Estimation of tool wear in orthogonal cutting using the finite element analysis

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Abstract

In metal cutting, tool wear on the tool–chip and tool–workpiece interfaces (i.e. flank wear and crater wear) is strongly influenced by the cutting temperature, contact stresses, and relative sliding velocity at the interface. These process variables depend on tool and workpiece materials, tool geometry and coatings, cutting conditions, and use of coolant for the given application. Based on temperatures and stresses on the tool face predicted by the finite element analysis (FEA) simulation, tool wear may be estimated with acceptable accuracy using an empirical wear model.

The overall objective of this study is to develop a methodology to predict the tool wear evolution and tool life in orthogonal cutting using FEM simulations. To approach this goal, the methodology proposed has three different parts. In the first part, a tool wear model for the specified tool–workpiece pair is developed via a calibration set of tool wear cutting tests in conjunction with cutting simulations. In the second part, modifications are made to the commercial FEM code used to allow tool wear calculation and tool geometry updating. The last part includes the experimental validation of the developed methodology. The focus of this paper is on the modifications made to the commercial FEM code in order to make reasonable tool wear estimates.

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1. Introduction

Tool wear has a large influence on the economics of the machining operations. Thus, knowledge of tool wear mechanisms and capability of predicting tool life are important and necessary in metal cutting. A view of the functional elements that affect the wear of a cutting tool is illustrated in Fig. 1 and can be summarized in four major groups, as follows:

1. The workpiece material and its physical properties (mechanical and thermal properties, microstructure, hardness, etc.), which determine cutting forces and energy for the applied cutting conditions.
2. The interface conditions: In 80% of the industrial cutting applications, coolants are used to decrease cutting temperatures and likely reduce tool wear. Increasingly new technologies, such as the minimum liquid lubrication, have been developed to reduce the cost of coolant that makes up to 10% of the total machining costs [1].
3. The cutting tool: Tool parameters such as tool material, tool coatings, and tool geometric design (edge preparation, rake angle, etc.) need to be appropriately chosen for different operations (roughing, semi-roughing, or finishing). The optimal performance of a cutting tool requires a correct combination of the above tool parameters and cutting conditions (cutting speed, feed rate, depth of cut, etc.).
4. The dynamic characteristics of the machine tool, affected by the machine tool structure and all the components taking part in the cutting process, play an important role for successful cutting. Instable cutting processes with large vibrations (chatters) result in a fluctuating overload on the cutting tool and often lead to the premature failure of the cutting edge by tool chipping and excessive tool wear.

The present study focuses on the prediction of gradual tool wear formation associated with the properties of the workpiece and tool materials and the interfacial conditions. In this initial study, the effects of machine tool dynamics and three-dimensional chip flow (i.e. up-curl and/or side-curl of the chip) on tool wear are not considered.
2. Prediction of tool wear and tool life

The conventional way to characterize tool wear for a cutting operation in the industry is to perform cutting tests with varying cutting conditions (cutting speed, feed, etc.) and then analyze the tool wear data using optimization techniques, such as design of experiments (DOE) and response surface methodology (RSM). The disadvantage of using such an empirical approach is that in order to achieve acceptable accuracy, this modeling procedure usually requires a large number of experimental tests and hence it is time-consuming and cost-intensive.

In the last decade, the finite element method (FEM) has been successfully applied to simulate various cutting processes [2–6]. It has been shown that the FEM cutting simulation can be used to estimate the process variables that are not directly measurable or very difficult to measure during a cutting operation, such as normal stress and temperature on the tool face, chip temperature, and chip sliding velocity along the tool rake face. The knowledge of these process variables provides a better understanding of the fundamental cutting mechanics and enables the engineering analysis of tool wear. Furthermore, the correlations of these variables to the tool life may lead the researchers to implement a systematic approach for the process optimization.

However, the major drawback of a cutting simulation is that it does not provide direct information on the increase of tool wear (or tool wear rate), as opposed to the experimental approach. Nevertheless, it is expected that the tool wear growth (crater and flank wear) is dependent on the cutting temperature, contact stresses, and sliding velocity produced during cutting. By predicting the distributions of these process variables from cutting simulations and implementing in the FEM code, the tool wear model, governed by these process variables, the distribution of tool wear rate along the tool face may be effectively estimated. The new geometry of the worn tool can then be calculated based on the tool wear rate data and compared to the measured tool profile corresponding to the same cutting time.

