

Calculation of the Intensity of Adhesive-Fatigue Wear of Cutting Tools

V Bibik¹, N Ivushkina², D Arhipova³

¹Assistant Professor, Yurga Institute of Technology of Tomsk Polytechnic University
652055, Russia, Kemerovskaya oblast, Yurga, Leningradskaya str., 26.

²Assistant, Yurga Institute of Technology of Tomsk Polytechnic University
652055, Russia, Kemerovskaya oblast, Yurga, Leningradskaya str., 26.

³Student of Yurga Institute of Technology of Tomsk Polytechnic University
652055, Russia, Kemerovskaya oblast, Yurga, Leningradskaya str., 26.

E-mail: bibik@tpu.ru

Abstract. On the base of kinetic equation of strength the authors suggest the method carbide tools wear intensity calculation allowing comparing wear resistance basing on tool material thermal diffusivity coefficient. The authors obtain equations of tool wear resistance dependences upon its thermal diffusivity which show close correlation between these parameters. The results of wear intensity calculations correspond well to the experimental results under the cutting rates corresponding to the region of adhesive-fatigue wear. The inserts with low thermal diffusivity coefficient are characterized by lower wear rate at the initial and normal stages of wear.

1. Introduction

Modern unmanned NC machine tools are very sensitive to the homogeneity of the cutting properties of inserts. Modern engineering production also makes wide use of split-design tools, such as face milling cutters which apply several inserts at one time when cutting. If these inserts have highly disperse wear resistance properties, then, some of them may fail prematurely which can lead either to defects or to equipment failure.

At the same time, due to their composition and technology of fabrication, inserts are characterized by great dispersion of wear resistance. Wear resistance for the inserts of the same brand but of different batches may differ by dozens of times, within one batch – by several times, for the various points of the insert – by 1.5 – 3 times [1].

2. Results and Discussion

Analysis of carbide cutting tools operation under factory conditions showed that in most cases the tools are operated at cutting rates corresponding to the temperatures of up to 900°C in the cutting area, i.e. under cutting rates that are smaller than the rate corresponding to the break-over point of the curve showing dependence of wear upon the temperature. In that region of the curve wear resistance of the tool increases with temperature growth. This temperature region corresponds to adhesive wear. Adhesive wear occurs when particles of tool material are broken away due to combined forces of adhesion between the tool material and the workpiece. According to many experimental studies [2],



the contact surfaces of tools are always covered with adhered particles of worked material of varying thickness. It means that destruction of adhesive bonding far more often affects the area of the worked material than that of tool material. That is why the areas of actual contact of tool material appear to be loaded with cyclic forces and their destruction occurs due to fatigue. Thus, it is important to estimate the wear rate of carbide tools under temperatures below 900°C with consideration to the fatigue nature of adhesive wear.

According to many research papers the process of fatigue damage accumulation and development is associated with micro- and macroflow of material which is determined by the processes of nucleation, motion, generation and disappearing of linear defects – dislocations [3].

Starting to talk about hard alloy destruction we should note the peculiarities of hard alloy structure. It contains grains of tungsten carbide cemented together with cobalt bound and according to many researchers its strength is due to its carbide “skeleton”. When the hard alloy is destroyed the fracture may develop along the following directions: across tungsten carbide grains, along the boundary between carbide grains, along WC-Co phase boundary and, finally, across the cobalt phase. The major amount of destruction affects the boundary between the cementing phase and the carbide grains, i.e. the fracture goes round carbide grains and the view of the breaking shows well-defined contours of tungsten carbide grains.

Assuming that carbide grain is exposed to constant shear load and accepting the simplification that carbide grain is spherical, let us use the kinetic equation of strength [4] to calculate the time until cobalt bound destruction and carbide grain detachment, replacing normal stress σ with tangential stress τ^* .

$$\tau = \tau_0 \exp \frac{U_0 - \gamma \tau^*}{kT}, \quad (1)$$

where T – test temperature; k – Boltzmann constant; U_0 – energy of interatomic bond; τ_0 – period of thermal oscillation of atoms; γ - activation volume.

In paper [4] it is established that energy of interatomic bond

$$U_0 = \varepsilon_* \frac{C_a}{\alpha}, \quad (2)$$

where ε_* - ultimate strain of the interatomic bond; C_a - atomic heat; α - linear expansion coefficient.

Activation volume [4], i/e/ the volume where the initial crack is formed can be determined as

$$\gamma = \frac{C_a}{\alpha E} \chi, \quad (3)$$

where E – modulus of elasticity; χ - overload factor.

Overload factor [4]

$$\chi = \Lambda/a_0, \quad (4)$$

where Λ - free path length of phonons; a_0 – atomic size.

