Development of a mathematical model for tool wear in dry machining of Ti6Al4V with coated cemented carbide tool

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Abstract

The current work is to develop a mathematical model for tool wear. A mathematical model of adhesive tool wear has been developed at dry machining condition. The model is further validated with the help of experimental results for 25 minutes. The maximum error in the prediction is limited to 7% for the current model and set of process parameters. It is expected that the current model can be extended for other set of tool material, coating and workpiece at different cutting conditions (flood, cryogenic and MQL).

Keywords: Tool wear, flank wear, wear model, Ti6Al4V, Milling, CrTiAlN coating, carbide tool.

1. INTRODUCTION

Ti and its alloys are mostly used in aerospace application for manufacturing of engine spares, compressors, and frames due to their excellent mechanical characteristics and high corrosion resistance [1]. It is widely used in biomedical applications and chemical industries [2]. Despite all valuable properties Ti shows poor machinability. Poor thermal conductivity and high hardness results in high rate of tool wear that hampers surface quality, dimensional accuracy and increase in the machining cost. Lim S et.al. [3] Suggested that either have more flank wear resistance tools or improve machining environments will result in lower tool damage. The flank wear mechanism of hard coated carbide tool during machining of Ti6Al4V have reported by Wang et.al.[4]. According to them, flank wear occurs in a non-uniform manner and the adhesion is the major cause of the flank wear of the tool. Venugopal et. al. [5] have performed the machinability test which shown that the high cutting temperature throughout the machining leads to damage of the tool. Lee et.al. [6] shown the effect of the machining condition on the tool life by taking three types of machining environment as dry, cryogenic and cryogenic plus preheated workpiece in their experiment. Zhao et.al. [7] have studied the effect of internal cooling on tool flank wear in orthogonal machining.

In machining tool wear at cutting edge is classified as crater wear, flank wear, nose wear, and chipping of material. Tool life is more frequently determined by the amount of flank wear because it has the direct effect on surface finish and dimensional accuracy of the final product [8]. Several analytical models are developed by various researchers to predict the tool wear rate and tool life for different tool work piece combination. Literature survey showed that the mathematical modeling of tool wear with hard coating is not reported in the open literature. The tool wear prediction can help in improving machining parameters and hence can reduce the cost of the product.

2. FLANK WEAR MODELING FOR MACHINING TITANIUM ALLOY (TI6AL4V)

Flank wear occurs due to abrasion wear, adhesive wear, diffusion wear, fatigue and delamination wear. It depends on the various factor such as cutting temperature, cutting speed, tool and workpiece material, machining environment and tool geometry. Abrasive wear is the damage to a surface, which arises due to the motion relative to the surface of either harder asperities or perhaps hard particles trapped at the interface [9]. Relative hardness is the key factor for the wear rate of metals. Rabinowicz [10] found that three body divided abrasive wear behavior is mainly depend on the relative hardness. Based on the Rabinowicz [10] analysis, Kramer [11] summarized that the abrasive wear volume per unit sliding distance was related to $K(P_a^{(n-1)}/P_t^n)$ where P_a and P_t are the hardness of abrasive particle and tool material respectively, n and K are constants that depend upon the ratio of abrasive and tool hardness. Wang et.al. [4] showed that no symptom of abrasive wear was detected on the tool in turning of Ti alloy. The measured hardness of Ti6Al4V alloy is in the range of 380-390 HV and the Hardness of hard coated CrTiAlN carbide tool is 30 GPa [12]. Due to the significant difference in the hardness of job and tool the abrasive wear can be ignored.
Adhesive wear can be considered as key mechanism for tool wear, due to the highly chemical reactivity of Ti alloy [13]. Arched Equation for Adhesive wear is given by Shaw and Drike [14] as:

$$dW = A_r \frac{c}{b} Z dL \tag{1}$$

Where dW = wear volume for sliding distance dL, A_r = real contact area, c = postulated plate height corresponding wear particle, b= mean spacing of asperities and Z= Holm's probability.

For defining the normal stress at contact surface Usui and Shirakashi [15] revised equation (1) as follows:

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$$dW = \frac{\sigma_t}{H}\frac{c}{b}ZdL \quad (2)$$

Where $\sigma_t =$ normal stress on flank face (assumed) and H= Hardness of the metal

The apparent area of the flank wear

$$A = wh_f \quad (3)$$

Where, w is the width of the cut and h_f is the flank wear land on the tool

Hence,

$$\sigma_t = \frac{F_t}{A} = \frac{F_t}{wh_f} \quad (4)$$

Here F_t is the thrust force in cutting.

