A Study of the Cutting Temperature in Milling Stainless Steels with Chamfered Main Cutting Edge Sharp Worn Tools

Chung-Shin Chang

Abstract: The main purpose of this paper is to study the carbide tip’s surface temperature and the cutting forces of milling stainless steel with chamfered main cutting sharp worn tools. The carbide tip’s mounting in the tool holder are ground to a wear depth that is measured by a toolmaker microscope and a new cutting temperature model incorporating tool wear factor and using the variations of shear and friction plane areas occurring in tool worn situations are presented in this paper. The tool tip and cutting edges are treated as a series of elementary cutting tips. The forces and frictional heat generated on elementary cutting tools are calculated by using the measured cutting forces and the oblique cutting analysis. The carbide tip’s temperature distribution is solved by finite element analysis (FEM) method.

Keywords: Milling, stainless steel, cutting temperatures, FEM.

1 Introduction

Many experimental techniques for measuring the metal cutting temperatures can also be found in the literature Leshock and Shin (1997). The drawbacks of the many simplified assumptions associated with the analytical solutions were overcome by the finite element analysis (FEM) of Tay, Stevenson, de Vahl Davis, and Oxley (1976). Singamneni (2005) demonstrated that the mixed finite and boundary element model enabling the estimation of cutting temperatures is simple, efficient, and at the same time quite easing implemented. Chang (2007) also presented a force model and an FEM model that agreed in predicting the cutting temperatures for turning stainless steel with a sharp chamfered main cutting edge tool. The aim of this paper is to clarify the cutting temperatures and the cutting forces of stainless steel when the sharp chamfered main cutting edge tool is worn down.

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2 Theoretical Analysis

Chang (2005) the basic force model for a sharp worn corner tool with a chamfered main cutting edge \((R = 0)\) shown in Fig. 1 was derived as follows:

![Figure 1: (a)Basic and (b) detailed model of the chamfered main cutting tool when wear occurs \((f > R, R = 0)\)](image)

Table 1: Tool geometry specifications (chamfered main cutting edge sharp worn tools)

<table>
<thead>
<tr>
<th>side cutting edge angle</th>
<th>tool No.</th>
<th>positive and negative radial angles (\alpha_1, \alpha_2)</th>
<th>nose roundness (R)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20°</td>
<td>1</td>
<td>10°, -10° (10°, -10°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>20°</td>
<td>2</td>
<td>20°, -20° (20°, -20°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>20°</td>
<td>3</td>
<td>30°, -30° (30°, -30°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>30°</td>
<td>4</td>
<td>10°, -10° (10°, -10°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>30°</td>
<td>5</td>
<td>20°, -20° (20°, -20°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>30°</td>
<td>6</td>
<td>30°, -30° (30°, -30°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>40°</td>
<td>7</td>
<td>10°, -10° (10°, -10°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>40°</td>
<td>8</td>
<td>20°, -20° (20°, -20°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
<tr>
<td>40°</td>
<td>9</td>
<td>30°, -30° (30°, -30°)</td>
<td>0.0 (sharp and worn)</td>
</tr>
</tbody>
</table>

For the case of chamfered main cutting edge, temperatures and forces depend on nose radius \(R\), worn depth \(d_B\), cutting depth \(d\), feed rate \(f\), cutting speed \(V\), positive radial angle \(\alpha_{r1}\), negative radial angle \(\alpha_{r2}\) and axial angle \(\alpha_a\) as shown in Table 1.
Fig. 1, $C_s$ is the side cutting edge angle, $C_e$ is the end cutting edge angle, $\alpha_{r1}$ and $\alpha_{r2}$ is used. The process for deriving the shear plane areas is divided into segments with tool wear and without wear.

