Int. J. Heat Mass Transfer. Vol. 34, No. 4/5, pp. 983–992, 1991
Printed in Great Britain

0017-9310/91 $3.00+0.00

Coupled heat transfer to workpiece, wheel, and fluid in grinding, and the occurrence of workpiece burn

ADRIENNE S. LAVINE and TIEN-CHIEN JEN

Mechanical, Aerospace and Nuclear Engineering Department, University of California, Los Angeles, Los Angeles, CA 90024-1597, U.S.A.

(*Received* 23 *May* 1989 *and in final form* 7 *June* 1990)

Abstract—A model of heat transfer in grinding was previously developed which predicts the temperature in the grinding zone. This model is used here to predict the occurrence of film boiling of the grinding fluid, and to determine whether or not workpiece burn would subsequently occur. Both film boiling and workpiece burn are assumed to occur at critical grinding zone temperatures. The effects of various parameters are explored, such as fluid and abrasive grain types, and conventional or creep feed grinding conditions.

INTRODUCTION

DURING a grinding process, essentially all of the power supplied to the grinding machine is converted to heat in the region where the wheel contacts the workpiece (the grinding zone) [1]. Grinding fluids are used to lubricate (thereby reducing the grinding power) and to remove heat from the grinding zone. Under some circumstances, the grinding fluid may undergo film boiling, causing a sudden increase in temperature [2–6]. Depending on a variety of factors, the resulting elevated temperature may or may not cause thermal damage to the workpiece. A model of heat transfer in grinding has previously been developed [7, 8] which predicts the temperature in the grinding zone by considering the coupled heat transfer to the workpiece, wheel, and grinding fluid. In this paper, this model is slightly modified, and is then used to predict when film boiling will occur, and whether it will result in thermal damage to the workpiece.

Figure 1 illustrates a grinding wheel in contact with a workpiece over the grinding zone of length l and depth b (into the page). Some typical grinding parameters are shown in Table 1. Note that creep feed grinding differs from conventional grinding in that the workpiece speed, v_w, is much lower, and the depth of cut, a, is much higher (and consequently so is the length of the grinding zone, l). (The remaining quantities in the table will be introduced later.)

A review of the literature reveals a considerable research effort devoted to thermal aspects of grinding. Snoeys *et al.* [9] and Malkin [10] provide literature reviews. Much of the work [1, 11–14] has concentrated on predicting workpiece surface temperatures in dry grinding, in the absence of significant convective heat transfer. However, convective cooling due to the grinding fluid has been explored by a variety of researchers, see for instance refs. [15–18].

Grinding fluids are especially crucial in creep feed grinding. Because of the low workpiece speeds and long grinding zones, a point on the workpiece surface remains in the grinding zone longer and therefore tends to be hotter than in conventional grinding. Several papers [2–5] have investigated thermal aspects of creep feed grinding. The results of Shafto *et al.* [2] suggest that only about 5% of the generated heat remains in the workpiece because a substantial portion of the grinding energy is removed by convection to the fluid. These papers on creep feed grinding have also addressed the phenomenon known as 'surge', in which the power consumption of the grinding machine suddenly fluctuates, associated with metallurgical damage to the workpiece surface. It has been hypothesized that surge is associated with the transition from nucleate to film boiling of the coolant. This hypothesis is supported by the research of Ohishi and Furukawa [5] and Yasui and Tsukuda [6], who showed that when the workpiece surface temperature reaches a value somewhat in excess of 100°C for water based grinding fluid and 300°C for oil, the

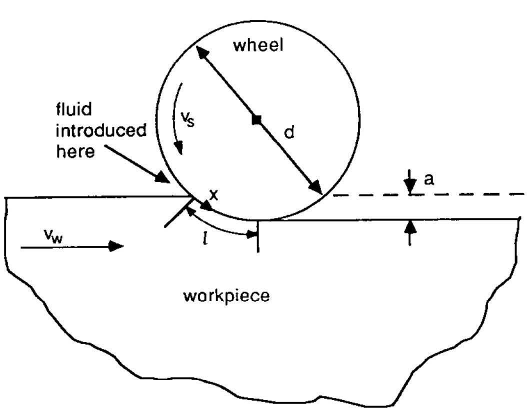

FIG. 1. The grinding geometry.

NOMENCLATURE

a	depth of cut
A	fractional grain–workpiece contact area, A_g/A_{tot}
A_g	total grain–workpiece contact area
A_{tot}	grinding zone area, lb
b	grinding zone depth
c_p	specific heat
d	wheel diameter
$f(\zeta)$	function defined by equation (11)
h	heat transfer coefficient
k	thermal conductivity
l	grinding zone length
l_g	width of individual grain heat source
L	$v_s l/\alpha_w$
L_g	$v_s l_g/\alpha_w$
Pe	Peclet number, vs/α
q	heat transfer rate
q'	heat transfer rate per unit depth of grinding zone
q''	heat flux
s	heat source width in direction of motion
T_i	temperature of solid before encountering heat source
v	velocity
v_s	wheel velocity
v_w	workpiece velocity
x	distance along surface from beginning of grinding zone
$\hat{x}$	coordinate along surface relative to leading edge of heat source.

Greek symbols

α	thermal diffusivity
ζ	$(\pi\alpha_g x/l_g^2 v_s)^{1/2}$
θ	temperature rise relative to ambient temperature
κ	defined by equation (19)
ρ	density
Φ	non-dimensional temperature, $h_{wb}\theta_{wb,s}/q''_{tot}$.

Subscripts

burn	workpiece burn
f	fluid
fb	film boiling
g	grain
grind	grinding power
s	surface (except in v_s)
tot	total
w	workpiece
wb	workpiece background
wg	workpiece under grain.

temperature rises rapidly, accompanied by thermal damage to the workpiece surface.