Free path length of phonons can be found from Debuy equation

$$\lambda = \frac{1}{3} cS\Lambda\rho, \quad (5)$$

where λ - thermal conduction; c – specific thermal capacity.

Consequently

$$\Lambda = \frac{3\lambda}{cS\rho}. \quad (6)$$

Thermal conduction is expressed through thermal diffusivity (a)

$$\lambda = c\rho a. \quad (7)$$

After combining (7) and (6) we obtain

$$\Lambda = 3a/S. \quad (8)$$

Applying (8) to (4) and then, applying the obtained expression to (3) we obtain

$$\gamma = \frac{3C_a a}{\alpha E S a_0}, \quad (9)$$

where a – thermal diffusivity

Thus, durability of carbide grain

$$\tau = \tau_0 \exp \frac{\frac{C_a}{\alpha} \left(\varepsilon_* - \frac{3a\tau_*}{E S a_0} \right)}{kT}. \quad (10)$$

Under the real conditions the carbide grain is affected by cyclic saw-like loading

$$\tau_* = 0.5\tau_{\max} - \frac{\tau_{\max}}{\pi} \left(\sin \omega t + \frac{1}{2} \sin 2\omega t + \frac{1}{3} \sin 3\omega t + \dots \right), \quad (11)$$

where $\omega = 2\pi/p$ – cyclic frequency; p - period.

Considering every contact point being exposed to 10^3 -fold shear stress impact [2] and knowing the cutting rate we can determine the period of loading.

According to the theory of damage summation and according to Bailey criterion [5] during the time period Δt durability reduce $\Delta t/\tau_i$, occurs if the destruction rate during this period was $1/\tau_i$. The cobalt bound will be completely destroyed under [5]

$$\sum \left(\frac{\Delta t}{\tau_i} \right) = 1. \quad (12)$$

Or in the integrated form

$$\int_0^{\tau} \frac{dt}{\tau(t)} = 1 \quad (13)$$

Replacing in formula (13) $\tau(t)$ with the durability formula (10) we obtain

$$\int_0^{t_p} \frac{dt}{\tau_0 \exp \frac{U_0 - \gamma(a)\tau_*(t)}{kT}} = 1. \quad (14)$$

Applying the form of loading (11) into formula (14), knowing the experimental value of thermal diffusivity and solving the equation by iteration with reference to t_p , we can find the durability of carbide grain. Taking the simplification that carbide grain is spherical we find the volume of carbide grain (V), wear intensity

$$J = \frac{V}{t_p}. \quad (15)$$

Set by the wear criterion the worn part of the tool is found. For the front surface [6]

$$V_w = \frac{2}{3} a b h_0, \quad (16)$$

where a – width of the crater; b - length of the crater; h_0 – depth of the crater.

For the back surface [6]

$$V_w = \frac{tg \alpha h_k^2 b}{2(1 - tg \gamma tg \alpha)}, \quad (17)$$

where a – width of the chip; α - back clearance angle; γ - face angle; h_s – wear of the back surface. And so, we can find the tool durability

$$T = \frac{V_w}{J}. \quad (18)$$

In equation (14) activation volume γ is the function of thermal diffusivity, so to compare the intensity of wear of a batch of inserts we need to measure the thermal diffusivity of every insert and calculate the approximate intensity of wear.

To measure the thermal diffusivity the method of "flash" [7] was chosen. This method allows finding the thermal diffusivity within several seconds. The test unit included the following equipment: helium-neon laser LG-78, pulse neodymium-glass laser GOS-301, a unit for noncontact temperature measuring TAU-4 (Tomsk Polytechnic University), pulse generator G5-56, oscillograph S8-14 [8].

Two techniques were applied for the thermal diffusivity measuring. The first one was to expose the face of a flat specimen which initial temperature was maintained at the same level to uniformly distributed pulse heating. At the back of the specimen chronological thermogram was registered (the two-sided technique). The second technique was to heat the specimen face with the chronological diagram of this face being registered (the one-sided technique).

The specimen was heated by the neodymium laser impulse (wave-length 1.06 micrometer, pulse duration 4 ms, energy 1.3÷14 Joule, the diameter of the heated area on the specimen surface – 4÷9 mm). The temperature measurements of the heated areas were carried out with TAU-4 (Tomsk Polytechnic University) and registered by an oscillograph with a storage tube. The pulse generator was used to delay the oscillograph for the time of the neodymium lasering, (for the one-sided technique). The visible helium-neon laser is necessary for optical channel mastering. To separate the thermal radiation of the specimen (wave length 2-8 micrometer) from the reflected laser impulse (wave-length 1.06 micrometer), one-sided thermal diffusivity measurement technique being used, a germanium filter placed at TAU-4 input port was applied

For the two-sided technique, we employed the Parker formula Eq. 19, [7] to evaluate thermal diffusivity.

$$a = 0.139L^2/\tau_{0.5} \quad (19)$$

where $\tau_{0.5}$ - half of the time needed to achieve the maximum temperature on the specimen, L – specimen thickness.