Figures 2 reveals that the geometrical specification of tool wear on the tool face (triangle CNM) can be derived from the values of $t_w$ and $\varphi_A$ when already measured.

\[
A = A_1 + A_2 + A_3 + A_s \quad \text{(as shown in Fig. 1a)}
\]

\[
A_1 = \frac{1}{2}a_3b_3 \sin \theta_3 = \frac{1}{2}a_3b_3 \left[ 1 - \frac{a_5^2 + b_5^2 - c_5^2}{2a_3b_3} \right]^{1/2} \quad (A_1 = \Delta NBE)
\]

\[
A_2 = \frac{1}{2}(a_4 + b_4) \cdot h_4 \quad (A_2 = \text{retangle } MDFE')
\]

\[
A_3 = A_{31} + A_{32} \quad (A_3 = \Delta ME'\bar{E} + \Delta MNE)
\]

\[
A_{31} = \frac{a_5b_5}{2 \cos \phi_e} \sin \left( \frac{\pi}{2} + \alpha_b + \angle A_{31} \right)
\]

\[
\angle A_{31} = \cos^{-1} \left[ \frac{c_5^2 + d_5^2 - e_5^2}{2c_5d_5} \right]
\]

\[
A_{32} = g_5h_5 \frac{\sin(\angle A_{32})}{2 \cos \phi_e}
\]

\[
\angle A_{32} = \cos^{-1} \left[ \frac{h_5^2 + n_5^2 - m_5^2}{2h_5n_5} \right] - \sin^{-1} \left[ \frac{l_5}{s_5} \sin \left( \frac{\pi}{2} - \alpha_a \right) \right]
\]

\[
A_s = \frac{1}{2} W_e^2 \frac{\cos^2 \alpha_{r1} \cdot \tan C_s}{\cos \alpha_a \sin \phi_e} \quad (A_s \text{ is the area of scendary chip: } \Delta D'\bar{Y}J)
\]

\[
Q = Q_1 + Q_2 + Q_3
\]

\[
Q_1 = \frac{0.5(d / \cos C_s - W_e \cos \alpha_{r1} \tan C_s)}{\cos \alpha_a} \times \frac{f \cos C_s - W_e \cos \alpha_{r1}}{\cos \alpha_{r2}} - \frac{CN \cdot NM \sin \theta_B}{2} \quad (11)
\]

\[
Q_2 = \frac{W_e \cos \alpha_{r1}(d / \cos C_s - W_e \cos \alpha_{r1} \tan C_s)}{\cos \alpha_a} - (CN \cdot W_e \cos \alpha_{r1}) \quad (12)
\]

\[
Q_3 = \frac{1}{2} \frac{W_e^2 \cos \alpha_{r1} \tan C_s}{\cos \alpha_a} \quad (Q_3 \text{ is the area of trangle } DD'\bar{Y}) \quad (13)
\]
$$CM = t_W (\cos C_s + \sin C_s \cdot \tan \theta_A)$$ (14)

$$CN = \frac{t_W (\cos C_s + \sin C_s \cdot \tan \theta_A)}{(\sin \theta_A \tan \theta_A + \cos \theta_B)}$$ (15)

$$NM = (CM^2 + CN^2 - 2CM \cdot NM \cos \theta_B)^{1/2}$$ (16)

$$\angle CMN = \cos^{-1} \left[ \frac{(CM^2 + CN^2 - NM^2)}{2CM \cdot CN} \right]$$ (17)

$$\angle CNM = \cos^{-1} \left[ \frac{(CN^2 + NM^2 - CM^2)}{2CN \cdot NM} \right]$$ (18)

Measurements are according to the setting location of the tool and the wear condition of the tool. The contact length of the tool edge can be considered as two types, as shown in Figs. 2 to 3.

