Analysis of the Life of Controlled Contact Tools via Flank Contact Temperature Estimation

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An analysis of the influence of controlling tool-chip contact length on the associated tool life, T, in orthogonal cutting is presented. Following recent work by the authors on machining with natural contact tools, the tool-work contact at the flank wear land is assumed to be discrete, the size of the contact spots being a function of the work-hardening history of the workpiece surface layers which, in turn, depends on the tool-chip contact length. A quantitative description of the above mechanism is used in order to estimate the thermal constriction resistances associated with the discrete flank contact. The mean flank spot temperature, $\bar{\theta}_{fs}$, is then estimated taking into account these constriction resistances and the thermal interaction between the rake and flank contact areas. Empirical data on the machining of mild steel with controlled contact H.S.S. tools is then used to demonstrate that a unique T-$\bar{\theta}_{fs}$ relationship exists irrespective of whether the tool-chip contact length is natural or controlled. It is thus suggested that it is possible to predict tool life of controlled contact tools by computing $\bar{\theta}_{fs}$ from chip geometry and cutting force data alone provided that a T-$\bar{\theta}_{fs}$ relationship is already established for the same tool/work material combination.

NOMENCLATURE

a	Mean idealised flank spot radius
B	A constant
c, c_1, c_2, C_1	Constants
C	Taylor's Constant
F_c	Cutting force component in the direction of cutting speed
F_v	Cutting force component perpendicular to the machined surface
F_c', F_v'	Contributions to F_c and F_v respectively due to extrusion action at the cutting edge
$(h_m)_s$	Workhardening occurring while traversing the primary deformation zone
$(h_m)_f$	Workhardening occurring while traversing the flank wear land
H_o	Original hardness of workpiece
H_m	Hardness of machined surface
H_w	Mean hardness of work material in contact with tool at flank wear land
i, i'	Indices of sub-areas at rake face
j, j'	Indices of sub-areas at flank wear land
k_t, k_w	Thermal conductivities of tool and work materials respectively
ℓ	Natural tool-chip contact length
ℓ_c	Controlled tool-chip contact length
ℓ_f	Flank wear land length
ℓ_o	Flank wear land criterion for tool life
$(\ell_c/t_1)_t$	Transition value of ratio ℓ_c/t_1
L	A length parameter defined in Fig. 1
m	Number of sub-areas at rake face
n, n_2	Taylor's speed and feed indices
n'	Number of sub-areas at flank face
$\bar{N}$	Mean apparent contact spot density at flank wear land
p_1	Mean normal stress acting over L plane
p_m	Apparent normal pressure at flank wear land area
q'	Index of $\bar{\theta}_{fs}$ in T-$\bar{\theta}_{fs}$ relationship
q_f	Mean apparent heat generation rate per unit area at flank wear land
$q_{f,j}$	Magnitude of q_f at flank sub-area j
$q_{r,i}$	Mean heat generation rate per unit area at rake sub-area i
$q_{c,i}, q_{t,i}$	Mean rates of heat per unit area entering the chip and the tool respectively from rake sub-area i
$q_{t,j}, q_{w,j}$	Mean rates of heat per unit apparent area entering the tool and the workpiece respectively from flank sub-area j
r_t, r_w	Tool-side and workpiece side unit thermal constriction resistances respectively
S	Shear stress acting over the lower boundary of primary deformation zone
t_1	Uncut chip thickness
t_2	Chip thickness
T	Tool life
V	Cutting speed
W	Engaged length of cutting edge
α	Rake angle
β	Clearance angle
ε	Index of V in $\bar{\theta}_{fs}$-V relationship
η	Index of t_1 in $\bar{\theta}_{fs}$-t relationship
η'	A parameter independent of ℓ_c
ω	A constant characterising the nature of wear at flank
$\bar{\theta}_{fs}$	Mean flank spot temperature
$\theta_{fs,j'}$	Magnitude of $\bar{\theta}_{fs}$ at flank sub-area j'
$\theta_{r,i'}$	Temperature at rake sub-area i'
$\bar{\theta}_r$	Mean tool-chip contact temperature
$\bar{\theta}_s$	Mean shear plane temperature
τ_f	Apparent tangential stress over flank wear land area
ζ	Parameter independent of ℓ_c
λ	Tool obliquity
ϕ	Merchant's shear plane angle

INTRODUCTION

While there exist several analyses of chip formation and cutting forces when machining with controlled contact tools, for example [1-5], the analysis of the flank wear of such tools has received little attention. Chao and Trigger [1], seeking an understanding of the contact conditions at the tool-chip interface, did note that a substantial improvement in tool life, T, can be achieved by controlling the tool-chip contact length, ℓ_c, i.e. by reducing the contact length below its 'natural' value, ℓ. At the same time, they observed that when the contact length is controlled, the tool-chip interface temperature $\bar{\theta}_r$ decreases substantially (as much as 55°C) which was considered to be significant in view of Schallbroch et al's [6] observation that tool life is very sensitive to tool-chip interface temperature (eg tool life varies as the 20th power of $\bar{\theta}_r$ when dry cutting with H.S.S. tools).