ANALYSIS

Heat is generated in the vicinity of the contacts between abrasive grains and the workpiece (see Fig. 2). In the model, all the heat is assumed to be generated at the grain–workpiece interfaces, although to be more precise, heat is also generated at chip–grain interfaces and at the workpiece–chip shear planes. The heat generated at a grain–workpiece interface (q''_{grind}) conducts into either the workpiece or the abrasive grain (see Fig. 3). Thus

$$q''_{grind} = q''_{wg} + q''_g \tag{1}$$

where q''_{wg} is the heat flux into the workpiece at the grain location and q''_g the heat flux into the grain. Once heat enters the workpiece, it may either remain in the workpiece or be removed by convection to the fluid (see Fig. 3). (The rate at which heat leaves with the chip is typically not large, and will be neglected here.) Thus

$$q''_{wg}A_g = q''_{wb}A_{tot} + q''_f(A_{tot} - A_g) \tag{2}$$

where A_g is the total actual grain–workpiece contact area, so that the left-hand side is the total rate at which heat enters the workpiece. On the right-hand side, q''_{wb} is the heat flux which remains in the workpiece, assumed evenly distributed over the total grinding zone area, $A_{tot} = lb$. Finally, q''_f is the heat flux into the fluid, assumed uniform over the area exposed to the fluid, $A_{tot} - A_g$.

A model has previously been developed of the coupled heat transfer to the workpiece, the fluid, and the abrasive grain [7, 8]. One outcome of this model is a prediction of the workpiece temperature. The model will now be summarized (with the workpiece model somewhat modified). The separate models for heat transfer to the workpiece and fluid build on the classical theory of moving heat sources (see, for instance, Jaeger [19]). Consider a rectangular source

Table 1. Typical grinding conditions (in round numbers): $v_s = 30$ m s^{-1}; $d = 200$ mm; $l_g \approx 0.1$ mm $\Rightarrow L_g = v_s l_g/\alpha_w \approx 200$

	v_w (mm s^{-1})	a (mm)	$l \approx \sqrt{(ad)}$ (mm)	$L = v_s l/\alpha_w$
Conventional	100	0.01	1	2000
Creep feed	1	1	10	20000

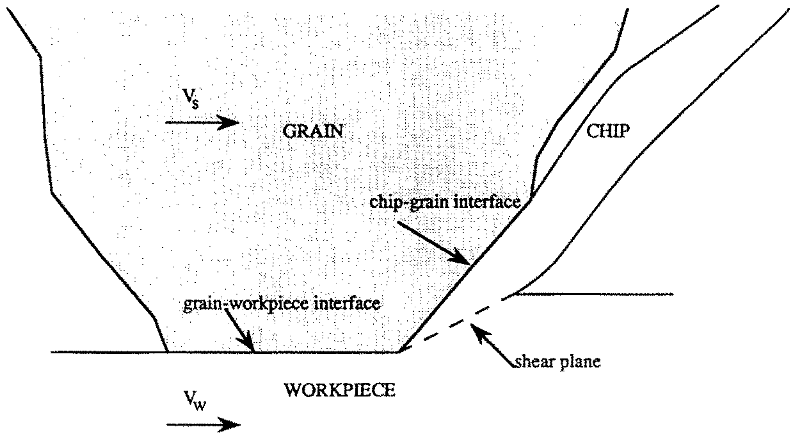

FIG. 2. Locations of heat generation.

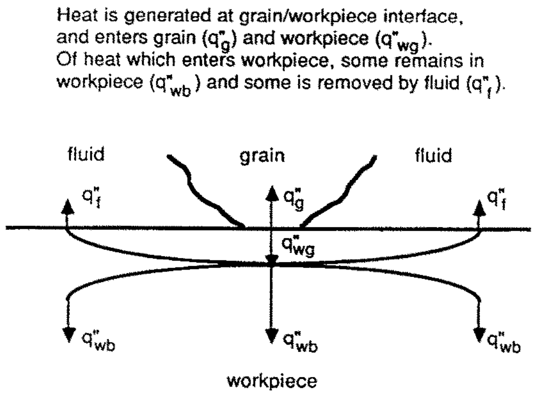

FIG. 3. Heat transfer paths.

of heat (heat flux uniform over the source) moving over the surface of a semi-infinite solid, starting at time $t = 0$. Three simplifications will be made in the models to follow, so as to yield a closed form solution for the temperature in the solid.

(a) A steady-state temperature field has been reached (in a frame of reference fixed to the moving heat source). Based on the formulae presented in Jaeger [19], numerical calculations show that the steady-state assumption is valid provided the heat source has moved a distance of the order of a few source widths. The exact distance which must be traveled depends on the Peclet number, $Pe = vs/\alpha$, where v is the velocity of the source and s the width of the source in the direction of motion. For example, consider a band heat source (infinite in the direction perpendicular to the direction of motion). For $Pe > 8$, the surface temperature at the center of the band will have reached 95% of its steady-state value after the source has moved less than one source width. For $Pe = 0.8$, the source must have traveled 10 source widths.

(b) The rectangular heat source can be approximated as a band source, infinite in the direction perpendicular to the direction of motion. For $Pe = 8$, the agreement between the surface temperatures for a square and a band heat source appears to be within 12% (based on a figure presented in Jaeger [19]). Agreement improves as the Peclet number increases.

(c) Conduction in the direction of motion can be neglected. This assumption is also best for large Peclet number. For $Pe = 20$, for a band source, the error incurred in neglecting conduction in the direction of motion is less than 13% (except for very near the leading edge of the source, where the temperature is low, anyway).

With these three assumptions, the solution is the well-known error function solution for the temperature distribution in a semi-infinite solid moving in the positive $\hat{x}$-direction with velocity v, past a uniform heat source at its surface, starting at $\hat{x} = 0$. The solution for the surface temperature rise is

$$\theta_s(\hat{x}) = q''\sqrt{(4\hat{x}/\pi k \rho c_p v)} \tag{3}$$

where θ_s is the surface temperature rise relative to T_i (the temperature of the solid for $\hat{x} < 0$, before it encounters the source), and q'' the heat flux at the surface. Note that this solution is only valid underneath the source, since beyond the source the temperature rise decays back to zero. A local heat transfer coefficient can then be defined

$$h(\hat{x}) = q''/\theta_s(\hat{x}) = \sqrt{(\pi k \rho c_p v/4\hat{x})}. \tag{4}$$

Heat transfer to workpiece

Many individual abrasive grains produce heat at discrete points on the workpiece surface. An approximate model of this which has been justified in the past [11–13] is to consider the temperature distribution to be the superposition of a 'background' temperature rise due to a uniform heat source acting over the entire grinding zone, and an 'individual grain' temperature rise which applies only underneath a grain. While the maximum workpiece temperature occurs underneath a grain, it is actually the workpiece *background* temperature which determines thermal damage to the workpiece [13]. There are two reasons for this: (1) the peak temperature under a grain occurs for a short time, and thermal damage requires time to occur, and (2) the peak temperature occurs in material which will probably be removed, so that it will not affect the quality of the finished surface. However, the peak workpiece temperature under a grain is still of importance, because the grain is continually exposed to this temperature, and therefore this temperature determines the heat flux into the grain.