Figure 2: Specifications of tool with wear

Figure 3: Contact length $L_f$ and $L_p$ with chamfered main cutting edge tool
A Study of the Cutting Temperature in Milling Stainless Steels

From the above diagram, the contact length is

\[
l_f = \bar{H}N + \bar{NM} + \bar{MD} = \bar{i} \cdot \bar{n} + \bar{i}i \cdot \bar{iii} + \bar{iii} \cdot \bar{iv}
\]

\[
= \left[ \frac{f \cos \alpha_i - W_e \cos \alpha_r}{\cos (C_e - C_s)} \right] + \bar{NM} + \frac{d}{(\cos C_s \cos \alpha_a)} - \bar{CM}
\]

(19)

\[
l_p = \bar{H}N \cos C_e + \bar{NM} \cos (\angle CNM - C_e) + \bar{MD} \sin C_s
\]

\[
= \left( \frac{f \cos C_s - W_e \cos \alpha_r}{\cos \alpha_e \cos (C_e - C_s)} \right) + \bar{NM} \cos (\angle CNM - C_e) + \left( \frac{d}{\cos C_s} - \bar{CM} \right) \sin C_s
\]

(20)

This situation is the same as Fig. 1, in which the shear area can be calculated. That is,

\[
A = A_5 + A_6 + A_s
\]

(21)

\[
A_5 = \frac{1}{2} a_6 b_6 \left( 1 - \frac{a_6^2 + b_6^2 - c_6^2}{2a_6 b_6} \right)^{1/2}
\]

(22)

\[
A_6 = \frac{1}{2} (e_6 + g_6) \cdot h_6
\]

(23)

\[
A_s = \frac{1}{2} \frac{W_e^2 \cos^2 \alpha_r \tan C_s}{\cos \alpha_e \sin \phi_e} \quad (A_s \text{ is the area of secondary chip: } \Delta D'YJ)
\]

(24)

\[
Q = Q_4 + Q_2 + Q_3
\]

(25)

\[
Q_4 = 0.5 \left\{ \left[ \frac{f \cos C_s}{\cos \alpha_e} - W_e \cos \alpha_r \right] \tan \theta_A + 2 \left( \frac{d}{\cos C_s} - \bar{CM} \right) \left( \frac{f \cos C_s}{\cos \alpha_e} - W_e \cos \alpha_r \right) \right\}
\]

(26)

\[
Q_2 = \frac{W_e \cos \alpha_r (d / \cos C_s - W_e \cos \alpha_r \tan C_s)}{\cos \alpha_a} - (\bar{CN} \cdot W_e \cos \alpha_r)
\]

(27)

\[
Q_3 = \frac{1}{2} \frac{W_e^2 \cos \alpha_r \tan C_s}{\cos \alpha_a}
\]

(28)
2.1 Energy method of predict cutting force

The shear energy per unit time $U_s$ and the friction energy per unit $U_f$ can be determined by the following equations.

$$U_s = F_s V_s = \frac{\tau_e A \cos \alpha_e}{\cos(\phi_e - \alpha_e)} V$$  \hspace{1cm} (29)

$$U_f = F_t \cdot V_c = f_t \int_0^{B_1} d b \cdot V_c = \frac{\tau_e \cdot \sin \beta \cdot \cos \alpha_e \cdot Q \cdot V}{\cos(\phi_e + \beta - \alpha_e) \cdot \cos(\phi_e - \alpha_e)}$$  \hspace{1cm} (30)

$$F_s = \frac{\tau_e \cdot A; V_s}{\cos(\phi_e - \alpha_e)}; V_c = \frac{V \sin \phi_e}{\cos(\phi_e - \alpha_e)}$$  \hspace{1cm} (31)

$$U = U_s + U_f$$  \hspace{1cm} (32)

$$(F_H)_{U_{\min}} = \frac{U_{\min}}{V} = \left\{ \frac{\tau_e \cos \alpha_e \cdot A}{\cos(\phi_e - \alpha_e)} + \frac{\tau_e \sin \beta \cos \alpha_e Q}{\cos(\phi_e - \alpha_e + \beta) \cos(\phi_e - \alpha_e)} \right\}$$  \hspace{1cm} (33)

$$(R_t)_H = N_t \cos \alpha_{r2} \cdot \cos \alpha_d + (F_t)_{U_{\min}} \cdot \sin \alpha_e = (F_H)_{U_{\min}}$$  \hspace{1cm} (34)

where the frictional force is determined by

$$F_t = \frac{\tau_e \sin \beta \cos \alpha_e Q}{\cos(\phi_e + \beta - \alpha_e) \sin \phi_e}$$  \hspace{1cm} (35)