Consequently the improved tool life associated with controlled contact tools was attributed in [1] mainly to a reduction in $\bar{\theta}_r$. However, we believe this to be an over-simplification since the rate of flank wear should really be a function of the temperature, θ_f, prevailing in the vicinity of flank wear land (although we recognise $\bar{\theta}_r$ has some influence on θ_f because of heat conduction through the tool). Thus we need to understand the relationship between controlled contact length, ℓ_c, and flank temperature, θ_f, in order to explain the influence of ℓ_c on tool life.

The estimation of flank temperature and its relation to flank wear in machining has been studied only recently [7] where the present authors examined empirical evidence on machining mild steel with natural contact H.S.S. tools and noted that the tool-work contact at the flank wear land must be essentially discrete (and not continuous as assumed by Chao and Trigger [8]). The flank contact was idealised as a set of uniformly distributed circular contact spots of equal size which, being identical to the flank contact idealisation used earlier by Rubenstein [9] in analysing flank wear, enabled the modelling of flank temperature and flank wear from a common set of basic assumptions. The mean temperature, $\bar{\theta}_{fs}$, occurring over these idealised flank contact spots was then estimated taking into account the thermal constriction resistances associated with the contact spots as well as the thermal interaction between the heat sources at the rake and flank contact areas due to heat conduction through the cutting tool. Empirical evidence was then presented which demonstrated a clear correlation between $\bar{\theta}_{fs}$ and tool life, T, based on flank wear land criterion (whereas flank temperature estimates, assuming continuous-flank-contact [8] resulted in several anomalies).

These concepts are applied and suitably amended here in order to analyse the influence of controlled contact length on flank contact zone temperature and tool life.

THEORETICAL CONSIDERATIONS

Influence of ℓ_c on chip thickness and cutting forces

Previously, it has been shown that metal cutting data obtained with natural contact tools ($\ell_c \geq \ell$) [10,11] and with controlled contact tools ($\ell_c < \ell$) [5] can be explained in terms of the following assumptions:

(i) that the lower boundary of the primary deformation zone consists of a part OA, of length L (= $t_1(\cot\phi - 1)$), extending from the tool tip in the direction of cutting and a part AB, extending from the extremity of L (remote from the tool tip) at an angle of 45° to OA until it intersects the free surface of the workpiece at B (see Fig. 1)

(ii) a uniformly distributed shear stress, S, acts along OAB

(iii) over a length Z of OA (where $Z = C_1 t_2 \sec\alpha$, C_1 being a constant) a uniformly distributed tensile stress acts

(iv) over the remainder of the boundary, a uniformly distributed compressive stress P (numerically equal to S) acts

(v) because of the finite curvature of the cutting edge, force components F'_c in the direction of cutting and F'_v normal to the cutting direction exist

(vi) along the rake face there exists a region of the tool-chip contact zone, of length X_o, for $\ell_c > X_o$, and of length ℓ_c, for $X_o \geq \ell_c$, over which the chip adheres to the rake face

(vii) over the remainder of the tool-chip contact zone, of length $\ell_c - X_o$ (when $\ell_c \geq X_o$) the chip slides over the rake face

(viii) in the adhesion zone, *vide* (vi), movement of chip material parallel to the rake face occurs by shear within the chip material.

With these assumptions it has been shown [5] that a transition value $(\ell_c/t_1)_t < \ell/t_1$ exists and for $\ell_c/t_1 > (\ell_c/t_1)_t$, ϕ, F_c and F_v are equal to the respective values obtained with natural contact tools ($\ell_c = \ell$), while for $\ell_c/t_1 \leq (\ell_c/t_1)_t$

$$\cot\phi = 1 + B\,\ell_c/t_1 \qquad (1)$$

$$F_c = F'_c + wS(2t_1 + L) = F'_c + wSt_1(2 + B\ell_c/t_1) \qquad (2)$$

$$F_v = F'_v + wp_1 L \qquad = F'_v + wp_1 Bt_1(\ell_c/t_1) \qquad (3)$$

where

t_1 is the uncut chip thickness,
B is a constant for a given rake angle, and
p_1 is the mean normal stress acting over L.

