Thermal Analysis of Grinding

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Abstract
Thermal damage is one of the main limitations of the grinding process, so it is important to understand the factors which affect grinding temperatures. This paper presents an overview of analytical methods to calculate grinding temperatures and their effect on thermal damage. The general analytical approach consists of modeling the grinding zone as a heat source which moves along the workpiece surface. A critical factor for calculating grinding temperatures is the energy partition, which is the fraction of the grinding energy transported as heat to the workpiece at the grinding zone. For shallow cut grinding with conventional abrasive wheels, the energy partition is typically 60%–85%. However for creep-feed grinding with slow workspeeds and large depths of cut, the energy partition is only about 5%. Such low energy partitions are attributed to cooling by the fluid at the grinding zone. Heat conduction to the grains can also reduce the energy partition especially with CBN abrasives which have high thermal conductivity. For High Efficiency Deep Grinding (HEDG) using CBN wheels with large depths of cut and fast workspeeds, preheated material ahead of the grinding zone is removed together with the chips, thereby lowering the temperature on the finished surface. Analytical models have been developed which take all of these effects into account. Much more research is needed to better understand and quantify how grinding temperatures affect the surface integrity of the finished workpiece.

Keywords:
Grinding, Temperature, Analysis

1 INTRODUCTION
The grinding process requires a high energy expenditure per unit volume of material removed. Virtually all of this energy is dissipated as heat at the grinding zone where the wheel interacts with the workpiece. This leads to the generation of high temperatures which can cause various types of thermal damage to the workpiece, such as burning, metallurgical phase transformations, softening (tempering) of the surface layer with possible rehardening, unfavorable residual tensile stresses, cracks, and reduced fatigue strength [1,2]. Thermal damage is one of the main factors which affects workpiece quality and limits the fatigue strength [1,2]. Thermal damage is one of the main limitations of the grinding process, so it is important to understand the factors which affect grinding temperatures. This paper presents an overview of analytical methods to calculate grinding temperatures and their effect on thermal damage. The general analytical approach consists of modeling the grinding zone as a heat source which moves along the workpiece surface. A critical factor for calculating grinding temperatures is the energy partition, which is the fraction of the grinding energy transported as heat to the workpiece at the grinding zone. For shallow cut grinding with conventional abrasive wheels, the energy partition is typically 60%–85%. However for creep-feed grinding with slow workspeeds and large depths of cut, the energy partition is only about 5%. Such low energy partitions are attributed to cooling by the fluid at the grinding zone. Heat conduction to the grains can also reduce the energy partition especially with CBN abrasives which have high thermal conductivity. For High Efficiency Deep Grinding (HEDG) using CBN wheels with large depths of cut and fast workspeeds, preheated material ahead of the grinding zone is removed together with the chips, thereby lowering the temperature on the finished surface. Analytical models have been developed which take all of these effects into account. Much more research is needed to better understand and quantify how grinding temperatures affect the surface integrity of the finished workpiece.

Grinding, Temperature, Analysis

The grinding process requires a high energy expenditure per unit volume of material removed. Virtually all of this energy is dissipated as heat at the grinding zone where the wheel interacts with the workpiece. A critical parameter needed for calculating the grinding temperature exceeds the burnout temperature of the fluid at the grinding zone, which can be justified when the grinding temperature exceeds the burnout temperature of the fluid at the grinding zone, which can be justified when the grinding temperature exceeds the burnout temperature of the fluid. By inverting the heat transfer solution, the allowable power corresponding to a critical surface temperature can then be specified in terms of the grinding parameters, which enables in-process control of thermal damage. The calculated temperatures can also be coupled with metallurgical reaction rate kinetics to predict tempering and rehardening effects for grinding of hardened steels.

For shallow cut grinding with conventional abrasive wheels, the energy partition is typically 60%–85%. By
contrast, the energy partition for creep-feed grinding with slow workspeeds and large depths of cut is much smaller, typically 5% or even less. It is shown that such low energy partition can be attributed to cooling by the fluid at the grinding zone. Effective cooling without burnout of the fluid is crucial for creep-feed grinding to avoid thermal damage. Composite and single grain thermal models are presented to account for the effect of cooling on the energy partition. Furthermore, the workpieces are often short relative to the long wheel-workpiece contact lengths in creep feed grinding, so a transient thermal analysis may also become necessary.

As compared with shallow cut grinding using conventional aluminum oxide wheels, thermal damage with CBN superabrasive wheels is generally found to be much less of a problem [13]. Lower temperatures and reduced thermal damage with CBN might be attributed to the very high thermal conductivity of the abrasive grains so that more grinding heat is conducted to the grinding wheel rather than to the workpiece. The energy partition is typically about 20% or even much lower with vitrified and electroplated CBN wheels due to the added benefit of cooling by the fluid at the grinding zone. It will be shown that these effects can be taken into account using the single grain model mentioned above.

This paper concludes with a consideration of High Efficiency Deep Grinding (HEDG). The HEDG process utilizes mainly CBN wheels with large depths of cut, comparable to or bigger than for creep-feed grinding, and also relatively fast workspeeds, comparable to conventional shallow cut grinding. As such, HEDG processes provide extremely high removal rates per unit width. From the thermal point of view, a unique characteristic of HEDG is the inclined heat source associated with the large depth of cut. Such large depths of cut together with fast workspeeds lead to a situation whereby some heated material in the wedge ahead of the grinding zone is removed with the chips during the grinding process, thereby resulting in lower temperatures on the finished ground surface.

2 CONVENTIONAL SHALLOW CUT GRINDING

2.1 Thermal analysis

Grinding occurs by the interaction of discrete abrasive grains on the wheel surface with the workpiece. It has been shown that the total grinding energy due to the interaction between the wheel and the workpiece can be considered to consist of chip formation, plowing, and sliding components [14]. Peak ‘flash’ temperatures are generated which approach the melting point of the material being ground [15,16]. However, these peak temperatures are of extremely short duration and highly localized on the shear planes of microscopic grinding chips. Just beneath the surface, the workpiece ‘feels’ nearly continuous heating owing to the multiplicity of interactions with the abrasive grits passing quickly through the grinding zone. Therefore, the temperature associated with ‘continuous’ heating over the grinding zone, rather than the peak ‘flash’ temperature, is found to be responsible for most thermal damage. This temperature is often referred to as the ‘grinding zone’ or ‘background’ temperature [17]. Also of interest is the much smaller bulk temperature rise of the workpiece, which causes thermal expansion possibly leading to distortions and dimensional inaccuracies [18-21].

In order to calculate the grinding zone temperature rise, consider the cylindrical plunge-grinding situation illustrated in Figure 1(a). The grinding energy is dissipated over the rectangular grinding zone of length \( l_c \) along the arc of contact and width \( b \) normal to the plane of the figure. For simplicity, the grinding heat flux \( q_w \) to the workpiece is shown as being uniformly distributed over the grinding zone \([15,22-27]\), although a different distribution (e.g. triangular) may prevail. Since the cylindrical workpiece is generally much bigger than the dimensions of the grinding zone, the heated area can be likened to a plane band source of heat which moves along the surface of a semi-infinite solid (the workpiece) at the workpiece velocity. For this two-dimensional heat transfer model illustrated in Figure 1 (b), the temperature rise can be written as [28]:

\[
\theta = \frac{1}{k} \int q_w(\xi) \frac{V(x-\xi)}{2\alpha} K_0 \left[ \frac{V}{2\alpha} \sqrt{\left[(x-\xi)^2 + z^2\right]} \right] d\xi 
\]

(1)

where \( K_0(x) \) is the modified Bessel function of the second kind of order zero, \( q_w(\xi) \) is the heat flux distribution at the surface on the semi-infinite body, \( k \) is the thermal conductivity of the workpiece material, \( \alpha \) is its thermal diffusivity, \( I \) is the half of the heat source length, and \( V \) is the heat source velocity.

![Figure 1: Illustration of (a) external cylindrical plunge grinding and (b) thermal model for grinding temperatures.](image)

In many practical cases, the maximum temperature rise \( \theta_m \) is of particular interest. If the heat source is moving sufficiently fast so that heat conduction in the direction of motion is much slower than the heat source velocity, the maximum temperature rise for a uniform heat source \( q_w(\xi) = q_w \) can be approximated as [28]:

\[
\theta_m = \frac{1.595 \sqrt{q_w L W / 24 \alpha}}{k V^{1/2}} 
\]

(2)

This ‘high speed’ condition is satisfied if the dimensionless thermal number (Peclet number) \( L \equiv V / 2\alpha \) is bigger than 5, although Equation (2) is also fairly accurate down to \( L=1 \), which includes most actual grinding situations [22]. The average surface temperature \( \theta_0 \) over the source is \( 2\sqrt{3} \theta_m \) for \( L>5 \).

An expression for \( \theta_m \) can now be obtained by making the appropriate substitutions into Equation (2). The velocity \( V \) in the heat transfer model corresponds to the workpiece velocity \( v_w \):

\[
V = v_w 
\]

(3)

and the half-length \( I \) of the heat source is half the arc length of contact:
l = l_c/2 \quad (4)

From geometrical considerations, the arc length of contact is given by:

\[ l_c = (ad_e)^{1/2} \quad (5) \]

where \( d_e \) is the equivalent diameter calculated as:

\[ d_e = \frac{d_w d_s}{d_w \pm d_s} \quad (6) \]

This relationship is applicable to straight surface grinding \((d_s = d_e)\), external cylindrical grinding \((\text{plus sign in denominator})\), and internal cylindrical grinding \((\text{minus sign in denominator})\), so this thermal analysis is not limited to external cylindrical grinding but is also applicable to straight surface and internal grinding. Equation (5) neglects elastic deformation, which would make \( l_c \) bigger [29] in which case the calculated temperature would be less. However it has been shown using an inverse heat transfer analysis that most of the grinding energy is dissipated within the geometrical contact length \([30,31]\).

