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TOWARDS AUTONOMOUS COMPILATION OF TURNING FORCE DATABASES: UTILIZATION OF ON-MACHINE MEASUREMENT OF MACHINED PARTS

Zhan-Qiang Liu and Patri K. Venuvinod

Department of Manufacturing Engineering and Engineering Management, City University of Hong Kong, 81 Tat Chee Avenue, Kowloon, Hong Kong

ABSTRACT

Several hitherto unrealized advantages would follow if each shop floor machine could compile its own machining database autonomously. This paper focuses on CNC turning to develop a shop floor friendly method for determining the cutting force component normal to the machined surface as well as the lumped static stiffness parameters of the machining set up *solely* from on-machine part inspection data gathered immediately after machining. It is shown that one can also estimate the shear and chip flow angles as well as the tangential and feed forces by combining this method with a suitable predictive model of the turning operation.

INTRODUCTION

The ability to anticipate the cutting force magnitudes that are likely to result from a given cutting operation is of significant importance while selecting the machining equipment, planning the machining process, and selecting/designing the associated tooling. However, despite over half a century of scientific research conducted worldwide, the problem of cutting force prediction continues to be a major concern in machining industry. This is evident from the fact that three major industrial corporations from the USA have recently joined hands with National Institute of Standards and Technology (NIST) of USA to organize an international

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competition aimed at assessing presently available cutting force (and temperature) prediction models [1].

The traditional approach to cutting force anticipation has been through the utilization of a machining database that lists cutting force data obtained through off-line experimentation using a three-component dynamometer in a laboratory setting. This approach continues to dominate industrial practice even today. However, as indicated by the following extract from [1], there are several problems associated with this approach: ''Despite its economic and technical importance, machining remains to be poorly understood. Parameters are chosen through empirical testing and the experience of machine operators and programmers. This process is expensive and time-consuming. Furthermore, while large empirical databases have been compiled [2] to aid in process design, these databases lose relevance as new materials, machines, and workpiece materials are developed.''

A vast number of CNC machines (even if we confine ourselves to turning centers) are in use across the world. Depending on the specific portfolio of parts encountered by the machine shop where it is located, each machine tool experiences a large variety of input states that, in general, can be quite different from those experienced by other machines. The resulting combinatorial explosion means that, whatever the size of the traditional machining database, it would be found to be too sparse to result in generally reliable prediction. The laboratories compiling the databases would have to keep working incessantly merely to catch up. Further, the compilation procedures associated with traditional machining databases tend to be quite expensive and time-consuming since they are performed in laboratory settings and require significant human involvement and expertise. In addition, even if a database of substantial size is produced, only a tiny part of it is utilized in any given process planning exercise. Lastly, there might not be a close enough match between the process states experienced during the compilation and utilization stages of the databases. Mismatches could occur because of differences in machine dynamics, unexpected inclusions in the work material, differences in the chip breaking features of the cutting tool, etc. All this means that, quite frequently, predictions based on machining databases compiled at remote laboratory settings turn out to be of dubious accuracy despite the high expense involved. For instance, in the data reported in [1], there was up to a 50% variation in the cutting force magnitudes measured by four different laboratories despite the fact that great care had been taken to ensure that every laboratory applied the same input conditions.

Viewed essentially from a technical viewpoint, the problem of equipping a machine with a force-measuring device could appear to be trivial. Several three-component cutting force dynamometers have been commercially available for a long time. More recently, sensor integrated tools capable of cutting force monitoring have been developed [3]. One could fit each shop floor machine with such a dynamometer. Alternatively, machine tool builders themselves could supply machine tools with integrated force measuring elements. However, although such devices have been available for a long time, very few shop floors have pursued this strategy. For instance, none of the tens of thousands of the computer numerical control (CNC) machines used in and around Hong Kong seems to be equipped with a dynamometer or force measuring elements. We need strategies that are acceptable in sophisticated (e.g., aerospace) as well as modest shop floor scenarios.

One way of mitigating the problems associated with traditional machining databases is to equip each machine on the shop floor with an automated and autonomous capability to measure the cutting forces actually experienced while machining the normally scheduled parts. The machine could then enter the resulting mapping between the input vector (type of operation, tool-work material combination, combination of cutting conditions, etc.) and the measured cutting forces into its *own* machining database. Initially, such a database would be empty. When the first scheduled part arrives, it will perform force measurement and record the input conditions and the force measurement results. When the machine encounters the next machining operation, it will first examine whether the new input vector is significantly different from any of the previously encountered input vectors and then decide whether or not to add a new record to its database. Thus, in time, the machine would have developed a machining database that, while being smaller, is more pertinent to its own shop floor experiences than the traditional database compiled through remote experimentation.

Several conditions need to be satisfied if the above distributed database strategy were to succeed in practice. First, the force measurement system should be shop floor friendly. It should not be overly expensive. Its shop floor level assimilation should not require excessive manual intervention or expertise. In other words, the system should be easy to automate. Finally, the system should not overly divert the machine from productive work.

Motivated by the above considerations, the authors' team at City University of Hong Kong has been investigating the applicability of several cutting force estimation techniques other than traditional dynamometry. In particular, two techniques have been explored so far. The first of these utilizes the empirically observed correlation between each actual cutting force component and the corresponding axis-motor current signal output by a specially fitted but inexpensive set of Hall effect transducers. This method has produced encouraging results with respect to turning operations (including contour turning) performed on CNC turning centers equipped with ac drives [4]. However, the method has been found to be applicable only when the desired cutting force component is *active*, i.e., the force component can be resolved into sub-components each of which acts along (or, in the case of torque measurement, about) a machine axis. In the case turning, the tangential and feed cutting forces, F_y and F_z respectively, are active force components. Hence they are measurable by motor current. In contrast, the *passive* force component (i.e., the quasi-static force component normal to the machining surface), F_x, needs to be measured by some other technique.

Let us now examine the response of a machine's structure to a quasi-static cutting force component F_i. Owing to the elastic nature of the structure, there would be a relative displacement (say δ_{fi}) between the tool tip and the machined surface in a direction normal to the machined surface. Several elements in the machine-fixture-workpiece-tool (MFWT) structural loop would have contributed to this relative displacement. Suppose that one of these elements is element j and we are able to reliably compute the stiffness, k_{ij}, of this element. Suppose further that we are able to empirically determine the contribution, δ_{fij}, of element j to δ_{fj}. In such a case, we would be able to estimate F_i easily from the relationship $F_i = k_{ij}\delta_{fij}$.