Fig. 2 summarizes the comparison of cutting process optimization between the empirical approach through cutting tests and the FEM simulation approach.

3. Wear mechanisms and tool wear models

Since the tool wear in cutting operations involves complex wear mechanisms, researchers have attempted to directly correlate the results of tool life to the applied machining parameters (cutting speed, feed rate, etc.). The well-known Taylor’s tool life relationship and its various extended equations are of this type [7–9], as given in the left column of Fig. 3. These equations describe the relationship between tool life and machining parameters, such as cutting speed and feed rate, and involve a few constants that need to be experimentally determined for the given combination of tool and workpiece materials.

In contrast to the above tool life models, the tool wear rate models (the right column of Fig. 3) describe the rate of local volume loss on the tool contact face (rake or flank face) per unit area per unit time. The derivations of this type of
tool wear rate models require the knowledge of wear mechanisms associated with the tool and workpiece materials and the range of cutting conditions used. It is generally accepted that for carbide tools under practical cutting conditions, the wear rate is dominated by a temperature-sensitive diffusion process, in particular at higher cutting speeds [10–12]. Takeyama and Murata derived a fundamental wear rate equation by considering abrasive wear, which is proportional to the average contact temperature 

$$\frac{dW}{dt} = kT \cdot \exp \left( - \frac{E_a}{RT} \right)$$

where $W$ is the wear rate, $T$ is the temperature, $k$ is the frequency factor, $E_a$ is the activation energy, and $R$ and $T$ are the gas constant and temperature, respectively. Mathew [10] analyzed the tool wear of carbide tools when machining carbon steels and results have shown that the Takeyama and Murata's diffusion equation can be used to effectively analyze the tool wear rate to the average contact temperature of the tool. At cutting temperature higher than 800 °C (1150 K) was observed, and according to the authors, attributed to the formation of fragile carbide compounds and disappearance of WC grains (a diffused layer) in the tool material at high temperatures. It should be noted that despite considering different tool wear mechanisms, Mathew [10] and Usui and coworkers [14] both could fit the experimental wear data well due to the fact that the diffusive and adhesive wear models used, respectively, have the same form with the temperature dependence in the exponential term (see Fig. 3).

Similar to the tool life models, the wear rate models involve unknown wear constants that depend on the given workpiece and tool materials and need to be determined by conducting some calibration cutting tests. DeVir and coworkers [16] observed experimentally that the region of plastic flow of the workpiece material on the flank face of a worn tool grows linearly with the increase in the total wear land width. Based on this concept, they developed a worn tool force model for three-dimensional cutting operations that required a minimum number of sharp tool tests and only one worn tool test. Jawahir et al. [17] analyzed the tool wear mechanisms in grooved tool inserts based on the associated chip flow patterns through high-speed filming and scanning electron microscopy (SEM). The results indicated that in reality different forms of tool wear in grooved tools could be observed due to the mechanical action of the "undesirable" chip flow (e.g., chip hammering) rather than due to the adhesion or diffusion process. Chou and Evans [18] experimentally investigated the effects of tool and workpiece microstructures on the wear of cubic boron nitride (CBN) tool in finish hard turning. They found that carbide sizes of the workpiece have a decisive effect on fine scale attrition, the dominant wear mechanism for low CBN content tools, and that the wear resistance increases with decreasing CBN grain size. A review of typical wear behaviors of soft and hard film coatings, metallic alloys, composites, and ceramics in relation to their frictional characteristics was summarized by Kato [19].

In previous studies, Shatla et al. [20] have attempted to relate the experimental flank wear data in cutting 0.2% carbon steel, given by Mathew [10], to the values of tool temperature, stress and sliding velocity predicted by cutting simulations, based on the Usui’s wear model. The determined tool wear model was then applied to arbitrary cutting conditions to predict tool life. However, the tool life was predicted by extrapolating the tool wear data from a sharp tool model and the geometry of the initial sharp tool was not updated. A similar approach was also taken in analyzing different carbide tools by using an in-house computer program (OX-CUT) based on Oxley’s analytical machining theory [21]. Further improvements by accounting for geometric changes, as proposed in this paper, have been summarized in [22].