Combining Equations (2) to (6), the maximum grinding zone temperature rise becomes:

\[ \theta_m = \frac{1.13q_w a_w^{1/2} a_s^{3/4} d_e e^{1/4}}{k v_w^{1/2}} \quad (7) \]

With a triangular heat source, instead of the uniform rectangular one considered here, the factor 1.13 in Equation (7) reduces slightly to 1.06 [32].

For calculating \( \theta_m \), the parameter which remains to be determined is \( q_w \), which is the heat flux to the workpiece at the grinding zone. Of the total grinding energy, only the fraction \( \varepsilon \) is conducted as heat to the workpiece at the grinding zone. The parameter \( \varepsilon \) is often referred to as the ‘energy partition’. For a specific grinding energy \( u \) or power \( P \), the heat flux to the workpiece can be written as:

\[ q_w = \frac{\varepsilon u v_w + d e^{-1/2}}{l_c b} = \frac{\varepsilon P}{l_c b} \quad (8) \]

where the numerator is that portion of the grinding power entering the workpiece as heat and the denominator is the area of the grinding zone.

2.2 Energy partition

In order to calculate \( q_w \), it is necessary to specify the energy partition to the workpiece \( \varepsilon \). The first direct measurement of the energy partition using a calorimetric method was reported in 1961 as 84% [33], although subsequent measurements reported in 1974 for shallow cut \( (\text{‘pendulum’}) \) grinding of steels with conventional abrasive wheels ranged from about 60 to 85% [34]. More recent studies have shown that the energy partition can vary significantly, depending on the type of grinding, fluid application conditions, and wheel composition. For example, it will be seen in the following sections that creep feed grinding with porous aluminum oxide wheels can give extremely low energy partitions of only 3-7%, and that comparable low values may be obtained for grinding with vitrified and electroplated CBN wheels.

For shallow cut grinding of steels with aluminum oxide wheels, the energy partition typically varies from about 60 to 85% [11,34]. These results have been rationalized in terms of a grinding energy model whereby the total specific energy includes contributions from chip formation, plowing, and sliding [14]:

\[ u = u_{ch} + u_{pl} + u_{sl} \quad (9) \]

Here \( u_{ch} \), \( u_{pl} \), and \( u_{sl} \) are the chip-formation, plowing, and sliding components, respectively. From heat transfer considerations, it can be shown that almost all the sliding energy generated at the interface between the wear flats and the workpiece is conducted as heat to the workpiece. Likewise, virtually all the plowing energy is retained as heat in the workpiece, as plowing involves workpiece deformation without material removal. Results from calorimetric measurements indicate that approximately 55% of the chip-formation energy is transported to the workpiece, which is consistent with expectations from a heat transfer analysis of the chip-formation process [34]. This would imply that all the grinding energy except for about 45% of the chip-formation energy is conducted as heat to the workpiece, so the overall fraction of the grinding energy entering the workpiece is:

\[ \varepsilon = \frac{u_{pl} + u_{sl} + 0.55u_{ch}}{u} = \frac{u - 0.45u_{ch}}{u} \quad (10) \]

Substituting this result into Equation (7) and combining with Equation (8) leads to the maximum temperature rise:

\[ \theta_m = \frac{1.13\varepsilon^{1/2} a_e^{3/4} v_w^{1/2}}{k d_e^{1/4}} \quad (11) \]

Various techniques have been used to measure the energy partition in grinding. As stated above, early investigations were based upon calorimetric methods whereby the heat content in the workpiece was obtained by measuring its average temperature rise immediately after grinding [33,34]. The energy partition was then obtained as the ratio of heat content in the workpiece to the total grinding energy obtained from measurements of forces or power. Calorimetric methods have been successfully applied only to dry grinding.

The energy partition can also be obtained using temperature matching and inverse heat transfer methods [11,30,31]. Both of these methods utilize measurements of the temperature response in the workpiece subsurface, rather than its average temperature rise. The temperature response in the workpiece subsurface can be measured during straight surface plunge using either an embedded thermocouple or infrared detector with an optical fiber. The thermocouple or optical fiber is fed into a blind hole from the underside of the workpiece toward the surface being ground. The temperature response is measured at the bottom of the blind hole as the grinding wheel passes over the workpiece. With each successive grinding pass, the temperature response is measured closer to the surface being ground.

With the temperature matching method [8-11], it is necessary to find the energy partition \( \varepsilon \) for which the measured temperature response most closely matches the temperature computed from the thermal model. For example, it is seen in Figure 2 that the individual temperature responses at various depths for grinding of a mild steel with an aluminum oxide wheel match the analytically computed temperature responses quite well for \( \varepsilon = 63\% \). Nearly the same energy partition \( \varepsilon = 65\% \) was obtained by matching the maximum temperature at various depths to the analytically computed temperature as seen in Figure 3.

With the inverse heat transfer method [30,31], the heat flux to the workpiece surface is calculated from the measured temperature distribution within the workpiece. An example of a heat flux distribution obtained in this way is shown in Figure 4 for grinding of a hardened bearing.
steel with an aluminum oxide wheel. It can be seen that the resulting heat flux distribution consists of both positive and negative values. Positive heat flux indicates heat flow into the workpiece at the grinding zone, and negative heat flux indicates localized cooling by the applied grinding fluid behind the grinding zone. The area under the heat flux curve at the grinding zone is the total energy to the workpiece per unit width of grinding. For this example, the energy partition is approximately 75%.

Energy partition values obtained from temperature measurements tend to be comparable to but smaller than predicted from Equation (10). The specific energy for the example in Figure 2 and Figure 3 was 45 J/mm³, which according to Equation (10) would result in a predicted energy partition of 86%. Likewise for the example in Figure 4, the specific energy is about 40 J/mm³, which would imply an energy partition of about 85%. These discrepancies between predicted and measured values may be attributed to a number of factors. First of all, the thermal model assumes that all the energy expended by grinding is converted to heat. However it has been shown that about 3% of the energy expended by plastic deformation may not be converted to heat for the large strains encountered in machining [35]. The thermal model also assumes that all the sliding energy due to rubbing between the wear flats and the workpiece is conducted as heat into the workpiece. But an analogous friction-slider thermal model for aluminum oxide on steel indicates that 90-93% of the sliding energy, rather than all of it, should be conducted as heat to the workpiece. Likewise, all the plowing energy associated with deformation of the workpiece material was assumed to be retained as heat in the workpiece, but the abrasive grains in contact with the workpiece may conduct away some of this heat. Furthermore, cooling by the fluid would also tend to remove some heat from the workpiece. Heat conduction to the abrasive grains and cooling by the fluid at the grinding zone are considered in the following sections.

As with most thermal analyses, a linear heat transfer model was used with the assumption of thermal properties independent of temperature. But the grinding zone temperatures are often so high that this assumption may not be justified. One approach to account for the influence of temperature on thermal properties has been to use an iterative procedure whereby the constant-property solution is used but with the thermal properties evaluated at the average surface temperature over the heat source [36]. A more precise numerical analysis of this moving-heat-source problem with temperature-dependent thermal properties for a plain carbon steel, which would be comparable to those of other plain carbon and low-alloy steels, has shown that the increase in specific heat and decrease in thermal conductivity with temperature partially offset each other so that the linear constant-property solution (Equation (10)) only slightly underestimates the actual temperature up to a maximum temperature of about 1000°C [37]. With nickel-base alloys, however, it has been shown using a finite element analysis that the constant-property linear thermal model may significantly overestimate grinding temperatures [38].
2.3 Critical temperature

Thermal damage control may require that the maximum temperature rise be kept below a critical value \( \theta' \). For conventional grinding with aluminum oxide wheels, the allowable specific grinding energy corresponding to a maximum temperature rise \( \theta_m \) can be obtained by rearranging Equation (11) to [22]:

\[
u = u_0 + Bd_e^{1/4}a_e^{-3/4}v_w^{-1/2} \tag{12}\]

where

\[
u_0 = 0.45u_{ch}\]

and

\[B = \frac{k\theta_m}{1.13\alpha^{1/2}} \tag{14}\]

Equation (12) by the material removal rate (Equation (15) provides a practical basis for in-process monitoring of the grinding power and hence \( \theta_m \) can be obtained by multiplying Equation (12) by the material removal rate \( Q_w = bv_wa \):

\[P = u_0bv_wa + Bd_e^{1/4}a_e^{-3/4}v_w^{-1/2} \tag{15}\]

where \( b \) is the grinding width and \( u_0 \) and \( B \) are as defined in Equations (13) and (14).

Equation (15) provides a practical basis for in-process identification of thermal damage which occurs above a critical maximum grinding zone temperature. For this purpose, a corresponding limiting value of the parameter \( B \) must be specified, which can be obtained from Equation (15) by in-process monitoring of the grinding power and post-grinding inspection of workpiece quality. The actual critical temperature \( \theta' \) need not be specified. After calibrating the thermal damage limit in this way, the measured grinding power can be compared with the allowable threshold power (Equation (14) with \( \theta_m = \theta' \)) to identify whether thermal damage is occurring. This is illustrated in the following section for thermal damage of steels by workpiece burn.

The foregoing analysis considers the grinding zone temperature on the workpiece surface. Thermal damage also occurs in the subsurface. For the moving-band heat source (Figure 1(b)), the maximum dimensionless temperature rise at depth \( z \) beneath the surface can be approximated by [40]:

\[\tilde{\theta}_m = \left( \frac{\pi kV}{2acw} \right) \theta_m = 3.1L^{0.53}\exp[-0.69L^{-0.37}Z] \tag{16}\]

where \( L \) is the thermal number and \( Z \) is the dimensionless depth defined from the actual depth \( z \) as:

\[Z = \frac{Vz}{2a} \tag{17}\]

More accurate approximations covering a wider range of \( L \) and \( Z \) have also been obtained [1]. From Equation (16) it can be readily shown, for a given maximum surface temperature, that a steeper gradient of maximum temperature, and hence shallower heat penetration, is obtained with a faster velocity \( V \) and, to a lesser extent, with a shorter heat source half-length.

