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The Estimation of the Dependence of Tool Life on Rake Angle Using Computed Flank Temperatures

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Assuming that contact over a tool flank is discrete, a mean representative flank contact temperature, $\bar{\theta}_{fs}$, can be derived in terms of which, tool wear phenomena can be explained. In particular tool life is related to $\bar{\theta}_{fs}$ by an inverse power function. If, from a few cutting tests, the constants in the power law can be determined then tool life can be predicted for other conditions provided data are available from which to calculate $\bar{\theta}_{fs}$. To date the model has been tested at one value of rake angle. In the present work the analysis is extended to cover a range of rake angles and deductions are shown to be consistent with empirical observation.

1. Introduction

To extend the applicability of Taylor's tool life equation, $VT^n = C$, we require the influence of cutting tool geometry on C and n. Previously, this could only be done empirically. However, an analysis of tool flank wear has been proposed [1] assuming that tool wear occurs by the adhesion mechanism and that contact at the tool flank is discrete. These ideas were used in [2] to derive the dependence of the Taylor Constant C on tool clearance angle, β, and in [3] the effect of rake angle, was incorporated - the results of these semi-empirical analyses were shown to be consistent with observation.

However, the effect of tool geometry and cutting parameters reflects the fundamental relationship between tool life and the flank contact temperature, θ_f. In [4] a method for estimating θ_f is proposed so that, for the first time, the models for tool wear analysis and for temperature estimation were both based on the same assumption of discrete contact.

In [4], the analysis of Chao and Trigger [5] was modified to incorporate this assumption and the thermal constriction resistances at the contacting spots were introduced in accordance with Holm's analysis of sliding contact temperatures [6]. On this basis, a mean representative flank contact temperature $\bar{\theta}_{fs}$, was calculated and it was shown that by using $\bar{\theta}_{fs}$ to represent θ_f, theoretical deductions were consistent with experimental trends. More important, anomalous deductions from previous tool temperature analyses [5], are now seen to stem from the erroneous assumption of continuous flank contact.

Here we intend to determine the influence of rake angle on Taylor's tool life equation in free orthogonal cutting by introducing $\bar{\theta}_{fs}$ as the representative flank contact temperature.

2. Theoretical Considerations

2.1 Influence of Rake angle on the Nature of Contact and Thermal Constriction Resistances at the Tool/Work Contact Area at the Flank

The tool/workpiece contact consists of tool/chip contact (= C_n), contact at the cutting edge, at the wear land (= ℓ_f) and some due to elastic recovery of the workpiece after extrusion below the tool.

The apparent area of contact at the flank is $W\ell_f$ where W is the engaged length of cutting edge. Let P_m and τ_f be the apparent normal and tangential stresses acting over the flank wear land. Data for mild steel cut by H.S.S. tools [7], show that P_m and τ_f are independent

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of rake angle. Further, there is evidence [7] that plastic deformation occurs only at the tool/workpiece asperity contacts. These contact spots may be assumed to be uniformly distributed circular contact spots of equal size (Fig. 1) with the spot radius given by

$$a = [p_m/(\pi \bar{N} H_w)]^{\frac{1}{2}} \qquad (1)$$

where $\bar{N}$ is the spot density (number of spots per unit apparent area of contact) and H_w is the mean hardness of the workpiece asperity material [1,4].

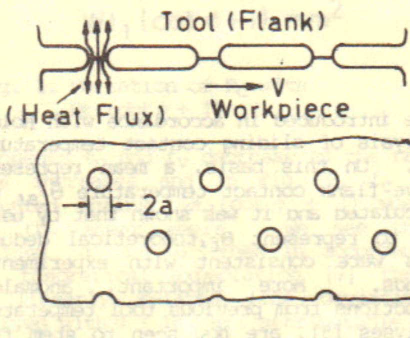

Fig. 1 Idealisation of Flank contact [4].

When the work material strain-hardens, H_w will depend on the deformation history of the surface layers of the machined workpiece.