For the one-sided technique, thermal diffusivity was calculated from a theoretical equation describing the cooling stage of the specimen in the cylindrical coordinates Eq. 20 [9]

$$T(x, r) = \frac{W}{c\rho L} \left[1 + 2 \sum_{n=1}^{\infty} \cos n\pi x \cdot \exp(-n^2 \pi^2 F_0) \right] \times \left[1 + 2 \sum_{m=1}^{\infty} \frac{J_1(Z_m R_H/R) J_0(Z_m r/R)}{(Z_m R_H/R) J_0^2(Z_m)} \exp\left(-Z_m^2 \frac{L^2}{R^2} F_0\right) \right], \quad (20)$$

where J_0, J_1 , - Bessel first-class zero and first order function; Z_m - positive roots of a characteristic equation: $J_1(Z)=0$; W - absorbed energy; c - heat capacity; ρ - density; $F_0=a \tau/L^2$ - Fourier criterion (no dimensional time); a - thermal diffusivity; τ - time; R - the specimen radius; R_H - - the heated area radius.

To try the suggested method of adhesive-fatigue wear calculation we calculated the intensity under structural steel 1040 machining by a cutter produced from hard alloy MR7 (9% Co, 6% Tic, 85% WC) for one of the inserts.

The volume of the worn part of the tool was calculated according to formula (16). The calculated wear intensity was 0.12 mm³/min. Intensity of wear obtained in the course of wear testing was 0.01 mm³/min.

To analyze the thermal diffusivity influence upon the tool wear resistance the following types of reversible cemented carbide cutting plates were chosen: MP7, P35 (9% Co, 91% WC, three-layer coating TiC-Ti(CN)-TiN), produced in Sweden.

All the plates were thoroughly examined for fractures, crumbling and other defects with an optical microscope. Resistance of MP7 grade plates was tested when steel-turning (1040 steel) under the

following cutting conditions $V=200$ m/min, $S=0.18$ mm/rev $t=1$ mm. The resistance criterion – wear crater width 0.8 mm. P35 grade plate resistance was taken from [10].

The plate thermal diffusivity was estimated according to the method described above.

In the conclusion the correlation between the cemented carbide cutting inserts wear resistance and their thermal diffusivity coefficients was estimated. The corresponding correlation diagrams and graphical correlations are represented in fig. 1-3. The regression equations and correlation coefficients are shown in table 1.

Table 1. Correlation coefficients

Tool material	Technique	Regression equation	Correlation coefficients
MP7	One-sided	$T=9.38 - 0.23a$	-0.63
MP7	Two-sided	$T=11.65 - 0.12a$	-0.62
P35	One-sided	$T=51.18 - 2.92a$	-0.66

Diagram 4 shows MP7 grade plates wear curves having the smallest, the medium and the largest thermal diffusivity coefficient values. It is evident that the plates with the lower thermal diffusivity coefficient are characterized by lower wear factor at the initial and normal wear stages.

3. Conclusion

Under the cutting rates corresponding to adhesive-fatigue wear, wear of cutting tool occurs due to accumulation of defects in the crystal lattice in the cobalt bound. When the density of dislocations in the cobalt bound achieves its critical values, crack initiation and its further growth occur resulting in detachment of carbide phase grain. On the base of kinetic equation of strength the authors suggest a method for calculating carbide tool wear intensity. The given method allows completing comparative analysis of wear resistance on the base of tool material thermal diffusivity coefficient. The intensity of tool wear is inversely related to thermal diffusivity coefficient.

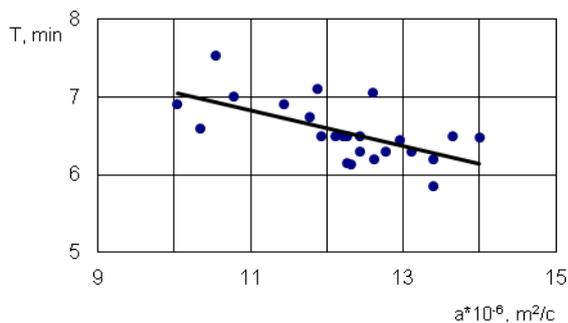


Figure 1. Correlation diagram and T (a) function for MP7 (two-sided technique)

