</td>
<td>Vel. exp.: 2.0</td>
<td>Type: Steel</td>
</tr>
<tr>
<td>Diameter: 2&quot;</td>
<td>Equiv. chip exp., β: 1.0</td>
<td>Hardness: 200 BHN</td>
</tr>
<tr>
<td>Flute Length: 1&quot;</td>
<td>Axial depth exp., q: 0.5</td>
<td></td>
</tr>
<tr>
<td>Shank Length: 4.25&quot;</td>
<td>Therm. cycl. exp., p: 2.0</td>
<td></td>
</tr>
<tr>
<td>Cost: $61.4</td>
<td>Tool life const., c: 18e8</td>
<td>Rated power: 3 HP</td>
</tr>
<tr>
<td>Max. equ. chip thick.: 0.0035&quot;</td>
<td>Cutting Constants</td>
<td>Overhead rate: 4.3 c/min</td>
</tr>
<tr>
<td>Minimum feed: 0.0005&quot;</td>
<td>Cutting Pressure: 45e4 psi</td>
<td>Tool change time: 2 min</td>
</tr>
<tr>
<td>Max. Radial Force: 120 lbf</td>
<td>Force ratio: 0.6</td>
<td></td>
</tr>
</tbody>
</table>

Table 9. Process Parameters for Steel Experiment

* The tool life is assumed to follow the relationship established by Yellowley and Barrow:

$$ T = \frac{\Phi_b}{2\pi} \frac{c}{X^n s_{eq}^{1/2} V^{p} a^q} $$
The CAPP system elects to machine the slot followed by the surface and finally the shoulder. The surface is a three quarter immersion up milling operation, and the slot is a quarter immersion up milling operation. The required cutting conditions for these operations are shown in Table 10.

<table>
<thead>
<tr>
<th></th>
<th>Slot</th>
<th>Surface</th>
<th>Shoulder</th>
</tr>
</thead>
<tbody>
<tr>
<td>Immersion</td>
<td>Full</td>
<td>Three quarter</td>
<td>One quarter</td>
</tr>
<tr>
<td>Type</td>
<td>up milling</td>
<td>up milling</td>
<td></td>
</tr>
<tr>
<td>Feed direction</td>
<td>+Y</td>
<td>-X</td>
<td>+X</td>
</tr>
<tr>
<td>Feed Magnitude</td>
<td>12.9 [in/min]</td>
<td>15.1 [in/min]</td>
<td>17.4 [in/min]</td>
</tr>
<tr>
<td>Speed</td>
<td>653 [RPM]</td>
<td>583 [RPM]</td>
<td>583 [RPM]</td>
</tr>
</tbody>
</table>

Table 10. Cutting Conditions for the Steel Experiment

The actual spindle speed is manually set to 630 RPM before the start of the experiment because this value is close to the average of the required value of speeds in the three cutting operations.

5.5.3.1 Identification of Cutting Parameters

The CAPP system initially assumes a cutting pressure of 4e5 psi and a force ratio of 0.5. During the steady state portions of the cut, the monitoring system identifies the actual values of cutting pressure and force ratio. This identification task assumes accurate values of radial width and axial depth that are supplied by the CAPP system. Figure 83 shows a plot of the identified values.
The cutting pressure is between $40 \times 10^4$ and $45 \times 10^4$ psi for all three cutting operations. The force ratio is approximately 0.8 for the slot and the shoulder and 0.4 for the surface. The discrepancy in cutting pressures has been observed in a number of experiments. It is attributed to spindle tilt which leads to backcutting.

### 5.5.3.2 Feed Control

During the steel experiments the feed is controlled by a maximum force constraint and an equivalent chip thickness constraint. The maximum allowable radial force is set to 120 lbf, and the maximum allowable chip thickness is set to 0.0035". The force limits were set artificially low in order to show that the ACO system is able to control multiple constraints. In a real cutting situation, face mills would only be constrained by the maximum equivalent chip thickness constraint.

#### 5.5.3.2.1 Slotting

The slot has a width of 2" and a length of 1.5". Since the entry and exit transients are each 1" long, the steady state between entry and exit last for 0.5".
Figure 84 shows the traces of identified immersion angles, axial depth, resultant force, equivalent chip thickness, and feed for the slotting operation in steel.

During the tool entry the mean immersion angle remains at 90°, and the swept angle of cut increases from 0° to 180°. The maximum resultant force constraint of 120 lbf is violated immediately on tool entry, and consequently the feed is reduced to 65% of the prescribed feed. The equivalent chip thickness remains under 0.0035". Figure 84 shows that the identification system is able to identify the immersion angles and the axial depth with a high degree of accuracy.
During the steady state portion of the cut, the maximum resultant force constraint remains active, and the feedrate override is kept at approximately 65%.

In the exit transient the inserts come in contact twice with the workpiece during a single revolution (see Figure 85). The identification routines have not been designed for this scenario; a theoretical plot of the immersion parameter $Q$ and the mean immersion angle during this exit transient reveals the actions that the control system will take.

Figure 86 shows a plot of the parameter $Q$ (compare equation (37) and Figure 16) and the mean immersion angle against the difference between $\phi_3$ and $\phi_2$ for the case where the tool is exiting a slot. The figure covers the range between the initial slotting stage to the point where the tool is removing two quarter immersions. The swept angle of cut is dramatically underestimated in the later
portion of the double immersion period. This effect is caused by the addition of
more high frequency components due to the double cutting of the inserts. This
increases the fundamental components of the cutting forces more than the mean
value, and the result is an overestimate of $Q$ which leads to an underestimate of
the identified swept angle of cut. It should be pointed out, however, that the
mean immersion angle remains at the mid immersion point.

For a tool with zero nose radius, the maximum chip thickness will be selected
close to the mid immersion point, which in turn requires a conservative value of
feed. For a tool with a finite nose radius, the underestimate of swept angle of
cut leads to an overestimate of the axial depth which is then transferred to an
overestimate in equivalent chip thickness. This overestimate in turn requires a
very conservative choice of feed.

In the experiment shown here, the axial depth is overestimated and the feed is
reduced in order to keep the equivalent chip thickness low. In reality, the
resultant force and the maximum chip thickness gradually decrease.
Consequently the allowable feed should actually increase, but the control
scheme chooses a more conservative feed rate. When the identified swept angle
of cut drops below zero, the ACO system dictates to increase the feed to the
level that would be required for an exact axial depth during a slotting operation
with no force constraint. In the case shown here, the feed rate override the is
95% since the actual speed is lower than the required speed.
This example has shown that IPAC’s control strategy leads to very good results in entry and steady state conditions for slotting operations with a dominant maximum force constraint. In the exit transient, initially a very conservative feed is chosen, but when the identification routine predicts a zero swept angle of cut, a safe feed for theoretical values of depth and width is adapted.

### 5.5.3.2.2 Three Quarter Immersion Surface

![Graphs showing immersion identification, axial depth identification, maximum resultant force, and chip thickness identification.](image)

Figure 87. Three Quarter Immersion Surface Milling in Steel

The surface has an axial depth of .025", a radial width of 1.5", and a length of 2.125". This leads to a three quarter immersion up-milling operation that has a
1.125" long steady state cutting portion. Figure 87 shows the traces of identified immersion angles, axial depth, resultant force, equivalent chip thickness, and feed for the slotting operation.

During the tool entry transient the insert entry angle initially decreases from 90° to 60°. The insert exit angle increases from 90° to 120° during the same initial entry period, and it then remains constant during the remainder of the entry transient where the insert entry angle further decreases from 60° to 0°. Figure 87 shows that the identification system cannot catch the first part of the transient where both insert entry and insert exit angles change because the force levels are too low. During this period, a feed corresponding to the theoretical width and depth are chosen; that feed is only modified because of the difference between the actual and the reference speed. During the second part of the entry transient, the identification routine is able to show the expected trends quite well. Since the axial depth has been reduced by half from the slotting operation, the maximum resultant force constraint is no longer dominant. The equivalent chip thickness constraint keeps the feed rate override at 110%. This 10% increase from the planned value is caused by the difference in actual spindle speed to planned spindle speed.

