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# An Analysis of Carbide Tool Wear using Adhesive Wear Model

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**〈Abstract〉**

The present work is to analyze the effect of cutting temperature and the other fundamental cutting parameters on tool wear when high strength materials are machined. An analysis of carbide tool wear is carried out based on the mechanism of adhesive wear considering the effect of temperature.

Both of the derived wear equation for flank and crater are arranged into the Taylor tool-life equation and shown to be in accordance with the experimental results.

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# 응착마모 모델을 이용한 카바이드(Carbide) 공구 마모 해석

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기 계 공 학 과
(1980. 11. 17 접수)

**〈요 약〉**

본 연구는 고강도 재질의 절삭시 생기는 절삭열 및 그외 다른 기본절삭 인자들이 공구 마모에주는 영향을 해석함에 있다.

Carbide 공구마모의 해석이 온도의 영향을 고려한 응착마모 기구의 관점에서 행해졌다.

여기서 유도된 측면 및 경사면 마모에 대한 마모식은 모두 Taylor의 공구 수명식으로 정리되었으며, 실험 결과와 일치됨을 보였다.

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## Ⅰ. Introduction

One of useful wear relationships based on the adhesive wear mechanism was obtained by Archard [1] who considered encounters of asperities on the rubbing surface, assuming that the height of a wear particle would be equal to its width and that the sliding distance associated with the formation of a single particle was also equal to this width. With these assumptions the wear volume for a sliding distance S can be written as

$$W = K \cdot \mathrm{Ar} \cdot S \qquad (1)$$

where Ar is the real area of contact, and $K$ is the wear coefficient.

In the present work, a wear equation based on the Archard's wear model will be derived for the conditions occuring in the clearance and in the rake face. In deriving this equation the temperature dependence of the adhesion occuring at the point of contact is considered and the results of the analysis are compared with the experimental observation.

## Ⅱ. General Wear Theory

### 1. Real area of contact Ar

It will be assumed that, at any instant, there are $n_0$ asperity contacts per unit apparent area $A$ of the contact region that all have the same shape. Let the load between workpiece and tool be $L$, then each asperity contact supports a load

$$Li=L/n_0A \quad (2)$$

Let the load cause sufficient deformation to produce an area of contact $a$ at each asperity contact.

If the hardness of the deforming asperities is $H$, then

$$H=Li/a \quad (3)$$

where $a$ is the mean area of each asperity.

So, the real area of contact is expressed by

$$Ar=n_0\cdot A\cdot a=\frac{L}{H}. \quad (4)$$

### 2. Coefficient of Wear $K$

During metal cutting process, the surfaces in contact at the flank and rake of the cutting tool are nascent and clean. In this case it may be assumed that the wear rate will depend on the rate at which an interfacial material is formed through diffusion, dependent on the solubility of the different phases of the tool material in the metal over the surface [2]. Therefore the wear coefficient $K$ will vary almost in the same manner as the diffusion coefficient of the tool material

$$K=K_0 \exp(-E/R\cdot\theta) \quad (5)$$

where $K_0$: a constant

$E$: activation energy

$R$: universal gas constant

$\theta$: absolute temperature at the contact region.

It is known that the diffusion coefficient of material $D$ varies in a similar manner

$$D=D_0 \exp(-E/R\cdot\theta) \quad (6)$$

where $D_0$: a constant.

Fig. 1 shows the variation of diffusion coefficient of tungsten with respect to iron with temperature.

For simplicity of analysis, the nonlinear "$D$-$\theta$" characteristic curve can be linearized by two straight lines at low temperature zone and high temperature zone.