### 4. Research objectives

The overall objective of this study is to develop a FEM-based methodology to predict the evolution of tool wear and tool life for orthogonal cutting. Specifically, the research tasks include:

(i) implementation of tool wear rate model(s) in the commercial FEM code (DEFORM®-2D) that relates the wear rate to the predicted process variables;
(ii) development of a procedure for process simulation to predict the evolution of tool wear and the resulting worn tool geometries, and thus enabling estimation of tool life;

(iii) validation of the methodology by predicting worn tool profiles at different cutting conditions and comparing the results with the experiments (not included in this paper).

5. Procedure for tool wear prediction in cutting simulation

It is postulated in this study that the growth of tool wear can be evaluated at discrete points in time, although in reality it is a continuous process. The proposed procedure for predicting tool wear at any time instance \( t_k \) can be divided into four phases, as shown in Fig. 4. Completion of these four phases makes one simulation cycle that corresponds to one data point on the flank wear (VB) versus cutting time curve at \( t = t_k \).

Prior to the calculation of wear rates, the quasi-steady state field solutions of the cutting variables (strain, temperature, stress, etc.) are determined in Phase 1 and Phase 2. Local tool wear rates and the worn tool geometry are then calculated in Phase 3, based on the obtained values of cutting variables and the wear rate model used. In Phase 4, the rake face and flank face geometries of the tool in the FEM model are subsequently updated with the results obtained in Phase 3. When one full simulation cycle is completed, the same procedure is repeated for the next data point, of which the location may be projected based on the local slope of the wear curve at \( t = t_k \) (i.e. wear rate, \( \frac{dVB}{dt} \)) and a properly selected time increment \( \Delta t_k \). As will be detailed later, this leads to an estimate of the new flank wear width (VB\(_{k+1}\) at \( t = t_{k+1} \)) for the updated tool geometry that is used in subsequent simulation. Details for each phase in Fig. 4 are now described in the following sections.

5.1. Phase 1: continuous cutting simulation—Konti-Cut

The wear prediction procedure starts with a coupled thermo-viscoplastic Lagrangian cutting simulation with isotropic strain-hardening using DEFORM®-2D. In order to obtain the cut chip geometry near the steady state, a special simulation module, “Konti-Cut”, developed by WZL at University of Aachen (RWTH), Germany, was utilized[23]. With this simulation module, it is possible to run a cutting simulation for a sufficiently long cutting time rather than only a few milliseconds, as seen in typical Lagrangian cutting simulations. The principle of “Konti-Cut” is described briefly below and elsewhere in[23]. Each time the remeshing of the workpiece is launched due to severe element distortion near the tool tip, the “Konti-Cut” subroutine cuts off excessive chip material away from the cutting zone and the machined workpiece material behind the cutting edge by means of a user-defined ‘control area’. Meanwhile, new material is constantly supplied from the boundary on the uncut side of the workpiece. By repeating the same procedure, the cutting simulation can thus be run for a long cutting time.

Fig. 5 gives the typical result of chip formation obtained from the “Konti-Cut” simulation. The shape of the chip, namely, chip thickness, contact length, shear angle, and curling radius of the chip, did not change noticeably after this step and remained constant. It can be seen from Fig. 5 that the workpiece material behind the cutting edge was removed, so was the chip material on the top of the chip after leaving the control area.

During the “Konti-Cut” simulation, the chip geometry, chip temperature, and tool face temperature were tracked until no significant changes were observed (a near steady state). The temperatures in the chip and the tool were recorded with the Eulerian and Lagrangian point tracking functions,
respectively. The Eulerian point tracking of the chip was implemented in the FEM code with user-defined FORTRAN subroutines.

In the case of Fig. 5, the contact temperatures on both sides of the tool-chip and tool-workpiece interfaces were plotted by defining two tracking points on the rake face (TP1 and TP2) and one tracking point on the flank face (TP3), as shown in Fig. 6. The three curves with solid symbols represent the tool face temperatures obtained from the Lagrangian point tracking, whereas the other three curves represent the inner surface temperatures of the chip obtained from the Eulerian point tracking. After approximately 0.6 ms, all curves appear to have reached a nearly constant value.