Background temperature rise. Here, the entire grinding zone is modeled as a uniform source of heat acting over the region $0 < x < l$, with uniform heat flux q''_{wb} into the workpiece. The workpiece moves relative to this heat source with velocity v_w. Since the workpiece is generally many times longer than the grinding zone, the steady-state assumption is valid except for very near the leading edge of the workpiece. The Peclet number for the workpiece, $v_w l/\alpha_w$, varies greatly between conventional and creep feed grinding. In creep grinding, it is not always large enough to justify using a band source and neglecting conduction in the direction of motion. However, for creep feed grinding, heat transfer to the workpiece is generally a small fraction of the total grinding power, so that a significant error in this portion will not cause a large error in the temperature. Then the local heat transfer coefficient corresponding to the workpiece background temperature is

$$h_{wb}(x) = q''_{wb}/\theta_{wb,s} = \sqrt{(\pi(k\rho c_p)_w v_w/4x)}. \quad (5)$$

Individual grain temperature rise. This model is a modified version of the one presented in ref. [8]. An individual grain is modeled as a band heat source of width l_g, causing a heat flux q''_{wg} into the workpiece surface. Relative to an individual grain heat source, the workpiece moves with velocity $v_s - v_w \approx v_s$ (in the opposite direction from the wheel). Since the grinding zone is usually of the order of a millimeter or longer, and an individual grain–workpiece contact area is of the order of 100 μm or less, the grain moves at least several times its length, and the steady-state assumption is therefore reasonable. The Peclet number for the grain heat source, $v_s l_g/\alpha_w$, is typically at least 20 or more, so the assumptions of a band heat source and no conduction in the direction of motion are quite good. (These last two assumptions were not made in ref. [8], and the results were within 10% of the approximate solution given here, for $v_s l_g/\alpha_w > 20$.) The workpiece surface temperature rise due to an individual grain heat source is then given by

$$\theta_{wg,s}(\hat{x}) = q''_{wg}\sqrt{(4\hat{x}/\pi(k\rho c_p)_w v_s)}. \quad (6)$$

Note that the individual grain temperature rise is given as a function of $\hat{x}$, the coordinate measured relative to the leading edge of the grain heat source. Later, the average temperature underneath a grain heat source will be needed

$$\bar{\theta}_{wg,s} = (2q''_{wg}/3)\sqrt{(4l_g/\pi(k\rho c_p)_w v_s)}. \quad (7)$$

The heat transfer coefficient corresponding to the average workpiece temperature underneath a grain heat source is

$$\bar{h}_{wg} = q''_{wb}/\bar{\theta}_{wg,s} = (3/2)\sqrt{(\pi(k\rho c_p)_w v_s/4l_g)}. \quad (8)$$

Heat transfer to grinding fluid

The actual contact area between the workpiece and the grains is typically only a few percent of the total grinding zone area, so that most of the workpiece surface is exposed to the grinding fluid. It is assumed that there is a uniform heat flux q''_f into the fluid from the workpiece surface. The grinding fluid is assumed to completely fill the space around the wheel grains, to a depth greater than the thermal boundary layer thickness [20]. It is further assumed that the fluid moves past the workpiece with the wheel velocity v_s. Finally, based on the large Peclet number (typically $v_s l/\alpha_f$ is of the order of 10^5 or greater), a band heat source is used, and conduction in the direction of motion is neglected. The local heat transfer coefficient for the fluid is then

$$h_f(x) = q''_f/\theta_{f,s} = \sqrt{(\pi(k\rho c_p)_f v_s/4x)}. \quad (9)$$

This solution is not valid if the fluid boils. Later in this paper, it will be assumed that when film boiling occurs, the heat transfer to the fluid becomes negligible, i.e. $h_f = 0$.

Heat transfer to abrasive grain

This last model does not build on the classical theory of moving heat sources. A grain moves past the workpiece surface with velocity v_s. If the heat flux into the grain at the workpiece surface is q''_g, and the grain is taken to be a frustum of a cone, then there is an exact solution for the cross-sectionally lumped grain temperature [7]. The local heat transfer coefficient for the grain is given by

$$h_g(x) = q''_g/\bar{\theta}_{g,s} = \sqrt{(\pi(k\rho c_p)_g v_s/4x)}f(\zeta) \quad (10)$$

where $\zeta(x) = (\pi\alpha_g x/l_g^2 v_s)^{1/2}$, and

$$f(\zeta) = \frac{2}{\pi^{1/2}}\frac{\zeta}{1-\exp(\zeta^2)\operatorname{erfc}(\zeta)}. \quad (11)$$

Coupling the models

The individual thermal models are coupled by requiring that the surface temperatures match. At a point on the workpiece surface which is exposed to the fluid, the workpiece background temperature rise, $\theta_{wb,s}$, equals the fluid temperature rise, $\theta_{f,s}$

$$\theta_{wb,s}(x) = \theta_{f,s}(x) \Rightarrow \frac{q''_{wb}}{h_{wb}(x)} = \frac{q''_f}{h_f(x)}. \quad (12a,b)$$

Underneath a grain, the grain temperature rise, $\bar{\theta}_{g,s}$, equals the sum of the workpiece background temperature rise and the workpiece temperature rise due to an individual grain

$$\bar{\theta}_{g,s}(x) = \theta_{wb,s}(x) + \bar{\theta}_{wg,s} \Rightarrow \frac{q''_g}{h_g(x)} = \frac{q''_{wb}}{h_{wb}(x)} + \frac{q''_{wg}}{\bar{h}_{wg}}. \tag{13a,b}$$