It follows from equations (1) to (3) that when ℓ_c is decreased, for $\ell_c/t_1 \leq (\ell_c/t_1)_t$, other conditions remaining the same, ϕ increases and F_c and F_v decrease so that both the size and strength of the heat source at the shear plane is decreased and the size of the rake plane heat source is decreased. Consequently, we can anticipate a decrease in the mean shear plane and rake plane temperatures, $\bar{\theta}_s$ and $\bar{\theta}_r$, as ℓ_c is decreased (we are ignoring here the secondary influence of the changes induced in the source velocities).

Influence of ℓ_c on the Hardness of the Machined Surface

The magnitude of Z (see assumption iii above) has been shown to play a significant role in determining the hardness, H_w, of the machined surface [12,13] where empirically it was found that

$$H_m = H_o + (h_m)_s + (h_m)_o + (h_m)_f \qquad (4)$$

where H_o is the original hardness of the workpiece stock, $(h_m)_s$ is the hardening due to extrusion below the cutting edge, $(h_m)_f$ is the hardening occurring while traversing the flank wear land area, and, further

$$(h_m)_s \approx cZ = c_1 t_2 \sec\alpha,\ (h_m)_o \approx H_o/3,\ (h_m)_f = c_2\ell_f \qquad (5)$$

where c, c_1 and c_2 are constants and t_2 is the chip thickness.
Combining (4) and (5)

$$H_m \approx 1.33\,H_o + c_1 t_2 \sec\alpha + c_2\ell_f \qquad (6)$$

Thus, when ℓ_c is decreased, t_2 decreases due to the increase in the Merchant shear plane angle, ϕ, (see equation 1) and, from (6), the hardness of the machined surface, H_w, decreases.

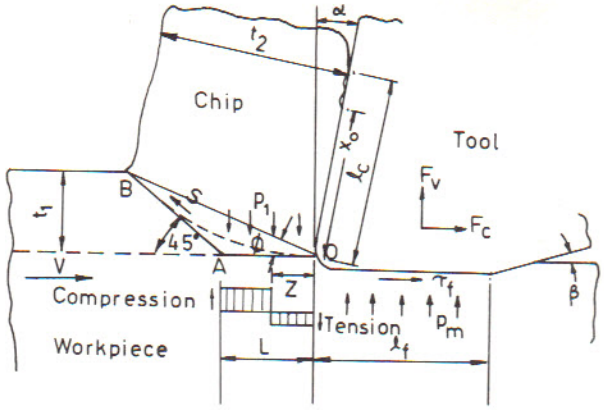

Fig. 1 Schematic Diagram of Cutting Process [5,10,11]

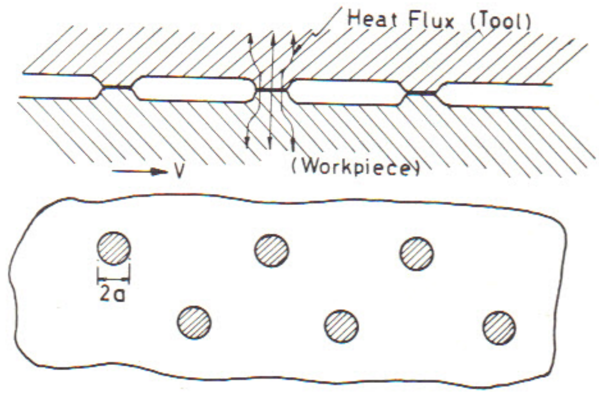

Fig. 2 Idealisation of Contact at Flank land [7]

Influence of ℓ_c on the Nature of Contact and on Thermal Constriction Resistances at the Flank Wear Land

Consider now the conditions at the flank wear land contact with controlled tools. Let ℓ_f be the flank wear land length and W be the engaged length of the cutting edge so that $W\ell_f$ is the apparent contact area cut the flank.

Let p_m and τ_f be the apparent normal and tangential stresses acting over the flank wear land area. Machining data for mild steel cut by natural contact H.S.S. tools show that p_m and τ_f are independent of rake angle [14] as well as of tool obliquity [15]. It appears therefore that p_m and τ_f are independent of the cutting phenomena in the chip formation zone and at the rake face whence it is reasonable to assume that p_m and τ_f remain insensitive to changes in ℓ_c.