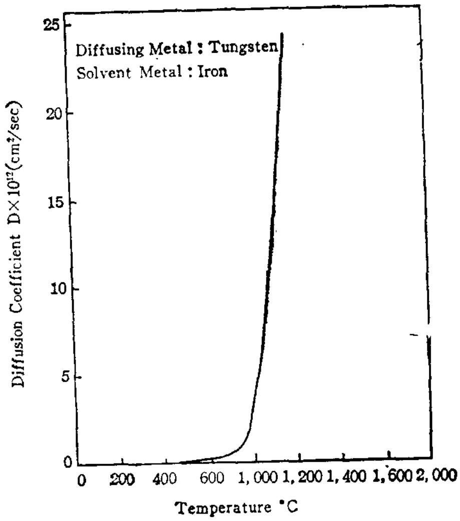


**Fig. 1 Variation of Diffusion Coefficient with Temperature for Tungsten.**

By analogy with diffusion coefficient, it may be assumed that the wear coefficient $K$ varies with temperature according to the equation [3]

$$K=K_0\cdot\theta^{\gamma} \quad (7)$$

where $\gamma$ varies between 2.05 and 14.3 for a monocarbide tool.

By substituting equations (4) and (7) into eq. (1), and it can be shown that

$$W=K_0\cdot\frac{L}{H}\cdot\theta^{\gamma}\cdot S \quad (8)$$

and $$dW=K_0\cdot\frac{L}{H}\cdot\theta^{\gamma}\cdot dS \quad (9)$$

## Ⅲ. Application of Adhesive Wear Theory to Tool Wear

Consider the situation when a tool of width $b$, rake angle $\alpha$, and clearance angle $\beta$ has worn until the length of wear land $l_f$ and the max. depth of crater $y$ are $l_0$ and $y_0$, respectively. [Fig. 2]

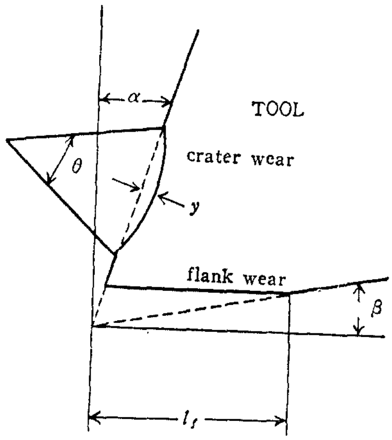


**Fig. 2 Idealized Profile of Worn Tool**

**1. Flank wear**

It has been shown [4] that during cutting with a tool of finite sharpness a small depth of workpiece material is extruded below the tool and that this causes a pressure $P_m$ to act over the clearance face/workpiece contact region, hence, the load between workpiece and tool,

$$L=P_m \cdot b \cdot l_f. \qquad (10)$$

Now $P_m$ will be related to the yield stress of the workpiece material which in turn is proportional to the workpiece hardness $H_m$, i.e.,

$$P_m=M_1 \cdot H_m \qquad (11)$$

where $M_1$: a constant.

By substituting eq. (10) and eq. (11) into eq. (9),

$$dW=K_0\frac{M_1 \cdot H_m}{H} \cdot \theta_f{}^{\gamma} \cdot b \cdot l_f \cdot dS \qquad (12)$$

where $\theta_f$: the temperature of flank face.

From Fig. 2, the wear volume removed from the clearance face of the tool

$$W=\frac{1}{2}\ \frac{b \cdot l_f{}^2}{[\cot\beta-\tan\alpha]} \qquad (13)$$

From eq. (12) and eq. (13),

$$\frac{dW}{dt}=\frac{b \cdot l_f}{[\cot\beta-\tan\alpha]}\ \frac{dl_f}{dt}=K_0\frac{M_1 \cdot H_m}{H} \cdot \theta_f{}^{\gamma} \cdot V \cdot b \cdot l_f \qquad (14)$$

where $V=dl/dt$,

$$\text{ie. } \frac{dl_f}{dt}=\Psi\, K_0\, \frac{M_1 \cdot H_m}{H}\, \theta_f{}^{\gamma} \cdot V \qquad (15)$$

where $\Psi=[\cot\beta-\tan\alpha]$.

Now hardness of the carbide tools $H$ drops rapidly with increasing temperature as shown in Fig. 3, but they remain much harder than steel under almost all considerations.