Taking q''_{grind} in equation (1) to be known, equations (1), (2), (12b), and (13b) are four equations which can be solved for the four heat fluxes (q''_{wb}, q''_{wg}, q''_f, and q''_g). Then the four temperature rises ($\theta_{wb,s}$, $\bar{\theta}_{wg,s}$, $\theta_{f,s}$, and $\bar{\theta}_{g,s}$) are all known as well. An inconsistency now arises. All heat fluxes were taken to be uniform. But then equation (13b) cannot be satisfied, because the two sides of the equation do not have the same x-dependence. This contradiction arises because the actual solution to the coupled heat transfer problem does not have uniform heat fluxes into each of the various components. Some or all of the heat fluxes must depend on x. An approximate method for handling this problem was detailed in ref. [8]. Only the results for the workpiece background temperature will be given here, since as mentioned previously, it is this temperature which governs thermal damage to the workpiece. The workpiece background temperature at the workpiece surface is given by

$$\theta_{wb,s}(x) = \frac{q''_{tot}/h_{wb}(x)}{\left[1 + \frac{h_f}{h_{wb}}(1-A)\right]\left[1 + \frac{\bar{h}_g}{\bar{h}_{wg}}\right] + A\overline{(h_g/h_{wb})}}. \tag{14}$$

In this expression, A is the fractional grain–workpiece contact area, i.e. $A = A_g/A_{tot}$, and q''_{tot} is the average grinding power flux based on the total grinding zone area, i.e. $q''_{tot} = q_{tot}/A_{tot}$. Here q_{tot} is the total grinding power, i.e. the integral of q''_{grind} over the actual contact area. The two functions $\bar{h}_g$ and $\overline{(h_g/h_{wb})}$ are defined as follows:

$$\bar{h}_g = \frac{1}{l}\int_0^l h_g \, dx \tag{15}$$

$$\overline{(h_g/h_{wb})} = \frac{1}{l}\int_0^l (h_g/h_{wb}) \, dx. \tag{16}$$

A non-dimensional temperature rise is now defined

$$\Phi = h_{wb}(x)\theta_{wb,s}(x)/q''_{tot} = \frac{1}{\left[1 + \frac{h_f}{h_{wb}}(1-A)\right]\left[1 + \frac{\bar{h}_g}{\bar{h}_{wg}}\right] + A\overline{(h_g/h_{wb})}}. \tag{17}$$

It has been shown in ref. [8] that Φ is a function of seven parameters

$$\Phi = \Phi(\kappa_g, \kappa_f, \alpha_g/\alpha_w, v_s/v_w, A, L = v_s l/\alpha_w, L_g = v_s l_g/\alpha_w) \tag{18}$$

where

$$\kappa_g = \sqrt{((k\rho c_p)_g/(k\rho c_p)_w)}, \quad \kappa_f = \sqrt{((k\rho c_p)_f/(k\rho c_p)_w)}. \tag{19}$$

The first three parameters depend only on the material properties of the workpiece, grain, and fluid. The last four parameters depend as well on operating conditions and wheel geometry. Of all these parameters, A and L_g are least accurately known.

It should be noted that the non-dimensional temperature rise Φ does not depend on x. It can be interpreted in two ways. It is the workpiece surface temperature normalized by the maximum possible surface temperature which would occur if all of the grinding power went into the workpiece, i.e. $\Phi = \theta_{wb,s}/\theta_{max.poss.}$, where $\theta_{max.poss.} = q''_{tot}/h_{wb}$. And, it is the fraction of the grinding power which remains in the workpiece, since $\Phi = h_{wb}\theta_{wb,s}/q''_{tot} = q''_{wb}/q''_{tot}$.

From the point of view of predicting film boiling of the coolant and thermal damage, the dimensional temperature will be required. In particular, the maximum value of the workpiece background temperature, at $x = l$, will be used as an indication of whether these phenomena occur.

RESULTS AND DISCUSSION

The results of the analysis were compared to experimental data in ref. [8]. The predictions were excellent for conventional grinding conditions and for creep feed grinding with oil, but were not very good for creep feed grinding with water. Considering the fact that there are no adjustable constants in the model, the agreement is reasonable. The dependence of Φ on the seven non-dimensional parameters was also investigated in ref. [8]. The results showed a strong dependence on the fluid type and abrasive grain type (as quantified by the parameters κ_f, κ_g, and α_g/α_w), and on the velocity ratio v_s/v_w. The remaining parameters (A, L, and L_g) showed smaller, but not negligible, effects. (It should be noted that a decrease in Φ does not necessarily correspond to a decrease in actual dimensional temperature. Recall that $\Phi = h_{wb}(x)\theta_{wb,s}(x)/q''_{tot}$, and both h_{wb} and q''_{tot} depend on the various grinding parameters.)

Film boiling and workpiece burn

Previous experimental studies [5, 6] have shown that when the grinding zone temperature reaches approximately 100–130°C for water based grinding fluid and 300°C for oil, the temperature of the workpiece suddenly increases. This has been attributed to film boiling of the fluid. When film boiling occurs, it is reasonable to assume that heat transfer to the fluid becomes negligible compared to heat transfer to the wheel and workpiece. Thus, when film boiling occurs, the equations derived previously can be used, with h_f (or κ_f) set to zero. The following approach can therefore be used to calculate the workpiece temperature. First, it can be calculated assuming the grinding fluid

Table 2. Material properties and non-dimensional parameters

	Water	Oil	Al_2O_3	CBN	Steel
k	0.65	0.15	46	1300	60.5
ρ	1000	820	4000	3450	7854
c_p	4180	2000	770	506	434
κ_f	0.115	0.0345			
κ_g			0.829	3.32	
α_g/α_w			0.841	42	

remains liquid. If the temperature calculated in this way would exceed the temperature at which film boiling has been observed to occur, the temperature can be recalculated assuming there is no fluid present (i.e. dry grinding). (Note that this approach assumes an abrupt transition between no boiling and film boiling, and therefore will cause an overestimate of the temperature under conditions for which nucleate boiling would actually occur. This flaw is not extremely important, because if the fluid is undergoing nucleate boiling, the workpiece will remain cool enough to avoid thermal damage, and an accurate knowledge of the temperature is not crucial.)