Machining data presented in [11] show that p_m is about one third of the yield strength of the workpiece material so that the overall workpiece deformation is elastic, plastic deformation being confined to the tool/workpiece asperity contacts. Following [7], these contact spots may be assumed to be uniformly distributed circular spots of equal size (Fig. 2) with the spot radius given by

$$a = \{p_m/(\pi\,\bar{N}\,H_w)\}^{\frac{1}{2}} \qquad (7)$$

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

Since $(h_m)_f = c_2\ell_f$ in equation (7) for H_m, it follows that the hardening of the workpiece surface layers increases linearly as the tool wear land is traversed and hence H_w can be taken as

$$H_w \approx 1.33\,H_o + c_1 t_2 \sec\alpha + \tfrac{1}{2}c_2\ell_f \qquad (8)$$

Consider now the rate of frictional heat generation per unit apparent contact area, $q_f (= V\tau_f)$, at the tool flank. Since τ_f is independent of ℓ_c, q_f is also independent of ℓ_c.

This heat flux is actually generated at the discrete contact spots at the flank and must flow

$$T = C/(V^{1/n} t_1^{1/n_2}) \qquad (13)$$

we obtain

$$q'n\varepsilon = q'n_2\eta = 1 \qquad (14)$$

where C is Taylor's Constant and n and n_2 are the Taylor speed and feed indices.

We shall examine later the validity of equation (12) for machining with controlled contact tools.

EXPERIMENTS

The experimental work consisted of the orthogonal machining of mild steel (BHN 120) tubes (OD 50.8 mm and wall thickness 2 mm) with H.S.S. tools (grade M2, RC 64, rake angle 10° and clearance angle 5°). The experiments were performed dry and covered the cutting speed range $21 \leqslant V \leqslant 64$ m/min and the feed range $0.05 \leqslant t_1 \leqslant 0.25$ mm. The tool-chip contact length, ℓ_c, was controlled by appropriately grinding secondary rake faces on the tools with the secondary rake angles being sufficiently large to ensure complete absence of secondary rake contact. The smallest ℓ_c/t_1 used was equal to 2. Chip thickness (t_2), natural contact length (ℓ), cutting forces (F_c and F_v) and tool life (T) were measured using the techniques described in [7].

RESULTS AND DISCUSSION

Experimentally t_2/t_1, ℓ, F_c and F_v were found to be insensitive to changes in cutting speed in the range $21 \leqslant V \leqslant 64$ m/min. Further t_2/t_1 and ϕ were found to be insensitive to changes in t_1, in the range of feed used.

Fig. 4 shows the variation of cotϕ with ℓ_c/t_1 when V = 41 m/min and t_1 = 0.1 mm. The results clearly conform with theoretical expectation [5] (see equation 1) and yield B = 0.39 and $(\ell_c/t_1)_t \approx 8$. Following [5] again, the sticking length of tool-chip contact, X_o, should be slightly smaller than $(\ell_c)_t$ so that $X_o \approx 7t_1$ (this value is used in the computation of $\bar{\theta}_{fs}$ later).

Figs. 5 and 6, showing the variation of cutting force components F_c and F_v with functions $(2t_1 + L)$ and L respectively, indicate that the measured force components vary in accordance with theoretical expectation [5] (see equations 2 and 3) and yield S = 363 MPa and p_1 = 435 MPa.

These data have been used to estimate the mean flank spot temperature, $\bar{\theta}_{fs}$, using the computational procedure developed in [7] with m = 7, n' = 6 and $\bar{N}$ taken as 46.5 x 10^6 per m^2. Following [7], p_m and τ_f were taken equal to 123 MPa and 160 MPa respectively whereas, following [13], constants c_1 and c_2 in equation (9) were taken equal to 7.75 x 10^5 MPa/m and 2.9 x 10^5 MPa/m.

Fig. 7 shows how the computed temperatures $\bar{\theta}_s$, $\bar{\theta}_r$ and $\bar{\theta}_{fs}$ vary with ℓ_c when V = 41 m/min and t_1 = 0.1 mm. That $\bar{\theta}_s$ and $\bar{\theta}_r$ both decrease as ℓ_c decreases agrees with the findings presented in [1] for machining AISI 4142 steel with K3H carbides (except that, in [1], a reversal in the trend was observed at very low values of ℓ_c i.e. $\ell_c < t_1$ where, undoubtedly there must have been some secondary-rake contact). As mentioned earlier, these changes in $\bar{\theta}_s$ and $\bar{\theta}_r$ result from a decrease in the sizes and strengths of the heat sources at the shear and rake planes.