Up to this point, the heat transfer analysis neglects any influence of cooling. The workpiece has been modeled as a semi-infinite body (Figure 1(b)) with its surface thermally insulated except at the moving heat source. Most grinding operations are performed using a grinding fluid which cools the workpiece, and an analysis of the moving-heat-source problem has been presented which also considers the effect of cooling [17]. In order for cooling to lower the grinding zone temperature to any significant degree, it is necessary for heat to be removed from within the grinding zone area. For most shallow cut grinding, cooling by grinding fluids at the grinding zone is ineffective. However for creep feed and some other types of grinding, it will be shown that cooling at the grinding zone is crucial. Grinding fluids also provide bulk cooling of the workpiece, thereby helping to control dimensional inaccuracies due to thermal deformations [41]. Furthermore grinding fluids can also be credited with lowering grinding zone temperatures by providing lubrication, which reduces the energy input.

Straight oils are generally better lubricants and more effective in reducing the grinding energy than most synthetic oils and water-based soluble oils and emulsions. However, water-based fluids are much better coolants than oils, since their specific heats are typically two to three times and their thermal conductivities about four times those of oils.

Another factor which has been neglected up to this point is repetitive grinding over the same nominal area. With cylindrical plunge grinding, for example, the heat source passes over the same nominal location on the workpiece periphery once per workpiece revolution. The temperature rise in Equation (16) is for one pass over the workpiece, which should be superposed on the residual temperatures from previous grinding passes. In most practical cases the residual temperature is a negligible portion of the maximum temperature rise for a single pass, and it is usually neglected.

Although the thermal analysis has been developed for plunging grinding of straight cross-sections without profiles, it can also be applied to grinding of gentle profiles having moderate variations in equivalent diameter across the width. For example, the present heat transfer analysis may suffice for grinding of typical profiles on ball-bearing races. With more severe profiles, significant temperature variations may occur across the grinding width. From heat transfer considerations, the highest temperatures should be developed near the crests of convex protrusions (‘peaks’) in the profile [1] where there is less material to conduct away the heat, which explains why these areas...
are usually more prone to thermal damage and why the lowest temperatures tend to occur at concave locations (‘valleys’).

The present two-dimensional heat transfer analysis can also be applied to traverse grinding with cross-feed provided that the radial wheel wear towards the leading edge is much less than the depth of cut. In this case, the grinding action and, thus, the heat source can be considered to be concentrated over the leading edge of the wheel within width s, corresponding to the cross-feed per workpiece revolution. By analogy with plunge grinding, the cross-feed s would correspond to the grinding width b. A correction factor in the thermal analysis may also be necessary if the cross feed (source width) is comparable in size or smaller than the arc length of contact (source length) [28]. If the low wheel-wear condition is not satisfied, then the energy is distributed in steps across the grinding width [42], and it is necessary to know the radial wheel wear on each step and to estimate the energy distribution among the steps in order to calculate the grinding zone temperature.

2.4 Thermal damage

Excessive grinding temperatures cause thermal damage to the workpiece. In this section, a few common types of thermal damage will be considered. By establishing a direct relationship between the heat transfer analysis of the previous section and some types of thermal damage, it becomes practically feasible to predict and control thermal damage by in-process monitoring of the grinding power.

2.4.1 Workpiece burn

The most common type of thermal damage is workpiece burn. This phenomenon has been investigated mainly for grinding of plain carbon and alloy steels, although it is also a problem with other metallic materials [2,43]. Visible workpiece burn with steels is characterized by bluish temper colors on the workpiece, which are a consequence of oxide-layer formation [3,44]. Temper colors are usually removed by spark-out at the end of the grinding cycle, especially with cylindrical grinding, but this effect is cosmetic and the absence of temper colors on the ground surface does not necessarily mean that workpiece burn did not occur.

From microhardness distributions in the subsurface of hardened steels, visible burn has been found to be accompanied by reaustenitization of the workpiece and rehardening due to the rapid cooling [3,15]. For a hardened steel ground without any burning, there is generally some softening due to tempering close to the surface [1,3,15,40]. Some examples of rehardening and tempering behavior are shown for grinding of a hardened tool steel at four depths of cut in Figure 6 [40,45]. Starting with an initial hardness of about 8 GPa, a greater degree of tempering can be seen with increasing wheel depth of cut due to higher temperatures to a greater depth below the surface. With the onset of burning, rehardening of the steel workpiece also occurs towards the surface at the biggest depth of cut in Figure 6. Rehardening is a consequence of reaustenitization followed by the formation of untempered martensite, which appears after etching as a white phase in the surface layer or in patches. Workpiece burn and austenitization by grinding heat of soft steels, even hardenable types, is not necessarily accompanied by surface hardening. The metallurgical evidence and microhardness measurements suggest that the visible burn threshold is virtually coincident with the onset of that for austenitization [15]. While such hardening is generally considered to be detrimental, hardening by grinding heat also provides a means to produce hardened surfaces without the need for subsequent heat treatment [46,47].

Burning might be expected to occur when a critical grinding zone temperature is exceeded. The critical specific energy at the burning threshold, \( u^* \), might behave according to Equation (12), so that a graph of \( u^* \) plotted against the quantity \( d_a \) would yield a straight line as in Figure 5 [22]. Such behavior is shown in Figure 7 for straight and external cylindrical grinding of a wide variety of steels having similar thermal properties. The particular straight line at the workpiece burn threshold shown in Figure 7 corresponds to Equation (12) with a critical slope \( B^* = 7.2 \text{ J/mm}^2 \cdot \text{s}^{1/2} \). The intercept value is \( u_0 = 6.2 \text{ J/mm}^2 \), which corresponds to 0.45\( \text{J/mm}^2 \), and, according to the thermal model, is that portion of the specific energy not entering the workpiece. This same approach has been applied to controlling thermal damage for grinding of hardened alloy steels for helicopter gears [48].

![Figure 6: Microhardness versus depth beneath surface for SK7 tool steel at four depths of cut.](image)

The maximum grinding zone temperature \( \theta_0 \) at the burning threshold in Figure 7 can be calculated from Equation (11). Using room temperature values for the thermal properties \( k \) and \( \alpha \), the magnitude of the slope corresponds to a maximum temperature rise \( \theta_0 = 650^\circ C \). Adding to this a typical initial temperature of 40 to 70°C, and taking into account the inaccuracies in the thermal model associated with the assumption of constant thermal properties as discussed in the previous section, the maximum temperature becomes comparable to the eutectoid temperature of 723°C for plain carbon steels. This would be the minimum temperature required for austenitization, although a somewhat higher temperature of about 800°C might be necessary in order to form untempered martensite by rehardening at the ground surface [1].

These findings offer a practical means for in-process identification and control of thermal damage. During grinding, the measured specific energy can be compared with the critical specific energy \( u^* \), in order to predict whether workpiece burn is occurring. Likewise the allowable specific energy can be set higher or lower than \( u^* \), depending on the particular situation. For grinding of high strength critical components, it may be important to completely avoid any thermal damage, whereas non-critical components may be more efficiently ground if some workpiece burn is allowed as least during initial rough grinding. Machine power can be accurately measured in production using inexpensive solid-state power transducers. In order to determine the net grinding power, it is usually sufficient to subtract the measured idling power from the total power. The net grinding power can then be compared with the threshold burning power as given by Equation (15) using the values of \( u_0 \) and \( B^* \) as above. In-process power monitoring in this way also...
facilitates adaptive optimal control of the grinding process while satisfying surface quality requirements.

Workpiece burn of bearing steels has been found to have a deleterious effect on fatigue life, which can be attributed mainly to the formation of untempered martensite. This has been dramatically demonstrated for rolling contact fatigue lives of hardened bearing rings finished under various grinding conditions [49]. The results summarized in Figure 8 show a direct relationship between the $L_{10}$ fatigue life and the ratio of the specific energy to the critical specific energy at the burning threshold, $u/u^*$. ($L_{10}$ is the fatigue life exceeded by 90% of the specimens.) Below the predicted burning limit ($u/u^* < 1$) the $L_{10}$ fatigue life is about 50 hours, whereas just above the burning limit it drops catastrophically to about 10 hours.

Figure 7: Specific energy at workpiece burn threshold for straight surface and external cylindrical grinding of carbon and low-to-medium-alloy steels.

Figure 8: $L_{10}$ fatigue life versus specific energy ratio $u/u^*$ for AISI S2100 bearing rings.

### 2.4.2 Tempering and Rehardening

Steels are usually ground in the hardened state. Transformations which may occur due to excessive grinding temperatures include tempering (softening) of the hard martensite phase, and also the formation of hard and brittle martensite (rehardening) if the temperature is high enough and persists long enough for reaustenitization to occur. Untempered martensite is formed by rapid cooling of the reaustenitized material mainly by heat conduction to the workpiece bulk after the grinding zone (heat source) passes [45,50].

Tempering is a complex phenomenon which is mainly due to carbon diffusion and is dependent upon both temperature and time. In general, the hardness $H$ obtained after tempering at temperature $\theta$ for time $t$ can be expressed by a single-valued function of the time-modified temperature parameter, $\theta(C + \log t)$, or [51]:

$$H = H(C + \log t)$$  \hspace{1cm} (18)

where $C_T$ is an experimentally determined constant for each steel.

As a practical matter, it would be useful to be able to predict the reduction in hardness due to grinding heat by combining the tempering behavior (Equation (18)) with the thermal analysis. The application of this approach to grinding necessitates determining an effective temperature and corresponding effective time at that temperature at varying depths below the surface. While the temperature history at any point in the subsurface is very complex, the maximum temperature could be taken as the effective temperature, particularly in view of the very strong influence of temperature on tempering. The maximum temperature rise at a depth $z$ below the surface can be obtained from Equation (16). The effective time might be conveniently expressed by the parameter $(l/v_w)$ which is how long it takes for the grinding zone (heat source) to pass any given point on the workpiece surface. Experimental results for the microhardness distribution in the subsurface of a hardened steel due to tempering by grinding heat show the expected trends, whereby a higher temperature and/or longer time parameter $(l/v_w)$ causes a greater degree of softening [4]. However, these and other results cannot be fitted to a single master tempering curve (Equation (18)).