In the following, we will describe a part inspection protocol that, in principle, is capable of yielding δ_f and k_x *solely* from dimensional inspection of machined parts according to a specified inspection protocol. Next, we will examine data obtained from cylindrical turning experiments with a view to assessing the force estimation accuracy of the proposed approach. This will be followed by a discussion of some important issues concerning shop floor level implementation of the proposed approach. Finally, we will discuss how our prior knowledge of F_x could be leveraged in the model-based estimation of the active cutting force components (these cannot be determined from part inspection results alone). We will also show that the injection of F_x into the predictive process model enables the determination of the shear angle that, otherwise, would have required the length or thickness of each sampled chip to be measured.

AN INSPECTION PROTOCOL FOR IDENTIFYING THE ELASTIC DEFLECTION, δ_f, NORMAL TO THE MACHINED SURFACE

We will now describe a machined part inspection protocol that enables the estimation of δ_f. Originally, the protocol was developed by the present authors for the purpose of developing a software-based method of dimensional error compensation for turned parts [5]. In contrast, the present paper uses it for the on-line estimation of F_x. The protocol will be illustrated in the context of cylindrical turning on a CNC turning center. The radial direction is represented by axis X whereas the longitudinal feed direction is represented by axis Z. Thus, axis Y is the cutting speed direction. It will be assumed that the tool has been properly centered and that the machine is capable of performing on-machine measurement (OMM).

Workpiece dimensional errors in machining have been subject to intense research for a long time (see [6] for a review of the geometric aspects). Much of this work has been inspired by a desire to design more accurate machines or to compensate the errors by appropriately modifying the CNC program. However, this information provides significant clues regarding how we may proceed to estimate cutting forces from workpiece measurements.

Conventional CNC programming tacitly assumes that the machine, machine set up, and the machining operation perform in a perfect manner, i.e., when we program for a desired dimension (a diameter in turning), D_{des}, we get exactly that. However, this is never achieved in practice. There always is a deviation (error), δ_{tot}, between the dimension actually obtained after machining and D_{des}. For instance, the authors found in their study of a CNC turning center that this deviation could be as large as 100 μm although the positioning repeatability of the machine axes was of the order of 4 μm.

Traditionally, workpiece dimensional inspection has been performed using a suitable measuring instrument such as a coordinate measuring machine (CMM). This measuring strategy may be called post process measurement (PPM) since it is performed *after* the machining process has been completed and the workpiece has been removed from the machine. Let D_{pp} be the dimension corresponding to D_{des} as obtained by PPM. Then,

$$\delta_{tot} = D_{pp} - D_{des} \tag{1}$$

A review of literature indicates that δ_{tot} can be expressed as the sum of errors arising from several quasi-static systematic effects:

$$\delta_{tot} = \delta_g + \delta_{th} + \delta_f + \delta_{other} \tag{2}$$

where δ_g is the error arising from the geometric errors inherent in the machine tool, δ_{th} is the error arising from the thermally induced distortions of elements in the MFWT structural loop, δ_f is the error arising from the static deflections of the MFWT system under the cutting force, and δ_{other} is the sum of the errors arising from other causes such as the workpiece clamping force, tool wear, etc. Note that all the above errors are defined as relative displacements between the tool tip and the work surface in a direction normal to the machined surface at the point of interest along the tool path.

In recent years, following the development of Renishaw touch-trigger probe, there has been much interest in on-machine measurement (OMM) [7]. Today, many CNC shop floors regularly perform OMM using touch-trigger probes notwithstanding the fact that these probes are quite delicate and expensive. OMM has however received a further boost with the subsequent development of the Fine-Touch probe [8] that enables the cutting tool itself to be used as the touch-probe while ensuring measurement accuracy of the order of 1 μm [9]. This method of probing can be easily implemented on CNC turning centers using the technique described in [10].

Mou and Liu have examined the differences between the meanings of measurement results from PPM and OMM [11,12] and demonstrated that that the ‘‘difference between CMM [D_{pp}] measurement and on-machine measurement [D_{om}] is positioning error’’ of the machine. They also demonstrated that the difference is equal to ($\delta_g + \delta_{th'}$) where $\delta_{th'}$ is the thermal error associated with the particular thermal state of the machine during OMM. Mou and Liu had made these observations while measuring specially designed artifacts. The present authors suggest that one can take the machined part itself to be an artifact and, hence, these observations are also applicable in the context OMM of machined parts.

Now suppose that we conduct the OMM *immediately* after the part has been machined, i.e., such that the time lag between the machining and OMM phases is equal to zero. In such a case, we can expect the thermal error associated with the OMM phase to be exactly equal to that associated with the machining phase, i.e., $\delta_{th'} = \delta_{th}$. This is because the relative motion between the tool and the workpiece is subjected to the same positioning error whether one is performing cutting or OMM. However, in practice, there will always be a finite time lag between the machining and OMM phases so that there would be an error associated with the assumption that $\delta_{\mathrm{th}'} = \delta_{th}$. But, fortunately, there is evidence to show that the time constants associated with thermal deformations of machine tools are quite large in magnitude [13]. Hence, provided that the time lag between the OMM and machining phases is kept small compared to the time constant associated with the thermal deformations of the machine, it can be assumed that

$$\delta_g + \delta_{th} = D_{PP} - D_{omw} \tag{3}$$

where D_{omw} is the part dimension determined by OMM immediately after the part has been machined.

Combining equations (1-3),

$$\begin{aligned}\delta_f &= \delta_{tot} - (\delta_g + \delta_{th}) - \delta_{other} = (D_{pp} - D_{des}) - (D_{pp} - D_{omw}) - \delta_{other} \\ &= D_{omw} - D_{des} - \delta_{other}\end{aligned} \tag{4a}$$

$$\rightarrow D_{omw} - D_{des} \text{ (when } \delta_{other} \rightarrow 0) \tag{4b}$$

As a first approximation, equation (4b) will be adopted in the rest of the paper.

Equation (4b) suggests that only one on-machine measurement (D_{omw}) performed immediately after the machining operation suffices to determine δ_f at a given location on the machined surface in a given machining operation.

RELATING δ_f TO THE CUTTING FORCE

Since machine tools are usually designed to undergo only elastic deflections under the action of cutting force, it should be possible to obtain some information concerning the cutting force from δ_f if the magnitude of the effective stiffness of the MFWT structural system of the machine is known.