Fig. 7 shows that $\bar{\theta}_{fs}$ also decreases as ℓ_c decreases albeit to a lesser extent than does $\bar{\theta}_r$. As noted earlier this decrease in $\bar{\theta}_{fs}$ is attributable primarily to the decreasing work hardening of the machined surface (resulting in a decrease in the unit thermal constriction resistances r_t and r_w at the flank) and secondarily due to the smaller $\bar{\theta}_r$ (which alters the heat transfer occurring through the tool).

Fig. 7 also shows the improvement in the measured tool life observed with controlled contact tools (55% improvement in tool life when $\ell_c = \ell/5$). Whereas, this trend was attributed in [1] to a decrease in $\bar{\theta}_r$, we now suggest that, since flank wear must really be a function of flank temperature, the improvement in T at small values of ℓ_c is due to the decrease in $\bar{\theta}_{fs}$, which, in turn, is primarily due to the decrease in hardness

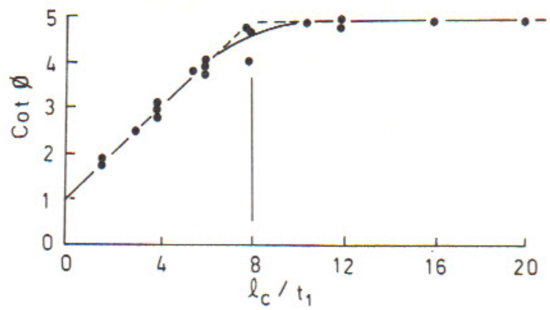

Fig. 4 Variation of Cot ϕ with ℓ_c/t_1 (V = 41 m/min, t_1 = 0.1 m/min)

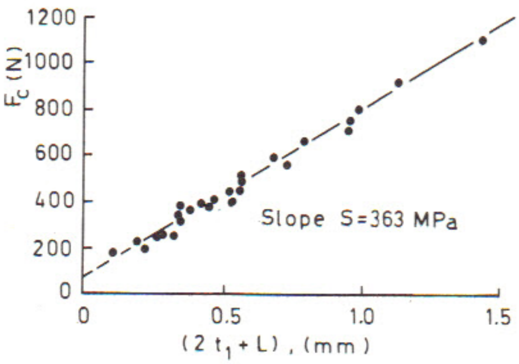

Fig. 5 Variation of F_c with $(2t_1 + L)$

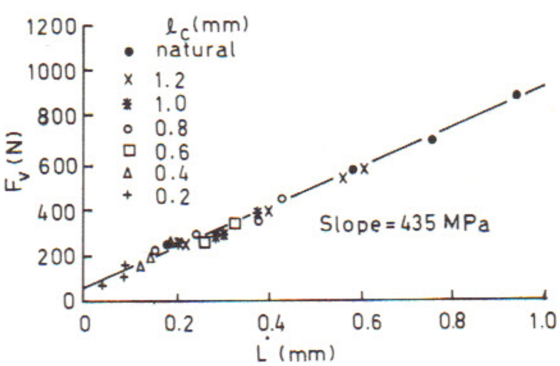

Fig. 6 Variation of F_v with L

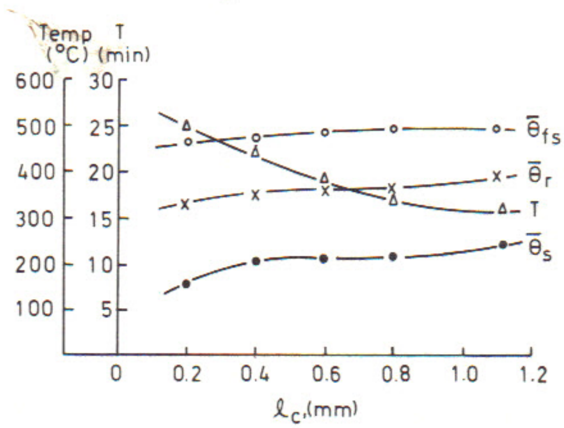

Fig. 7 Variation of $\bar{\theta}_s$, $\bar{\theta}_r$, $\bar{\theta}_{fs}$ and with ℓ_c (V = 41 m/min, $\bar{N}$ = 46.5 x 10^6 per m^2, t_1 = 0.1 mm)

from these spots (of finite size) into what are, effectively, semi-infinite conducting media (the tool and workpiece materials) and, hence thermal constriction resistances are encountered. These thermal constriction resistances can be expressed as the unit thermal constriction resistances r_t (on the tool side) and r_w (on the workpiece side) - a unit thermal constriction resistance being defined as the temperature difference across the constriction per unit heat flux per unit apparent area entering the conducting medium [7]. Expressions for r_t and r_w are

$$r_t = \eta'/(\pi a \bar{N} \zeta k_t) \text{ and } r_w = \eta'/(\pi a \bar{N} k_w) \quad (9)$$

where η' and ζ are parameters depending only on the conditions at the flank (details available in [7]) and k_t and k_w are the thermal conductivities of tool and workpiece materials respectively.