During steady state the radial width and the axial depth are identified well, and the feed rate override is again 110% because of the spindle speed offset.
During the tool exit transient, the inserts initially contacts the workpiece twice over one cutter revolution (see Figure 88). Since the immersion identification algorithm was not designed for this condition, it is again useful to investigate the theoretical values of the immersion parameter $Q$ and the mean immersion angle.

![Figure 88. Double Contact Geometry During the Exit of Three Quarter Immersion](image)

Figure 88 shows a plot of the parameter $Q$ (compare equation (37) Figure 16) and the mean immersion angle against the difference between $\phi_3$ and $\phi_2$ for the case where the tool is exiting a three quarter immersion cut. The figure covers the range between the initial three quarter immersion stage to the point where the tool is removing a quarter immersion. During the double contact, the maximum chip thickness is identified conservatively because the mean immersion angle is close to the half immersion point. For a tool with zero nose radius, the conservative estimate of chip thickness leads to a conservative
choice of feed rate. For a tool with a large nose radius, a secondary effect comes into place. The axial depth is underestimated because the mean immersion angle indicates too large a chip thickness. This underestimate in turn leads to an underestimate in equivalent chip thickness, which is compensated for by the control system through an increase in feed rate. This increase in feed rate is, however, allowable because the actual maximum chip thickness during the exit transient decreases. Consequently, for the three quarter immersion case, the feed will be selected conservatively for a tool with zero nose radius, but it will be selected at close to optimum conditions for a tool with a finite nose radius.

In the experiment shown here, the axial depth and the equivalent chip thickness are underestimated; thus, the feed is required to increase by 18%. At the same time, the actual maximum chip thickness decreases by 14%, a decrease which makes an increase of feed rate by the same 14% allowable. This increase means that there is a slight overshoot of feedrate during the double immersion part of the exit transient. The second part of the tool exit transient is equivalent to the exit transient of a one quarter immersion operation. IPAC identifies the radial width and the axial depth quite accurately for two revolutions, and the feed rate is chosen at approximately 120%, a percentage which corresponds to the 8% difference in speeds and a 60° rather than a 120° immersion angle. During the rest of the transient, the forces drop below the noise level, and the feed is selected on the same basis as during the initial stage of the tool entry transient.
The ACO system is able to identify the cutting conditions at three quarter immersion reasonably well. When the equivalent chip thickness constraint is active, good values of feed are selected for the tool entry and steady state conditions, and conservative values of feed are chosen in the exit transient.

5.5.3.2.3 Quarter Immersion Shoulder

The shoulder has a radial width of 0.5", an axial depth of 0.025", and a length of 2.125". This geometry corresponds to a quarter immersion up milling operation with a 1.125" steady state portion. Figure 90 shows the traces of identified immersion angles, axial depth, resultant force, equivalent chip thickness, and feed for the slotting operation in steel.

The equivalent chip thickness constraint dominates the resultant force constraint.

During the entry transient the insert entry angle decreases from $60^\circ$ to $0^\circ$, whereas the insert exit angle remains at $60^\circ$. The forces during most of the entry transient are below the noise cutoff level. During this portion of the cut, a feed rate corresponding to a slotting operation with the nominal axial depth and the ratio of actual speed to nominal speed is selected. Because the spindle speed is 8% higher than the required spindle speed, but a half immersion cut requires a feed rate 14% lower than a quarter immersion cut, the feed rate is reduced overall.
Chapter 5  IPAC: Integrated Process Planning and Control

Immersion Identification

\[ \phi_1 & \phi_2 \text{[deg]} \]

Revolutions

40 80 120

Maximum resultant force

\[ F_r \text{[lbf]} \]

Revolutions

40 80 120

Chip thickness Identification

\[ h_e \text{[in]} \]

Revolutions

40 80 120

Axial Depth Identification

\[ \text{Depth [in]} \]

Revolutions

40 80 120

Feed override

\[ \% \text{ Override} \]

Revolutions

40 80 120

Figure 90. Quarter Immersion Shoulder Milling in Steel

During the steady state portion of the cut, the radial width and the axial depth are identified satisfactorily. The feed is increased with respect to the entry transient by 15-20%. This increase is attributed to the lower maximum chip thickness in quarter immersion compared to half immersion.

At the tool exit transient the entry angle remains at 0°, whereas the insert exit angle decreases from 60° to 0°. Again, the forces throughout most of the tool
exit transient are below the noise level. Consequently, the safe feed from the entry transient is used in the exit transient as well.

During the quarter immersion cut, the IPAC system selects conservative values of feed for the entry and exit transients and optimizes the feed during steady state for the maximum equivalent chip thickness constraint. By reducing the noise cutoff level, improvements could be achieved in the transients. The axial depth in this cut was very small, and in a deeper cut, the transient identification algorithm is expected to perform more effectively.

5.5.3.3 Identification of Runout

The process monitor attempts to identify relative radial runout which can be used by the CAPP system to infer surface finish. The absolute radial runouts of the cutter used for milling the steel part were measured with a dial gauge that had a resolution of .0001". These measurements are shown in Table 11 together with the relative runouts.

<table>
<thead>
<tr>
<th>Insert</th>
<th>Insert 2</th>
<th>Insert 3</th>
<th>Insert 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Absolute runout</td>
<td>0</td>
<td>-0.0006</td>
<td>-0.0008</td>
</tr>
<tr>
<td>Relative runout</td>
<td>0.0007</td>
<td>-0.0006</td>
<td>-0.0002</td>
</tr>
</tbody>
</table>

Table 11. Radial Runouts Measured with a Dial Gauge

Figure 91. Relative Radial Tooth Size in Steel
runout shown in Figure 91. The identified values in three quarter immersion and quarter immersion are close to the measured ones. In the slotting operation discretization errors due to the low sampling rate are encountered. (They are likely to occur at immersion angles equaling multiples of the tooth spacing angle; thus, for a four tooth cutter, inconsistencies in the runout identification algorithm are expected at 0°, 90°, and 180° immersion. (see also section 4.4.1.3))

5.5.4 Machining Titanium

The titanium alloy was machined with the same face mill as the steel part, but the inserts were grade K20. Table 12 lists the parameters that were used by the CAPP system for the process optimization.

<table>
<thead>
<tr>
<th>Tool Data</th>
<th>Tool Life Data</th>
<th>Material Data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flutes: 4</td>
<td>Vel. exp.: 2.0</td>
<td>Type: Titanium</td>
</tr>
<tr>
<td>Diameter: 2&quot;</td>
<td>Equiv. chip exp.: 0.55</td>
<td></td>
</tr>
<tr>
<td>Flute Length: 1&quot;</td>
<td>Axial depth exp.: 0.5</td>
<td></td>
</tr>
<tr>
<td>Shank Length: 4.25&quot;</td>
<td>Therm. cycl. exp.: 1.6</td>
<td></td>
</tr>
<tr>
<td>Cost: $61.4</td>
<td>Tayl. equn. const.: 1e6</td>
<td></td>
</tr>
<tr>
<td>Max. equiv. chip thickn.: 0.001&quot;</td>
<td>Cutting Constants</td>
<td>Overhead rate: 1.3 $/min</td>
</tr>
<tr>
<td>Minimum feed: 0.0005&quot;</td>
<td>Cutting Pressure: 4e5 psi</td>
<td>Tool change time: 2 min</td>
</tr>
<tr>
<td>Max radial force: 120 lbf</td>
<td>Force ratio: 0.8</td>
<td></td>
</tr>
</tbody>
</table>

Table 12. Process Parameters for the Titanium Experiment

The CAPP system arrives at the same cutting sequence as that for the steel work part: The slot is followed by the surface and the shoulder. Table 13 shows the cutting conditions of the three operations:

<table>
<thead>
<tr>
<th></th>
<th>Slot</th>
<th>Surface</th>
<th>Shoulder</th>
</tr>
</thead>
<tbody>
<tr>
<td>Immersion</td>
<td>Full</td>
<td>Three quarter</td>
<td>One quarter</td>
</tr>
<tr>
<td>Type</td>
<td>up milling</td>
<td>up milling</td>
<td></td>
</tr>
<tr>
<td>Feed direction</td>
<td>+Y</td>
<td>-X</td>
<td>+X</td>
</tr>
<tr>
<td>Feed Magnitude</td>
<td>3.19 [in/min]</td>
<td>3.48 [in/min]</td>
<td>4.56 [in/min]</td>
</tr>
<tr>
<td>Speed</td>
<td>582 [RPM]</td>
<td>500 [RPM]</td>
<td>566 [RPM]</td>
</tr>
</tbody>
</table>

Table 13. Cutting Conditions for the Titanium Experiment
In this experiment the spindle speed was manually set to 550 RPM before the start of the experiment.