Now, let F_x, F_y, and F_z be the cutting force components in directions X, Y, and Z respectively. In principle, each of these force components can contribute to δ_f so that

$$\begin{aligned}\delta_f &= \delta_{fx} + \delta_{fy} + \delta_{fz} \\ &= 2(F_x/k_x) + 2(F_y/k_y) + 2(F_z/k_z)\end{aligned} \tag{5a}$$

$$\rightarrow 2F_x/k_x \text{ (if } k_y \rightarrow \infty \text{ and } k_z \rightarrow \infty) \tag{5b}$$

where δ_{fi} is the signed contribution to δ_f of the cutting force component in direction i, and k_i is the magnitude of the force component in direction i required to cause unit magnitude of tool-work displacement in direction X.

In general, the effectiveness stiffness terms k_x, k_y, and k_z, will depend, *inter alia*, upon the specific location where δ_f has been determined and the particular machining setup (workpiece dimensions, the specific work and tool holding set up used, etc.). These conditions will be different in different cutting situations, albeit on the same machine.

The traditional approach to determining the MFWT stiffness distributions (k_x, k_y, and k_z) involves subjecting the machine to different loading conditions (using load cells) and then measuring the resulting deflection distributions (using dial gauges, etc.). Such a procedure is clearly too cumbersome for routine shop floor use. Further, it is not in keeping with our present goal of being able to determine cutting forces *solely* from dimensional measurements of machined parts.

We now propose an approach that seems to hold promise at least in the case of turning machines. Note that k_x is a *direct* stiffness term (since, by definition, the displacement is measured in the same direction as that of the force) whereas k_y and k_z are *cross* stiffness terms (displacement is measured in a direction orthogonal to the direction of the force). Hence we can expect the magnitudes of k_y and k_z to be much larger than that of k_x. Therefore it is reasonable to adopt equation (5b) as a first approximation.

A SIMPLE MODEL FOR ESTIMATING THE RADIAL STIFFNESS OF A TURNING SET UP

Equation (5b) may be utilized to develop simple analytical models for estimating the stiffness values of the common range of machining set ups. The method described below is applicable to turning a workpiece chucked at one end, which is the most common work-holding set up (see Figure 1). k_x can be expressed as follows in turning a workpiece with regard to such a setup:

$$1/k_x = 1/k_{t,x} + 1/k_{wp,x} + 1/k_{sp,x} \tag{6}$$

where $k_{t,x}$ is the overall direct stiffness of the tool and the structure supporting it in direction X, $k_{wp,x}$ is the stiffness of the workpiece on its own subject to the specific work-holding conditions, and $k_{sp,x}$ is the overall stiffness of the chuck/spindle assembly including the headstock-side structure. Note that each of these stiffness terms should be interpreted as the magnitude of F_x needed to obtain unit deflection in direction X. Further, $k_{t,x}$ is usually constant for a given machining setup.

However, $k_{wp,x}$ varies continuously as the cutting tool traverses the cutting path. The distribution of $k_{wp,x}$ is mainly a function of the work-holding setup, the instantaneous geometry of the workpiece, and the elastic modulus of the work material. The elastic modulus of the work material is easily and reliably obtained from material data handbooks. The instantaneous workpiece geometry is a little more involved since the workpiece shapes encountered by CNC turning centers can be quite complex with external and internal profiles, grooves, etc. However, whatever the workpiece shape, it is straightforward to estimate the workpiece stiffness by applying well-known principles of the Theory of Elasticity. The authors have developed a finite difference program incorporating such principles. It has been found that the error associated with the program is smaller than 1% even when applied to a workpiece with complex geometry.

Just as $k_{wp,x}$, $k_{sp,x}$ also varies continuously as the cutting tool traverses the cutting path. A review of literature indicates that the distribution of $k_{sp,x}$ can be estimated, in principle, from a finite element analysis (FEA). However, FEA could be perceived to be too complex for routine shop floor use. Further, in FEA, it is difficult to account for the contact deflections occurring at the various mating faces in a given machine tool assembly.

In early literature from the former USSR, there were references to the fact that, at least in the case of some sub-assemblies within a machine tool structure, the sub-assembly so behaves under elastic loading as to appear to rotate rigidly about a remotely located but fixed center (see Figure 2). Murthy and Venuvinod had demonstrated in 1969 that this observation is true with respect to the spindle-headstock sub-assembly of lathes [14]. This observation (which somehow has rarely been exploited despite its inherent simplicity) is used in the present work while modeling $k_{sp,x}$ for different work holding configurations on a CNC turning center. In particular, for a workpiece chucked at one end with the other end free,

$$k_{sp,x} = K_{csh}/(R + L - z)^2 \tag{7}$$

where z and L are the instantaneous axial distances between the free end of the workpiece and the current position of the tool tip P and the chuck face respectively, R is the axial distance between the chuck face and the plane normal to spindle axis that contains the rotation center (see Figure 1), and K_{csh} is the rotational stiffness (Nm per radian) of the chuck-spindle-headstock assembly about the rotation center.

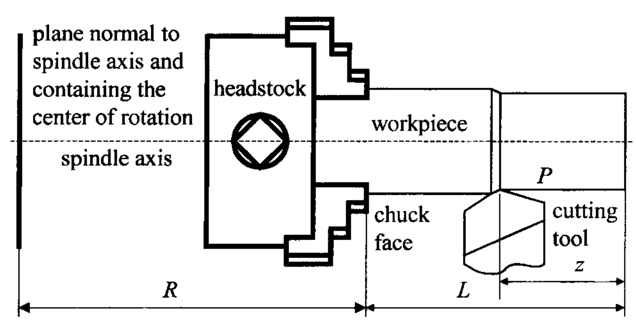


Figure 1. Workpiece set up and rotation center of chuck/headstock assembly in cylindrical turning.

Combining equations (4b), (5b), (6) and (7), it can be shown that

$$F_x = \frac{D_{omw} - D_{des}}{2\left\{\dfrac{1}{k_{t,x}} + \dfrac{1}{k_{wp,x}} + \dfrac{(R + L - z)^2}{K_{csh}}\right\}} \tag{8}$$

Equation (8) consists of nine parameters: F_x, D_{omw}, D_{des}, $k_{t,x}$, $k_{wp,x}$, R, L, z, and K_{csh}. Of these, the magnitudes of D_{des}, L, and z are easily extracted from the CNC part program whereas $k_{wp,x}$ can be determined by using the finite difference program for workpiece deflections referred to earlier. The remaining four parameters (F_x, $k_{t,x}$, R, and K_{csh}) can be determined by performing four on-warm-machine-measurements (D_{omw}) for each machining set up at four different locations on the same part (it could be *any* part encountered during normal shop floor operation of the machine) machined at constant cutting conditions (so that the magnitude of F_x can be expected to be the same) and solving the resulting simultaneous equations obtained by applying equation (8). However, as with any measurement procedure, the measurements may be replicated on several such parts in order to obtain statistically significant estimates.