In the case of Fig. 6, a large interface heat transfer coefficient, $h_{int}$, was defined for the contact interface during simulation, assuming a 'perfect' interface heat transfer condition. It can be noted from the figure that the surface temperatures of the tool tend to be higher than that of the workpiece and chip counterparts near the same locations of the interface. This may be explained by the fact that the friction heat transferred to the chip is immediately transported away by the chip flow, whereas the friction heat conducted to the tool gets accumulated near the tool surface and takes time to diffuse into the inner region of the tool. Experimental results of temperatures using the two-color pyrometry measurement during cutting have shown similar observations [24]. In addition, the flank face temperatures are lower than the rake face temperatures, consistent with the results given in [14], due to less plastic deformation in the tertiary shear zone.

The “Konti-Cut” simulation has two other features:

1. It avoids possible convergence and contact problems when the long chip produced curls down and touches the uncut workpiece surface. However, it should also be pointed out that the use of “Konti-Cut” has a potential risk of underestimating the chip contact length and forces, and thus tool wear, due to the negligence of the chip-curling effect.

2. Better control of refined mesh quality is achieved and fewer elements are required.

5.2. Phase 2: pure heat transfer analysis of the tool

To reach a thermally steady state for the cutting tool, the simulation needs to be run for a much longer cutting period on the order of a half second. This will considerably increase the computational time and data storage capacity, making cutting simulations expensive. Therefore, an approximate method was used to obtain the steady state tool temperatures, which involves the pure heat transfer analysis for the tool only.

The calculated heat flux on the tool surface that represents the interface heating from the chip/workpiece objects was applied as customized thermal boundary conditions on the contact elements. For this purpose, a user subroutine was developed. As shown in Fig. 7, the total heat flux $Q$ is considered to consist of the friction heat, $Q_1$, and the heat flux caused by the temperature difference between the two contact surfaces at the interface, $Q_2$. The contact surface temperature of the chip, as shown in Fig. 6, was considered to be nearly independent of the value of $h_{int}$ used and may be assumed to be constant in the calculation of $Q_2$. Thus, the $Q_1$ component is computed as an output from Phase 1, whereas $Q_2$ is calculated through iteration as the tool contact temperature ($T_{tl}$) changes with time in the pure thermal analysis.

The results of the pure thermal analysis of the tool are presented in Fig. 8, showing the tool temperature field evolving from the start (end of Phase 1), the transition, to the steady state. As the chip flow and tool temperature solutions were obtained in two separate phases, the process variables for the chip/workpiece and the tool had to be combined by an external program to display the results in their entirety in the post-processor.
Fig. 7. Calculation of boundary heat flux components on the tool contact surface.

The magnitude of tool temperatures, calculated using the above heat flux model, was very sensitive to the value of the interface heat transfer coefficient, \( h_{\text{int}} \), and sometimes would not converge, especially when a large value of \( h_{\text{int}} \) was used. Therefore, an alternative based on the near-constant tool interface temperatures obtained from Phase 1 (e.g., Fig. 6) was also utilized. The solution of tool surface temperatures for the contact region was defined as the temperature boundary conditions.

5.3. Phase 3: tool wear rate calculation

For the results presented in this paper, the wear rate for each node on the tool contact surface was calculated based on the predicted nodal data of normal stress \( \sigma_n \), temperature \( T \) and sliding velocity \( V_S \) and Usui’s tool wear rate model:

\[
\dot{w} = A \sigma_n V_S \exp \left( -\frac{B}{T} \right)
\]

where the values of the wear constants \( A \) and \( B \) for plain carbon steels versus uncoated carbide tools given in [15] were used:

\[
A = 7.80 \times 10^{-9} \quad \text{and} \quad B = 5.302 \times 10^3 \quad \text{for} \quad T < 1150 \text{ K}
\]

\[
A = 1.198 \times 10^{-2} \quad \text{and} \quad B = 2.195 \times 10^4 \quad \text{for} \quad T \geq 1150 \text{ K}
\]

Similarly, different wear rate models in relation to the cutting variables (e.g., Takeyama and Murata’s wear model) can also be implemented for comparison.