5.5.4.1 Identification of Cutting Parameters

During the steady state portions of the cuts, the actual cutting pressures and force ratios are identified on the assumption of exact values of immersion and axial depth. Figure 92 shows a plot of the identified values.

The cutting pressure in the slotting and the three quarter immersion operation are approximately 4e5 psi, whereas the cutting pressure in the quarter immersion cut is only 3e5 psi. This underestimate of cutting pressure is offset by a doubling of the force ratio between the first two cutting operations and the beginning of the last cutting operation. It also shows that the force ratio continuously increases during the quarter immersion operation. This indicates a large wear rate. (There was in fact appreciable wear.)
5.5.4.2 Feed Control

The feed during the titanium experiments was controlled by the maximum equivalent chip thickness criterion only because the allowable equivalent chip thickness with titanium is lower than for steel.

5.5.4.2.1 Slotting

During the tool entry, the mean immersion angle remains at 90°, and the swept angle of cut increases from 0° to 180°. Because the axial depth is constant during the transient, the feed has to stay constant in order to ensure that the equivalent chip thickness is kept at its maximum value. Figure 93 shows that the identification system is able to identify the immersion angles and the axial depth with a high degree of accuracy, and it is able to keep the equivalent chip thickness close to the required 0.001". The feed rate override is kept close to 90% since the ratio of required spindle speed is higher than the actual spindle speed.

During the steady state portion of the cut, the swept angle of cut and the axial depth are identified satisfactorily, and consequently, the same feedrate as in the entry transient is selected.

In the exit transient, the inserts come in contact twice with the workpiece during a single revolution. At the start of the double contact the swept angle of cut is underestimated, the axial depth and the equivalent chip thickness are overestimated, and consequently the feedrate needs to drop. Very soon
thereafter a zero swept angle of cut is identified, and IPAC reverts to choosing
the feed based on the ratio between the actual and nominal spindle speeds and
the nominal values of axial depth and swept angles of cut.

This section showed that during slotting, IPAC's control strategy leads to very
good results in entry and steady state conditions and to a conservative choice in
feed for the exit transient.

Figure 93. Slotting Operation in Titanium
5.5.4.2.2 Three Quarter Immersion Surface

Figure 94. shows the traces of identified immersion angles, axial depth, resultant force, equivalent chip thickness, and feed.

During the tool entry transient the insert entry angle initially decreases from 90° to 60°. The insert exit angle increases from 90° to 120° during this initial entry period, and it then remains constant during the remainder of the entry transient where the insert entry angle further decreases from 60° to 0°. During the first
part of the entry transient, the force level is below the noise cutoff level; thus, a feed corresponding to the nominal values of width and depth, as well as the ratio between actual and nominal feed, is selected. In this case, a feed rate override of 110% is required, because the actual speed is higher than the nominal speed. During the second part of the entry transient, the identification routine has some difficulties finding the end of the transient. This difficulty is reflected in the oscillatory nature of the identified insert entry angles down to zero and the corresponding reduction of identified insert exit angles. This problem can be alleviated by imposing a stricter steady state criterion. For the equivalent chip thickness control, however, this identification problem does not lead to serious trouble because the resulting identified axial depth and equivalent chip thickness are close to the expected values. The feed is set to approximately 100% since the slight overestimate of axial depth and equivalent chip thickness cancels the increase in feed due to the spindle speed offset.

During steady state, the radial width and the axial depth are slightly overestimated, but since the required spindle speed is lower than the actual spindle speed, the feed rate override is kept at approximately 100%.

During the tool exit transient, the inserts initially come in contact twice with the workpiece during one cutter revolution. This double contact leads to an underestimate of the axial depth, and the feed is then increased by 10% in order to keep the equivalent chip thickness constant. This increase is lower, and
hence more conservative, than the optimum feed rate that would be prescribed by the concurrent reduction of chip thickness by 14%. The second part of the tool exit transient is equivalent to exiting a quarter immersion cut. IPAC slightly overestimates the axial depth during two revolutions of this section which leads to a conservative reduction in feed. Thereafter, the forces drop below the noise cutoff level, and the same feed as in the start of the tool entry transient is selected.

IPAC performed well during the three quarter immersion operation. Optimum or conservative values of feed were selected during the complete cut.

5.5.4.2.3 Quarter Immersion Shoulder

Figure 87 shows the traces of identified immersion angles, axial depth, resultant force, equivalent chip thickness, and feed.

The rubbing forces in a low immersion up milling operation in Titanium are extremely high. These rubbing forces leads to a change in force ratio from 0.8 to 2.0. Furthermore, the inserts wear at a considerable rate in this cutting condition, an effect which further increases the force ratio. These effects have not been incorporated into the axial depth identification algorithm. Nevertheless, the chip thickness control algorithm arrives at a safe choice of feed rate.

During the entry transient, the insert entry angle decreases from 60° to 0°, whereas the insert exit angle remains at 60°. The identification algorithm has
considerable difficulties during the entry transient. The forces are quite low, and consequently, the immersion identification algorithm does not provide a result during most of the transient. In this starting portion, the feed is selected according to the reference axial depth and a maximum chip thickness at 90° rather than 60°. This, in turn, leads to a feed rate override of 85%. The nominal spindle speed is very close to the actual spindle speed; thus, it has little influence on the selection of speed. During the remainder of the transient, the change in force ratio drastically reduces the identified mean immersion angle; thus, the identified insert entry angle is negative, and the identification routine then defaults it to 0°. The reduced identified immersion angle leads to an underestimate of the maximum chip thickness and an overestimate of the axial depth. The chip thickness overestimate dominates the opposing axial depth overestimate, and the feed rate overshoots by 30%. This overshoot, fortunately, did not lead to edge breakage because the duration of the overshoot was small.

During the steady state portion of the cut, the inserts are rapidly wearing, and the cutting ratio is further increasing. This effect is reflected in a continuously increasing estimate of the axial depth and a decreasing feed rate override.

At the tool exit transient, the entry angle remains at 0°, whereas the insert exit angle decreases from 60° to 0°. The axial depth is overestimated because of increased rubbing forces at the start of the exit transient. However, this effect is dominated by a decrease in identified maximum chip thickness; thus, the feed
increases. As for the entry transient, the force levels during the remaining large portion of the exit transient are below the noise cutoff level; the feed then defaults to the same value as at the start of the entry transient.

Figure 95. Quarter Immersion Shoulder Milling in Titanium

The quarter immersion operation demonstrates the effect of wear on the identification system. Since cutting parameters were not updated during the operation, estimates of axial depth lead to conservative values of feed during the steady state portion of the cut.
5.5.4.3 Identification of Runout

The process monitor attempts to identify relative radial runout which can be used by the CAPP system to infer surface finish. The absolute radial runouts of the cutter used for milling the titanium part was measured with a dial gauge that had a resolution of .0001". These measurements are shown together with the relative runouts in Table 14.