While determining F_x from warm-OMM, we are in effect using the machine's structural loop as the elastic element of a dynamometer. As with a classical dynamometer, we sense the deflection of this elastic element under load. Different types of dynamometers utilize different sensing strategies (strain gages, piezoelectric transducers, etc.). In the present case, we are sensing it through variations in warm-OMM data. In classical dynamometry, calibration is performed through external loads applied through a load cell. In our case, we achieve calibration by identifying the magnitude of workpiece deflection under the unknown load and multiplying by the reliably computable stiffness, $k_{wp,x}$, of the workpiece on its own. In effect, we are using the machine tool itself as a single component cutting force dynamometer.

EXPERIMENTAL VALIDATION OF WARM-OMM BASED ESTIMATES OF F_x

Dry cylindrical turning tests were conducted on a commercially available CNC horizontal turning center equipped with a 'Q-setter [10]' and a six-tool turret. Cylindrical workpieces of different sizes (diameter: 30–75 mm, and length: 90–200 mm) were chucked at one end with the other end free. Two commercial types of carbide tool-inserts (DNMG156004QM, and DNMG15608-QM) with and without chip formers were used to turn aluminum alloy and mild steel workpieces over cutting speed range of 2.5 to 6 m/s. The feed rate and depth of cut were kept small (0.1 mm/rev and 0.5 mm respectively) so as to check whether equation (8) was effective even at low cutting force levels.

So as to determine D_{omw} values, the machined parts were subjected to warm-OMM using the combination of Fine Touch sensing and Q-setter [10] at a few points along the part profile immediately after machining the part. The procedure was repeated a few times on the same part so as to yield obtain statistically reliable estimates of F_x, R, $k_{t,x}$, and K_{csh}.

In order to verify the force estimates obtained from the OMM exercises, a large subset of the cutting operations was replicated, but this time measuring the cutting force components F_x, F_y, and F_z using a piezoelectric dynamometer. Figure 2 compares the F_x estimates so obtained with the corresponding estimates obtained from OMM exercises.

An ideal 'dynamometer' should exhibit a calibration curve that is repeatable and has its output proportional to the input. Repeatability may be assessed by means of the degree of closeness of the coefficient of determination, r^2, to 1. Proportionality may be assessed by the closeness of the slope of the regression line to 1 and that of the intercept to 0.

On the above basis, the calibration curve in Figure 2 seems to exhibit a fair degree of proportionality. However, the r^2 value ($= 0.914$) does not seems to be that good (one also notices quite a visible scatter in Figure 2). Further, a statistical analysis of the data underlying Figure 2 showed that the *sum of squares of deviations* (SS) was equal to $4850N^2$ and the largest prediction interval about the regression line for individual estimates over the data range was ± 20.6N. Could this error be due to the variability of the warm-OMM technique used for force estimation? Or, could it be due to the variability inherent in the cutting process? (We, of course, assume that the piezoelectric dynamometer used as the reference in establishing the calibration curve is 'perfect').

With a view to obtaining a broad estimate of the variation in F_x attributable to the inherent variability of the machining process, we conducted a separate set of cylindrical turning tests under input conditions that were approximately midway between the range of input conditions utilized while arriving at Figure 2. This experiment yielded an SS value of $3740N^2$ and a 95% prediction interval for indi-

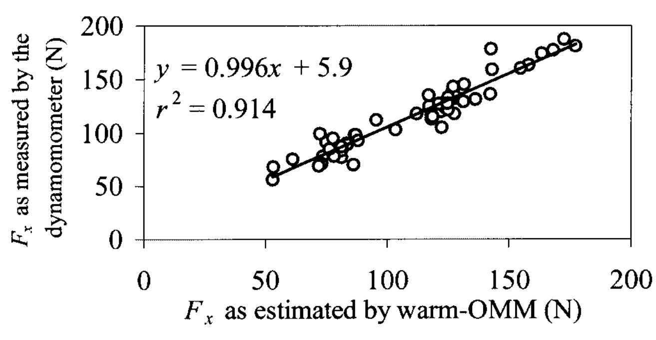


Figure 2. Comparison between F_x estimates from OMM and the piezoelectric dynamometer (work material: mild steel).

vidual estimates equal to ±11.4N. Since, this set of experiments had involved only dynamometer measurements, the interval should be interpreted as arising due to the variability in the dynamometer measurement technology plus that of the machining process itself. However, since the dynamometer could be assumed to be 'perfect', all this variability could be attributed to the machining process itself. This suggested that the SS associated with the warm-OMM technique was of the order of $1110N^2$. On this basis, the largest 95% prediction interval associated with the warm-OMM technique itself over the data range covered was found to be ±9.8N. This level of performance of warm-OMM is unexpected in the light of the various simplifying assumptions made and the positioning errors inherent in the machine. For instance, experience has shown that the positioning error of our machine under 'cool' state could vary between 0 and 4 μm. Given the stiffness figures in Table 1, this error can translate into an error larger than 10N in the estimated force. Clearly, further research is needed to clarify the error sources affecting warm-OMM based estimation of F_x.

Table 1 shows the mean magnitudes and standard deviations associated with the estimates of $k_{t,x}$, R, and K_{csh} as obtained *solely* from on-warm-machine-measurements of machined parts. Note that the standard deviation for each of the three set up parameters is quite small in comparison to the corresponding mean estimate.

Several independent tests were conducted to verify the estimates of the mean magnitudes of $k_{t,x}$, R, and K_{csh} determined through the warm-OMM exercises. The tests involved loading of chucked workpieces of large diameter (so that the workpiece deflections could be ignored) through a load cell mounted on the tool turret. The resulting relative displacement in the radial direction between the work surface (at the work section where the load was applied) and the tool was measured with the help of a dial gage. The procedure was repeated several times at several locations along the workpiece axis so as to obtain statistically significant results. The test data thus obtained were processed using equation (8): taking F_x to be equal to the load cell reading, ($D_{omw} - \mathrm{D}_{des}$) equal to the deflection noted by the dial gauge, and $k_{wp,x}$ equal to ∞. The resulting estimates of $k_{t,x}$, R, and K_{csh} were compared with the corresponding estimates from OMM data using the Student-t test. The results are shown in Table 1. Note the agreement between the OMM estimates of the three set up parameters and the corresponding load cell-based

Table 1. Comparison Between the Estimates of Stiffness Parameters $k_{t,x}$, R, and K_{csh} Obtained from Warm-OMM Data and Load Cell Measurements

	Estimates from Warm-OMM		Estimates from Load Cell		Confidence (t-test)
	Mean	Std. Dev.	Mean	Std. Dev.	
$k_t \times 10^7$(N/m)	1.771	0.056	1.799	0.031	91.2%
$K_{csh} \times 10^{11}$(Nm/rad.)	5.878	0.039	5.867	0.030	97.6%
R(m)	0.191	0.0098	0.203	0.012	97.3%

estimates. In particular, the confidence levels with regard to R and K_{csh} are over 97%. The confidence level associated with $k_{t,x}$, while being acceptable at around 91%, is however smaller. Further investigation is needed to improve this performance feature.