Therefore, $N_t$ is rewritten as

$$N_t = \frac{[(F_H) - (F_t)_{U_{\min}} \sin \alpha_e]}{\cos \alpha_{r2} \cos \alpha_d}$$  \hspace{1cm} (36)

The values of $F_T$ and $F_V$ are determined from the components of $N_t$ and $F_t$. That is

$$F_T = -N_t \cos \alpha_{r2} \sin \alpha_d + F_t (\sin \eta_c \cos \alpha_d - \cos \eta_c \sin \alpha_{r2} \sin \alpha_d)$$  \hspace{1cm} (37)

By contrast with the turning operation, as shown in Fig. 7, the workpiece carries out a rotary motion and the tool has a plane motion. The tooth path of a face-milling cutter is a cycloid as shown in Fig. 5. The comparison of tool geometry between the face milling cutter and turning tool is shown in Fig. 6. Where the radial angle, $\alpha_{r1}$, the axial angle, $\alpha_d$, and lead angle of face milling cutter are equal to the second normal side rake angle, $\alpha_{r2}$, the back rake angle $\alpha_d$ and the side cutting edge angle, $C_s$, respectively.

$$t_1 = f_\theta \cos C_s$$  \hspace{1cm} (38)
\[ f_\theta = f \sin \theta_X \] (39)
and
\[ W = d / \cos C_s \] (40)

where \( f = \text{feedrate} / (\text{rev} \cdot \text{per} \cdot \text{tooth}) \).

Figure 4: Flow chart of the inverse heat transfer solution
Figure 6 shows the unit chip cross section and various cutting force components exerted on workpiece at cutting edge where $F_{HH}$, $F_{VV}$ and $F_{TT}$ are equal to the cutting force components in turning. Thus the cutting forces are given by

$$F_X = F_{HH} \cos \theta_X + F_{VV} \sin \theta_X$$

$$F_Y = F_{HH} \sin \theta_X - F_{VV} \cos \theta_X$$

$$F_Z = F_{TT}$$

$$F_{VV} = (F_V)_M \cdot \cos C_s - (F_T)_M \cdot \sin C_s$$

$$F_{TT} = (F_T)_M \cdot \cos C_s + (F_V)_M \cdot \sin C_s$$

![Figure 5: Cutting forces model of face milling](image)

![Figure 6: Tool geometric between (a) turning (b) milling cutter](image)
2.2 Solid modeling of carbide tip

To develop a 3D finite element model for thermal analysis, a solid model of the tip can be established in three steps. First, the tip cross-section profile (TCSP) perpendicular to the main cutting edge was measured using a microscope, then CAD software, SolidWorks™, was used to generate the tip body by sweeping the TCSP along the main cutting edge with the specified pitch. Finally the tip’s main cutting edge was simulated to remove unwanted material and create a solid model of turning tip geometry, as shown in Fig. 6.

2.3 Finite element model

The finite element mesh of the carbide tip is shown in Fig. 7, which was modeled by 58,000 four-node hexahedral elements. As shown in the top view of Fig. 8, 8 × 6 nodes are located on the projected contact length between the tool and the workpiece, 3 × 6 nodes are located on the chamfered width of the main cutting edge, and 1 × 6 nodes are placed on flank wear.

![Figure 7: model chamfered edge tool](image_url)

![Figure 8: experimental cutting set-up](image_url)
2.4 Modified carbide tip temperature model

Magnitude of the tip’s load is shown in the following Eqs. (46) and (47)

\[ K = \frac{U_f}{A'} \]  
(46)

\[ A' = L_p(d + W_e + V_b) \]  
(47)

Where \( A' \) is the area of friction force action, \( U_f \) is the friction energy, \( W_e \) is the tip’s chamfered width, \( d \) is the cutting depth, \( V_b \) is the flank wear of the tip and for simplification, the value of \( V_b \) is set to be 0.1mm. \( L_f \) is the contact length between the cutting edge and the workpiece (Eqs. 19), \( L_p \) is the projected contact length between the tool and the workpiece, as referred to in Fig. 3, and can be determined by Eq. 20 and the following conditions.