5.4. Phase 4: updating of the tool geometry due to wear

Along with the wear rate data, the new rake face and flank face geometries of the tool due to tool wear were calculated and used in Phase 4 to generate the new input keyword file for the next simulation. Considering the nature of the ‘flat’ flank wear land observed in the cutting tool, two methods were adopted for the modification of the flank face geometry: (1) updating by individual nodal movements and (2) updating based on the averaged values of cutting variables for the flank wear land. This is because any sharp peaks on the flank wear surface are virtually unstable and will be torn flat immediately by continuous machined workpiece deformation. For the modification of the rake face geometry, only the individual nodal movement method was used. Another possible method for updating the tool geometry might be through deletion of the tool surface elements.

5.4.1. Updating by individual nodal movements

The evolution of tool wear on the rake face and tool tip (referred to as ‘tool rake’ hereafter) normally follows a concave shape (crater wear) in the cross-sectional profile. Therefore, tool rake adjustments by moving individual surface nodes in the normal direction can be used to account for varying wear depths along the tool rake face. The wear depth for each
node is described by its nodal wear rate alone, as the wear rate (volume loss per unit area per unit time) is equivalent to the wear depth under the two-dimensional condition. Therefore, for the given cutting time increment \( \Delta t_k = t_{k+1} - t_k \) (Fig. 4), the nodal displacement caused by tool wear between two simulation cycles can be approximated by

\[
\Delta d_{i,k} = \dot{w}_{i,k} \Delta t_k, \quad i = 1, \ldots, N
\]

where \( \dot{w}_{i,k} \) is the wear rate for the node \( i \) at time \( t_k \) and is assumed to be constant throughout the time period \( \Delta t \). \( N \) is the total number of the contact nodes. \( \Delta d_{i,k} \) is the nodal displacement for the node \( i \) corresponding to \( \Delta t \). The total wear depth for the node \( i \) \( (\Delta \tilde{d}_i) \) is thus equal to the vector sum of all incremental nodal displacements associated with the node \( i \) throughout the total cutting time:

\[
\Delta \tilde{d}_i = \Delta \tilde{d}_{i,0} + \Delta \tilde{d}_{i,1} + \cdots + \Delta \tilde{d}_{i,k} + \cdots
\]

The direction of the inward normal to the local surface at any node \( i \) can be obtained as shown in Fig. 9. For example, the node X is to be moved; the node A and node B are two neighboring nodes after and before the node X, respectively (in counter-clockwise direction). The angle \( \psi \) between the line AB and the horizontal line passing through the node A can be obtained by:

\[
\psi = \tan^{-1} \left( \frac{y_{B} - y_{X}}{x_{B} - x_{X}} \right)
\]

where \((x_A, y_A) \) and \((x_B, y_B) \) are the Cartesian coordinates of the node A and the node B, respectively. The angle \( \psi \) is calculated for all the nodes of the tool contact surface in counter-clockwise direction and thus has a negative value.

The inward-normal direction to the tool face at the node X is then approximated by rotating the line AB with respect to the node X by 90° in the positive clockwise direction \( (\psi' = \psi + 90^\circ) \). Therefore, the final coordinates of the node X after \( \Delta \tilde{d}_i \) can be computed with the angle \( \psi' \) and the nodal wear depth \( \Delta \tilde{d}_i \) obtained from Eq. (2):

\[
\begin{align*}
\text{X coordinate:} & \quad x_{X|t_{k+1}} = x_{X|t_k} + \Delta \tilde{d}_i \cos \psi' \\
\text{Y coordinate:} & \quad y_{X|t_{k+1}} = y_{X|t_k} + \Delta \tilde{d}_i \sin \psi'
\end{align*}
\]

where \((x_{X|t_k}, y_{X|t_k})\) and \((x_{X|t_{k+1}}, y_{X|t_{k+1}})\) are the Cartesian coordinates of the node X before and after the nodal movement, respectively.

5.4.2. Updating of tool flank face based on the averaged values of cutting variables

The individual nodal movement method can also be applied to the tool flank face, if the obtained nodal wear rates on the flank wear land are uniform so that a realistic flat flank wear land can be generated. However, this is usually not the case due to the numerical errors associated with the stresses and temperatures predicted for the flank wear land. The deviation from the flatness is especially noticeable for the regions near the tool tip and the end of the flank wear land. Therefore, updating of the flank face geometry while keeping a flat wear land was done by using the averaged values of flank temperature, normal stress, and sliding velocity in Eq. (1).