SOME SHOP FLOOR-LEVEL IMPLEMENTATION ISSUES

Conventional probing using a touch trigger probe does not appear to be capable of achieving the desired 'immediacy' of probing. This problem may be solved by utilizing the recently developed 'Fine Touch' probe [8–10] that enables the cutting tool itself to act as the contact probe. Thus, one can save the time lost in replacing the tool with the conventional touch trigger probe. Further, the thermal energy embedded in the tool is not lost owing to its replacement by a relatively 'cool' touch trigger probe. Without these features the proposed warm-OMM technique would not succeed.

The application of the warm-OMM technique does not require any sophisticated devices (such as a cutting force dynamometer or force sensing elements) to be incorporated into the machine's structural loop. The only requirement is that 'Fine Touch' probing is used rather than the traditional touch trigger probe. Note that, since 'Fine Touch' utilizes the cutting tool itself as the contact probe, all we need is a relatively simple and inexpensive (compared to a touch trigger probe) signal-processing unit. The data-processing procedures themselves are easily implemented in software. Once a machine is interfaced with the 'Fine Touch' signal processing unit and the warm-OMM inspection software has been installed, the machine can proceed to collect and process the required OMM data autonomously according to a strategically determined schedule and store the results in the machine's own machining database. The machine needs to conduct OMM only when the input vector extracted from the CNC program for the next part does not match any of the vectors already stored in its machining database.

In addition to F_x, the warm-OMM technique yields useful information concerning the static stiffness of the MFWT loop of the *specific* machining set up. In particular, it identifies the static stiffness of the spindle head (in terms of R and K_{csh}) and the tool-side structure (expressed as k_t). This information is implicit in the warm-OMM data thus obviating the need for cumbersome structural tests on each shop-floor machine. This information should be particularly useful while choosing machining setups for jobs with different accuracy demands.

The warm-OMM exercise described in this paper is but one stage in the software based error compensation technique described in [5] where, in addition, one had to perform part inspection using cool-OMM (i.e., on machine measurement conducted after letting the machine set up to cool down) and post process inspection using an instrument such as a coordinate measuring machine (CMM). The error compensation strategy was shown to be capable of reducing machining errors in cylindrical/contour turning from a range of 20–60 μm to about 5 μm at every point along the part contour [5]. This paper highlights the fact that the adoption of such a compensation strategy implicitly provides the capability to build a database of thrust force, F_x. In addition, it provides useful information concerning the static stiffness of the machining set up. This might be sufficient motivation for some shop floors to embrace the technique. For others, it might not because the method misses out information on the other two force components, F_y and F_z.

In [4], an on-line sensing method for estimating F_y, and F_z on the basis axis motor currents was shown to be quite effective in turning. Current signals were collected simply by slipping inexpensive Hall effect transducers around the input lines of ac motors. However, the method has been found to be applicable only when the force vector is *not* normal to the relative velocity between the tool and the workpiece. In other words, the method could measure the active force components, F_y and F_z, but not the passive force component F_x. In short, warm-OMM technique of cutting force measurement complements the motor-current based technique.

PRIOR KNOWLEDGE OF F_x CAN SIMPLIFY MODEL-BASED PREDICTION OF CUTTING FORCES

Another method available for building a turning force database that includes information on all the three cutting force components is to utilize a predictive model derived from insights related to the physics of the machining operation. The importance of predictive models was highlighted as follows in [1]: ‘‘The advantage of this approach is that predictions are made from the basic physical properties of the tool and workpiece materials together with the kinematic and dynamics of the process. Thus, after the physical data [are] determined, the effect of changes in cutting conditions (e.g., tool geometry, cutting parameters, etc.) on industrially relevant decision criteria (e.g., wear rate, geometric conformance, surface quality, etc. [and cutting forces]) can be predicted without the need for new experiments. If robust predictive models can be developed, this approach can substantially reduce the cost of gathering empirical data and would provide a platform for *a priori* optimization of machining process parameters based upon the physics of the system.’’ The discussion in [1] continued to identify a number of difficulties associated with predictive models.

However, in the present authors’ opinion, two crucial difficulties associated with almost all analytical models were not highlighted with sufficient emphasis in [1]. One of these difficulties was discussed in [15] and both arise from the fact that almost every existing model of machining operation seems to require a previously compiled database of certain model parameters.

The first difficulty arises because there could be a mismatch between the process state associated with a specific model parameter record being applied to assess a given shop floor operation and that actually existing during the shop floor operation. For instance, the model might have assumed that the tool rake face was flat whereas the actual shop floor tool is ‘grooved’. Now suppose that we have sensed the magnitude of F_x resulting from this ‘grooved’ tool. This information should be useful in correcting (implicitly or explicitly) the idealized predictive model to better match the actual shop floor operation.

The second issue concerns the need for physical measurement of the chip length (or the chip thickness) associated with each input vector listed in most model-based machining databases. Consider, as an example, Armarego’s predictive (analytical) model of turning forces [16]. This model first geometrically transforms the input vector of the given turning operation into that of a generalized single edge orthogonal cutting operation, e.g., a tube turning operation with a single cutting edge. Usually, the generalized operation will turn out to be oblique, i.e., the generalized single edge is not orthogonal to the cutting speed vector. Armarego next predicts the cutting force magnitudes using his single edge oblique cutting model that, *inter alia*, assumes that chip formation is continuous and without built-up-edge, the rake face is plane, the shear zone is thin, and the principle of force-velocity collinearity holds at the shear and rake planes. (Actually, at this stage, one may choose a different single edge oblique model, e.g., Venuvinod and Jin’s model [17].) This stage requires the support of a previously compiled single edge orthogonal cutting database that includes information on a limited but essential set of model parameters. The estimated forces are then geometrically transformed to yield the predicted magnitudes of F_x, F_y, and F_z in the turning operation.