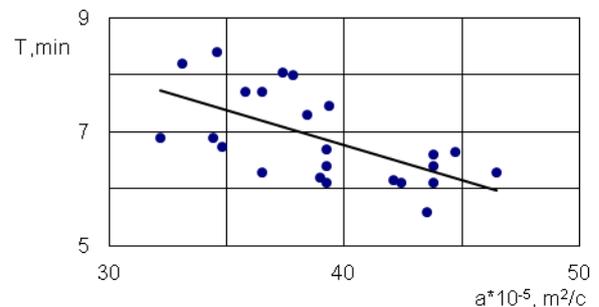


Figure 2. Correlation diagram and T (a) function for MP7 (one-sided technique)

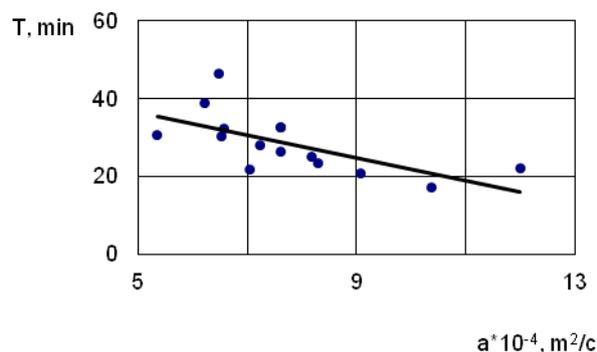


Figure 3. Correlation diagram and $T(a)$ function for P35 (one-sided technique)

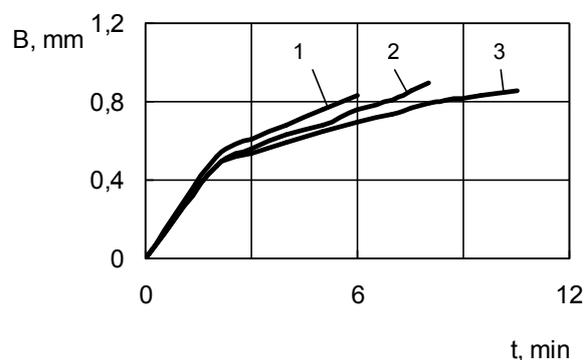


Figure 4. Plate wear curves for different thermal diffusivity values for MP7
 (1 – $43,5 \cdot 10^{-5} \text{ m}^2/\text{c}$; 2 – $39,2 \cdot 10^{-5} \text{ m}^2/\text{c}$;
 3 – $34,6 \cdot 10^{-5} \text{ m}^2/\text{c}$)

References

- [1] Etin A.O., Wilson A.L. Reliability of the cutting tools with modern cutting material inserts. // Machine tools and the cutting tool. - 1983, No 7. pp. 21-23 Development of the science dealing with metal-cutting./ Under the editorship of Bobrov V.F. and others - M.: Mashinostroyeniye, 1967. - 416 p.
- [2] Ivanova V.S. Fatigue degradation of metals. – M.: Publishing house for ferrous and non-ferrous metallurgy, 1963. – 272 p.
- [3] Zhurkov S.N. Dilaton mechanism of solid bodies strength // Physics of strength and plasticity. – L.: Nauka, 1986. – P. 5-11.
- [4] Regel V.R., Slutsker A.I., Tomashevsky E.E. The kinetic nature of solids. – M.: Nauka, 1974. – 560 p.
- [5] Petrushin S.I. Study of the cutting properties of inserted cutters when machining gray cast iron. Ph.D. thesis in Engineering Science: 05.03.01. – Pres. on 18.02.80 : Appr. 25.02.80. – M.: 1979. – 149 p.
- [6] Loladze T.N. Wear of cutting tools. – M.: Publishing house for technical and scientific literature. 1958. – 356 p.
- [7] Parker W.J., Jenkins R.J., Buttler C.P., Abbot G.L. Flash Method of Determining Thermal Diffusivity, Heat Capacity and Thermal Conductivity. - J. Appl. Phys., vol.32, No.9, Sept. 1961. - pp. 1679-1684.
- [8] Bibik V., Petrova E. Methods of Forecasting Wear Resistance of Cutting Tools Based on the Properties of Their Materials. Applied Mechanics and Materials Vol. 682 (2014) pp 491-494. Trans Tech Publication, Switzerland.
- [9] Nagorski Z. Podstawy teoretyczne impulsowych metod badania wspotczynnika wyrownywania temperaturi materialow modeli elementow konstrukcyjnych // Archiwum termodynamiki. - 1984. - Vol.5. - № 3/4.
- [10] Vavilov V.P., Pushnykh V.A., Shipulin A.V. Forecasting of tools wear resistance using their thermal diffusivity. // Problems of ensuring high quality of engineering products. / Proceedings of international scientific and technical conference. – Krasnoyarsk, 1994. – P. 223-240.