If film boiling occurs, the resulting temperature calculated under dry conditions may be high enough to cause thermal damage to the workpiece material. For instance, 'workpiece burn' is observed to occur at a temperature of approximately 700–800 C [9, 21]. In the remainder of this paper, the occurrence of workpiece burn will be used as a representative example of thermal damage.

Figure 4 shows an example of Φ vs $(v_s/v_w)^{1/2}$ for an aluminum oxide wheel, with a water based grinding fluid, oil, and no fluid (dry). These choices determine the values of κ_g, κ_f, and α_g/α_w, assuming a steel workpiece (see Table 2). The wear flat area is taken to be 1%, and $L_g = 200$ (see Table 1). Two values of L are considered, corresponding to typical conditions for conventional ($L = 2000$) and creep feed ($L = 20\,000$) grinding (see Table 1). It should be recalled that typical values of the velocity ratio are lower in conventional than in creep feed grinding (see ranges indicated on the abscissa of Fig. 4).

The quantity Φ of course decreases as v_s/v_w increases, since larger v_s/v_w means more heat is removed by the wheel and fluid, relative to the workpiece. The effect of the type of grinding fluid is as expected. A water based grinding fluid is most effective in removing heat from the grinding zone, and therefore yields the lowest value of Φ, followed by oil, with dry grinding yielding the largest value of Φ.

Two numerical examples will now be considered for typical conventional and creep feed grinding conditions with a water-based grinding fluid. It should be noted that grinding power is actually a dependent variable which is determined by the workpiece material, wheel and fluid types, and grinding conditions. Generally speaking, grinding power is of the same order of magnitude for conventional and creep feed grinding, but since the grinding zone area is larger by roughly an order of magnitude in creep feed grinding, the grinding heat flux, q''_{tot}, is corres-

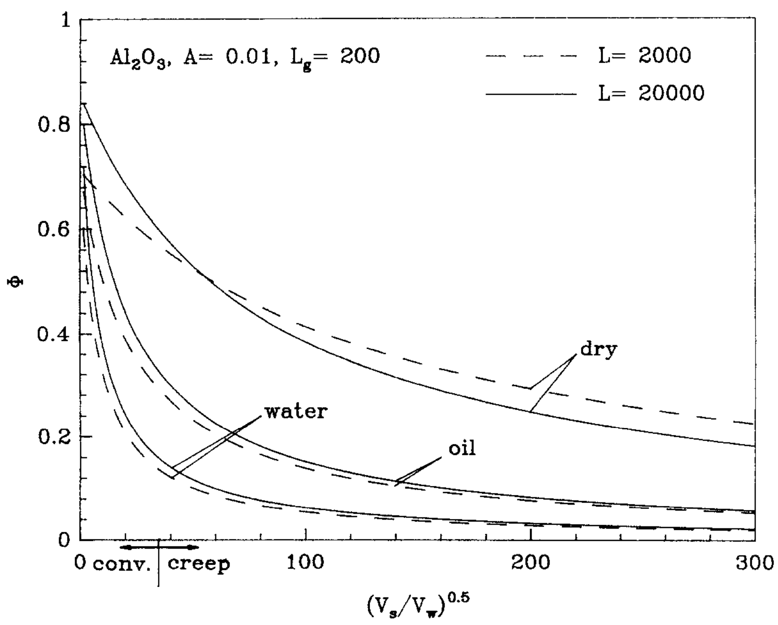

FIG. 4. Φ vs $(v_s/v_w)^{1/2}$ for aluminum oxide wheel, with water, oil, and without fluid.

pondingly lower in creep feed grinding. In the following numerical example, the grinding power will be chosen somewhat arbitrarily (although not unrealistically), to force film boiling to occur. The objective is to see whether or not workpiece burn then occurs. Recall that $\Phi = \theta_{wb,s}/\theta_{max,poss.}$, where $\theta_{max,poss.} = q''_{tot}/h_{wb}$. For typical conventional grinding conditions (see Table 1), with $q''_{tot} = 100\ \mathrm{W\ mm^{-2}}$, it is calculated that $\theta_{max,poss.}(l) = 780°\mathrm{C}$. Then from the graph, with $(v_s/v_w)^{1/2} = 17$ and $L = 2000$, $\Phi_{water} = 0.23$, and thus $\theta_{wb,s}(l) = 180°\mathrm{C}$, which is well over the film boiling temperature. Therefore, the temperature should actually be calculated from the upper dashed curve, which gives $\Phi_{dry} = 0.63$ and $\theta_{wb,s}(l) = 490°\mathrm{C}$. At this temperature, the workpiece would not undergo workpiece burn. Next, a typical creep feed grinding case is considered (see Table 1). With $q''_{tot} = 10\ \mathrm{W\ mm^{-2}}$, $\theta_{max,poss.}(l) = 2500°\mathrm{C}$. Then, since $(v_s/v_w)^{1/2} = 170$ and $L = 20\,000$, $\Phi_{water} = 0.038$ and thus $\theta_{wb,s}(l) = 95°\mathrm{C}$, which again is above the film boiling temperature (taking into account an ambient temperature $T_i = 30°\mathrm{C}$ or so). Thus the temperature should be calculated from the upper solid line ($L = 20\,000$), for which $\Phi_{dry} = 0.28$, $\theta_{wb,s}(l) = 700°\mathrm{C}$, and workpiece burn would probably occur.

In the example given above, when film boiling occurred in conventional grinding, workpiece burn did not occur, but when film boiling occurred in creep feed grinding, workpiece burn did occur. These results are fairly typical for grinding with an aluminum oxide wheel [22], and can be explained as follows. Since workpiece speeds are much lower in creep feed than in conventional grinding, the heat transfer to the workpiece is much lower. Therefore, heat transfer to the fluid is relatively more important, and when it is reduced due to film boiling, the temperature rise is greater in creep feed grinding than in conventional grinding.