Irrespective of the single edge oblique cutting model utilized while implementing the above turning force prediction model, one needs to go through the tedious process of building a database of the model parameters of a sufficiently large size. For instance, in the case of [17], one needs a database of the following parameters: s = shear flow stress of work material stock, C = a work material dependent constant, ϕ_{nG} = normal shear angle, η_{cG} = chip flow angle, and K_{1P} and K_{1Q} = the ‘edge force’ components (per unit width) that, respectively, are parallel to the cutting speed vector and normal to the machining surface respectively. A similar database is required if we choose to implement the procedure on the basis of Armarego’s single edge oblique cutting model [16] except that (i) instead of $\{s, C\}$ one needs information on the mean shear stress, τ_s, within the assumed thin shear zone, and (ii) an internal constraint within the model enables the determination of η_{cG}. The following discussion refers to the implementation of Venuvinod and Jin’s model [17].

Consider now the difficulty (in principle) of automating the process of empirically determining the model parameters. From this point of view, the parameter set associated with [17] can be divided into three subsets. K_{1P} and K_{1Q} pose the least difficulty since they are derivable from the input conditions and force magnitudes (we assume that the machine has a force measuring capability). The second subset consists of s and C. It is known that the magnitudes of these two parameters depend only on the work material. Traditionally these have been determined through measurement of chip dimensions and cutting forces [17]. However, it should be possible in principle to relate s and C to certain basic material properties (e.g., hardness, ductility, work hardening index, etc.) of the work material. For the moment, we will look ahead to the time when this problem has been satisfactorily solved through further research. The final subset consists of ϕ_{nG} and η_{cg} that have traditionally been determined empirically through dimensional measurements of the resulting chip [16,17]—a process that is very difficult to automate.

A question relevant to the present paper is how best we could take advantage of our prior knowledge of F_x to reduce the tedium involved in the compilation of databases of model parameters and/or facilitate the automation of the compilation processes. To address this question, we implemented the turning force prediction model on the basis of the predictive modeling strategy suggested by Armarego [16]. We also implemented a second (and hitherto untested) version by replacing Armarego's single edge oblique model with that of Venuvinod and Jin [17]. A summary of the latter version is given in the Appendix.

A study of the procedure described in Appendix shows that it is indeed capable of yielding the magnitudes of ϕ_{nG} and η_{cG} when $\lambda_{sG} \neq 0$ *solely* from a knowledge of the input conditions, K_{1P}, K_{1Q}, and the magnitudes of (s, C)—the same holds true with regard to the implementation of Armarego's procedure [16] except that one has to use τ_s instead of (s, C). Finally, F_y and F_z can be determined by substituting the resulting magnitudes of ϕ_{nG} and η_{cG} into the appropriate force equations.

The above finding is particularly fortuitous because ϕ_{nG} is the one parameter that, while having a large impact on the predicted cutting force magnitudes, could vary significantly from one machining operation to another. Further, its measurement is tedious and difficult to automate. This observation confirms the assertion in [18] that several problems associated with predictive modeling of machining operations can be resolved by augmenting the models with information derived from appropriate on-line sensing.

With a view to experimentally verifying the above implementations of predictive models, we conducted a series of tube turning experiments using single edge orthogonal cutting tools and input vectors similar to those used while conducting the warm-OMM exercises for verifying F_x estimates (described in a previous section). The cutting force components were measured using a piezoelectric dynamometer. We also measured the chip dimensions according to the method described in [17] so as to be able to verify the predicted magnitudes of ϕ_{nG} and η_{cG}. The experimental data were processed using the analysis presented in [17] to determine the magnitudes of the model parameters. Using this information and the warm-OMM based estimate of F_x, we finally obtained the predicted values of F_y and F_z.

Figure 3 illustrates the observed agreement between the normal shear angle values estimated from warm-OMM based F_x and the corresponding values (obtained from chip measurements) derived from the single edge orthogonal cutting database. A similar degree of agreement was found with regard to η_{cG}. Figures 4 and 5 respectively show the agreement between the estimates of tangential and feed force components derived on the basis of the warm-OMM based radial force

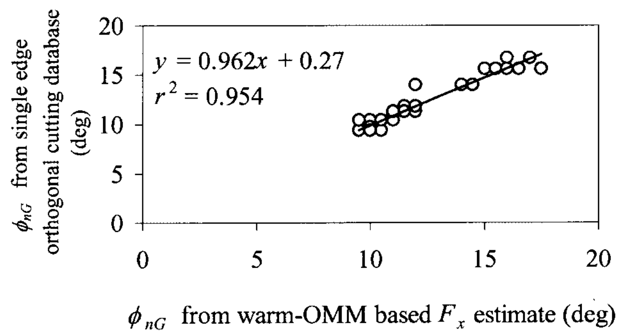


Figure 3. Comparison between shear angle magnitudes estimated through predictive modeling augmented by prior knowledge of F_x and the corresponding estimates from the single edge orthogonal cutting database (work material: aluminum alloy, $s = 70.9$MPa, $C = 0.82$).

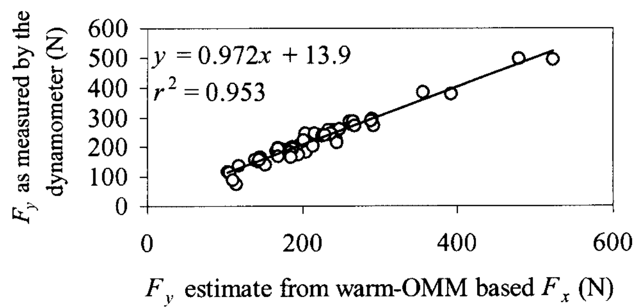


Figure 4. Comparison between F_y values estimated through predictive modeling augmented by prior knowledge of F_x and the corresponding values indicated by the piezoelectric dynamometer (work material: aluminum alloy, $s = 70.9$MPa, $C = 0.82$).

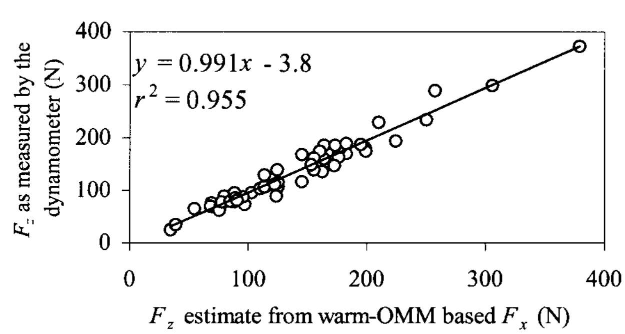


Figure 5. Comparison between F_z values estimated through predictive modeling augmented by prior knowledge of F_x and the corresponding values indicated by the piezoelectric dynamometer (work material: aluminum alloy, $s = 70.9$MPa, $C = 0.82$).

and the corresponding actual force values as measured by the dynamometer. The observed degree of agreement between the theoretical and measured values lends support to our model-based expectation that it should be possible to avoid the need for estimating the shear angle and the chip flow angle through chip measurements by taking advantage of our prior knowledge of the radial force. This is an advantage arising from the warm-OMM based estimation of F_x.