For mild steel cut by H.S.S. tools [4,8], H_w can be expressed as

$$H_w = 1.33H_o + c_1 t_2 \sec\alpha + \tfrac{2}{3} c_2 \ell_f \qquad (2)$$

The term $t_2 \sec\alpha$ reflects the influence of rake angle. For a given uncut chip thickness, t_1, the chip thickness, t_2, decreases as the rake angle increases, hence H_w decreases as rake angle increases.

Consider now the rate of frictional heat generation per unit apparent area, $q_f (=V\tau_f)$. Since τ_f is independent of rake angle [7], q_f is also independent of rake angle. This heat flux generated at the flank contact spots must flow into what are, effectively, semi-infinite conducting media (the workpiece and tool materials) and, hence thermal constriction resistances are encountered which can be expressed [4] as the tool side and workpiece side unit thermal constriction resistances, r_t and r_w - a unit thermal constriction resistance being defined as the mean temperature difference across one side of a contact due to unit rate of heat flux per unit apparent area passing through it. Expressions derived in [4] for r_t and r_w are

$$r_t = \eta'(\pi a \bar{N} \zeta k_t)^{-1} \text{ and } r_w = \eta'(\pi a \bar{N} k_w)^{-1} \qquad (3)$$

where η' and ζ are parameters independent of rake angle (see [4]) and k_t and k_w are the thermal conductivities of the tool and workpiece materials respectively.

Combining (1), (2) and (3) we find that as the rake angle increases, H_w decreases, the spot diameter, a, increases (for a fixed $\bar{N}$) and hence r_t and r_w decrease.

2.2 Influence of Rake Angle on the Mean Flank Contact Temperature $\bar{\theta}_{fs}$

The model developed in [4] for estimating $\bar{\theta}_{fs}$, while being superficially similar to that in [5], is different in that it allows for

(i) the different heat flux distributions in the sticking and sliding parts of the tool/chip contact area;

(ii) the effect of rake angle on the heat conduction path between the rake and flank surfaces; and

(iii) the flank contact being discrete.

These features are retained and adopted here.

The analysis developed in [4] suggests that when the rake angle is increased, $\bar{\theta}_{fs}$

(a) may decrease due to the reduction in the constriction resistances, r_t and r_w;

(b) may decrease due to a reduction in the heat flux received from the rake face (for a given wedge angle) since, generally, t_2/t_1, C_n and cutting forces all decrease as the rake angle increases i.e. both the sizes and strengths of the heat sources at the shear and rake surfaces decrease and, hence, the mean shear plane temperature $\bar{\theta}_s$ and the mean tool/chip interface temperature $\bar{\theta}_r$ decrease; and

(c) may increase, for a given rake temperature $\bar{\theta}_r$, because the heat conduction path between the rake and flank surfaces is shorter.

Thus, (a) and (b) cause $\bar{\theta}_{fs}$ to decrease as rake angle increases whereas (c) has the reverse effect so that one can anticipate that $\bar{\theta}_{fs}$ decreases initially, reaches a minimum and then increases as rake angle increases. In fact, empirical evidence is consistent with flank contact zone temperature decreasing monotonically as rake angle increases in the range $-15 \leqslant \alpha \leqslant 30^\circ$.

2.3 Prediction of Tool Life at Different Rake Angles From Computed $\bar{\theta}_{fs}$

Assuming that flank wear in machining occurs exclusively by the adhesion mechanism, it was shown in [4] that tool life, T, in free orthogonal cutting could be related to the rake and clearance angles and to the mean flank spot temperature $\bar{\theta}_{fs}$ as

$$(\cot\beta - \tan\alpha)\, T\, (\bar{\theta}_{fs})^{q'} = K_4 \qquad (4)$$

where q' and K_4 are suitable constants.

Examining the origins of equation (4) we find, (since p_m is independent of α), that K_4 must be independent of rake angle. Further, it was shown empirically in [4] that q' remains constant over a wide range of cutting speeds, V, and uncut chip thicknesses, t_1. More recently [9], q' was found to be independent of tool obliquity, λ, in the range $0 \leqslant \lambda \leqslant 50^\circ$. It appears reasonable to assume that q' in equation (4) is, likewise, insensitive to changes in α . Consequently, tool life, T, for different rake angles can be predicted by substituting computed values of $\bar{\theta}_{fs}$ in equation (4) provided q' and K_4 are determined from cutting tests made at one rake angle.