A more fundamental ‘differential’ analysis of the tempering process has been developed which couples the reaction kinetics with the thermal analysis [45]. Based upon an analysis of existing tempering data [51], a third order reaction rate equation was found to describe the tempering phenomena of the form:

$$\frac{d\psi}{dt} = h_t (1 - \psi)^3$$  \hspace{1cm} (19)

where $\psi$ is the probability (fraction) of transformation and $h_t$ is a thermally activated parameter:

$$h_t = A_t \exp(-\frac{U_t}{R\theta_a})$$  \hspace{1cm} (20)

Here $U_t$ is the activation energy for diffusion of carbon in alpha iron ($U_t = 80$ kJ/mol), $R$ is the universal gas constant, $\theta_a$ is the absolute temperature, and $A_t$ is a constant for a particular steel. Furthermore the hardness $H$ was assumed to follow a linear dependence on $\psi$:

$$H = H_3 - (H_3 - H_1)\psi$$  \hspace{1cm} (21)

where $H_3$ and $H_1$ are the minimum and maximum hardnesses of the fully tempered and fully quenched materials, respectively.

In addition to tempering, a similar ‘differential’ analysis was also developed for rehardening. Rehardening causes an increase in hardness or, in other words, a decrease in the probability $\psi$ of tempering. The rate controlling step in rehardening of steels is reaustenitization, which can be described by the first order rate equation [52,53]:
where analogous to tempering

\[
h_2 = A_2 \exp\left(\frac{U_2}{RT_0}\right)
\]

Here the activation energy \( U_2 \) is for diffusion of carbon in gamma iron \( (U_2 = 135 \text{ kJ/mol}) \).

In order to predict the effect of the grinding temperature on the metallurgical state and hardness distribution in the workpiece subsurface after grinding, the reaction rate kinematics (Equations (19) - (23)) were coupled with a thermal analysis, similar to the one presented above, which also took into account the effect of temperature on the thermal properties and of repetitive grinding passes over the same area [45]. Some examples of the results are shown by the solid lines for the subsurface hardness distributions in Figure 6. Considering the complexity of the tempering and rehardening phenomena, these analytical results are in very good agreement with the experimental measurements.

Tempering commonly occurs near the workpiece surface during grinding of hardened steels, and it may be accompanied in severe cases by rehardening. The depth of the thermally affected layer may be reduced with faster workpiece velocities which results in shallower heat penetration and shorter heating times. The thermally affected layer produced during aggressive rough grinding may be subsequently removed by gentler finish grinding and spark-out at the end of the grinding cycle.

2.4.3 Residual stresses

Grinding causes residual stresses in the vicinity of the finished surface, which can significantly affect the mechanical behavior of the material. Residual stresses are induced by non-uniform plastic deformation near the workpiece surface [1,2,54-61]. Mechanical interactions of abrasive grains with the workpiece result in predominantly residual compressive stresses by localized plastic flow. Residual tensile stresses are caused mainly by thermally induced stresses and deformation associated with the grinding temperature and its gradient from the surface into the workpiece. Thermal expansion of hotter material closer to the surface is partially constrained by cooler subsurface material. This generates compressive thermal stresses near the surface which, if sufficiently big, cause plastic flow in compression. During subsequent cooling, after the grinding heat passes, cooling of the compressed material causes residual tensile stresses to develop. In order to ensure mechanical equilibrium, residual compressive stresses also arise deeper in the material, but these are much smaller in magnitude than the residual tensile stresses. The formation of thermally induced residual stresses is further complicated by any metallurgical transformations which may occur during the heating and cooling cycle, since these generally involve volumetric changes.

Some examples of the distribution of the residual stress component along the grinding direction are shown in Figure 9 for an alloy steel [1]. Residual stress measurements, which are usually based upon X-ray methods, typically reveal a biaxial stress state in the surface layer with the stress along the grinding direction approximately equal to the stress across the grinding direction [54,60]. In much production grinding, the residual stresses are predominantly tensile, which would indicate that they are mainly thermal in origin. Residual compressive stresses are considered to have a beneficial effect on mechanical strength properties, whereas residual tensile stresses are considered to be detrimental. The influence of residual stresses is relatively more pronounced with higher strength brittle materials. More severe grinding conditions on high-strength steels and aircraft alloys generally cause bigger residual tensile stresses, thereby leading to reduced fatigue strength and cracking [1,2,61-67]. The situation may be further aggravated in steels by hydrogen embrittlement, owing to significantly higher levels of hydrogen being released as a result of grinding fluid breakdown [68]. Abusively ground hardened steel components exposed to hot acid develop surface cracks, which are associated with residual tensile stresses acting on brittle untempered martensite formed by workpiece burn. Cracks induced by acid etching and abusive grinding are usually oriented normal to the grinding direction [63,64], which suggests that the residual tensile stress component along the grinding direction is the predominant one. The tendency for microcracking in high strength nickel base alloys due to residual tensile stresses may be further promoted by the onset of non-equilibrium constitutional melting at elevated grinding temperatures [38].

Figure 9: Residual stress distributions for grinding of an alloy steel. Residual tensile stresses are considered to be thermally induced.

It is generally desirable to control the grinding conditions so as to induce residual compressive stresses or, at least, to limit the magnitude of the peak residual tensile stress. As a practical matter, demands for more efficient production and faster removal rates result in higher residual tensile stresses, such as seen in Figure 9. In order to obtain residual compressive stresses, it is usually necessary to maintain extremely low removal rates. However, the introduction of CBN abrasive wheels in place of aluminum oxide has been shown to induce compressive instead of tensile residual tensile stresses when grinding hardened bearing races [69,70]. This suggests reduced temperatures for grinding with CBN abrasive wheels as compared with conventional aluminum oxide wheels. It will be seen in Section 4 that the very high thermal conductivity of CBN promotes conduction to the wheel, thereby lowering the energy partition to the workpiece.

The thermally induced residual stress distribution can be analytically predicted from the grinding temperature solution coupled with thermal stress and strain calculations. Such analyses have been performed using the finite-element method [38,71,72], taking into account the initial elastic-plastic stresses and deformation during
thermal loading followed by elastic unloading during cooling. An extensive amount of computational time is usually required, especially when also taking into account the influence of temperature on the mechanical and thermal properties of the particular workpiece material [38], and the non-linear nature of the phenomenon does not allow for generalization of the results. Significant simplification may be achieved in some cases by the use of an approximate one-dimensional analysis [73]. Perhaps more practical interest is the experimental observation of a direct relationship between the maximum residual tensile stress and the maximum grinding zone temperature, as seen in Figure 10 for three different steels [1]. The discontinuity near temperature \( A c_1 \) of 721°C is due to austenite formation, and this corresponds to the visible burning threshold. These results suggest that the maximum residual tensile stress can be controlled by keeping the grinding temperature below a certain value.

3 CREEP-FEED GRINDING

Creep-feed grinding utilizes very slow (creep) workpiece velocities and extremely large depths of cut. Straight surface grinding under creep-feed conditions allows for much faster removal rates than can be reached with regular shallow cut grinding without causing thermal damage to the workpiece, even though creep-feed grinding generally requires much bigger specific energies. For creep-feed grinding of steels, the specific energy may substantially exceed the threshold value for workpiece burn as obtained for regular grinding in Section 2, but with no evidence of any thermal damage.

It has been suggested that the improved thermal situation with creep-feed grinding can be attributed to the extremely large depths of cut, such that much of the heat input to the workpiece is removed together with the grinding chips before it can be conducted out of the path of the advancing wheel [74]. In order to evaluate this effect for creep feed grinding, the heat transfer analysis was modified to take into account the large depth of cut by considering an inclined heat source, as illustrated in Figure 11 [75]. Owing to the inclination \( \phi \) of the heat source and the workpiece motion, part of the heat entering the workpiece at the grinding zone is not conducted down into the workpiece below \( B \), but is convected out across the advancing boundary \( AB \) together with the material (chips) being removed, thereby reducing the maximum temperature where the newly ground surface is generated at \( B \). However, for inclination angles typical of creep-feed grinding (typically 5-10 degrees) and the slow workspeeds, the calculations using a finite element method showed only a very moderate reduction in the maximum temperature at the trailing edge \( B \), as compared with zero inclination, and cannot account for the ability to creep-feed grind steels without workpiece burn. However it will be seen in Section 4 that this effect becomes significant for High Efficiency Deep Grinding (HEDG) which uses much faster workspeeds.

The most critical factor which enhances the thermal situation in creep-feed grinding is cooling at the grinding zone [76-84]. Creep-feed grinding requires a copious flow of fluid delivered at high pressure to the grinding zone in order to remove heat by forced convection. Fluid flow through the grinding zone is enhanced by the use of porous grinding wheels. Cooling by the grinding fluid is effective only up to a critical burnout temperature associated with film boiling. At this point, considerable vapor is generated, thereby making it difficult for the fluid to wet and cool the heated surface at the grinding zone. The burnout transition at a critical threshold temperature corresponds to a critical burnout heat flux at the grinding zone. The critical temperature at the burnout threshold is about 130°C with water-based soluble oils and 300°C with straight oils. However, the critical burnout heat flux with water-based fluids is usually much higher than with straight oils [80], owing to the higher thermal conductivity of water-based fluids. It should be noted that these critical temperatures are usually exceeded for regular shallow cut grinding, so cooling by the fluid at the grinding zone becomes ineffective.