CONCLUSIONS

The dimensional error on a machined part observed during an on-machine measurement (OMM) exercise conducted immediately after the machining operation is equal to the cutting force induced relative displacement between the tool tip and the machined surface. Hence, it should be possible to obtain an estimate of the cutting force component, F_x, normal to the machined surface by dividing the OMM-based estimate of dimensional error by the machining system's static stiffness in a direction normal to the machined surface. This paper has provided experimental data in support of the above theoretical expectations. It has also shown that the basic stiffness parameters of the machining setup can be determined with acceptable accuracy through the application of a simple stiffness model that assumes that the force induced deflection on the spindle side of a turning center occurs through rotation about an unknown center. The advantage of the approach is that no additional equipment other than Fine Touch signal-processing unit needs be incorporated into the machine.

The force estimation method described in this paper enables us to progress towards distributed databases by making it possible for each machine tool on the shop floor to compile its own machining database on the basis of its routine shop floor experiences. Thus, the new technique brings us one step closer to the rather distant goal of arriving at ''self evolving machining centers [19]'' through ''automatic acquisition of machining conditions [19].'' In comparison to the traditional method of relying on a general-purpose (hence, large) machining database compiled through experimentation at one or more remote sites, this approach is likely to lead to databases that are more compact and more closely tuned to the process design needs of the individual machines.

The OMM-based technique of cutting force estimation is fundamentally limited to the estimation of the force component, F_z, normal to the machined surface. It does not provide any information on the other two force components, F_y and F_z. The other two force components (but not F_z) are measurable through motor current sensing [4]. Hence the OMM-based technique of F_x estimation could act as an elegant complement to motor current sensing.

An alternative is to utilize a predictive process model for the estimation of F_y and F_z. However, the industrial use of predictive models has been woefully limited mainly because they usually require tedious measurements of shear and chip flow angles in laboratory settings. However, evidence has been presented in this paper to show that this disadvantage could be overcome (at least in the case of cylindrical turning operations) by taking advantage of our prior knowledge of F_x.

The present paper has merely established the feasibility of estimating F_x and the machine's basic stiffness parameters from warm-OMM data in the context of cylindrical turning operations. Although reasonable estimation accuracy has been achieved, it should be possible to improve it through further research. For instance, one could find superior OMM protocols and approaches towards modeling the stiffness of a given machining set up. Likewise, it should be possible to develop more accurate models of the turning process (in comparison to those described in [16,17]). Finally, extensive further research is needed to progressively generalize the techniques described in the present paper to other machining operations (e.g., milling).

NOTATION

C	material constant related to the normal stress distribution on the lower boundary of the shear zone
D_{des}	desired part dimension according to the CNC part program
D_{pp}	part dimension determined through post-process inspection
D_{omc}, D_{omw}	part dimensions determined through on-machine measurement before and after the machine has cooled respectively
F_x	cutting force component normal to the machined surface (radial force in cylindrical turning)
F_y	tangential (power) component of cutting force
F_z	cutting force component parallel to feed rate vector (axial force in cylindrical turning)
K_{csh}	rotational stiffness of the chuck-spindle-headstock assembly about the hypothetical rotational center
$k_{sp,x}$, $k_{t,x}$, $k_{wp,x}$	contributions to k_x arising from the stiffness characteristics of the chuck-spindle-headstock assembly, the tool side machine structure, and the workpiece respectively
k_x, k_y, k_z	cutting force magnitudes in directions X, Y, and Z respectively required to cause unit magnitude of tool-work displacement in direction X
K_{1P}	the part of F_y per unit cut width that may be attributed to extrusion and rubbing phenomena at the cutting edge
K_{1Q}	the part of the force component normal to the plane containing the cutting edge and the cutting speed vector that is attributable to extrusion and rubbing phenomena at the cutting edge divided by the cut width
L	axial distance from the chuck face of the tool position corresponding to D_{des}

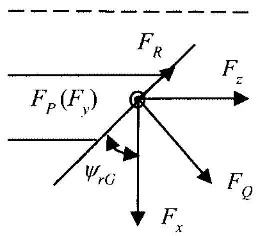
r	square root of coefficient of determination (equal to Pearson product-moment coefficient of correlation)
R	axial distance from the chuck face of the plane containing the rotation center
r_l	ratio of chip length to cut length
s	shear stress (assumed constant) over the lower boundary of the shear zone
z	axial distance of tool position from end-face of chucked workpiece
β	apparent tool-chip friction angle
δ_{fx}, δ_{fy}, δ_{fz}	contributions to δ_f arising from F_x, F_y, and F_z respectively
δ_f, δ_g, δ_{th}, δ_{other}	contributions to δ_{tot} due to force induced deflections, machine's geometric errors, machining system's thermal deformations, and other errors respectively
δ_{tot}	total dimensional error on D_{des}
ϕ_{nG}	normal shear angle associated with the generalized cutting edge
η_{cG}	chip-flow angle (angular deviation in the tool rake plane of the chip-velocity vector from the normal to the 'generalized' single edge cutting—see Appendix
τ_s	mean shear flow stress within the shear zone (assumed to be thin) associated with the generalized cutting edge

APPENDIX: ADAPTATION OF VENUVINOD'S SINGLE EDGE OBLIQUE CUTTING MODEL [17] FOR PREDICTING TURNING FORCES

We will summarize below one method by which we should be able to utilize our prior knowledge of the radial cutting force, F_x, in cylindrical turning (obtained, for instance, through the warm-OMM technique presented earlier in this paper) for predicting the shear angle, the chip flow angle, F_y, and F_z on the basis of certain insights derived from the physics of the cutting process. In particular, the method combines Armarego's approach to the modeling of turning forces with the single edge oblique cutting model of Venuvinod and Jin [17].

Procedure

Armargeo has adopted a generalized approach for the prediction of cutting forces in any single point machining operation [16]. He achieves this by replacing the actual cutting edge profile (in turning, this consists of the major cutting edge, the nose curve, and the minor cutting edge in engagement) by a single straight cutting edge (GSCE) that passes through the two end points of the edge profile.