<table>
<thead>
<tr>
<th></th>
<th>Insert 1</th>
<th>Insert 2</th>
<th>Insert 3</th>
<th>Insert 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Absolute runout</td>
<td>0</td>
<td>-0.0001</td>
<td>-0.006</td>
<td>-0.0012</td>
</tr>
<tr>
<td>Relative runout</td>
<td>0.0012</td>
<td>-0.0001</td>
<td>-0.0005</td>
<td>-0.0006</td>
</tr>
</tbody>
</table>

Table 14. Radial Runouts Measured with a Dial Gauge

There is good correlation between the measured and the identified relative runout in three quarter immersion. In quarter immersion, the cutting parameters have changed, and the identified runout is an overestimate of the actual runout values. In the slotting operation, discretization errors decrease the identified runout magnitude. This effect has been described in section 4.4.1.3.

The runout identification technique is reliable as long as the cutting parameter estimates are adequate. It was shown in section 4.4.1.2 that it is possible to
determine radial runout independent of cutting parameter estimates by normalizing the runout magnitudes with the quasi mean resultant force. This normalization has not been implemented in the identification algorithm presented here, and hence, the poor performance in the quarter immersion operation is expected.

5.6 Conclusion
This chapter describes IPAC, a 2-D CAPP system that is integrated with a process monitoring and a process control system. It is based on a novel architecture that allows for efficient adaptations to varying control strategies, as well as to system updates. The architecture also facilitates passing of process parameters between the planning and control components of the system. This facilitation leads to improvements, not only in the low level process identification and process control strategies, but potentially also in the high level planning stages.

The CAPP system within IPAC is driven by a natural language interface that allows efficient definitions of part geometries. Process plans are established by minimizing production costs. The CAPP system sends motion commands to the CNC system and synchronized monitoring and control commands to the ACO system. The ACO system is able to perform multiple constraint optimization. It ensures operation safety by enforcing a maximum force constraint, and it optimizes process economics by limiting the maximum equivalent chip thickness.
The system also monitors runout, which is a measure of tool condition, and consequently product quality. Calibration of cutting parameters can also be accomplished.

Two sets of experiments were performed using steel and titanium workpieces; these validated the robustness of the overall system. The experiments also demonstrated the use of an in-house built force table that is described in more detail in Appendix B. It is evident from these experiments that tool wear is (as might be expected) easily identified from changes in the force ratio. It is also, however, clear that practical implementations of the system should continuously update cutting parameters to avoid errors in the identification of cut dimensions.
Chapter 6
Conclusions

6.1 Introduction
This thesis has examined the possibility of incorporating a quasi real time planning system within a computer numerical control system. The means used to accommodate the integration has been a very flexible open architecture scheme, the basis of which has been used for other exercises involved with sensory feedback and optimization of metal cutting processes. The motivation for the work was the ambition to improve the efficiency of real time control through the provision of information from the planning system and to improve the accuracy of planning through the feedback of accurate process parameters. The system integration also provides the capacity for truly dynamic process planning activities, i.e., the process plan may be modified in real time either to improve efficiency or, alternatively, to allow a new feasible strategy to be adopted following a tool failure or other major problem (e.g., the presence of unexpected material on the blank).

The integration of planning and control yields improvements in a number of interrelated areas. The most visible difference on the machine itself is an improved user interface, because the machine no longer is programmed by tool path trajectories but, rather, by part geometries. This improved interface makes it easier to describe, display, and simulate the manufacture of the part; hence,
shop floor programming can be dramatically simplified. At the same time, process identification through sensory feedback becomes easier because a great wealth of process information is now available on the control system that traditionally could not be transferred from the planning system to the control system. This improved capacity to identify the process allows for reducing the cost of the current process plan in real time because variations in technological and geometric process parameters can be compensated for online. At the same time the manufacturing process becomes safer because impending tool breakage and constraint violations (force, feedrate, etc.) can be identified and avoided. It also becomes feasible to control part quality parameters such as surface finish or part accuracy through sensory feedback. Finally, it is possible to adapt the process plan itself in quasi real time when changing process parameters suggest or emergency situations require a new feasible strategy for the process plan.

6.2 Major Contributions

The major technological contributions made in this thesis are related to the overall design of a framework which allows the integration of high level planning with axis control, monitoring and real time optimization. The approach allows a user transparent access to the various planning and monitoring tasks; the timing of the system (synchronization of process and motion activities), being maintained by the front plane arrangement of the UBC control architecture. The approach taken to the description of planned volumes and processes is simple,
but it effectively demonstrates the manner in which process planning data can be useful in determining or modifying monitoring requirements. As might have been expected, the broad issue of system integration created the need for the solution of several detailed and challenging problems concerned with process mechanics, process monitoring and sensor design. A detailed summary of the contributions made in each of these areas is given in the remainder of this section.

6.2.1 An Upper Bound Cutting Model
Cutting forces were chosen as the primary indicator of process geometry and tool condition. Conventional cutting force models are able to relate cutting parameters to the power consuming force and the thrust force. Unfortunately, the flow angle, which relates the thrust force to the radial and axial cutting forces, is not easily determined for oblique cutting. Given that much of the work in this thesis was intended to address face milling, the first major undertaking was the development of a suitable cutting model for round nosed inserts with obliquity that would provide the flow angle that is missing in conventional cutting force models. The model for determining the flow angle is based on an upper bound approach, but includes a simple force balance between forces in the shear plane and forces on the rake face; thus, a Merchant type shear angle solution is found. Even though the model does not need experimental data to generate flow angle, both the accuracy of flow angle estimation and in particular the accuracy of prediction of effective shear and friction parameters can be
improved given equivalent orthogonal data. The model was used to prove Stabler’s flow rule for straight cutting edges and zero friction; it was also shown that its flow angle predictions agree with the most commonly accepted empirical models found in the literature.

6.2.2 Process Identification Algorithms

The upper bound model is able to describe the instantaneous cutting forces on a milling cutter. Identification of process parameters in milling however requires a consideration of the periodic and intermittent nature of the cutting process. Transforming cutting forces into the frequency domain facilitates the identification of machining conditions and subsequently monitoring of runout and breakage. In this thesis, standard force based frequency domain identification techniques for the cutting geometry and cutting parameters in end milling were extended to face milling. Based on the machining parameter identification, frequency domain algorithms for identifying radial and axial runouts of single teeth were derived. In-plane forces were related to radial runout, whereas axial forces were utilized to indicate axial runout. The robustness of the runout identification techniques was tested in an experimental investigation that also demonstrated that the same algorithms can be used to detect edge breakage in the presence of significant initial runout.
6.2.3 A Table Dynamometer
The identification techniques were designed with the goal of keeping the required frequency content of the force measurements to a minimum. This low bandwidth requirement allowed the design of a novel table dynamometer that provides the robust low cost force measurements that are required in practical milling applications.

6.3 Future Work
The contributions made in this thesis demonstrate the feasibility of integrating process planning and control; there are, however, still a number of improvements that are required for a practical system. First, the supported cutting geometries need to be extended to include a wide variety of machined features. (This extension should probably be combined with an interface to a commercial CAD system.) These additional features will involve new operations (e.g., drilling, tapping, reaming, etc.) that require new process identification and control algorithms. Furthermore, additional constraints, such as surface finish, part accuracy, and chatter, require appropriate identification algorithms and control. In addition, spindle speed should be regulated in order to ensure optimum tool life. Unfortunately, tool wear is very difficult to monitor reliably, because it will vary among batches of inserts, machines within the same production series, and even positions on a single workpiece. Thus, direct on-insert wear sensors are the only sensible solution to this problem; they still require considerable research effort. Finally, even though much ground work
has been laid for a dynamic process planning system, the replanning portion of such a system still needs to be implemented.
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References


References


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100. Form Talysurf 2 Laser Attachment, Taylor Hobson Inc., Rolling Meadows, IL 60008.