When burnout occurs and the cooling fails, the grinding zone temperature may jump to about 1000°C or more. This thermal instability may be accompanied by a surge phenomenon whereby the grinding power periodically builds up and drops off, owing to cyclical metal build-up on the wheel followed by self-sharpening [32,76,77]. An example of this cyclical burnout behavior is shown in Figure 12 [32]. In one investigation the workpiece burn threshold for creep-feed grinding of a bearing steel with a water based fluid over a wide range of conditions was
found to occur at nearly the same heat flux (power per unit area of grinding zone) of $q^* = 7-8 \text{ W/mm}^2$ [83]. Much bigger burnout fluxes up to 50 W/mm\(^2\) have also been reported [77-82]. More extensive investigation indicates that the burnout heat flux depends on the grinding conditions and the location along the grinding path as shown in Figure 13 [32,85]. For creep-feed grinding of workpieces which are long enough for the quasi-steady state temperature to be reached, burnout is usually observed to occur either in the middle portion of the pass where the quasi-steady state prevails, or at the end during cut out as the wheel disengages from the workpiece [85].

By analogy with Equation (7), the maximum temperature rise for a triangular heat source can be written as [32,85]:

$$\theta_m = \frac{\beta q}{(kpc)^{1/2}} \left( \frac{I_c}{v_w} \right)^{1/2}$$  \hspace{1cm} (24)

where the parameter $\beta$ considers the effect of the thermal number $L$ and the transient behavior during cutout as the wheel disengages. For the quasi-steady state [32]:

$$\beta = \begin{cases} 1.02L^{0.03} & 0.1 \leq L \leq 5 \\ 1.06 & L > 5 \end{cases}$$  \hspace{1cm} (25)

and for the transient during cutout at the end of the grinding pass:

$$\beta = \begin{cases} 1.02L^{0.03} + 0.23L^{-0.37} & 0.1 \leq L \leq 5 \\ 1.06 + 0.23L^{-0.37} & L > 5 \end{cases}$$  \hspace{1cm} (26)

The second term on the right side of Equation (26) in each case is the additional maximum temperature rise above the quasi-steady state due to cutout [84]. Therefore burnout during cutout should occur at a lower heat flux because of higher $\beta$ values than during the quasi-steady state.

Because the fluid burnout is a critical temperature phenomenon, the heat flux at burnout can be readily obtained from Equation (24) as:

$$q^* = \left( \theta^* - \theta_0 \right) \left( \frac{kpc}{L} \right)^{1/2} \left( \frac{I_c}{v_w} \right)^{1/2}$$  \hspace{1cm} (27)

where $\theta^*$ is the critical burnout temperature (about 130°C for water-based fluid and 300°C for oil) and $\theta_0$ is the initial temperature of the fluid. The corresponding critical grinding power is:

$$P_c^* = q^* \left[ \frac{1}{c} \right] = q^* \left[ \frac{1}{(a_e a_b)} \right] b$$  \hspace{1cm} (28)

In order to use Equation (27) to predict the critical burnout heat flux $q^*$ and Equation (28) to predict the corresponding burnout power $P_c^*$, it is necessary to specify the energy partition to the workpiece $\varepsilon$. For regular shallow-cut grinding, the energy partition model presented in Section 2 neglects cooling by the fluid because the grinding zone temperature is generally high enough to cause burnout and loss of cooling at the grinding zone. However, cooling by the fluid at the grinding zone becomes essential under creep-feed conditions and greatly reduces the energy partition.

### 3.2 Energy partition: composite model

Grinding energy is dissipated as heat at the grinding zone where the wheel interacts with the workpiece. The wheel surface is impregnated with fluid in its pores which cools the workpiece if the temperature is below the burnout limit. A fraction of the grinding energy equal to the energy partition is transported to the workpiece, which moves relative to the grinding zone at the workpiece velocity. Most of the remaining grinding energy is carried away by the wheel/fluid composite, which moves relative to the grinding zone at the wheel velocity. The applied fluid quickly accelerates up to the peripheral wheel velocity before it enters the grinding zone [86]. Some energy would also be carried away with the grinding chips, although this is usually a negligible fraction of the total heat generated in creep-feed grinding [84].
One relatively simple way to estimate the energy partition is to equate the maximum (or average) temperature rise on the workpiece at the grinding zone to the maximum temperature rise on the wheel-fluid composite surface [87]. The maximum workpiece temperature rise is given for a triangular heat source by Equation (24). The maximum temperature rise at the wheel-fluid composite surface can be written in an analogous manner by likening the grinding heat input to the wheel-fluid composite to a heat source which moves along its surface at the wheel velocity $v_s$. In this case, the thermal number $L_c$ for the wheel/fluid composite becomes:

$$L_c = \frac{v_s c_f}{2 \alpha_c}$$  \hspace{1cm} (29)

where the workpiece velocity is now replaced by the wheel velocity and $\alpha_c$ is the thermal diffusivity of the wheel/fluid composite. Since $v_s > v_w$, it is generally found that $L_c > 5$, so the maximum temperature rise becomes:

$$(\theta_m) = 1.06(1-\varepsilon)\frac{k_c}{\alpha_c} \left[ \frac{d_s}{v_w} \right]^{1/2} \left[ \frac{d_w}{v_s} \right]^{1/4}$$  \hspace{1cm} (30)

where the subscripts $c$, $w$, and $g$ refer to the composite and (1-g)q is the heat flux to the composite. By equating the composite surface temperature (Equation (30)) to the workpiece surface temperature (Equation (24)), the energy partition for $L>5$ and $\beta=1.06$ becomes:

$$\varepsilon = \frac{1}{1 + \left( \frac{v_s}{v_w} \right)^{1/2} \left( \frac{k_c}{k_f} \right)^{1/2} \left( \frac{v_s}{v_w} \right)^{3/4} \left( \frac{v_s}{v_w} \right)^{1/4}}$$  \hspace{1cm} (31)

In order to calculate the energy partition from Equation (31), it is necessary to estimate the composite thermal properties. Assuming that the surface porosity is completely filled with grinding fluid and that the thermal properties of the composite can be approximated by a weighted volumetric average of the thermal properties of the grain and grinding fluid, the composite thermal conductivity and volumetric specific heat are written as:

$$k_c = \phi_g k_g + (1-\phi_g) k_f$$

$$\rho c_c = \phi_g (\rho c_g) + (1-\phi_g) (\rho c_f)$$  \hspace{1cm} (32)

where $k$ is the thermal conductivity, $\rho c$ the volumetric specific heat, $\phi$ the near surface wheel porosity, and the subscripts $g$ and $f$ refer to the grain and fluid respectively. Although the bulk porosity of a vitrified aluminum oxide wheel for creep-feed grinding is typically about 50%, the near surface porosity would be much bigger. In order to take this effect into account, the grinding wheel was modeled as equally spaced spheres and the average near surface porosity calculated to a radial depth equal to the estimated thermal boundary layer thickness [87]. For creep-feed grinding with water-based fluids, the average near surface porosity $\phi$ was estimated to be about 87%. Similar values were obtained for creep-feed vitrified wheels when considering their ability to pump fluid through the grinding zone [88,89].

The estimated thermal properties for an aluminum oxide wheel with water based fluids for $\phi = 87\%$ would be $k = 9.7$ W/mK and $(\rho c) = 4.3 \times 10^6$ J/m$^3$. For grinding a plain carbon steel under typical creep-feed grinding conditions ($v_s = 30$ m/s, $v_w = 1.20$ mm/s), the energy partition predicted from Equation (31) varies from 1.3% to 5.4%, and even lower values would be obtained for grinding of nickel base alloys. These predictions agree quite well with measurements reported for down creep-feed grinding of a plain carbon steel [9].

### 3.3 Energy partition: variation along grinding zone

In the previous section, the energy partition was obtained by equating the maximum temperatures for the workpiece and for the wheel/fluid composite. However, it should be noted that the calculated temperature distribution along the grinding zone on the workpiece side may differ significantly from that on the wheel side if a constant energy partition to the workpiece is assumed. The difference is much greater for up grinding than for down grinding. The temperatures should match everywhere along the grinding zone at the interface between the workpiece and the composite, which requires an energy partition which varies along the grinding zone [90,91].

![Figure 14: Illustration of thermal situation in grinding.](image)

To analyze the energy partition variation along the grinding zone, consider the grinding situation as shown in Figure 14 [91]. The wheel/fluid composite moves relative to the grinding zone at the wheel velocity, and the workpiece moves relative to the grinding zone at the workpiece velocity. The grinding energy expended is transported away by the workpiece passing beneath the grinding zone and by the wheel/fluid composite passing above the grinding zone. As the wheel/fluid composite and the workpiece pass by the grinding zone, they should each be heated up to the same temperature at each point along their common interface.

For down grinding where the workpiece moves in the same direction as the wheel/fluid composite at the grinding zone, the leading edge of the heat source on the workpiece coincides with the leading edge of the heat source on the wheel/fluid composite. In this case, the energy partition to the workpiece should be almost constant along the grinding zone if the heat source moves sufficiently fast relative to both the workpiece and the composite ($L>5$). But for up grinding where the grinding wheel moves opposite to the workpiece at the grinding zone as in Figure 14, the composite surface of lower temperature comes into contact with the workpiece surface of higher temperature at the trailing edge. This could even cause local heat flow from the workpiece to the composite in the region near this location (negative energy partition). At the other end of the grinding zone at the leading edge, the workpiece of lower temperature comes into contact with the composite of higher temperature, which could cause heat flow from the composite to the workpiece. Therefore, the energy partition to the workpiece should deviate more significantly from a constant value for up grinding than for down grinding.