So, rewriting the equation (2) as

$$dW = \frac{F_t}{wh_f H}\frac{c}{b}ZdL \quad (5)$$

From the literature survey, it has been found that the tool wear depends on hardness and normal stress/thrust force. Thus, consider $K = (c/b)Z$ in equation (5), we have:

$$dW = \frac{F_t\,K}{wh_f H}dL \quad (6)$$

Assume the cutting speed is v and the distance travelled in dt time is dL by equation (6) the final wear equation can be written as:

$$dW = \frac{F_t\,K\,v}{wh_f H}dt \quad (7)$$

Geometric Flank Wear Loss:

The following assumptions have been considered to convert end milling cutting (oblique cutting) into 2-D orthogonal for simplicity as suggested by Shaw [16].

1. “The effect of rake angle α upon tool life, surface finish is the same with a milling cutter as it is with a simple 2-D tool.”

2. “Stabler’s rule simply means that the chip will take a direction relative to the cutting edge so that there is no change in width as the metal crosses the cutting edge.”

3. To determine the effective rake angle α for any tool with inclination stabler’s rule is used

$$sin\,\alpha = sin^2 i + cos^2 i\,.\,sin\,\alpha_n \quad (8)$$

Where $\alpha =$ effective rake angle, $i =$ inclination or helix angle, α_n = normal rake angle.

Fig.1 shows the flank wear growth characteristics, the volume of worn tool can be calculated at any instant (t) through a time interval of dt [17] . The volume of worn tool mainly depends upon the tool geometry i.e. tool rake angle, relief angle and width of cut. Here we consider tool rake angle and clearance angle as the key factor involved in geometrical flank wear loss of the tool.

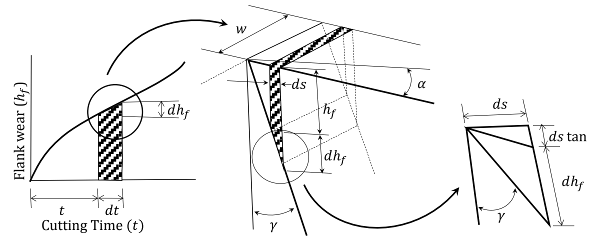

Fig. 1: Flank wear growth characteristics [17]

Therefore, the final flank wear rate equation on the basis of geometry of figure (1) was given by Bhattacharyya A. [17] as shown below:

$$\frac{dw}{dt} = \frac{\rho\,tan\,\gamma}{1 - tan\,\alpha\,tan\,\gamma}wh_f\frac{dh_f}{dt} \quad (9)$$

Where $\alpha =$ rake angle, $\gamma =$ relief angle, ρ =density of the tool material, $dh_f =$ increment of flank wear land growth during a time interval of time Δt.

The quantity $\left[\varphi = \frac{\rho\,tan\,\gamma}{1 - tan\,\alpha\,tan\,\gamma}\right]$ is depend upon tool geometry and can be taken as a constant for one type of tool geometry now rewriting the equation (9) as

$$\frac{dw}{dt} = \varphi wh_f\frac{dh_f}{dt} \quad (10)$$

Now equating equation (7) and equation (10)

$$\varphi wh_f\frac{dh_f}{dt} = \frac{F_t\,K\,v}{wh_f H} \quad (11)$$

Rearranging and integrating equation (11),

$$\int_0^{h_f} \varphi w^2 h_f{}^2 H\,dh_f = \int_0^t F_t K v dt + c \quad (12)$$

Applying boundary condition i.e. at $t = 0, h_f = 0$ and $c = 0$, thus the integration leads to

$$h_f = \left[\frac{3\,K\,F_t\,v\,t}{\varphi\,w^2\,H}\right]^{1/3} \quad (13)$$

Where $\varphi = \frac{\rho\,tan\,\gamma}{1 - tan\,\alpha\,tan\,\gamma}$ is a constant for one type of tool geometry.