These same concepts can be illustrated in a different way. Note that the workpiece surface temperature rise at the end of the grinding zone can be rewritten as follows:

$$\theta_{wb,s}(l) = \Phi q''_{tot}/h_{wb}(l) = \Phi \frac{2}{\sqrt{\pi}} (v_s/v_w)^{1/2} L^{-1/2} \frac{q''_{tot} l}{k_w}. \tag{20}$$

Thus the grinding power (per unit depth of the grinding zone) is given by

$$q'_{tot} = q_{tot}/b = q''_{tot} l = \frac{\sqrt{\pi}}{2} k_w (v_s/v_w)^{-1/2} L^{1/2} \theta_{wb,s}(l)/\Phi. \tag{21}$$

Therefore, the critical grinding power corresponding to film boiling of water is

$$(q'_{tot})_{fb,water} = \frac{\sqrt{\pi}}{2} k_w (v_s/v_w)^{-1/2} L^{1/2} \theta_{fb,water}/\Phi_{water} \tag{22}$$

and similarly for oil

$$(q'_{tot})_{fb,oil} = \frac{\sqrt{\pi}}{2} k_w (v_s/v_w)^{-1/2} L^{1/2} \theta_{fb,oil}/\Phi_{oil}. \tag{23}$$

For workpiece burn to occur, the critical grinding power is

$$(q'_{tot})_{burn} = \frac{\sqrt{\pi}}{2} k_w (v_s/v_w)^{-1/2} L^{1/2} \theta_{burn}/\Phi_{dry}. \tag{24}$$

These quantities are plotted in Fig. 5 as a function of $(v_s/v_w)^{1/2}$. The graph is for an aluminum oxide wheel. Once again, the values of A and L_g are held fixed, as indicated, and two values of L are presented. The values of $\theta_{fb,water}$, $\theta_{fb,oil}$, and θ_{burn} are taken to be 100, 270, and 700°C, respectively. For the parameter values considered here, the critical grinding power for film boiling of oil is usually slightly greater than that for water. However, this is not always the case, because there are two competing effects at work (see equation (23)). Since the temperature at which oil undergoes film boiling is higher than for water, there is a tendency for the critical grinding power to be higher for oil. On the other hand, the fact that oil does not remove heat from the grinding zone as effectively as water (i.e. Φ is higher) causes the critical grinding power to be lower for oil. As v_s/v_w increases, heat removed by the fluid becomes a larger fraction of the total, and so this latter effect becomes more important. Thus, it can be seen that as v_s/v_w increases, the two curves become closer, and they cross in the case of $L = 2000$. The graph also demonstrates the point made earlier concerning the occurrence of workpiece burn when film boiling occurs. For small v_s/v_w, the critical grinding power for burn is greater than that for film boiling (of either water or oil). Thus when $(q'_{tot})_{fb}$ is exceeded, film boiling occurs, but burn does not immediately occur (if it is assumed that the grinding power does not change much when the fluid undergoes film boiling). For large v_s/v_w, the opposite is true. Thus, when $(q'_{tot})_{fb}$ is exceeded, film boiling occurs, and immediately causes burn.

Also shown on Fig. 5 are three points taken from experimental results. Yasui and Tsukuda [6] indicated that when the grinding power exceeded about $5.2 \times 10^4\ \mathrm{W\ m^{-1}}$ for grinding with a water based fluid, with $(v_s/v_w)^{1/2} = 25$, the workpiece temperature suddenly increased to the value it had in dry grinding, but burn did not occur. This point is indicated with an octagon, and is seen to coincide very closely with the curve for film boiling of water (for $L = 2000$, which approximately coincides with the conditions of Yasui and Tsukuda's experiment). Since the curve for workpiece burn lies above the curve for film boiling of water, the workpiece would not be expected to burn under these conditions, as was found in the experiments. Also shown are two points from Ohishi and Furukawa's experiment [5]. They indicated the con-

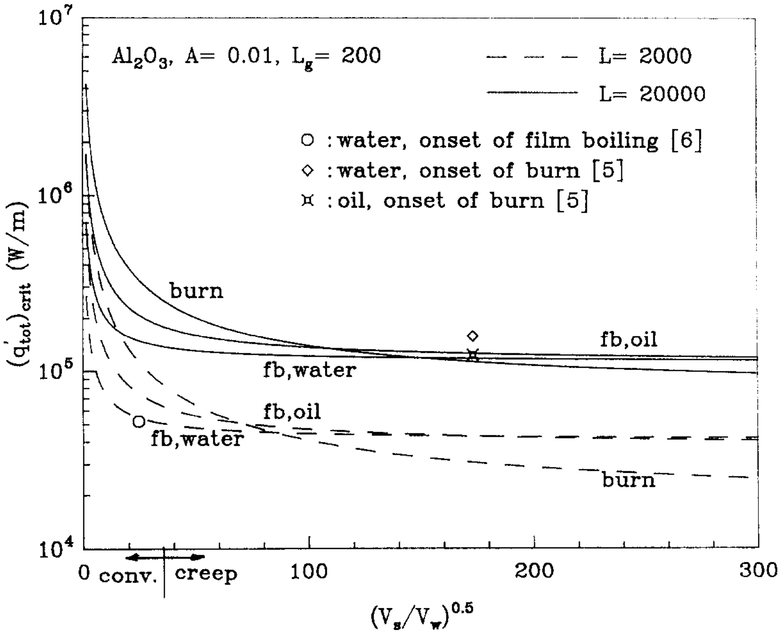

FIG. 5. Critical grinding power for film boiling and burning, for aluminum oxide wheel.

ditions under which burn occurred for grinding with water based fluid and oil, as shown by the diamond and four pointed star. The point for oil coincides quite closely with the curve for film boiling of oil (for $L = 20\,000$, which approximately coincides with the conditions of the experiment). Since this curve lies above the curve for workpiece burn, burn would occur as soon as film boiling occurred, in agreement with the experimental results. The point for water does not agree with the curve for film boiling of water. The reasons for this discrepancy are currently under investigation.

An alternative to aluminum oxide abrasives is to use CBN (cubic boron nitride) or diamond abrasive grains. There are two advantages of these so-called superabrasives. Because they are very hard, they remain sharp, and therefore tend to grind with lower grinding power than aluminum oxide abrasives. In addition, both of these materials have a very large thermal conductivity, so that the grains remove more heat from the grinding zone than aluminum oxide grains. It is difficult to determine the thermal conductivity of a single grain, but the values may be as high as 2000 W m^{-1} K^{-1} for diamond and 1300 W m^{-1} K^{-1} for CBN, as compared to 46 W m^{-1} K^{-1} for aluminum oxide [23]. The effect of the thermal properties of CBN will now be illustrated.