101. Lasercheck In-Process Surface Roughness Measurement Gage, Optical Dimensions, Bozeman, MT 59715.

102. SORM- Super fast Optical Roughness Measurement, Opdix OptoElectronic GmbH, Geretsried, Germany.


Appendix A

Identification of Cutting Torques from Variations in Spindle Speed

An AC induction type spindle drive slows down under load. This load speed characteristic is referred to as slip; it can be measured with a tachometer on the spindle drive which will indicate a drop in speed whenever the cutting torque increases.

**Torque Speed Characteristic of AC Induction Motors**

Slip is expressed in terms of the line frequency $\omega_p$, the rotor speed $\omega_m$, and the number of pole pairs per phase of stator windings $n$:

$$S = \frac{\omega_p - n \omega_m}{\omega_p}$$  \hspace{1cm} (133)

The motor torque is also related to the number of supply phases $p$, the supply voltage $v_f$, the rotor coil resistance $R_r$, and the rotor coil inductance $L_r$:

$$T_m = \frac{p n v_f^2 S R_r}{\omega_p (R_r^2 + S^2 \omega_p^2 L_r^2)}$$  \hspace{1cm} (134)

This expression is fairly linear in the region of little slip:

$$T_m = \frac{p n v_f^2}{\omega_p R_r} S = K_m \left( \frac{\omega_p}{n} - \omega_m \right)$$  \hspace{1cm} (135)
Equilibrium Equations of a Belt Driven Spindle Drive

Figure 97 shows a diagram of a typical belt driven spindle arrangement. The belt in between the two pulleys is approximated by a simple spring damper system with a very large stiffness. The moment equilibrium for the spindle and the linearized AC motor is given by the following equations:

\[ J_c \ddot{\omega}_c = F_b r_c - T_c \quad \text{(136)} \]

\[ J_m \dot{\omega}_m = K_m \left( \frac{\omega_m}{n} - \omega_m \right) - r_m F_b \quad \text{(137)} \]

Here \( F_b \) is the force in the belt which is approximated by a simple spring damper system:

\[ F_b = K_b (\dot{\theta}_m r_m - \dot{\theta}_c r_c) + C_b (\dot{\theta}_m r_m - \dot{\theta}_c r_c) \quad \text{(138)} \]

Simulation of Holke Spindle Drive

The spindle of the Holke milling machine at UBC has an AC induction motor that is a suitable test bed for identifying cutting torques from variations in spindle speed. It has a direct belt-driven, infinitely variable transmission. The parameters of this drive are shown in Table 15.
Appendix A  Identification of Cutting Torques from Spindle Speed

<table>
<thead>
<tr>
<th>Electrical Parameters</th>
<th>Mechanical Parameters</th>
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<tbody>
<tr>
<td>Rotor coil resistance 0.1 Ω</td>
<td>Spindle pulley radius 0.1 m</td>
</tr>
<tr>
<td>Rotor coil induction 0 H</td>
<td>Motor pulley radius 0.1 m</td>
</tr>
<tr>
<td>Field supply voltage 115 V</td>
<td>Spindle inertia 0.1544 kg m^2</td>
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<tr>
<td>Poles per phase 1</td>
<td>Motor inertia 0.3088 kg m^2</td>
</tr>
<tr>
<td>Phases 3</td>
<td>Belt stiffness 2e4 N/m</td>
</tr>
<tr>
<td>Line frequency 60 Hz</td>
<td>Belt damping 0 Ns/m</td>
</tr>
</tbody>
</table>

Table 15. Holke Spindle Parameters

Equations (139) to (141) were solved numerically, and the response of the Holke induction motor to a disturbance torque of 2 Nm, which is typical for a cutting operation, is shown in Figure 98.

The system has a natural frequency of approximately 6.7 Hz, and the change in Spindle speed to this type of disturbance torque is 8 RPM. Since typical spindles rotate between 600 RPM and 2000 RPM, the change in spindle speed is often less than 1% of the nominal speed. Consequently a highly accurate tachometer is required in order to utilize spindle speed as a measurement of torque.

**Experimental Investigation**

An inductive pickup was mounted at a splined surface of the spindle of the Holke milling machine. This pickup provides 30 pulses per revolution. The periods of the pulses of this encoder type of sensor were related to spindle speed variations using an INTEL 80c196 based monitoring board, and the torque was then inferred from the spindle speed variations.
A number of experiments were performed in order to determine the dynamic response of the spindle. First, a three flute endmill was used as a drill. This operation produces a step input of torque for the spindle. The axial forces in this case should also correspond to the spindle torque. Figure 99 shows a plot of spindle speed and axial force. The spindle RPM were scaled to match the magnitude of the axial force, and a running average over 30 samples was performed. This experiment showed that there is a subharmonic at approximately half the spindle frequency.

In order to confirm this subharmonic resonant frequency, a DC Generator was hooked to the spindle and a velocity trace at 530 RPM was obtained. This trace also showed a clear fundamental at 6.1 Hz.

Since this subharmonic could be caused by the torsional vibrations of the spindle, an accelerometer was mounted tangentially to the spindle and it was excited with a small impact. This experiment showed that the torsional natural frequency of the spindle is found at approximately 150 Hz. This frequency is an order of magnitude higher than the resonant frequency obtained through velocity measurements; thus, the subharmonic resonance frequency is due to the belt stiffness.
A relationship between the spindle speed and the subharmonic frequency can be obtained if a simplified model of the spindle drive is assumed where the motor speed is approximately constant, whereas the spindle speed oscillates. This model requires that the natural frequency of the spindle be linearly related to the radius of the pulley driving the spindle:

\[ \omega_n = r_c \sqrt{\frac{K_b}{J_c}} \]  

(139)

This linear relationship compares well with results from the previous simulation and actual experiments; thus, a good estimate of mean velocity and torque can be achieved by applying a low pass filter with a cutoff frequency below the oscillation due to the belt.

The torque constant of the spindle drive was then experimentally determined from three slotting experiments at different feeds, where the mean cutting torque, \( \bar{T}_c \) was found from the mean normal force \( \bar{F}_y \), and the tool radius \( R \), by assuming negligible edge forces:

\[ \bar{T}_c = \bar{F}_y \frac{4R}{\pi} \]  

(140)

The relationship between cutting torque \( T_c \) and spindle speed variation contains the radius of the spindle pulley \( r_s \), the radius of the motor pulley \( r_m \), the spindle idle speed, \( \text{RPM}_{\text{idle}} \), and the spindle speed under load, \( \text{RPM}_{\text{load}} \):
In the experiment, the nominal spindle speed was 1650 RPM, a speed which leads to equal radii of the spindle pulley and the motor pulley. The depth of cut was 2 mm, the feed was 300 mm/min 450 mm/min, and 600 mm/min. The cutter had a diameter of 19.05 mm, with four cutting flutes. The material was Aluminum with a cutting pressure of 1300 N/mm$^2$ and a force ratio of 0.4.

Figure 100 shows that the relationship between torque and change in spindle speed that was obtained from the three feedrate experiments is linear. The torque constant of the spindle drive is 0.3 Nm/RPM. This finding is also in agreement with the simulation where a torque of 2 Nm resulted in a change in spindle speed of 8 RPM which corresponds to 0.25 Nm/RPM.
Figure 101 shows the cutting forces and the spindle velocity that were obtained during the test at 600 mm/min. In order to show trends more accurately, the z-forces were magnified by a factor of 10, and the torque measured from the spindle speed variation was magnified by a factor of 50. All measurements were averaged over 2 revolutions.