Consider now the influence of rake angle on n and C in Taylor's equation

$$VT^n = C \qquad (5)$$

Following [4], we can relate θ_f and T to cutting speed, V, for a constant t_1, as

$$\bar{\theta}_{fs} = \text{const}\; V^{\varepsilon} \qquad (6)$$

Combining (4), (5) and (6) we obtain

$$n = (q\,\varepsilon)^{-1}$$

$$\text{and } C = [(\cot\beta - \tan\alpha)(\bar{\theta}_{fs})^{q'}]^{-n} \qquad (7)$$

[Essentially similar equations were used in the semi-empirical analysis [3] of the influence of rake angle on tool life, where, instead of $\bar{\theta}_{fs}$, θ_f (equation 13) was assumed to represent flank temperatures and the measured tool/work thermocouple temperature, θ_m, was taken as an estimate of rake temperature, θ_r. Expressing θ_m as a power law

$$\theta_m = \text{const.}\; V^{\varepsilon_m} \qquad (8)$$

and following an analysis similar to that used in developing equations (4) to (7) it was shown that

$$(\cot\beta - \tan\alpha)\, T\, (\theta_m)^{q_m} = \text{const} \qquad (9)$$

$$\text{where } q_m = (n\varepsilon_m)^{-1} \qquad (10)]$$

3. Experimental Details

Dry orthogonal cutting tests were performed using unworn tools to cut the same mild steel tubular workpiece stock (wall thickness 2 mm) as was used in the experiments reported in [3] and [4]. The tests covered a cutting speed range of 24 to 41.5 m/min, a rake angle range of -15 to +30°, an uncut chip thickness range of 0.05 to 0.3 mm while β (= 5°) was constant. The cutting force components, F_c, F_v, chip thickness, t_2, and tool-chip contact length, C_n, were measured using the techniques described in [4].

4. Results and Discussion

In the range $24 \leqslant V \leqslant 41.5$ m/min the cutting force components, F_c and F_v, the chip thickness ratio t_2/t_1 and the tool/chip contact length C_n, were almost independent of cutting speed. For a given uncut chip thickness (t_1=0.1 mm), t_2/t_1 decreased as the rake angle increased.

The cutting force components F_c, F_v varied linearly with uncut chip thickness t_1, so that each could be separated into an edge force element, F ', and a 'reduced' element, f, attributable to chip formation [7]. The reduced

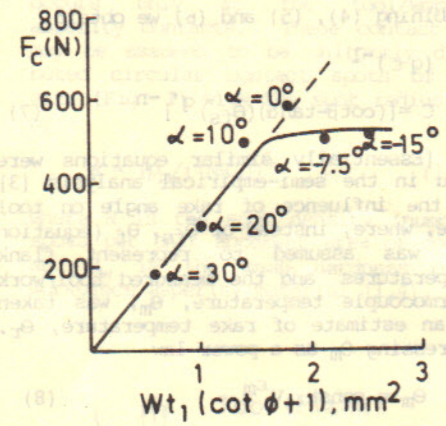

Fig. 2. Variation of F_c with $Wt_1(\cot\phi + 1)$

components f_c and f_v decrease as the rake angle increases and in Fig. 2, f_c is presented against $Wt_1(\cot\phi + 1)$ where ϕ is the shear angle. We see that provided $\alpha \geqslant 10^\circ$ then in accordance with theoretical expectation [7] these are linearly related (the deviations observed with $\alpha < 10^\circ$ are attributable to the considerable side spread which occurred at these rake angles).