A generic model has been developed to predict the variation of energy partition to the workpiece for both up and down grinding under both regular and creep-feed conditions.
conditions [91]. If \( q(\xi) \) is the total heat flux distribution along the grinding zone as in Figure 14 and \( q_w(\xi) \) is the corresponding heat flux distribution to the workpiece, then the heat flux distribution to the composite is \( q(\xi) - q_w(\xi) \). The fraction of the total grinding energy to the workpiece along the grinding zone can be denoted by \( \varepsilon(\xi) \), which is also the local energy partition:

\[
\varepsilon(\xi) = \frac{q_w(\xi)}{q(\xi)}
\]  

The workpiece moves relative to the grinding zone (heat source) at the work velocity \( v_w \) and the composite moves at the wheel velocity \( v_s \). The quasi-steady state temperature distribution on the workpiece surface \( \theta_w(x) \) and that on the composite surface \( \theta_c(x) \) can both be calculated using moving heat source similar to Equation (1), or

\[
\theta_w(x) = \frac{1}{\pi \alpha W} \int q(\xi) (1 - \varepsilon(\xi)) e^{-\frac{v_s(x-x_0)}{2\alpha W}} d\xi
\]

\[
\cdots \cdot K_0 \left[ \frac{v_s}{2\alpha C} (x - \xi)^2 + z^2 \right]^{\frac{1}{2}} d\xi
\]

The energy partition to the workpiece, \( \varepsilon(x) \), must satisfy the compatibility requirement that the temperature on the workpiece surface \( \theta_w(x) \) equals the temperature on the composite surface \( \theta_c(x) \) everywhere along the grinding zone:

\[
\theta_w(x) = \theta_c(x)
\]

The numerical solution of this problem gives the energy partition distribution along the grinding zone. Two examples are presented to illustrate energy partition variation along the grinding zone, one for down grinding and one for up grinding [91]. For the case of down grinding, the calculated energy partition distribution is shown in Figure 15 for a triangular heat flux distribution under creep-feed grinding conditions together with the constant energy partition value from Equation (31) for comparison. It can be seen that the energy partition distribution deviates significantly from the constant energy partition value near the leading edge of the grinding zone \((x/l=1)\), where it becomes negative, and near the trailing edge. However, the temperature distribution also included in Figure 15 is nearly the same.
But for up grinding shown in Figure 16, the energy partition deviates much more drastically from a constant value than for down grinding. It is positive at the leading edge ($x/l=1$) indicating net heat flow from the composite to the workpiece, and decreases along the grinding zone becoming negative near the trailing edge ($x/l=-1$) indicating net heat flow from the workpiece to the composite. The corresponding temperature distribution in Figure 16 for the constant energy partition model predicts a lower temperature near the heat source leading edge, a higher temperature near the trailing edge, and a lower maximum temperature.

### 3.4 Energy partition: single grain model

Another approach for analyzing the energy partition is the single grain model [32,92]. Like the composite model, this model also includes the effects of heat transfer to the abrasive grains, fluid, and workpiece by considering a single grain surrounded by fluid interacting with the workpiece as shown in Figure 17. Each single active grain is modeled as a truncated cone moving along the workpiece surface at the wheelspeed $v_s$ with all of the grinding energy uniformly dissipated as heat at the grain-workpiece interface of area $A_0$. At this interface, part of the energy $\epsilon_{day}$ is "initially" conducted to the workpiece and the remainder to the abrasive grain. Cooling by the fluid is then taken into account by considering the temperature at the fluid-workpiece interface within the grinding zone.

For this model, the maximum temperature rise at the grain-workpiece interface is [92]:

$$\theta_{maxg} = 1.13 \left(1 - \epsilon_{day}\right) \frac{q_{ref} v_s}{(k_c)_{fr}^{1/2} (k_c)_{gr}^{1/2}} \frac{1}{f(\zeta)A} \tag{37}$$

where $A$ is the fraction of wheel surface consisting of truncated grain tips (wear flats), $\epsilon_{day}$ is the "initial" energy partition to the workpiece, $k$ the thermal conductivity, $\rho C$ the volumetric specific heat, and the subscript 'g' refers to the grain material. The function $f(\zeta)$ is given by:

$$f(\zeta) = \frac{2}{\pi^{1/2}} \frac{\zeta}{1 - \exp(-\zeta^2)} \text{erfc}(\zeta) \tag{38}$$

where

$$\zeta = \left(\frac{L_c r_g^2 \gamma}{A_0 v_s}\right) / \sqrt{2} \tag{39}$$

$\gamma$ is a geometric grain shape factor defined as:

$$\gamma = \frac{dr_g}{dz} \tag{40}$$

and $r_g$ is the grain radius. Grinding fluid entering the wheel pores is quickly accelerated and can be considered to be moving at the wheelspeed within the grinding zone [86].

At its interface with the workpiece, the maximum temperature rise of the fluid can be written as [32]:

$$\theta_{maxf} = 1.06 \frac{q(\epsilon_{day} - \epsilon)}{(k_c)_{fr}^{1/2} (k_c)_{gr}^{1/2}} \frac{1}{v_s} \frac{1}{f(\zeta)A} \tag{41}$$

where $\epsilon$ is the fraction of the total energy not removed by the fluid (energy partition) and subscript $f$ refers to the fluid. The maximum workpiece temperature rise can be expressed as:

$$\theta_{max} = \beta \epsilon q \frac{(L_c)^{1/2}}{(k_c)_{fr}^{1/2} v_w} \tag{42}$$

If the maximum temperature at the workpiece-fluid interface is the same as at the workpiece-grain interface ($\theta_{max} = \theta_{maxg} = \theta_{maxf}$), the overall energy partition to the workpiece is finally obtained by combining Equations (37), (41) and (42):

$$\epsilon = \frac{1}{1 + \Omega \left(\frac{v_s}{v_w}\right)^{1/2}} \tag{43}$$

where

$$\Omega = 0.94 \left(\frac{(k_c)_{fr}}{(k_c)_{gr}}\right) \frac{A f(\zeta) + (k_c)_{fr}^{1/2}}{(k_c)_{gr}^{1/2} v_w} \frac{1}{(1 - A)} \tag{44}$$

The burnout heat flux predicted by substituting Equations (43) and (44) into Equations (27) and (28) agrees well with the measured burnout heat flux under various conditions as shown in Figure 13 [32]. In this figure, the upper line is the model prediction for burnout at the middle of the workpiece (quasi-steady state) and the lower line is the result for burnout during cut out. The data points indicate measured burnout heat flux data at the power surges as in Figure 12.

This single grain energy partition model can be used to account for the differences in energy partition to the workpiece under various grinding and fluid application conditions. For regular grinding with conventional aluminum oxide wheels and water-based fluids, the grinding zone temperatures are often much higher than the burnout limit of 130°C, so cooling by the fluid should not be effective at the grinding zone. In this case, the term $(k_c)_{fr}$ in Equation (43) is essentially zero, which leads to an energy partition of about 60-70%. For creep-feed grinding at lower temperatures below the burnout limit, the term $(k_c)_{fr}$ is approximately 2.72×10^6 J/m³ K^2 s which leads to an energy partition less than 5%, comparable to actual measurements [9]. In Section 4, this single grain model will also be used to account for the energy partition for grinding with CBN abrasive wheels.

### 3.5 Transient temperatures

The analyses in the previous sections considered the situation where the workpiece is long enough for the temperature within the grinding zone to reach a quasi-steady state. In such cases, transient temperatures occur as the grinding wheel engages with and disengages from the workpiece. Transient conditions also prevail throughout the entire grinding pass for workpieces which are shorter than needed to reach a quasi-steady state condition [85], which is often the case for creep-feed grinding. The temperature rises rapidly during initial wheel-workpiece engagement (cut in), reaches a quasi-steady state value only if the workpiece is sufficiently long, and increases still further during final wheel-workpiece disengagement (cut out) as workpiece material is
suddenly unavailable to dissipate heat. In this section, models are presented for transient temperatures under creep-feed conditions [85].

For analyzing transient grinding temperatures, the workpiece can be categorized as either long or short in terms of both the geometric conditions and the thermal conditions. A workpiece is geometrically long if its length exceeds the wheel-workpiece contact length at the full depth of cut \( (l_w > l_c) \) as shown in Figure 18, and it is geometrically short if its length is less \( (l_w < l_c) \). A geometrically long workpiece may be further classified as thermally long or short according to whether it is sufficiently long for the quasi-steady state surface temperature to be reached. Geometrically short workpieces are also thermally short.

Figure 18: Illustration of cut in, steady state, and cut out for grinding a geometrically long workpiece \( (l_w > l_c) \).

For grinding of a geometrically long workpiece, three sequential regimes can be considered as illustrated in Figure 18: cut in, steady state, and cut out. Heat input to the workpiece in each regime occurs over the wheel-workpiece arc length of contact which can be approximated by the chord length. During cut in, the grinding zone contact length \( l_i \) increases from zero to its geometrical steady state value \( l_c \) as the depth of wheel engagement with the workpiece in the downfeed direction increases from zero to the specified wheel depth of cut \( a_s \). The corresponding workpiece temperature at the grinding zone should rapidly rise during cut in as the material removal rate increases from zero to its steady state value. Whether the workpiece is thermally long enough to reach the quasi-steady state temperature depends on its thermal properties and the grinding conditions. Cut out occurs during disengagement at the end of the grinding pass as the wheel-workpiece contact length \( l_c \) decreases from its steady state value \( l_c \) back to zero. During cut out, the temperature may exceed the quasi-steady state value because workpiece material is suddenly unavailable to conduct heat away.

For a geometrically short workpiece as shown in Figure 19, the wheel depth of engagement in the downfeed direction is smaller than the specified wheel depth of cut \( a_s \) throughout the grinding pass. The grinding pass for the short workpiece can also be divided into three regimes: cut in, cut down, and cut out. The wheel-workpiece arc length of contact, again approximated here by the chord length as for the geometrically long workpiece, increases nearly linearly from zero to its maximum value during cut in. Cut down begins after the workpiece has traveled a distance \( L_c \) at which point the workpiece top surface becomes completely covered by the grinding zone. Since \( l_w \) is generally much longer than the maximum depth of engagement, the contact length in this regime can be approximated as being equal to \( l_w \). During cutout which begins when the workpiece has traveled a distance \( l_o \), the contact length decreases from its maximum value (workpiece length) back to zero.

Figure 19: Cut in, cut down, and cut out regimes for grinding a geometrically short workpiece \( (l_w < l_c) \). For illustration purposes, the workpiece is shown at different locations relative to the wheel.