Combining (7) and (9), it can be seen that, as ℓ_c is decreased, t_2 decreases so that H_w decreases, the idealised flank spot radius, a, increases and, hence, r_t and r_w decrease.

Influence of ℓ_c on the Mean Flank Spot Temperature

In [7] a method was developed for estimating the mean temperature, $\bar{\theta}_{fs}$, occurring over the idealised contact spots at the flank wear land area in free orthogonal cutting. While this procedure applied to natural contact tools, it can be ammended so as to be applicable to controlled contact tools merely by making the tool-chip contact length equal to the controlled contact length, ℓ_c.

Fig. 3 shows the geometric idealisation of tool-chip and tool-work contact areas. The tool-chip contact area (= $W\ell_c$) at the rake face is subdivided into m rectangular sub-areas whereas the tool-work contact area (= $W\ell_f$) at the tool flank is subdivided into n' rectangular subareas. The frictional heat flux, $q_{r,i}$, generated at a given rake sub-area i is assumed to be partitioned into $q_{t,i}$ and $q_{c,i}$ entering the tool and chip materials respectively. Likewise, the frictional heat flux, $q_{f,j}$, generated at a given flank sub-area j is assumed to be partitioned into $q_{t,j}$ and $q_{w,j}$ entering the tool and workpiece materials respectively. Two expressions for the temperature $\theta_{r,i'}$ at the centre of rake sub-area i' can be obtained - one from the moving heat source effect as viewed from the chip side and other from the stationary source effect as viewed from the tool flank side. Likewise, two expressions for the representative flank contact temperature, $\theta_{fs,j'}$, at the centre of sub-area j' can be obtained - one taking into account the unit thermal constriction resistance $r_{w,j'}$ as viewed from the workpiece and the other taking into account the combined influence of the unit thermal constriction resistances $r_{t,j'}$ and the stationary heat source effect as viewed from the tool side. Using these expressions and applying Blok's Partition Principle [16], a set of simultaneous equations is obtained

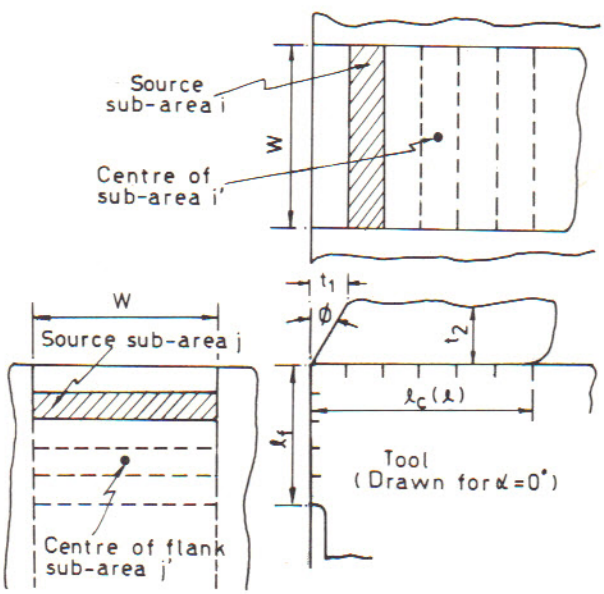

Fig. 3 Idealisation of Heat Sources at Rake and Flank surfaces

which on solution yield temperature distributions $\theta_{r,i'}$ and $\theta_{fs,j'}$ for a given set of $q_{r,i}$, $q_{f,j}$ and mean shear plane temperature $\bar{\theta}_s$ ($\bar{\theta}_s$ can be calculated by the well-known procedure of Loewen and Shaw [16]). These distributions can be averaged to yield the mean rake temperature $\bar{\theta}_r$ and the mean representative flank contact temperature $\bar{\theta}_{fs}$.

While the procedure detailed in [7] is superficially similar to that of Chao and Trigger [8], it differs from theirs in that it allows for

(a) the different heat flux distributions in the sticking and sliding parts of the tool-chip contact area (see assumptions vi and vii above);
(b) the effect of rake angle on the heat conduction path between the rake and flank surfaces; and
(c) the flank face contact being discrete.