**Conclusions**

The change in spindle velocity promises to be a very good indicator of cutting torque. There are, however, practical problems with its utilization in this undertaking. The major problem is due to spurious vertical motion of the belt on the cone of the pulley, and that leads to speed fluctuations while unloaded, which are of the same order as those generated under load.
Appendix B
A Table Dynamometer

Introduction
Real time process optimization systems require the capacity to measure geometrical and technological parameters to be able to ensure optimum process control. These parameters, rather than being measured directly, are indirectly determined by a monitoring system from sensory feedback. A wide variety of sensor types measuring different physical parameters of the cutting process have been employed; force measurements have been used with the most success because fairly good models are available for relating cutting forces to the cutting process. The only drawback to the use of cutting forces is the high cost of the commercially available dynamometers. Alternative sensing devices were discussed in section 2.4; this appendix will discuss a new low cost table dynamometer. This dynamometer was designed particularly for the low bandwidth requirements of the identification algorithms that were introduced in Chapter 4. These low bandwidth requirements allow for a simple and low cost design.

Design Criteria
Cutting force dynamometers are required to meet various criteria to ensure both that forces are measured accurately and that the machining operations are not severely limited. The following list details the most important requirements:
Appendix B. A Table Dynamometer 221

- High Rigidity of the dynamometer ensures that the dimensions of the finished part are accurate.

- The bandwidth of the dynamometer needs to be matched to the bandwidth requirements of the identification algorithms. The identification algorithms presented in this thesis require force measurements with frequency components up to the tooth passing frequency. This requirement demands bandwidths of around 200 Hz in typical milling applications.

- The sensitivity of the dynamometer should be as high as possible, while drift due to temperature variations, humidity changes, or drift with time should be minimized.

- Cross sensitivity between measured forces need to be kept at a minimum. Mutual interference between force measuring elements can be compensated, but this compensation increases the computational load for the identification system.

- The dynamometer should be simple to install on the machine tool, it should not severely limit the working envelope of the machine, and it should also interfere little with tool changing and workpiece clamping operations.

Self tuning filters have been proposed to compensate for some of the inherent conflicting mechanical design requirements. These systems are computationally complex, and they are based on very exact models of the machine tool, the
sensor, and the cutting conditions. Successful practical applications on a real machine tools have not been demonstrated\textsuperscript{132}.

**Design Philosophy**

The dynamometer design proposed in this work utilizes piezoelectric load cells for transducing force. The mechanical structure of the design carries a large proportion of the load, thereby reducing the stiffness/load carrying capacity requirement of the load cells which, in turn, allows for low capacity load cells. The system, in essence, is an attempt to design a structure which, while taking load, does not lead to a variation in sensitivity with force location. At the same time, it was, of course, desired to minimize the number of force cells required.

The structure that was selected to meet these requirements can more easily be understood by first considering a simplified two component measuring system that measures forces only in the vertical direction \(z\), and in one horizontal direction \(x\) (see Figure 102). Clearly, if the top plate is infinitely stiff, the spring flange elements show large resistance to moments around the vertical axis or forces in the \(y\) direction, but they do
allow for bending deflection in the x direction. Thus, Gauge X will show forces in x only, independent of forces in y, or moments around the vertical.

If the stiffness of the Gauge-Z is orders of magnitudes higher than the bending/shear stiffness of the webs, then the location of an applied axial force will also have little influence on the force measured by the centrally located load cell. This can be proven by the force balance shown in Figure 103. It is assumed that the top plate is infinitely stiff, and, as a result, the springs have to deflect the same amount in opposite directions when the top plate pivots on the center load cell. Consequently, the forces applied to the two spring elements are of equal magnitude but of opposite sign. The externally applied force on the top plate must then be matched by the force applied by the load cell on the top plate in order to achieve force equilibrium. Moment equilibrium requires that the force couple due to the applied force and the load cell cancels the force couple created by the spring forces. It might be noted that the compliance required is confined to a web connecting the actual flange to the top plate.

The design of a three force component system with spring flange elements that are orthogonal to each other is shown in Figure 104. The right x directional flange in this drawing is transparent in order to show the geometry of the y directional flanges. The flanges in the x direction are connected to the milling...
A Table Dynamometer

Apendix B.

Table and a stiff crossmember. The flanges in the y direction connect the crossmember to the top plate where the forces are applied.

Figure 104. Three Dimensional Force Table (Taken from Chapter 5)

The orthogonal positioning of spring flanges results in a structure whose top plate is relatively free to move in x or y, but it meets great resistance to moments around the vertical axis. Hence, the deflection of the sensor in the horizontal plane is relatively independent of the location of the applied force.

There are two possibilities for mounting load cells to measure the applied forces:

- A single component load cell can be mounted on each of the springs. These load cells provide a good measure of in plane forces. The axial force component can be measured with a single centrally mounted load cell.

- A single three component load cell can be centrally mounted in place of the single direction axial load cell of the previous approach.
The two approaches should yield equivalent performance, but the second one, perhaps, has the benefit that the load cell is better shielded from the environment. In this work, the first approach was chosen out of convenience.

The location of the load cells, the wiring diagram of the load cells, and the labels for the dimensions of the force table are shown in Figure 105. Two load cells for transducing forces in the $x$ direction are mounted on the outside of the longitudinal springs; a single centrally mounted loadcell for transducing forces in the $y$ direction is mounted on the inside of the transverse springs. The load cell for the $z$ direction is placed under the center of the table.
**Simple Bandwidth Estimations**

The system natural frequencies are determined for the geometry of the simple plate bending elements as far as X and Y are concerned. It is important to have approximately equal natural frequencies in both directions.

Simple beam equations can be used to approximate the bending moment in the springs. The moment diagram is shown in Figure 106.

![Figure 106. Moment Diagram](image)

\[
EI \frac{d^2 y}{dz^2} = -M = -F_y z + M_0
\]

(142)

Integrating this equation twice and substituting zero slopes at both ends yields the expression for the deflection of the beam:

\[
y = \frac{F_y}{EI} \left[ -\frac{1}{6} z^3 + \frac{1}{4} I z^2 \right]
\]

(143)

The stiffness of such a system is the proportionality constant between displacement \( y \) and applied force \( F_y \):

\[
k_{spring} = \frac{F_y}{y} = \frac{12EI}{l^3}
\]

(144)
The natural frequency of the force table can now be expressed as a function of the spring geometries, the Young’s modulus of the spring material, and the mass of the covering plate.

\[
\omega_n^x = \sqrt{\frac{2k_{\text{spring}}}{m_{\text{plate}}}} = \sqrt[3]{\frac{2Ea}{l_1^3 m_{\text{plate}}}}
\]
\[
\omega_n^y = \sqrt{\frac{2k_{\text{spring}}}{m_{\text{plate}}}} = \sqrt[3]{\frac{2E_{\text{Aluminum}} b t_2^3}{l_2^3 m_{\text{plate}}}}
\]

(145)

The natural frequency in the x direction would usually be designed to be equal to that in the y direction. This equality is achieved by selecting appropriate spring thicknesses:

\[
\frac{t_2}{t_1} = \frac{3a}{b}
\]

(146)

Finally, it should be pointed out that the z axis case is much more complex. The most important factor is to ensure the low stiffness around the periphery; this low stiffness is achieved through shear of the web.