The measured cutting data are sufficient to compute $\bar{\theta}_{fs}$ provided we know p_m, τ_f and c_1 and c_2 in equation (2). Since, the current cutting tests used the same workpiece stock and the same speed and feed ranges as in [4], we may assume p_{m_2} = 123 MPa, τ_f = 160 MPa, c_1 = 79 kgf/mm^2 per mm and c_2 = 29.6 kgf/mm^2 per mm; the values quoted in [4]. Using these values, the measured data and the procedures developed in [4], values of $\bar{\theta}_r$ and $\bar{\theta}_{fs}$ were obtained for different rake angles and cutting speeds. The results of these computations (for t_1 = 0.1 mm, ℓ_f = 0.18 mm, W = 2 mm, $\bar{N}$ = 46.5 x 10^6 per m^2) are shown in Figures 3 to 5.

It will be appreciated that $\bar{\theta}_{fs}$ is the outcome of the idealised model which we have chosen and hence it cannot be measured physically i.e. we have no way of verifying $\bar{\theta}_{fs}$ directly. However, we intend to demonstrate, indirectly, that we may use $\bar{\theta}_{fs}$ as a valid representation of the flank contact zone temperature when tool rake angle is changed.

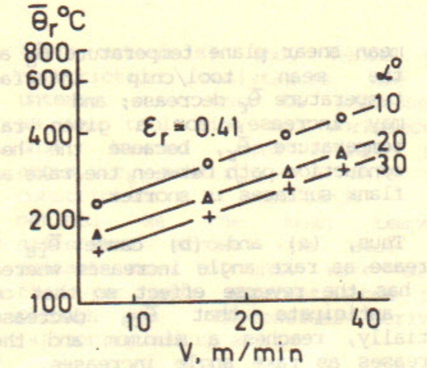

Fig. 3 Variation of $\bar{\theta}_r$ with V and α

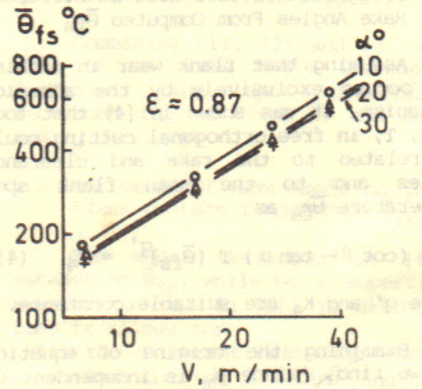

Fig. 4 Independence of ε from α

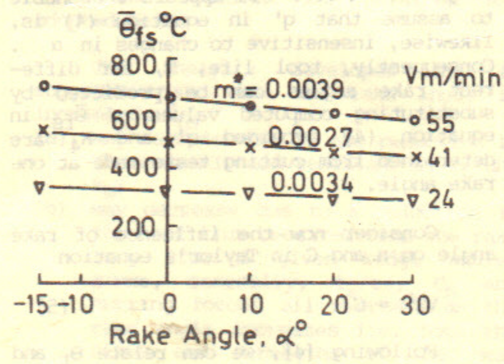

Fig. 5 Estimation of m* from $\bar{\theta}_{fs}$

Consider the approach adopted in [3] where, empirically it was found that the mean tool/work thermocouple temperature, θ_m, decreases as rake angle increases in the rangle $-10 \leqslant \alpha \leqslant 30^0$. Assuming that, over a limited range of rake angle,

$$\theta_r = \theta_{ro}\ (1 - m_1\,\alpha) \qquad (11)$$

where θ_{ro} is the value of θ_r at $\alpha = 0$ and m_1 is a constant for a given tool/work combination.

It was then assumed that the flank temperature, θ_f, could be expressed as

$$\theta_f = A\theta_r\ (1 + m_2\,\alpha) \qquad (12)$$

where m_2 reflects the reduction in the conduction path from rake to flank face as rake angle increases.

Combining (11) and (12)

$$\theta_f = A\theta_{ro}(1 - m^*\alpha)\ \text{(since } m_1 m_2 \simeq 0) \qquad (13)$$

$$\text{where } m^* = m_1 - m_2 \qquad (14)$$

From a semi-empirical analysis of tool life and θ_m data it was concluded in [3] that, for dry cutting in the low cutting speed range, $24 \leqslant V \leqslant 41.5$ m/min; $\varepsilon_m = 0.37$, $q_m = 15.5$, $n = 0.21$ and $m^* = 0.003$, and that these magnitudes are independent of rake angle.