\[ \rho c \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial x^2} + k \frac{\partial^2 T}{\partial y^2} + k \frac{\partial^2 T}{\partial z^2} \]  
(48)

where \( \rho \) is the density, \( c \) is the thermal conductivity, and \( k \) is the heat capacity.

\[ q_f = F_f V_c, q_{\text{tool}} = K q_f \] \[ \text{[Li and Shih (2005)]} \]  
(49)

3 Experiment method and procedure

To verify these force models, experiments were conducted using the set-up in Fig. 8. In measuring the cutting forces a Kistler type 9257B, three-component piezoelectric dynamometer was used with a data acquisition system that consisted of Kistler type 5807A charge amplifiers, all measured data were recorded by a data acquisition system and analyzed by the control software (Easyest). The composition of workpiece is \( C = 0.05\% \), \( Mn = 1.17\% \), \( P = 0.34\% \), \( S = 0.24\% \), \( Si = 0.29\% \), \( Ni = 9.14\% \), \( Cr = 18.45\% \), 168HB. The cutting tools used in the experiments are Sandvik p10, type SIP [Brookes (1992)]. Carbide-tipped tools: Back rake angle= 0°; side rake angle= 6°; end relief angle= 7°; side relief angle= 9°; end cutting edge angle= 70°; side cutting angles= 20°, 30°, 40°; and nose radius= 0.0 ~ 0.1mm.

4 Results and Discussion

4.1 The cutting forces

Fuh and Chang showed that increasing the side rake angles \( \alpha_{r1} \) and \( \alpha_{r2} \), decreases the cutting forces \( F_{HH} \), \( F_{VV} \) and \( F_{TT} \) as Ref. [Chang (2005)].
4.2 The cutting temperatures

Based on Li and Shih (2005), according to Eqs. (48) and (49), the flowchart for inverse heat transfer solution of $K$ is described in Fig. 5.

The results obtained from the finite element analyses are shown in Figs. 9-10 and described as follows:

1. Fig. 9 shows the cutting temperatures vs. cutting time for different values $C_s$ at $\alpha_{r1} = -30^\circ$ and $\alpha_{r2} = 30^\circ$ with a chamfered and an unchamfered sharp worn tool at $d=2.00\text{mm}$, $f=0.33\text{mm/rev}$, $V=120\text{m/min}$ respectively.

2. Fig. 9 shows that the cutting edge temperature of the chamfered main edge sharp worn tool was lower than unchamfered main cutting edge worn tool.

3. Fig. 9 shows that the cutting temperatures of the chamfered main cutting edge worn tool is the lowest, when $C_s = 30^\circ$, $\alpha_{r1} = -30^\circ$ and $\alpha_{r2} = 30^\circ$, and the temperature does not exceed $410^\circ\text{C}$.

4. Fig. 10, shows that the distribution of chamfered main cutting edge worn tool’s temperature was close to Fig. 8.

![Graph showing cutting temperatures vs. Cs, radial angle of unchamfered and chamfered sharp worn tools.](image)

Figure 9: shows the cutting temperatures vs. cutting time for different values $\alpha_{r1}$ and $\alpha_{r2}$ with a chamfered and an unchamfered sharp worn tool at $d=2.0\text{mm}$, $f=0.33\text{mm/rev}$, $V=120\text{m/min}$ at $30^\circ$ respectively.
5 Conclusions

Good correlations were obtained between predicted values and experimental results of forces during milling stainless steel with sharp tools [Chang (2005)]. A new model for sharp worn tools with chamfered main cutting edge has been developed by including the variation of shear plane areas. In this model, the energy method is also used to more accurately predict cutting force. The FEM and Inverse heat transfer solution for tool temperature in stainless milling is obtained and compared with experimental measurements. The good agreement demonstrates the accuracy of proposed model.

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References