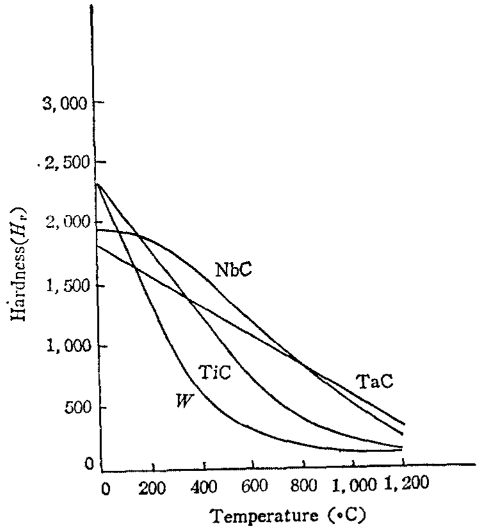


**Fig. 3 Hot Hardness Tests of Monocarbides of Four Transition Elements**

The tool hardness $H$ can be represented [5] as

$$H=H_0\left[\frac{\theta_f}{\theta_0}\right]^{-\delta} \qquad (16)$$

where $H_0$: the hardness of the tool at room temperature $\theta_0$

$\delta$: a constant.

Substituting eq. (16) into eq. (15)

$$\frac{dl_f}{dt}=\Psi \cdot K_1 \cdot \frac{M_1 \cdot H_m}{H_0} \cdot \theta_f^{\,p} \cdot V \qquad (17)$$

where $K_1=K_0\theta_0{}^{-\delta}$ and $p=\gamma+\delta$.

Shaw et al. [6] showed that the mean temperature $\bar{\theta}$ at the tip of a carbide cutting tool is given by the following expression obtained by dimensional analysis;

$$\bar{\theta}=R_1 \cdot \frac{u}{(k\rho c)^{1/2}}(VF)^{1/2} \qquad (18)$$

where $u$: the energy per unit volume,

$V$: the cutting speed

F: the feed

$k$: the thermal conductivity of the workpiece

$c$: the volume specific heat of the workpiece

$R_1$: a constant for a given tool.

Boothroyd, et al. [7] have suggested that the mean temperature $\bar{\theta}$ should bear some relation to $\theta_f$, the mean temperature in flank contact region and $\theta_c$, the mean temperature in crater region.

Therefore, from eq. (18)

$$\theta_f = R_1 \frac{u}{(k\rho c)^{f/2}} V^f \cdot F^f \tag{19}$$

where $R_1$, $f$ are constants.

Substituting eq. (19) into eq. (17)

$$\frac{dl_f}{dt} = \Psi \cdot K_1 \cdot R_1^p \cdot M_1 \cdot \frac{H_m}{H_0} \cdot \frac{u^p}{(k\rho c)^{pf/2}} \cdot V^{fp+1} \cdot F^{fp}. \tag{20}$$

Suppose the tool life criterion is chosen as $l_f = l_0$ and that $l_f$ becomes equal to $l_0$ after a cutting time $t=T$

$$l_0 = \Psi \cdot K_1 \cdot R_1^p \cdot M_1 \cdot \frac{H_m}{H_0} \frac{u^p}{(k\rho c)^{pf/2}} V^{fp+1} \cdot F^{fp} T. \tag{21}$$

This can be rearranged as

$$VT^{n_1} = C_1 \tag{22}$$

i.e., Taylor's tool-life equation

where $n_1 = \dfrac{1}{fp+1}$

and $C_1 = \left[ \dfrac{l_0 \cdot H_0 \cdot (k\rho c)^{pf/2}}{\Psi \cdot K_1 \cdot R_1^p \cdot M_1 \cdot H_m \cdot u^p \cdot F^{fp}} \right]^{n_1}$.

**2. Crater wear**

Consideration of normal and shear stresses in the rake face of cutting tool [8] has led to the model of orthogonal cutting shown in Fig. 4.