These features are retained and adopted here.

It must be emphasised that $\bar{\theta}_{fs}$ is the outcome of a conceptual idealisation and is used to represent the actual asperity contact temperatures which will vary in magnitude from asperity to asperity and in time for any given asperity. Further the prediction of $\bar{\theta}_{fs}$ requires the flank spot density $\bar{N}$ (see equation 7) to be known *a priori* whereas we have absolutely no information about $\bar{N}$ at the moment. Notwithstanding these uncertainities, it has been demonstrated in [7] that excellent correlation between computed $\bar{\theta}_{fs}$ and tool life, T, exists (we shall discuss this feature later) as long as $\bar{\theta}_{fs}$ is calculated from an arbitrarily selected value of $\bar{N}$ which is used unaltered in all temperature calculations. Examination of the method for computing $\bar{\theta}_{fs}$ [7] reveals that the tool-chip contact length ℓ_c could affect $\bar{\theta}_{fs}$ via

(i) a change in the unit thermal constriction resistances r_t and r_w at the flank wear land and

(ii) a change in the heat flux received at the flank from the rake face as a result of heat conduction through the tool (due to a change in $\bar{\theta}_r$).

Since both r_t and r_w as well as $\bar{\theta}_r$ decrease with decreasing ℓ_c, it follows that effects (i) and (ii) reinforce each other and lead to a decrease in $\bar{\theta}_{fs}$ as ℓ_c is decreased.

Prediction of Tool Life of Controlled Contact Tools

In [7,9], an analysis of flank wear in free orthogonal cutting was developed assuming that flank wear occurred exclusively by the adhesion mechanism. This analysis was based on the same discrete flank contact idealisation as has been used in the computation of $\bar{\theta}_{fs}$ [7]. In particular it was shown in [7] that

$$T(\bar{\theta}_{fs})^{q'} = K_4 \quad (10)$$

where

$$K_4 = [\ell_o^{\omega-2} K_2 p_m^{\omega} W^{\omega-1}(2-\omega)(\cot\beta - \tan\alpha)]^{-1} \quad (11)$$

in which

ℓ_o is the magnitude of ℓ_f at which tool life, T, is assumed to be terminated; α and β are the tool rake and clearance angles; K_2 is a constant (for a given $\bar{N}$); ω is a constant (equal to 0.75 for mild steel machined with H.S.S. tools at medium speeds).

It has been demonstrated that q' and K_4 in in equation (11) are insensitive to cutting speed and feed [7] and to rake angle [13]. It appears reasonable to assume therefore that q' and K_4 would also be insensitive to changes in ℓ_c so that equation (10) provides a basis for predicting the the tool life of controlled contact tools provided q' and K_4 are determined, *a priori* , from, say, data on natural contact tools.

It was further demonstrated in [7] that when machining with natural contact tools, $\bar{\theta}_{fs}$ can be expressed as

$$\bar{\theta}_{fs} \propto V^{\varepsilon} t_1^{\eta} \quad (12)$$

where ε and η are constants.

Combining (10) and (12) with Taylor's Extended Law

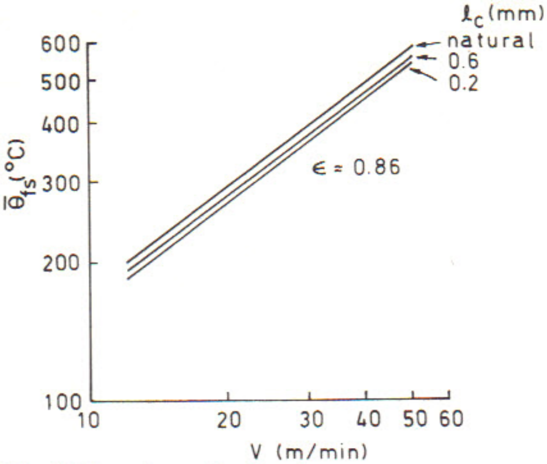

Fig. 8 Independence of ε from ℓ_c

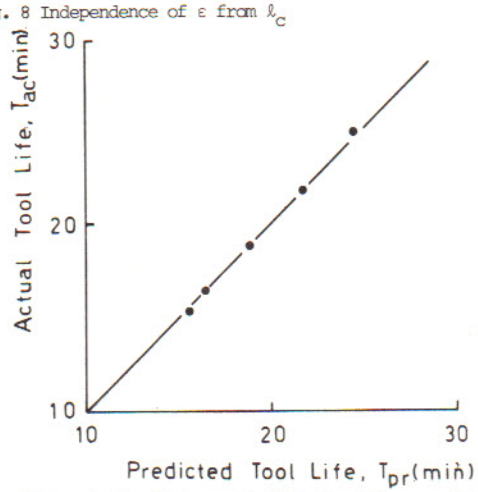

Fig. 9 Correlation between Predicted and Measured Tool Lives.