**Physical Design**

The prototype table was machined from a single piece; it is expected that this manufacturing process will lead to production versions of the table which will have very low cost. Because of a lack of suitable production facilities, the prototype was machined from aluminum; the production version will be from steel. A covering steel plate is fastened to the spring assembly with bolts.
Pictures of the dynamometer are printed in Appendix E. Table 16 shows the dimensions of the force table.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Name</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Width</td>
<td>a</td>
<td>30 cm</td>
</tr>
<tr>
<td>Length</td>
<td>b</td>
<td>60 cm</td>
</tr>
<tr>
<td>Height</td>
<td>l</td>
<td>4 mm</td>
</tr>
<tr>
<td>Spring thickness in width direction</td>
<td>t&lt;sub&gt;1&lt;/sub&gt;</td>
<td>3.2 mm</td>
</tr>
<tr>
<td>Spring thickness in length direction</td>
<td>t&lt;sub&gt;2&lt;/sub&gt;</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>Young's modulus of aluminum</td>
<td>E&lt;sub&gt;aluminum&lt;/sub&gt;</td>
<td>69 GPa</td>
</tr>
<tr>
<td>Mass of covering plate</td>
<td>m&lt;sub&gt;plate&lt;/sub&gt;</td>
<td>19 kg</td>
</tr>
</tbody>
</table>

Table 16. Force Platform Dimensions

The theoretical natural frequencies in x and y are determined from equation (145):

\[
\omega_n^x = \sqrt{\frac{2 k_{\text{spring}}}{m_{\text{plate}}}} = \sqrt{\frac{2 E_{\text{aluminum}} a t_1^3}{l^3 m_{\text{plate}}}} = 1056 \text{ rad/ sec} = 168 \text{ Hz} \tag{147}
\]

\[
\omega_n^y = \sqrt{\frac{2 k_{\text{spring}}}{m_{\text{plate}}}} = \sqrt{\frac{2 E_{\text{aluminum}} b t_2^3}{l^3 m_{\text{plate}}}} = 1031 \text{ rad/ sec} = 164 \text{ Hz} \tag{148}
\]

It is expected that the natural frequency of the steel production version will be 75% higher than that one for the aluminum prototype.

**Calibration**

Calibration of the Dynamometer requires a response to static loads and dynamic loads. The static loads must be applied on different points of the table to obtain a calibration map for the transducer sensitivities and to identify the cross-sensitivity between the three force components. Dynamic load tests are
required to identify the resonant frequencies and the magnitude response of the
dynamometer.

**Calibration Apparatus**

Kistler 9102 piezo electric compression rings are used in combination with
Kistler 5004 charge amplifiers. The transducer sensitivities selected on the
charge amplifiers are shown in Table 17.

<table>
<thead>
<tr>
<th></th>
<th>X</th>
<th>Y</th>
<th>Z</th>
</tr>
</thead>
<tbody>
<tr>
<td>pC/V</td>
<td>1.12</td>
<td>0.08</td>
<td>2.15</td>
</tr>
</tbody>
</table>

Table 17. Transducer Sensitivities

The charge amplifiers were connected to a Krohn-Hite Model 3905B
Multichannel filter which was used in the Butterworth low pass configuration.
The break frequency of this filter was set according to the experimental
requirements. The filtered load values were sent to a Tektronix 420A
Oscilloscope that was used to record the data. The post processing of the data
was performed on a 486 based PC using MATLAB.

**Static Load Calibration**

![Diagram](image)

During the static load experiments, the load was applied to the
dynamometer through a 100 lbf s-
load cell built by Interface with the
model number SSM-AJ-100. This
load cell is of the strain gauge type,
and it was connected to the scope through a Wheatstone bridge. The antialiasing filter was set to 100 Hz. A picture of this setup is shown in Figure 107.

This arrangement was utilized to apply known forces in three orthogonal directions on evenly spaced points in the dynamometer. There were eight measurement points in x and five measurement points in y. Overall this arrangement makes for forty points of measurement applications. From these measurements it was concluded that there is less than 10% cross sensitivity between the x and the y axis. It was also concluded that the force readings in x and y are independent of the point of force application to within 10%. The calibration constant for the z direction is dependent on the x position of the applied force and independent of the y position of the applied force.

![Linear fit for Fx](image1)

![Linear fit for Fy](image2)

Figure 108. Sensitivity in X

Figure 109. Sensitivity in Y

Figure 108 and Figure 109 show superpositions of applied x and y forces versus measured x and y forces. There is a good correlation between applied and measured forces, and there is very little dependence on position.
Figure 110 shows the correlation between forces in the z direction and the forces indicated by the dynamometer and demonstrates that the indicated z force is dependent on the application location of the z force. A closer inspection reveals that the measured vertical forces are mostly dependent on x. In Figure 111 the measured axial forces have been corrected with respect to the x position of the applied forces. The form of this correction will be described in more detail during the investigation of cross sensitivities.

Figure 112 shows the sensitivity of the indicated force in the x direction to forces from x, y, and z. The plot shows the sensitivities as functions of x and y position. The cross sensitivities are an order of magnitude smaller than the direct sensitivity to an x force.
The sensitivity of the indicated forces in the y direction to forces applied in x, y, and z is shown in Figure 113. Again, the sensitivity in the y direction is much greater than the cross sensitivities.

The sensitivity of the forces indicated by the dynamometer in the z direction to applied forces in all directions is shown in Figure 114. It indicates that the measured z force is dependent on the x location of the applied z force. This dependency is shown in Figure 115.
A Table Dynamometer

Sensitivity in X to Load in Z

Sensitivity in Y to Load in Z

Sensitivity in Z to Load in Z

Figure 114. Sensitivity in Z to Forces in X, Y, and Z

One would have expected that this dependency of indicated z force on the x position of the applied force is symmetrical about the middle of the dynamometer. Unfortunately, careful measurement of the spring flanges of the dynamometer determined errors in manufacture. The difference in thickness of the flange led to the problem depicted in Figure 115.

**Frequency Response**

The frequency response of the dynamometer was determined through a center line face milling operation that produced an almost pure impulse forcing function in all three cutting directions. This response was then used to obtain the transfer function of the dynamometer.
The thickness of the center line cut was 2 mm, the depth of cut was 5 mm, the cut material was 200 BHN steel, and the weight of the workpiece was 15 kg. The tool used for this operation was a single tooth cutter face mill with TPKN2204PDR inserts from Sandvik. The forces indicated by the load cells were filtered at 1000 Hz, and the forces were sampled at 9500 Hz. Thereafter transfer functions were obtained by transferring the measured responses and the theoretical forcing function into the frequency domain and dividing the two.

Figure 116. Magnitude Response with fc = 1000 Hz
Figure 116 shows a plots of the transfer functions in all three directions and a plot of the magnitude response of the uniform forcing function. The cutoff frequency of the low pass filter is far above the resonant frequency of the dynamometer; consequently, it is possible to see the resonant peaks very clearly. The dynamometer, with a 15 kg workpiece, has a first resonant peak at approximately 150 Hz in all directions. Below this frequency the magnitude response is quite flat.

The resonant frequency of the dynamometer is related to the weight of the workpiece \( m_{\text{workpiece}} \), the weight of the covering plate \( m_{\text{plate}} \), and the natural frequency of the table at no load, \( \omega_n^0 \):

\[
\omega_n^{\text{load}} = \omega_n^0 \sqrt{\frac{m_{\text{plate}}}{m_{\text{plate}} + m_{\text{workpiece}}}}
\]  

(149)

This equation can be fit to the resonant frequency of 150 Hz with the 15 kg load to yield the following expression:

\[
\omega_n^{\text{load}} \approx 200 \sqrt{\frac{19}{19 + m_{\text{workpiece}}}}
\]

(150)

Equation (150) has been validated with hammer tests using no load and a 25 kg workpiece.
Conclusion

A new low cost table dynamometer has been introduced in this appendix. It derives its advantages from a novel mechanical design. The x and y forces are taken by separate spring members which allow the horizontal forces to be measured independently with single directional load cells. The current design of the force table has a bandwidth of approximately 200 Hz with no load. The bandwidth of the table dynamometer is expected to be improved by 75% in production versions manufactured from steel rather than aluminum. Redesigning the dimensions of the sensor would also of course improve the bandwidth. The combination of this improved low cost design with the process identification techniques form Chapter 4 will yield a good tool for practical process identification applications.