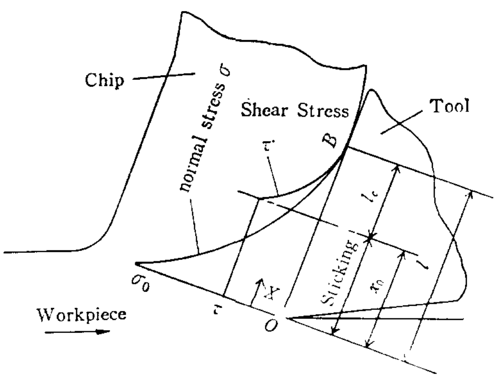


**Fig. 4 Model of Chip-Tool Friction in Orthogonal Cutting**

From the figure, the normal stress disribution on the tool face could be represented by the expression

$$\sigma = \sigma_0 \left(1 - \frac{x}{l}\right)^m \tag{23}$$

where $\sigma_0$: the max. normal stress, and $m$: a constant. The normal stresses between the chip and the tool are sufficiently high over the region of length $x_0$ adjacent to the cutting edge, termed the sticking region.

Therefore, sliding occurs only in the length $(l-x_0)$. The shear yield stress of the work material $\tau_s$ intersects the sliding stress curve at a point $x_0$, the adhesion length.

Suppose that the coefficient of friction acting between the chip material and tool material at the conditions obtaining during cutting is $\mu$. Thus, $x=x_0$ is defined as the point where $\tau_s = \mu\sigma$: that is,

$$\tau_s = \mu\sigma_0 \left(1 - \frac{x_0}{l}\right)^m \tag{24}$$

whence $$\left(1 - \frac{x_0}{l}\right)^m = \frac{\tau_s}{\mu\sigma_0}. \tag{25}$$

Now the normal force acting on the crater region $x_0 B$

$$L = b\int_{x_0}^{l} \sigma b x = b\sigma_0 \int_{x_0}^{l} \left(1 - \frac{x}{l}\right)^m$$

$$= \frac{b\sigma_0(l-x_0)}{1+m} \left(1 - \frac{x_0}{l}\right)^m. \tag{26}$$

Substitute eq. (25) into eq. (26)

$$L = \frac{M_2 \cdot b \cdot l_c \cdot H_m}{(1+m)} \tag{27}$$

where $\tau_s = M_1 \cdot H_m$, $M_2$: a constant and $l_c$: crater length $(= l - x_0)$.

And by substituting eq. (27) into eq. (9)

$$dW = K_0 \frac{M_2 b\ l_c}{H(1+m)\mu} H_m \cdot \theta_c^{\gamma} \cdot dS \tag{28}$$

where $\theta_f$: the temperature at crater face.

Taylor [9] has shown that if the crater cross-setion is considered to be a shallow segment bounded by a chord and a curve of the second degree, then, approximately,

$$\frac{dW}{dt} = \frac{d}{dt}\left(\frac{2}{3} b.y.l_c\right) \tag{29}$$

where $y$ is the maximum depth of the crater.

Assuming that the crater length $l_c$ is nearly kept a constant value at a given cutting condition,

$$\text{then, } \frac{dW}{dt}=\frac{2}{3}bl_c\frac{dy}{dt}. \tag{30}$$

From eq. (28) and eq. (30)

$$\frac{2}{3}bl_c\frac{dy}{dt}=K_0\frac{M_2\cdot b\cdot l_c}{H(1+m)\mu}H_m\theta_c^{\gamma}\cdot V. \tag{31}$$

Rearranging eq. (31), we will get

$$\frac{dy}{dt}=\frac{3K_0}{2(1+m)\mu}\frac{M_2H_m}{H}\theta_c^{\gamma}\cdot V. \tag{32}$$

And similar to eq. (16), the tool hardness $H$ can be expressed as,

$$H=H_0\left[\frac{\theta_c}{\theta_0}\right]^{-\delta}. \tag{33}$$

Substitute eq.(33) into eq. (32),

$$\frac{dy}{dt}=\frac{3K_1}{2(1+m)\mu}\cdot\frac{M_2H_m}{H_0}\cdot\theta_c^{p}\cdot V \tag{34}$$

where $K_1=K_0\theta_0^{-\delta}$ and $p=\gamma+\delta$.

Similar to eq. (19): it can be assumed that $\theta_c$ can be expressed as,

$$\theta_c=R_2\frac{u}{(k\rho c)^{pr/2}}V^rF^r \tag{35}$$

where $R_2$ and $r$ are constants.