Fig. 3 depicts the variation of computed $\bar{\theta}_r$ with cutting speed, V, and rake angle, α, plotted on logarithmic scales. The slopes of these lines, (= ε_m if we assume $\bar{\theta}_r = \theta_m$) are seen to be independent of α and equal to 0.41 which is in fair agreement with $\varepsilon_m = 0.37$ quoted in [3]. (The difference is not unreasonable in view of the assumption $\bar{\theta}_r = \theta_m$.)

Fig. 4 shows the variation of $\bar{\theta}_{fs}$ with cutting speed, V, and rake angle, α, plotted on logarithmic scales. Here again, the slopes of these lines (= ε, see equation 6) are independent of rake again.

Substituting $q' = 5.5$ (quoted in [3]) and $\varepsilon = 0.87$ (obtained from Fig. 4) into equation (7), we find $n = (5.5 \times 0.87) = 0.21$ which agrees with the empirical value of n quoted in [3].

In Fig. 5 the data of Fig. 4 are presented as $\bar{\theta}_{fs}$ versus rake angle and their relation can be expressed as $\bar{\theta}_{fs} = \bar{\theta}_{fso}(1 - m^*\alpha)$ - exactly the same as equation (13) which was introduced in [3]. Further, from Fig. 5 we find $0.0024 \leqslant m^* \leqslant 0.0039$ which agrees with the value of $m^* = 0.003$ quoted in [3]. Thus, if we adopt $\bar{\theta}_{fs}$ as representative of θ_f, the mean flank contact temperature, we obtain results in close conformity with experiment. This confirms our previous findings [4].

Consider now the tool life data quoted in [3] for $56 \leqslant V \leqslant 179$ m/min when $n = 0.41$ i.e. twice the low cutting speed value of n. We lack information on workpiece hardening at high cutting speeds to be able to calculate $\bar{\theta}_{fs}$ (and hence to obtain an estimate of n) at these speeds. However, we can anticipate that H_w will decrease at high cutting speeds. Consequently, as cutting speeds increase, the flank spot size will decrease (for a given $\bar{N}$) so that the thermal constriction resistances r_t and r_w will reduce (equation 3). Inevitably, the rate of increase of $\bar{\theta}_{fs}$ with cutting speed, V, (see Fig. 4) will reduce at higher cutting speeds resulting in a smaller value of ε which, (equation 6), would lead to a higher value of n at higher cutting speeds provided q' remains unchanged and all the evidence obtained to date [4, 9] suggests that, for a given tool/workpiece material combination, q' is indeed constant.

The advantage of using $\bar{\theta}_{fs}$ instead of θ_m (as in [3]) in tool life analysis is that the former is a fundamental parameter related directly to the conditions at the flank wear land whereas θ_m reflects the conditions at the rake surface. This explains why $q' = 5.5$ (obtained from $\bar{\theta}_{fs}$) is much smaller than $q_m = 15.5$ (obtained from θ_m), ie. $\bar{\theta}_{fs}$ is more sensitive to the parameters affecting tool life, T, than is θ_m.

5. Conclusions

5.1 Previously [3], in order to explain the empirically observed dependence of tool life on rake angle, it was assumed that the flank temperature, θ_f, decreased linearly with rake angle, ie. $\theta_f = \theta_{ro}\ (1 - m^*\alpha)$. Here it has been shown that the mean representative flank contact temperature $\bar{\theta}_{fs}$ varies with rake angle exactly in accordance with this expression and, furthermore, the value of m^* obtained here agrees with the empirical value.

5.2 It has been shown that index ε in equation $\bar{\theta}_{fs}$ = const V^{ε} is independent of rake angle which is consistent with the empirical observation quoted in [3] that the Taylor index, n, is independent of rake angle.

5.3 Thus, the previous conclusion [4] that $\bar{\theta}_{fs}$ is a valid representation of flank contact temperature, which was based on data obtained with a given rake angle, is found now to be applicable even when the rake angle is changed.

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