The thermal model for analyzing the transient temperature distribution during straight surface grinding of a rectangular workpiece of height \( l_h \) and length \( l_w \) is illustrated in Figure 20. The heat flux to the workpiece at the grinding zone is modeled as a continuously distributed planar band source of intensity \( q(x') \) along its length \( 2l_w \) in the grinding direction and moving at the workpiece velocity \( v_w \). The length of the heat source corresponds to the wheel-workpiece contact length which varies during cut in and cut out as described above. The dimensionless form of the governing two-dimensional heat transfer equation is:

\[
\frac{\partial \theta}{\partial \tau} = \frac{1}{4L} \left( \frac{\partial^2 \theta}{\partial X^2} + \frac{\partial^2 \theta}{\partial Z^2} \right) \tag{45}
\]

where

\[
\begin{align*}
\tau & = \frac{v_w t}{l_c}, \quad X = \frac{x}{l_c}, \quad Z = \frac{z}{l_c}, \\
L_w & = \frac{L_w}{l_c}, \quad \bar{\theta} = \frac{\partial \theta}{\partial q(x')}, \quad L = \frac{v_w l_c}{4\alpha}
\end{align*}
\tag{46}
\]

The temperature distribution \( \theta(x,z,t) \) within the workpiece is obtained by solving Equation (45) with appropriate initial and boundary conditions. The initial temperature can be taken as zero everywhere in the workpiece, and the bottom of the workpiece is assumed to be sufficiently remote from the top surface so as to remain at its initial temperature throughout the grinding pass. At any location \( x \) on the workpiece top surface as in Figure 20, the intensity of the heat input to the workpiece is time
dependent due to the motion of the workpiece along the grinding direction. At time $t$ the leading edge of the heat source (grinding zone) has traveled a distance $V_w t$ from the left end of the workpiece. The heat flux to the workpiece from the top surface at any location $x$ and any time $t$ is given by $q(t,x)$. The heat flux should increase during cut in and decrease during cut out.

Grinding fluid is applied to the crevice formed by the wheel and workpiece top surface, at the leading edge side for down grinding and at the trailing edge side for up grinding. A fraction of the applied fluid is carried through the grinding zone by the rotating wheel, while most of the remaining fluid falls back into the stream of fluid on the workpiece top surface [86,88]. Most of the fluid passing through the grinding zone is ejected slightly upward by the high-speed rotation of the grinding wheel so that only a small amount actually falls on the workpiece top surface ahead of the grinding zone. Therefore, convective heat transfer on the top surface should be greatest on that side of the grinding zone where the grinding fluid is directly applied (trailing edge side for up grinding and leading edge side for down grinding), and much smaller on the opposite side. For simplicity, two different convective heat transfer coefficients are assumed for the regions ahead of and behind the grinding zone.

Within the grinding zone, fluid being carried through by the porous wheel rotating at high speed may also provide convective cooling. Cooling by the fluid within the grinding zone can be considered to reduce the heat source intensity $q(x)$ to the workpiece. Such cooling can be analytically taken into account separately in terms of the energy partition to the workpiece [9].

Some results from the thermal analysis are presented in Figure 21 which shows the transition from a thermally short to a thermally long workpiece. The maximum dimensionless workpiece temperature $\theta_m$ during the grinding pass was obtained with the thermal numbers $L=1$ and $L=5$ for a number of dimensionless workpiece lengths ranging from $L_w=0.5$ to $L_w=4.5$. The results in Figure 21(a) are for a rectangular heat source and in Figure 21(b) for a triangular heat source. In both cases, the temperature rises very rapidly during cut in as the grinding wheel engages the workpiece. The quasi-steady state value is subsequently reached at $L_w=1.5$ for $L=5$ and at $L_w=4.5$ for $L=1$. Whether a workpiece can be considered to be thermally long depends on the thermal number. At the end of the pass during cut out, there is also an abrupt additional temperature rise in each case which is bigger for smaller thermal numbers and with the triangular heat source. Therefore thermal damage often occurs at this location.

As a practical matter, the maximum temperature reached during the entire grinding pass is usually of particular concern. For $0.2 < L_w < 5$ and $0.4 < L < 5$, the overall maximum dimensionless temperatures for the entire grinding pass for triangular heat sources can be approximated as [85]:

$$\theta_{\text{max}} = 1.06 e^{-0.06 L_w} \left(0.37 + 0.24 e^{-0.6 L_w} \right) + 0.10 e^{-0.83 L_w} + 0.66$$

(47)

The results of this transient analysis can be applied to predicting burnout in creep-feed grinding both at the middle and end of a thermally long workpiece (Figure 13). Furthermore, it can be shown that cooling at the end face of the workpiece using an additional fluid supply nozzle can significantly reduce the temperature rise during cutout, and thereby also reduce the tendency for burnout at this location [32].

![Figure 21: Maximum dimensionless temperature vs. dimensionless time for various dimensionless workpiece lengths with (a) rectangular and (b) triangular heat sources.](image)

### 4 CBN ABRASIVE WHEELS

In the previous sections, grinding temperatures were analyzed by considering the grinding zone as a moving heat source. Temperatures calculated for given operating parameters were generally found to be proportional to the rate of energy expended and to the fraction of that energy which is transported to the workpiece as heat at the grinding zone (energy partition). For regular shallow cut grinding with conventional abrasive wheels, heat transfer to the workpiece is especially important, as the energy partition typically ranges from 60% to 85%. This is in sharp contrast to the situation for creep-feed grinding using aluminum oxide wheels. In the absence of fluid burnout, the energy partition for creep feed grinding is typically only 3 to 7%. Thermal damage should not occur if the temperature remains below the burnout limit for the fluid.

The present section is concerned with the thermal aspects of grinding with cubic boron nitride (CBN) superabrasive wheels. As compared with conventional aluminum oxide wheels, thermal damage with CBN is generally found to be less of a problem [13]. Workpiece burn is much less likely to occur, and residual stresses at the ground surface are usually found to be predominantly compressive...
[69,70]. These observations are indicative of much lower temperatures with CBN than with aluminum oxide wheels. Lower temperatures with CBN were originally attributed to the somewhat smaller specific energies which are typically found in practice. However it was subsequently postulated that the main effect is due to the very high thermal conductivity of CBN, so that a much larger fraction of the grinding heat is transported to the grain instead of to the workpiece (lower energy partition) [92]. The thermal conductivity of CBN grain is approximately 35 times bigger than that of aluminum oxide. Cooling by the grinding fluid may also be an additional factor with CBN.

Vitrified and electroplated CBN wheels presently account for the vast majority of CBN wheels in industry. In recent years, electroplated CBN wheels have been growing in popularity, especially for automotive and aerospace applications. The inherent structural porosity of vitrified CBN wheels would seem to provide favorable conditions for cooling by the fluid at the grinding zone. By contrast, this type of cooling would seem less likely to occur with single layer electroplated CBN wheels due to their limited surface porosity, but it will be seen that this is not necessarily true.

High Efficiency Deep Grinding (HEDG) also uses mostly single layer CBN wheels operating at high speeds with very large depths of cut, comparable to or even bigger than for creep feed grinding, together with much faster work speeds. Despite the extreme removal rates which can be achieved, the HEDG process has found only limited application in industry up to now.

4.1 Vitrified CBN wheels

Very low energy partitions of only a few percent can be obtained for creep-feed grinding due to cooling by the fluid at the grinding zone. Two analytical models were developed to account for this behavior, one which matches the temperatures on the workpiece and the wheel/fluid composite at the grinding zone, and a second one which considers the thermal situation for a single active grain surrounded by grinding fluid.

The single grain model was originally developed in order to assess how the high thermal conductivity of CBN abrasive grains might lower the energy partition [92]. The first energy partition measurements for grinding with CBN abrasive wheels were reported a few years later for regular shallow cut grinding [11]. In that investigation, temperature matching methods were used to obtain the energy partition for grinding of various steels using both vitrified and electroplated CBN wheels. The underlying idea was to ascertain the role of the thermal conductivity of CBN as implied by the single grain model. When conducting these experiments, no attempt was made to account for the possible role of the grinding fluid. Experiments were performed on a simple surface grinder equipped with a typical low pressure flood application device at a low fluid flow rate (up to 2.4 liters/min). With this application method, the fluid does not directly reach the crevice between the wheel and the workpiece, but instead hits the rotating wheel and then falls on the workpiece [88].

One set of results obtained for grinding of an AISI O1 die steel with a vitrified CBN wheel (AISI 01) is shown in Figure 22 [11], where the maximum temperature rise is plotted versus the depth beneath the ground surface. This measured temperature was then matched to the theoretical temperature, taking into account the dynamic response of the thermocouple, in order to obtain the energy partition. In this particular example, the effect of cooling ahead of the grinding zone where the fluid hits the workpiece top surface was also taken into consideration by specifying a cooling coefficient $h = 15,000 \text{ W/m}^2\text{ K}$ at this location. As compared with the usual assumption of an insulated surface, the main effect of this change in the boundary conditions is to increase the cooling rate of the workpiece material after it passes through the grinding zone. It has virtually no influence on the maximum temperature and energy partition. The measured and theoretical temperatures in Figure 22 match quite well for an energy partition of 20%. For numerous other tests conducted under the same conditions on this die steel and on hardened AISI 52100 bearing steel, comparable energy partition values were obtained by applying temperature matching methods to both the maximum subsurface temperatures and to temperature responses during single grinding passes.