The data used in the equation (13) is as $\rho = 14.4 \times 10^3 kg/m^3$ [18], $\gamma = 7°,\ \alpha = 21°,\ v = 25m/min,\ K$ is the experimental coefficient, and the value of thrust force calculated by the dynamometer is 9.19 N. In the above formula (13) time (t) and hardness *(H)* are the variables. H varies with temperature as well as with the flank wear length. As wear length increases the hard coating reduces. Since, the coating thickness is only 3 μm and its horizontal length component is 27 μm (rake angle 21° and clearance angle 7°). It shows after a flank wear length of 27 μm the wear will open to the tool base material (WC). Therefore, after a flank wear length more than the length of coating the rate of tool wear will increase rapidly. Hardness of coating varies between 30 GPa to 20 GPa [19] and hardness of tool material between 17 GPa to 4 GPa [20] with a temperature range of 300°K to 1173°K. Since, the cutting temperature reaches at 1173°K after few minutes [5], all values of hardness have been taken at this temperature.

3. EXPERIMENTAL WORK AND MODEL VALIDATION

Experiments have been performed to validate the formulation. Fig.2 (a) shows a three-axis desktop vertical milling machining set up with resolution of 10 µm. WC end milling cutter of size ϕ3 mm coated with CrTiAlN have been taken in the experiments. Kistler dynamometer (9256C2) was used to estimate the cutting forces during the machining operation. The process parameter used in the study were set as cutting speed 25 m/min, a depth of cut 50 µm and a feed rate of 50 µm/tooth/rev. Optical microscope was used to measure the wear length on flank face. Tool wear image is shown in Fig. 2 (b). Experimental and predicted values of tool wear with time is plotted in Fig. 3.

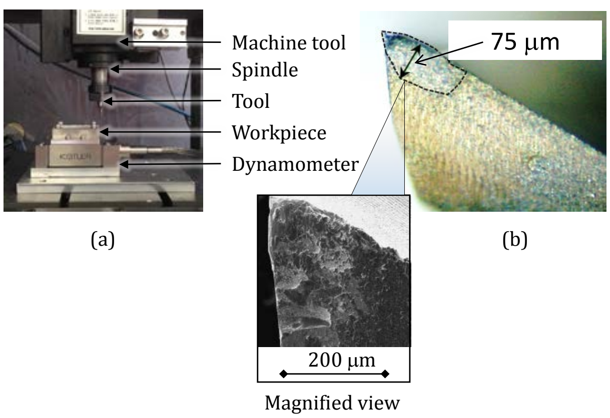

Fig. 2: (a) Experimental set up; (b) Tool wear morphology of the 3mm CrTiAlN-coated tungsten carbide tool under the dry machining condition.

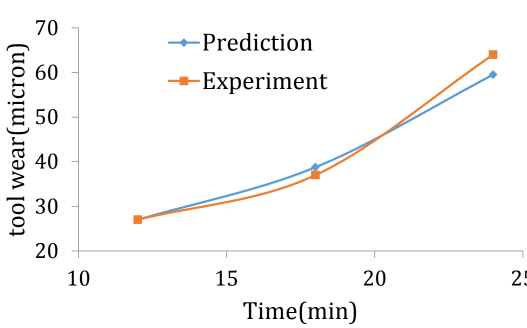

Fig. 3: Flank wear comparison between experimental data and wear model prediction

4. CONCLUSION

A mathematical model has been developed in the current work. The model is considering adhesive wear while the abrasive wear was neglected. The model considers the effect of hard coating and removal of coating. The model is predicting the tool wear with a maximum error in prediction is 7% at a cutting temperature of 900°C (cutting zone temperature). It is expected that the model can further improve to predict the tool wear at different cutting environments (eg. CNT based coolant and cryogenic).

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NOMENCLATURE

dW	Wear volume for sliding distance
A_r	Real contact area
c	Postulated plate height corresponding wear particle
b	Mean spacing of asperities
Z	Holm's probability
σ_t	Normal stress on flank face
H	Hardness of the metal
t	Time
w	Width of the cut
h_f	Flank wear land on the tool
F_t	Thrust force in cutting
v	Cutting speed
α	Effective rake angle
i	Inclination or helix angle
α_n	Normal rake angle
γ	Relief angle
ρ	Density of the tool material
dh_f	Increment of flank wear land growth during a time interval of Δt.
K	Coefficient of experiment
φ	Constant for one type of tool geometry