Substitute eq. (35) into eq.(34),

$$\frac{dy}{dt}=\frac{3}{2(1+m)\mu}K_1R_2^pM_2\frac{H_m}{H_0}\cdot\frac{u^p}{(k\rho c)^{pf/2}}V^{rp+1}\cdot F^{rp}. \tag{36}$$

Suppose that the tool life criterion is chosen as $y=y_0$ and that $y$ becomes equal to $y_0$ after a cutting time $t=T$, then,

$$y_0=\frac{3}{2(1+m)\mu}\cdot K_1\cdot R_2^p\cdot M_2\frac{H_m}{H_0}\cdot\frac{u^p}{(k\rho c)^{pf/2}}V^{rp+1}F^{rp}T. \tag{37}$$

This eq. (37) can be rearranged as,

$$VT^{n_2}=C_2 \tag{38}$$

where $n_2=\dfrac{1}{rp+1}$

and $C_2=\left[\dfrac{2y_0(1+m)\cdot\mu\cdot H_0(k\rho c)^{pr/2}}{3K_1\cdot R_2^p\cdot M_2\cdot H_m\cdot u^p\cdot F^{rp}}\right]^{n_2}$

## Ⅳ. Discussion

### 1. The comparison with flank wear and crater wear.

It can be expected the dependence of flank wear and crater wear on the individual cutting parameters by comparing with eq.(22) and eq.(38). Assuming that $p$ in the exponents $n_1$ and $n_2$ is constant, it can be found that the tool life exponents $n_1$ and $n_2$ depend on $f$ and $r$, respectively.

Here it can be stated that the tool life is affected by the crater wear than the flank wear, as the cutting temperature is higher and the temperature distribution is steeper. It will be explained that the wear rate under high cutting temperature is accelerated by the diffusion mechanism [10]. In reality, it is reasonable that the wear criterion of crater is applied as the tool-life constraint in machining of high strength materials at high cutting speed [11].

### 2. Effect of workpiece material

The workpiece material is an important variable affecting the tool-life, and the hardness of work material $H_m$ is the easiest variable to measure and to relate to tool life.

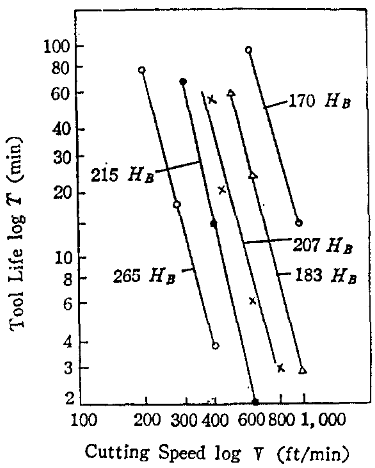


Fig.5 V-T Relation for Cast Iron

As might be expected from eq. (22) and eq. (38), the harder the work material the lower the tool life constants $C_1$ and $C_2$.

The tests [12] for machining of cast iron with different Brinell hardness are represented on Fig. 5, and the comparison with S55C and SCM4 are shown in Fig. 6 [11].