It is possible to replace the four single direction load cells with a single three component load cell that would be mounted in place of the current center load cell. This design would cost approximately the same as the current system, and it would also inherit its benefits. Most of the in-plane x and y forces would still be carried by the spring elements, and hence the center load cell is not required to carry large shear forces. Off-center axial forces should be largely independent of the location of force application, as long as the axial stiffness of the centered load cell is much larger than the vertical stiffness of the spring elements. The principal advantage of this transducer would be that the center load cell is better protected from environmental hazards than the load cells of the current design.
Appendix C

Identification of Mean Immersion Angle in the Time Domain

The mean immersion angle is defined as the average of insert entry angle and insert exit angle. This parameter is required for identifying cutting geometries in face milling operations. A frequency domain identification algorithm for this parameter is shown in Chapter 4. For completeness, an alternate time domain algorithm is presented in this Appendix.

The time domain algorithm is based on the assumption that only one tooth is in contact with the workpiece during a transient. This assumption, even though limiting, is still very useful for roughing tools with few teeth at low swept angles of cut.

The direction of the cutting force is related to the immersion angle as depicted in Figure 117. The force direction of the cutting forces in x and y is defined as the angle $\zeta$, whereas the force ratio between tangential and radial forces is given by the angle $\zeta$.

It is assumed that this angle $\zeta$ is constant for all cutting conditions, a circumstance which requires that the face forces on the tool dominate the edge
forces. The immersion angle itself is the sum of the direction of the cutting forces and the angle described by the force ratio:

\[ \tan \xi = \frac{F_y}{F_x} \quad \text{and} \quad \tan \zeta = \frac{F_r}{F_t} = \frac{J}{L} \quad \phi = \xi + \zeta \]  \quad (151)

A typical force trace of a single tooth in contact is shown in Figure 118. At time \( t_1 \) the cutting forces increase past the edge force level, and the cutting starts. The end of the cut is at time \( t_2 \), and it is at the point where the forces drop past the edge force level again. Since the cutter is rotating at approximately constant speed (RPM) the swept angle of cut, \( \phi_s \), is related to the time difference between \( t_1 \) and \( t_2 \):

\[ \phi_s = \frac{\text{RPM} \cdot 2\pi}{60} (t_2 - t_1) \]  \quad (152)

The force direction at the mid-time between \( t_1 \) and \( t_2 \) is the mean immersion angle, and it can be used to extrapolate to the actual tool entry and exit angles from the swept angle of cut:

\[ \phi_m = \phi_1 + \frac{\phi_s}{2} + \zeta = \tan^{-1} \left( \frac{F_y|_{t_1} + F_y|_{t_2}}{2} \right) + \zeta \]

\[ \phi_1 = \phi_m - \frac{\phi_s}{2} \]

\[ \phi_2 = \phi_m + \frac{\phi_s}{2} \]  \quad (153)
Appendix D

A Natural Language Interface for IPAC

The geometry definition of the workpiece in IPAC utilizes language constructs that are taken from feature definitions that would be used by machinists to describe the part. Such a convention leads to a portrait that starts with a stock part and describes features that are machined during the manufacturing process.

Stock parts can have many different shapes, depending on what type of operations have been performed on them before they reach the current machine. For simplicity, IPAC assumes that the stock part is originally a rectangular block. This block can then be modified with features such as surfaces, shoulders, and slots. The boundaries of these features are defined by surfaces. The stock part is subdivided into elemental volume elements which are defined by the same surfaces. Figure 119 shows an example of a simple part (PINNACLE). It has a right shoulder, a slot from the left to the right, and a top surface. PINNACLE has eighteen elemental volume elements. The faces of the volume elements are parallel to the x, y, and z planes. In the x direction the volume elements are bounded by planes SX0, SX1, and SX2. In a like manner planes SY0, SY1, SY2, and SY3 define the y-dimensions of PINNACLE, while SZ0, SZ1, SZ2, and SZ3 define the z dimensions of PINNACLE.
When a new part is defined, the number of volume elements in the three orthogonal directions must be specified because they determine how much memory will be allocated for the part. PINNACLE has two volume elements in x and three volume elements in y and z. Thus, PINNACLE is defined in the following manner through the NLI:

2 3 3 ELEMENTS_FOR_PART PINNACLE

The actual physical dimensions of the volume elements are also easily defined:

1.5 0.75 ARE_X_DIM_FOR PINNACLE

1.5 0.75 1.5 ARE_Y_DIM_FOR PINNACLE

0.1 0.2 0.1 ARE_Z_DIM_FOR PINNACLE

Up to this point, the geometry of the part is nothing more than the physical dimensions of a rectangular block with a number of virtual planes associated to it. There are several methods available for modifying the geometry of the part. These methods allow the operator to describe the final geometry of the part in terms of features that could be used to machine the part. The features that are
supported by the NLI are surfaces, shoulders, slots. They are defined in terms of the bounding surfaces that were selected at the creation of the part. The top surface of PINNACLE, for example, is on the second z surface (counted from zero at Z=0). The operator would relate this information through the method TOP_SURFACE to the part PINNACLE:

\text{TOP\_SURFACE \text{ON} \ Z2 \ OF \ PINNACLE}

Similarly, the right shoulder can be defined through the NLI:

\text{RIGHT\_SHOULDER \text{ON} \ Z1 \ ALONG \ X1 \ OF \ PINNACLE}

A closed shoulder is defined in the same fashion, except that another quantifier allows the selection of which planes the shoulder is in between. This quantifier allows the operator to define the slot in terms of a shoulder if such a description is favored over a slot. Consequently the following two statements are equivalent for defining the slot of PINNACLE:

\text{LEFT\_SHOULDER \text{ON} \ Z1 \ ALONG \ X1 \ BETWEEN \ Y1 \ & \ Y2 \ OF \ PINNACLE}

or

\text{LEFT\_RIGHT\_SLOT \text{ON} \ Z1 \ OF \ PINNACLE}

Since the slot actually reaches only to the first surface in x, the operator could define the slot more accurately as follows:

\text{LEFT\_RIGHT\_SLOT \text{ON} \ Z1 \ BETWEEN \ Y0 \ & \ Y1 \ OF \ PINNACLE}

Apparently, it is possible to define the geometry of a part through many different but equivalent methods. Often one method seems more precise than another to
the person describing the part, but the process planning system does not favor any description over another.

Following is a comprehensive list of the methods that are available to describe the geometry of a part. The terms in the squared brackets are optional in the cases where the feature reaches across a complete volume:

**Surfaces**

TOP_SURFACE ON Z #Z OF PARTNAME

BOTTOM_SURFACE ON Z #Z OF PARTNAME

FRONT_SURFACE ON Y #Y OF PARTNAME

BACK_SURFACE ON Y #Y OF PARTNAME

LEFT_SURFACE ON X #X OF PARTNAME

RIGHT_SURFACE ON X #X OF PARTNAME

**Shoulders**

FRONT_SHOULDER ON Z #Z ALONG Y #Y [ BETWEEN X #X1 & #X2 ] OF PARTNAME

BACK_SHOULDER ON Z #Z ALONG Y #Y [ BETWEEN X #X1 & #X2 ] OF PARTNAME

LEFT_SHOULDER ON Z #Z ALONG Y #Y [ BETWEEN X #X1 & #X2 ] OF PARTNAME

RIGHT_SHOULDER ON Z #Z ALONG Y #Y [ BETWEEN X #X1 & #X2 ] OF PARTNAME

**Slots**

FRONT_BACK_SLOT ON Z #Z [ BETWEEN Y #Y1 & #Y2 ] OF PARTNAME

LEFT_RIGHT_SLOT ON Z #Z [ BETWEEN X #X1 & #X2 ] OF PARTNAME
Appendix E

Pictures of The Monitoring and Control System

*Force Table Without a Covering Plate*

Figure 120. Prototype Table from the Top

*Force Table With a Covering Plate*

Figure 121. Force Table with Steel Covering Plate
Force Table on a Bridgeport Milling Machine

Figure 122. Force Table on a Bridgeport Milling Machine
Holke Milling Machine

Figure 123. Holke Vertical Milling Machine
Force Table on the Holke Milling Machine
Capacitive Spindle Synchronization Pickup

Figure 125. Capacitive Spindle Rotation Pickup