Direction P is parallel to the cutting velocity vector,
Direction Q is normal to the surface being generated,
Direction R is muturally normal to P and Q,
Direction x is normal to the axis of workpiece rotation,
Direction y is tangential to the workpiece diameter (same as P), and
Direction z is parallel to the feed motion.

Figure A1. Force components in single edge cylindrical turning with reference to the generalized single cutting edge.

The GSCE is uniquely determined by three angular orientation parameters: γ_{nG}, λ_{sG}, and ψ_{rG}. For the case of cylindrical turning, expressions are available in [16] for calculating these three single edge geometry parameters for a given turning tool geometry, feed rate, and depth of cut. The concept of a GSCE enables one to assume equivalency between the turning force components (F_x, F_y, and F_z) and the corresponding single edge cutting force components (F_P, F_Q, and F_R)—see Figure A1.

From Figure A1, we have

$$F_x = F_Q \sin \psi_{rG} - F_R \cos \psi_{rG} \tag{A1}$$

$$F_y = F_p \tag{A2}$$

and

$$F_x = F_Q \cos \psi_{rG} + F_R \sin \psi_{rG} \tag{A3}$$

We assume that F_x has already been determined through the warm-OMM technique. We now wish to utilize our prior knowledge of F_x to determine F_y and F_z. This objective can be realized by starting with the following equations presented in [16] on the basis of empirical evidence:

$$F_P = K_{1P} a_p / \cos \psi_{rG} + f_P \tag{A4}$$

$$F_Q = K_{1Q} a_p / \cos \psi_{rG} + f_Q \tag{A5}$$

$$F_R = K_{1R} a_p / \cos \psi_{rG} + f_R \tag{A6}$$

and

$$K_{1R} = K_{1P} \sin \lambda_{sG} \tag{A7}$$

Usually, $\lambda_{sG} \neq 0$. Hence it is useful to express f_R in terms of force components f_{edG} and f_{Pn} arising from chip formation (f_{edG} is parallel to the GSCE, and f_{Pn} is normal to the cutting edge while being parallel to the cutting plane). Performing the necessary geometric transformation in the cutting plane,

$$f_R = f_{Pn} \sin \lambda_{sG} - f_{edG} \cos \lambda_{sG} \tag{A8}$$

Combining equations (A1), (A5), (A6), (A7), and (A8) and rearranging,

$$F_x = K_{1Q}a_p \tan \psi_{rG} - K_{1P}a_p \sin \lambda_{sG} + f_Q \sin \psi_{rG} - (f_{Pn} \sin \lambda_{sG} - f_{edG} \cos \lambda_{sG})\cos \psi_{rG} \quad \text{(A9)}$$

Further, from the condition of chip equilibrium in a plane normal to the GSCE,

$$f_Q = f_{Pn} \tan\{\arctan(\tan \beta \cos \eta_{cG}) - \gamma_{nG}\} \quad \text{(A10)}$$

Now, we invoke Venuvinod and Jin's model [17] with a view to expressing f_Q and f_R in terms of database parameters s and C (both are constants for a given work material). Thus,

$$f_{Pn} \approx sA_{sG}(\cos \eta_{smG} \cos \phi_{nG} + C \cos \eta_{sw}^{ef} \sin \phi_{nG}) \quad \text{(A11)}$$

and

$$f_{edG} \approx sA_{sG} \sin \eta_{smG} \quad \text{(A12)}$$

where

$$A_{sG} = (a_p f)/(\cos \phi_{nG} \sin \lambda_{sG}) \quad \text{(A13)}$$

$$\eta_{smG} = \arctan[\{\tan \lambda_{sG} \cos(\phi_{nG} - \gamma_{nG}) - \tan \eta_{cG} \sin \phi_{nG}\}/\cos \gamma_{nG}] \quad \text{(A14)}$$

and

$$\eta_{sw}^{ef} = \arctan[\{\tan \lambda_{sG} \cos(45° - \gamma_{nG}) - \tan \eta_{cG} \sin 45°\}/\cos \gamma_{nG}] \quad \text{(A15)}$$

Equations (A11) to (A15) indicate that f_{Pn} and f_{edG} may be estimated if we know the magnitudes of eight variables: a_p, f, γ_{nG}, λ_{sG}, s, C, η_{cG}, and ϕ_{nG}. Among these, a_p and f can be extracted from the CNC part program; γ_{nG} and λ_{sG} can be calculated from the relevant equations available in [16] for the given tool geometry specification and the magnitudes of a_p and f; and we assume that work material constants s and C can be determined from a previously compiled database of single edge orthogonal cutting model parameters.

As a result, we can express f_{Pn} and f_{edG} in terms of *only two* unknown variables: the chip flow angle η_{cG}, and the normal shear angle ϕ_{nG}. Next, by substituting these expressions into equations (A9) and (A10), we arrive at two simultaneous equations for determining the two unknowns η_{cG}, and ϕ_{nG}—since among the new variables introduced, ψ_{rG} can be calculated from the given tool geometry specification and the magnitudes of a_p and f using the relevant equations available in [16]; the magnitudes of K_{1P}, K_{1Q}, and tool-chip friction angle β can be extracted from the model parameter database; and we already know the magnitude of F_x from the warm-OMM technique proposed in this paper. Once the magnitudes of η_{cG}, and ϕ_{nG} have been so determined, it is a straight forward task to determine F_y, and F_z by following equations (A1) to (A15) in approximately reverse order.

ADDITIONAL NOTATION

a_p	depth of cut in the turning operation under consideration
A_{sG}	area of the generalized shear plane passing through the GSCE
f	feed rate adopted in the turning operation
f_{edG}	cutting force arising due to chip formation in a direction parallel to the GSCE
f_P, f_Q, f_R	contributions to F_P, F_Q, and F_R respectively arising from the chip formation process
f_{Pn}	cutting force component arising from chip formation and directed normal to the GSCE in the plane containing GSCE and the cutting speed vector
F_P, F_Q, F_R	cutting force components in directions P, Q, and R respectively (see Figure A1)
GSCE	'generalized' single cutting edge as defined in [16]
K_{1R}	the part of F_R per unit cut width that may be attributed to extrusion and rubbing phenomena at the cutting edge
γ_{nG}	tool rake angle measured in a plane normal to the GSCE
η_{sw}^{ef}	angular parameter associated with the stress distribution on the lower boundary of shear zone (see [17])
η_{smG}	angle between shear velocity vector on the shear plane associated with the GSCE and the normal to the cutting edge in the shear plane
λ_{sG}	angle of inclination (angle of 'obliquity') of the GSCE
ψ_{rG}	side cutting edge angle of the GSCE

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