H_w of work material in contact with the flank wear land.

Fig. 8 shows the variation of $\bar{\theta}_{fs}$ with cutting speed (plotted in log-log coordinates) at different values of ℓ_c so that equation (12) is obeyed and, further, the index $\varepsilon (= 0.86)$ is independent of ℓ_c. Likewise, when the data obtained with varying t_1 was analysed, index η in equation (13) was found to be equal to 0.2 and to be independent of ℓ_c. These magnitudes of ε and η agree exactly with those quoted in [7] for machining mild steel with natural contact H.S.S. tools in the speed range $21 \leqslant V \leqslant 55$ m/min.

THE PREDICTION OF TOOL LIFE

We propose now to attempt to predict the life of controlled contact tools. To do this we will assume that as with natural contact H.S.S. tools cutting mild steel, so also with controlled contact tools, the tool life is related to the mean flank contact temperature $\bar{\theta}_{fs}$ by a relation of the form of equation (10). Since, in fact, a method of computing values of $\bar{\theta}_{fs}$ for controlled contact tools has been developed above, the problem reduces to the selection of suitable 'constants' K_4 and q'.

Examination of equation (11) reveals that K_4 should be independent of the magnitude of ℓ_c so that we may take the 'natural contact value' i.e. $K_4 = 1.106 \times 10^6$ as quoted in [7]. (This value for K_4 was obtained when $\bar{N}$ was selected as 46.5×10^6 per m^2 which explains why values of $\bar{\theta}_{fs}$ have been computed using this value of $\bar{N}$.)

All the experimental evidence so far accumulated when cutting mild steel with natural contact H.S.S. tools [7,14,15] reveals that q' has remained invariant (at 5.5) with respect to changes in cutting parameters. On this basis we propose to assume that q' is independent, also, of ℓ_c i.e. we assume that for controlled contact H.S.S. tools cutting mild steel, q' = 5.5.

Thus, we are suggesting that for $\alpha = 10^0$, $\beta = 5^0$, W = 2 mm the tool life at any given value of ℓ_c can be predicted from

$$T_{pr} = K_4(\bar{\theta}_{fs})^{-q'} = 1.106 \times 10^6 \; (\bar{\theta}_{fs})^{-5.5}$$

when T_{pr} is expressed in minutes and $(\bar{\theta}_{fs})$ is computed from cutting data appropriate to that particular value of ℓ_c and is expressed in degrees Celsius.

Fig. 9 shows the correlation between the predicted tool life, T_{pr}, and the measured tool life, T, when V = 41 m/min and t_1 = 0.1 mm. In fact the correlation is excellent and in view of the scatter to which tool life measurements are prone, this degree of correlation can only be regarded as fortuitous so that, in contradistinction to the normal formula, we would say here that notwithstanding the excellence of the demonstrated correlation, it can be concluded that $\bar{\theta}_{fs}$ provides an acceptable parameter in terms of which the prediction of the tool life in controlled contact cutting may be accomplished.

CONCLUSIONS

1. The concept of a mean representative flank contact temperature, $\bar{\theta}_{fs}$, developed in [7] for machining with natural contact tools can be extended so as to include machining with controlled contact tools.

2. The demonstrated correlation between predicted and measured values of tool life provides evidence in support of the suggestions

   (i) that as with natural contact tools, so also with controlled contact tools, tool life is related to $\bar{\theta}_{fs}$ by an equation of the form

   $$T = K_4 \; (\bar{\theta}_{fs})^{-q'}$$

   (ii) that K_w and q' are independent of the length of the controlled contact, ℓ_c.

3. The mean representative flank contact temperature, $\bar{\theta}_{fs}$, is related to cutting speed by a power-law, the cutting speed index, ε, being independent of ℓ_c and hence, being numerically equal to the index obtained when cutting with natural contact tools. As a corollary to this finding and the suggestion in conclusion 2 (ii), it follows from equation (14) that the Taylor indices n and n_2 are independent of ℓ_c - unfortunately we lack experimental data to test this deduction.

4. The tool life increases as the controlled contact length is reduced primarily as a result of the decrease in workhardening experienced by the surface layers of the machined workpiece.

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