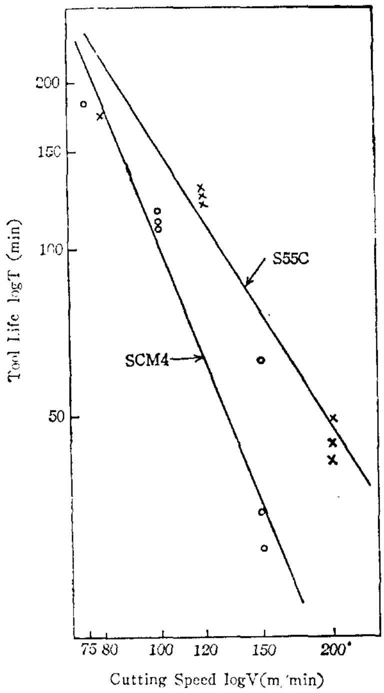


**Fig. 6 V-T Relation for S55C and SCM4.**

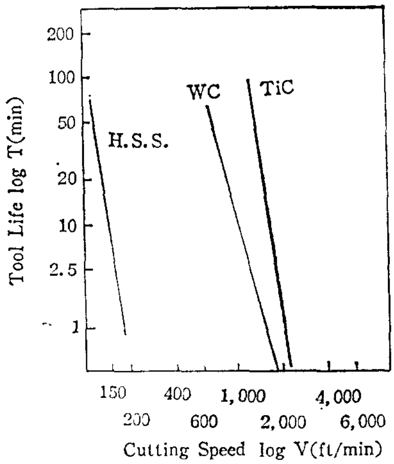


**Fig. 7 Effect of Tool Material Variation on V-T Relation**

### 3. Effect of tool material

At a given temperature, a high hardness of tool usually gives the good wear resistance and increases the tool life constant. Fig. 7 shows the comparison of different tool materials [12].

### 4. Effect of feed

The effect of a change in feed on the tool life-cutting speed relationship is plotted on Fig. 8, and the tool life constant $C_1$'s are listed with respect to the feed rate [12].

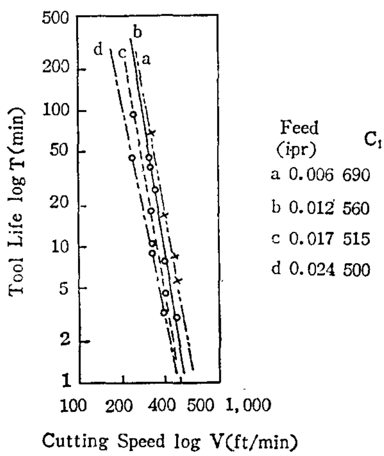


**Fig. 8 Effect of Feed Variation on V-T Relation**

### 5. Effect of tool-life criteria

Takeyama [13] has investigated the tool life constant $C_2$ related to several crater depth criteria [Fig. 9] and found that as $y_0$ increases, $C_2$ increases in agreement with eq. (38).

Weber [14] has shown the tool life constant $C_1$ related to several flank wear land criteria [Fig. 10] in agreement with the eq. (22) also.

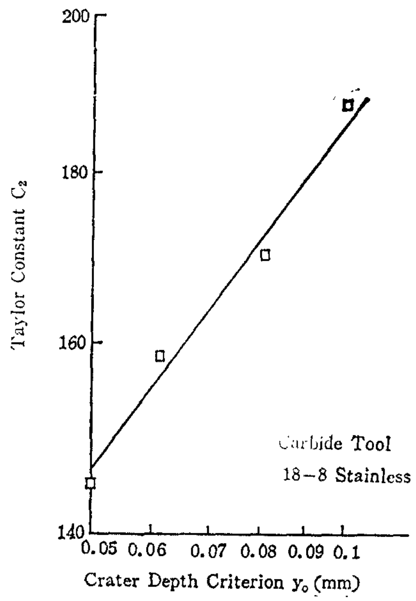


**Fig. 9 The Dependence of the Taylor Constant $C_2$ on the Crater Depth Criterion $y_0$**

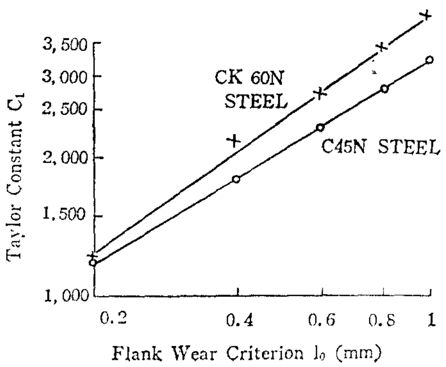


**Fig. 10 The Dependence of the Taylor Constant $C_1$ on the Flank Wear Land Criterion. $l_0$**

## Ⅴ. Conclusion

As a result of the above discussions it was found that the theoretical wear equations and tool life equations based on the adhesive wear model are consistent with experimental observations.

Futher ananysis will then be possible to develop quantitatively the equation.

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