Figure 6 shows Φ as a function of $(v_s/v_w)^{1/2}$ for CBN abrasives and water, oil, or dry grinding. This graph demonstrates that Φ is significantly lower for CBN abrasives than for aluminum oxide abrasives (compare to Fig. 4), due to the fact that the CBN abrasives remove a significant amount of heat from the grinding zone. This also results in less of a spread in the values of Φ for water, oil, and dry grinding, since the contribution of the grinding fluid to the heat transfer is comparatively less significant.

Figure 7 shows the critical grinding power values for grinding with CBN abrasives. The CBN abrasives yield higher critical grinding powers for film boiling and burn, due to the lower values of Φ. Over the entire range of v_s/v_w shown, there is no crossover of the curves (for a fixed value of L). That is, over the entire range, when film boiling occurs for either water or oil, workpiece burn would not occur at that same value of grinding power. The lack of crossover in the case of CBN abrasives is because the heat removed by the grinding fluid is relatively less important compared to the heat removed by the CBN abrasives.

CONCLUSIONS

A model of heat transfer in the grinding process has been used to predict the maximum grinding zone temperature, and thereby to explore the occurrence of film boiling and thermal damage to the workpiece (i.e. workpiece burn). The occurrence of film boiling was modeled by assuming that when a critical temperature is reached (approximately 100–130°C for water and 300°C for oil), the heat transfer to the fluid becomes negligible. This of course causes a sharp rise in the workpiece background temperature. For aluminum oxide wheels, this rise is typically insufficient to cause workpiece burn in conventional grinding, but does cause workpiece burn in creep feed grinding. For CBN wheels, the heat removed by the abrasive grains is significant. As a consequence, higher grinding power is required to cause film boiling, and it is predicted that workpiece burn would usually not occur even after film boiling occurs.

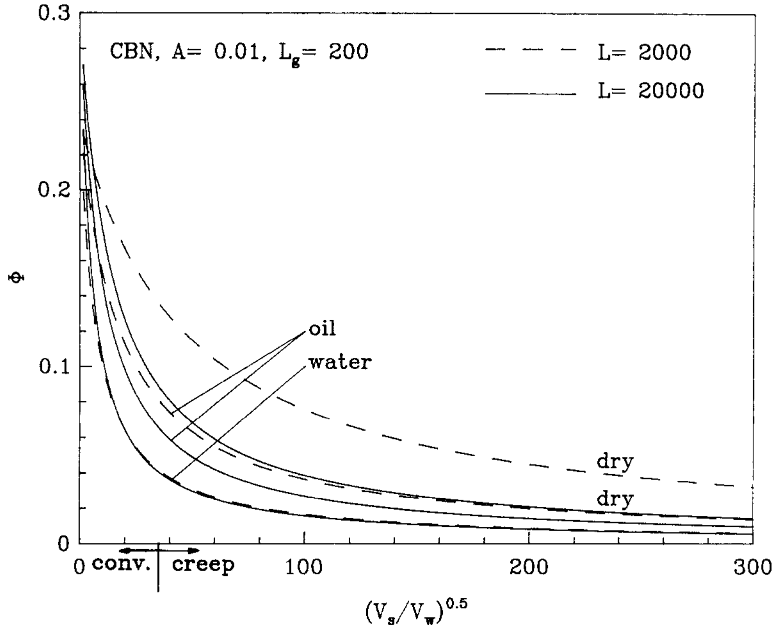

FIG. 6. Φ vs $(v_s/v_w)^{1/2}$ for CBN wheel, with water, oil, and without fluid.

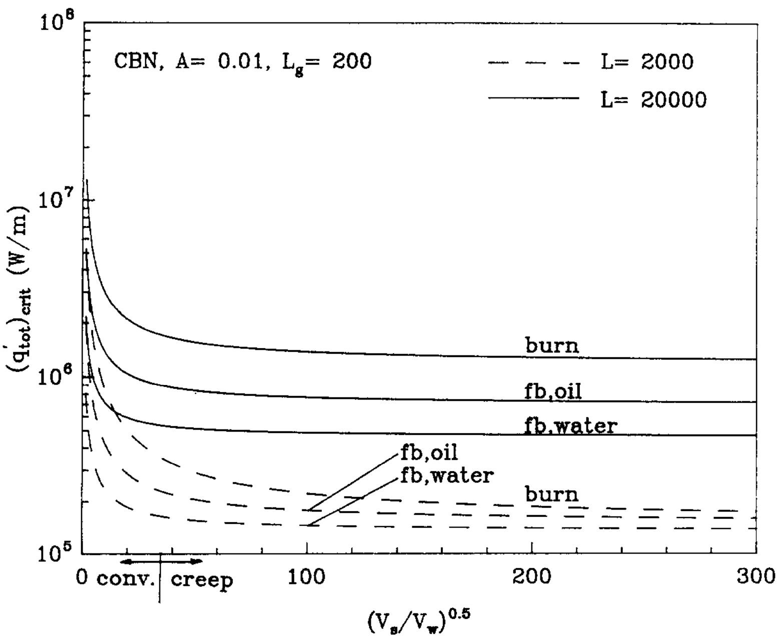

FIG. 7. Critical grinding power for film boiling and burning, for CBN wheel.

Acknowledgements—The support of the National Science Foundation, General Motors, General Electric, and Norton Company is gratefully acknowledged. The helpful comments and suggestions of Prof. Stephen Malkin were also gratefully appreciated.