To obtain the new flank wear width \( \text{VB}_{k+1} \) in Fig. 4, a relationship between the flank wear rate \( \dot{w} \) and the flank wear width \( \text{VB} \) was derived from their geometrical definition. For an infinitesimal time increment \( dt \), the increase in VB \((=d\text{VB})\) can be expressed by

\[
d\text{VB} = d\tan \alpha + \frac{dl}{\tan \gamma} = \frac{dl}{\tan \gamma} = \frac{w \, dt}{\tan \gamma}
\]

where the tool rake angle \( \alpha \) is usually less than 10° and \( \gamma \) is the tool relief angle. \( dl \) is the increase in flank wear width \((=w \, dt)\). Therefore, the increase in flank wear width after a cutting time \( \Delta t \) can be calculated by

\[
\Delta \text{VB} = \int_{0}^{\Delta t} \frac{w \, dt}{\tan \gamma} = \frac{w}{\tan \gamma} \Delta t
\]

In Phase 3 of the simulation, the average wear rate for the existing flank wear was first calculated. For the user-defined \( \Delta t \), a new flank wear width \((=\text{VB} + \Delta \text{VB})\) was obtained using Eq. (7). Then, the updating of the new flank wear geometry was achieved by offsetting the entire flank wear line along the inward-normal direction by the distance of \( \dot{w} \times \Delta t \).

6. Tool wear cutting simulation

The proposed procedure was tested using a simple up-setting model. The procedure was then applied to the orthogonal cutting process. The preliminary results would indicate the feasibility of this methodology for prediction of the gradual growth of tool wear (at least qualitatively).

6.1. Simulation of a sharp tool

The orthogonal cutting simulation of uncoated carbide tools with an initial sharp edge \((r_c = 20 \mu m)\) in cutting 0.45% carbon steel (AISI-1045) was carried out based on the proposed procedure. Fig. 10 shows the predicted results of wear rate contours (left), as highlighted in the oval and the updated tool rake face geometry (right). It can be seen...
that after a selected cutting time increment ($\Delta t$) of 20 s, only a slight amount of crater wear was seen on the rake face of the tool.

In Fig. 10, the maximum wear rate is clearly located on the tool rake face. However, only small wear rates were predicted on the flank side of the tool edge radius, which are about one order of magnitude smaller than the rake face wear rates. It should be noted that the experimental results for uncoated carbide tools showed that flank wear and crater wear on the tool face occur simultaneously at a similar wear rate [25].

The simulation showed that higher temperatures ($<1150 K$), lower normal stresses, and lower sliding velocities were observed on the rake face than around the edge radius where significant wear had been expected. Since wear rate increases exponentially with cutting temperature according to Eq. (1), this may be the reason for large tool wear rates predicted on the rake face rather than at the sharp tool edge. Besides, it is known that high wear rates are normally seen at the initial stage of cut for a sharp edge due to the weaker edge strength and possible micro-chipping. Thus, the tool wear models that account for gradual wear formation like Eq. (1) may not be suitable for the case of a sharp edge. It is also important to note that as the Usui’s wear model for carbon steels considers adhesive wear, it may better describe the flank wear behavior at higher temperatures after a small flank wear land has formed.

6.2. Simulation of a worn tool with a pre-defined flank wear width

Another simulation study was made with a worn tool initially including a pre-defined wear land of 0.06 mm on the tool flank face. Since the workpiece was defined as a rigid–plastic object, the flank wear face was slightly inclined (less than 3°) in order to maintain full contact with the tool. The results from this test simulation allowed us to understand how sensitive the prediction of process variables is to the existence of flank wear and how the existing flank dimension affects the predicted results of crater wear and flank wear.

To evaluate the predictive capability of the developed module, it is important to consider a few questions: (1) How does the predicted crater wear grow? (2) How does crater wear and flank wear influence the chip geometry? (3) Does a zig-zag profile for the tool surface occur after updating the geometry through nodal movement? Is an extra boundary-smoothing function required?