REFERENCES

1. J. O. Outwater and M. C. Shaw, Surface temperatures in grinding, *Trans. ASME* **74**, 73–86 (1952).
2. G. R. Shafto, T. D. Howes and C. Andrew, Thermal aspects of creep feed grinding, 16th Machine Tool Design Research Conf., Manchester, U.K., pp. 31–37 (1975).
3. J. W. Powell and T. D. Howes, A study of the heat flux at which burn occurs in creep feed grinding, 19th MTDR Conf., Manchester, U.K., pp. 629–636 (1978).
4. C. Andrew, Coolant application in creep feed grinding, Int. Conf. on Creep Feed Grinding, Bristol, U.K., pp. 167–183 (1979).
5. S. Ohishi and Y. Furukawa, Analysis of workpiece temperature and grinding burn in creep feed grinding, *Bull. JSME* **28**(242), 1775–1781 (1985).
6. H. Yasui and S. Tsukuda, Influence of fluid type on wet

grinding temperature, *Bull. Japan Soc. Prec. Engng* **17**(2), 133–134 (1983).
7. A. S. Lavine, S. Malkin and T. C. Jen, Thermal aspects of grinding with CBN wheels, *Ann. CIRP* **38**(1), 557–560 (1989).
8. A. S. Lavine and T. C. Jen, Thermal aspects of grinding: heat transfer to workpiece, wheel, and fluid. In *Collected Papers in Heat Transfer 1989*, ASME HTD-Vol. 123, pp. 267–274 (1989). Also, accepted for publication in *J. Heat Transfer*.
9. R. Snoeys, M. Maris and J. Peters, Thermally induced damage in grinding, *Ann. CIRP* **27**(2), 571–581 (1978).
10. S. Malkin, Grinding of metals: theory and application, *J. Appl. Metalworking* **3**(2), 95–109 (1984).
11. R. S. Hahn, The relation between grinding conditions and thermal damage in the workpiece, *Trans. ASME* **78**, 807–812 (1956).
12. N. R. DesRuisseaux and R. D. Zerkle, Thermal analysis of the grinding process, *J. Engng Ind.* **92**, 428–434 (1970).
13. S. Malkin, Thermal aspects of grinding. Part 2—Surface temperatures and workpiece burn, *J. Engng Ind.* **96**, 1184–1191 (1974).
14. E. M. Kopalinsky, A new approach to calculating the workpiece temperature distributions in grinding, *Wear* **94**, 295–322 (1984).
15. N. R. DesRuisseaux and R. D. Zerkle, Temperature in semi-infinite and cylindrical bodies subjected to moving heat sources and surface cooling, *J. Heat Transfer* **92**, 456–464 (1970).
16. W. J. Sauer, Thermal aspects of surface grinding, *Proc. Int. Grinding Conf.*, pp. 391–411 (1972).
17. D. G. Lee, R. D. Zerkle and N. R. DesRuisseaux, An experimental study of thermal aspects of cylindrical plunge grinding, *J. Engng Ind.* **94**, 1206–1214 (1972).
18. W. B. Rowe, J. A. Pettit, A. Boyle and J. L. Moruzzi, Avoidance of thermal damage in grinding and prediction of the damage threshold, *Ann. CIRP* **37**(1), 327–330 (1988).
19. J. C. Jaeger, Moving sources of heat and the temperature at sliding contacts, *Proc. R. Soc. N.S.W.* **76**, 203–224 (1942).
20. A. S. Lavine, A simple model for convective cooling during the grinding process, *J. Engng Ind.* **110**, 1–6 (1988).
21. S. Malkin, *Grinding Technology, Theory and Applications of Machining with Abrasives*, Chap. 6. Ellis Horward, Chichester/Wiley, New York (1989).
22. A. S. Lavine and S. Malkin, The role of cooling in creep feed grinding, *Int. J. Adv. Mf. Technol.* 97–111 (1990).
23. C. F. Gardinier, Physical properties of superabrasives, *Ceramic Bull.* **67**, 1006–1009 (1988).

TRANSFERT THERMIQUE COUPLE ENTRE LA PIECE, LE FORET ET LE FLUIDE PENDANT LE PERCAGE ET APPARITION DU BRULAGE DE LA PIECE

Résumé—Un modèle de transfert thermique dans le perçage a été précédemment développé pour prédire la température dans la zone du perçage. Ce modèle est utilisé ici pour prédire l'apparition de l'ébullition en film du fluide et pour déterminer si la pièce brûle ou non. L'ébullition en film et le brûlage de la pièce sont tous deux supposés apparaître dans la zone des températures critiques. Les effets de différents paramètres sont explorés tels que les types de fluide et de grains abrasifs et les conditions conventionnelles ou de grande vitesse de perçage.

GEKOPPELTER WÄRMEÜBERGANG ZWISCHEN WERKSTÜCK, SCHLEIFSCHEIBE UND FLÜSSIGKEIT BEIM SCHLEIFEN UND DAS ENTSTEHEN VON ZUNDER

Zusammenfassung—In einer früheren Arbeit wurde ein Modell für den Wärmeübergang beim Schleifen entwickelt, das die Berechnung der Temperaturverteilung in der Schleifzone erlaubt. Dieses Modell wird jetzt dazu verwendet, um das Auftreten von Filmsieden im Schleifmittel vorauszuberechnen und um festzustellen, ob infolgedessen am Werkstück Verzunderungen auftreten werden. Es wird angenommen, daß sowohl das Filmsieden als auch die Verzunderung bei kritischen Temperaturen auftreten. Der Einfluß verschiedener Größen wird untersucht: Art von Schleifflüssigkeit und -korn, herkömmliche Schleifbedingungen, Schleifen mit geringem Vorschub.

ВЗАИМОСВЯЗАННЫЙ ТЕПЛОПЕРЕНОС К ОБРАБАТЫВАЕМОЙ ДЕТАЛИ, КОЛЕСУ И ЖИДКОСТИ ПРИ ШЛИФОВКЕ И ВОЗМОЖНОСТЬ ПРОГОРАНИЯ ДЕТАЛИ

Аннотация—Разработанная ранее модель теплопереноса при шлифовке позволяет определить температуру в зоне шлифования. В данной работе эта модель используется для предсказания пленочного кипения в шлифовальной жидкости, а также определения возможности последующего прогорания обрабатываемой детали. Предполагается, что как пленочное кипение, так и прогорание детали происходят при критических температурах в зоне шлифования. Исследуются эффекты различных параметров, таких как виды жидкости и абразивных гранул, а также условия шлифовки с обычной или ползучей подачей.