Fig. 11 shows the results for the predicted wear rate distribution and the development of crater wear and flank wear. The contours of wear rate were plotted on the contact surface before the tool geometry was updated. Small cutting time increments of 10 s to 20 s had to be used for the first several updates, compared to 30 s to 60 s for the rest, as it was found that the selection of the time increment for the initial stage of crater wear formation has a crucial effect on
may be due to the following error sources: based on a criterion of VB of a carbide tool (Kennametal grade K68) is around 1 min underestimated, hence requiring a large the wear rate values obtained in the simulations may be the same cutting conditions [27] have shown that the tool life shows that the chip flow is completely conformed to the up-

Fig. 12. Results of wear rate (a) and updated tool geometry (b) for a tool with an initial flank wear and crater wear.

g its final shape. For all the cases, the flank wear geometry was modified using the second updating method (average values of process variables).

It is seen from the figure that the wear rates of crater wear and flank wear are of the same order (×10−4) and hence the increase of both types of tool wear is quite consistent. The location of the maximum wear rate is on the tool rake face and is nearly coincident with that of the maximum cutting temperature. In addition, a region of very low wear rates is observed close to the tool radius on the rake face side. This region corresponds well to the experimental result of the worn tool geometry, which usually features a small flat face near the cutting edge that is almost unaffected by cutting (denoted by ‘KF’ in the ISO-3685 standard [26]).

Similarly, Fig. 12 shows the results for another test sim-

ulation, where both crater wear and flank wear land (VB = 0.1 mm) were initially included in the tool geometry. It can be seen from Fig. 12(a) that large wear rates are concentrated on the upper region of the crater wear and thus cause the crater wear to develop in width. This is caused by the localized high temperatures and normal stresses due to the restricted chip flow. On the other hand, moderate values of wear rate are observed at the middle tool–chip contact region and cause the crater wear to develop in depth. Fig. 12(b) shows that the chip flow is completely conformed to the updated crater wear profile, leading to a reduced chip-curling radius.

The experimental results from the cutting tests under the same cutting conditions [27] have shown that the tool life of a carbide tool (Kennametal grade K68) is around 1 min based on a criterion of VB = 0.2 mm. This implies that the wear rate values obtained in the simulations may be underestimated, hence requiring a large ΔT. This inaccuracy may be due to the following error sources:

(i) The solution of the steady state temperature of the tool, which involves a few uncertainties: (a) the interface heat transfer coefficient, h int, (b) the friction model (a constant shear factor m = 0.6 was used), (c) calculation of heat partition for the friction heat generated at the interface, (d) effect of tool size and thermal boundary conditions (T = 20 °C), defined on the top and back face of the tool, and (e) other boundary heat flux components ignored in Fig. 7.

(ii) The values of the wear constants A and B when Usui’s wear rate model was used.

(iii) Numerical errors associated with the frequent remeshing and data interpolation from the old mesh to the new mesh throughout the simulation.

7. Summary and conclusions

This paper discusses the numerical implementation of the integration of tool wear models with FEM calculations to predict the evolution of wear over long cutting periods. For the estimation of tool wear rate for an uncoated carbide tool in cutting carbon steel, the Usui’s wear rate model, based on adhesive wear, was implemented into the FEM code (DEFORM®-2D). The complete procedure for tool wear prediction proposed consists of four different phases.

The simulations using a cutting tool with constantly up-
dated rake face and flank face geometries have shown that it is possible to predict the evolution of tool wear at any given cutting time from FEM simulations by using the methodology proposed in this study. The ultimate goal is to enable the complete construction of tool wear curves (i.e. VB versus cutting distance or time) and estimate the tool life through a FEM-based technique. With the developed simulation method, the engineering analysis for the effect of cutting conditions on cutting performance may be possible, which could lead to a further process optimization.

The preliminary results of tool wear simulations using the developed method tend to underestimate the wear rates associated with the crater wear and flank wear, when comparing the tool life with the measured data obtained at the same conditions. The reason may be partially due to the fact that the two wear constants in Usui’s wear model were directly borrowed from the literature [15].

The future work for this study requires: (1) determination of the wear constants (A and B in Eq. (1)) through the calibration cutting tests and corresponding cutting simulations for the same cutting conditions with the measured worn tool geometries; (2) validation of the proposed methodology for predicting tool wear curves for selected cutting conditions, different from those in the calibration set, and comparing the results with the experiments; (3) use of other wear rate models available in the literature (e.g. Takeyama and Murata’s wear model).

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