![Figure 22: Temperature rise for grinding AISI 01 die steel with a vitrified CBN wheel.](image)

With flood cooling as in these tests, it is highly unlikely that the amount of fluid actually reaching the grinding zone is sufficient to fill up the pores and cool the workpiece. Because of the ineffective fluid application and/or burnout of the fluid, it would seem reasonable to neglect cooling by the fluid at the grinding zone when applying the single grain energy partition model, which can be done by setting $(k_{cl})=0$ in Equation (41). The reduced energy partition in this case, relative to that for aluminum oxide wheels, would then be attributed to the high thermal conductivity of the CBN. Using the thermal properties for aluminum oxide $(k_{cl}=0.14 \times 10^9 \text{ J/m}^2\text{ K}^{-2}\text{ s})$ in place of the thermal properties for CBN $(k_{cl}=2.27 \times 10^9 \text{ J/m}^2\text{ K}^{-2}\text{ s})$ in the single grain model with no cooling by the fluid $(k_{cl})=0$ would lead to a much higher energy partition of about 60%, which is closer to what is found for shallow cut grinding with aluminum oxide wheels.

![Figure 23: Maximum temperature rise under various grinding conditions: vitrified CBN wheel, AISI 52100 steel workpiece.](image)
Subsequent energy partition experiments were conducted with vitrified CBN wheels on a bearing steel (AISI 52100) and on a nodular cast iron cast iron at much higher removal rates on a much bigger machine with much higher flow rates [93-95]. In most cases, the temperature remained below the burnout limit as seen in Figure 23, so fluid burnout should not have occurred except perhaps in one or two cases. The experiments at higher removal rates were accompanied by lower specific energies of about 20 to 25 J/mm³, as compared to about 35 J/mm³ for the previous CBN experiments described above. By applying temperature matching and inverse heat transfer methods, the energy partition values were found to be extremely small, ranging from about 4% to 8%. These results were found to agree quite well predictions from the single grain model (Equation (43)) as seen in Figure 24. In this case, the fluid was taken into account by setting \( k(x,w) = 2.72 \times 10^5 J/m^3 K^2 s \).

On the basis of these results, it can be concluded that low energy partition values with vitrified CBN wheels can be attributed to a number of factors. These include a low specific grinding energy, high thermal conductivity of the CBN grain which enhances heat removal from the grinding zone, and cooling by the grinding fluid at the grinding zone.

![Figure 24](image)

**Figure 24:** Energy partition under various grinding conditions: vitrified CBN wheel, AISI 52100 steel workpiece.

### 4.2 Electroplated CBN wheels

The initial investigation of the energy partition with CBN wheels using flood cooling [11], as mentioned above, also included experiments with an electroplated wheel containing 100 grit CBN abrasive. The results obtained for grinding with this electroplated wheel were very similar to what was obtained with the vitrified wheel, both in terms of specific energy and energy partition. The specific energy was about 35 J/mm³ and the energy partition approximately 20%.

As with the vitrified wheels, additional experiments were subsequently conducted with electroplated CBN wheels at much higher removal rates and fluid flow rates [96]. One of the objectives in undertaking these experiments was to ascertain the prospects for cooling by the grinding fluid. Unlike vitrified CBN wheels which have a porous structure, CBN wheels have only a shallow surface porosity to a radial depth from the outermost grain tips to the nickel layer holding the single layer of abrasive grains on to the wheel hub. A further complication arises because the topography of these wheels progressively changes with continued use, tending to increase the number of active grains and the wear flat area while decreasing the depth of the porous layer as implied in Figure 25. According to the single grain energy partition model, dulled wear flat areas on the CBN grain tips should enhance heat conduction to the abrasive grains and thereby reduce the energy partition to the workpiece. This may not necessarily result in lower grinding temperatures, since wheel dulling should also cause bigger forces and higher power.

![Figure 25](image)

**Figure 25** Illustration of plated CBN wheel.

The energy partition was experimentally obtained for grinding with an electroplated CBN wheel using temperature matching methods after various amounts of wheel wear. The inherent wear resistance of the CBN abrasive necessitated extensive grinding to wear the wheel down. During most of the wheel life, the wheel was worn down by grinding of hardened AISI 52100 steel, but a B1900 nickel base alloy workpiece was also used in the later stages to accelerate the wheel wear.

![Figure 26](image)

**Figure 26**: Specific energy versus wheel wear.

Figure 26 shows the experimental results for energy partition versus radial wheel wear. The energy partition at the start of grinding with the new wheel began at about 8%, progressively decreased down to about 3.2% at 60 µm wear, but then increased to a maximum value of 10% at 80 µm wear before suddenly dropping again to about 2.8% at 85 µm wear near the end of the wheel life. At this point numerous grains became dislodged from the wheel surface and failure of the wheel occurred.

![Figure 27](image)

**Figure 27**: Specific energy versus wheel wear.
The decrease in the energy partition, at least up to about 60 μm wear, may be due to the increase in dulled wear flat area on the wheel surface. This is reflected in the corresponding increase in specific energy seen in Figure 27. Up to this point, the grinding zone temperature shown in Figure 28 remained well below the burnout limit. This was followed by a steep increase in the temperature above the burnout limit, which accounts for the corresponding rise in the energy partition. As compared with creep-feed grinding, exceeding the burnout limit with the plated CBN wheel resulted in a much less catastrophic change in the grinding behavior.

The single grain thermal model was then applied to analyzing the energy partition for those results below the burnout limit with cooling. Matching the thermal model to the experimentally measured energy partition was found to require a progressive increase in the wear flat area as seen in Figure 29. A plot of the measured normal and tangential force components versus the estimated wear flat area, as seen in Figure 30, resulted in characteristic linear relationships [14,44]. This is a further indication that changes in the grinding forces and energy are related to dulling of the grinding wheel.

The role of the grinding fluid as a coolant was further explored by investigating the effect of applied fluid flow rate on the energy partition [96]. The results summarized in Figure 31 show nearly constant energy partition values of 4% - 6% at flow rates above 9 liters/min. The maximum grinding temperature in this regime remained well below the burnout limit. But at lower flow rates, the energy partition increased sharply and the burnout limit was exceeded. This suggests that an applied flow rate of about 9 liters/min was the minimum required to fill up the wheel surface porosity in this case.

4.3 High efficiency deep grinding (HEDG)

HEDG utilizes mainly single layer CBN wheels to achieve large depths of cut, comparable to or bigger than for creep-feed grinding, and also relatively fast workspeeds, comparable to what is used in conventional shallow cut grinding. HEDG processes are considered to provide the highest specific removal rates of any grinding process. Another important feature of HEDG grinding is the use of high wheel velocities, generally in excess of 100 m/s.

Investigations of the thermal aspects of HEDG have highlighted the unique characteristics of this grinding process [97-100]. It has become apparent that a number of assumptions which were adopted when dealing with the thermal aspects of other grinding processes may not be valid for HEDG.
As with creep feed grinding, the large depths of cut with HEDG might suggest the use of an inclined heat source for the grinding zone as illustrated in Figure 32. The inclination angle $\phi$ can be readily calculated in terms of the wheel diameter $d_s$ and depth of cut $a_e$ as:

$$\phi = \cos^{-1} \left( \frac{d_s - 2a_e}{d_s} \right)$$  \hspace{1cm} (48)

From an inclined heat source analysis for creep-feed grinding conditions, the effect of the inclination was found to have only a minimal effect on creep-feed grinding temperatures [75]. The inclination angles for HEDG may be comparable or somewhat even somewhat bigger than for creep feed grinding because of bigger wheel depths of cut and also smaller wheel diameters. However the main difference between the creep-feed situation and HEDG appears to be related to the much faster workspeeds with HEDG. With much faster workspeeds and, consequently, larger values of the thermal parameter (Peclet number) $L$ ($L = \frac{V_w l_c}{4a_c}$), more heat remains in the path of the advancing grinding zone in the material being removed without enough time for it to be conducted downward into the remaining workpiece. This phenomenon can have important implications for the grinding mechanisms, since the material being ground is essentially preheated. This could lower the energy for chip formation, which has been related to the energy to adiabatically take the material from the initial ambient temperature up to the liquid state at the melting point (approximately 9.8 J/mm³ for ferrous materials) [14].

Another important factor which needs to be taken into account with HEDG concerns the heat source distribution. In the previous thermal analyses, both uniform and triangular heat sources were used, and both gave comparable results for the maximum grinding zone temperature. Because the localized removal rate is essentially proportional to the distance along the grinding zone, a triangular heat source is more realistic, and this is consistent with results obtained by applying inverse heat transfer to measurements of grinding temperatures (Figure 4). When considering the possibility of thermal damage with the inclined heat source, it should be of particular interest to consider the temperature occurring at the finished surface at point $B$ in Figure 32. For a uniform heat source distribution, the maximum temperature along the grinding zone would tend to be skewed towards the trailing edge of the grinding zone. However for a triangular heat source, the maximum temperature would be skewed along the grinding zone away from the finished surface. In this case, the maximum temperature on the finished surface at $B$ can be considerably lower than the maximum grinding zone temperature.

The moving heat source thermal analysis has been modified in order to take into account the effect of depth of cut and a triangular rather than rectangular heat source [97]. On the basis of these results, the maximum temperature rise at the contact zone can be expressed, analogous to Equation (2) as:

$$\theta_m = \frac{C}{\sqrt{\kappa_e C}} \left( \frac{l_c}{V_w} \right)^{0.5} \kappa q$$  \hspace{1cm} (49)

where $l_c$ is the grinding zone chord length (AB in Figure 32), $q$ is the total average heat flux at the grinding zone, $\kappa$ is the energy partition, and $C$ is a dimensionless parameter ranging from 0 to 1.06, which takes into account the effect of both the thermal number $L = \frac{V_w l_c}{4a_c}$ and the inclination angle $\phi$. Values for $C$ are summarized in Figure 33. As expected, a bigger thermal number and steeper inclination angle, as with HEDG, reduces $C$. For an inclination angle $\phi = 0$ and thermal number $L > 5$, $C$ = 1.06, which is the case of regular shallow grinding as discussed in Section 2.

![Figure 33: Thermal parameter C versus Peclet number L.](image)

For considering the possibility of thermal damage, the temperature of interest should be the maximum temperature at the finished surface at point $B$ in Figure 32 rather than the maximum temperature along the grinding zone. For the same inclined heat source analysis [97], the maximum temperature rise on the finished surface $\theta_{max}$ was also calculated as a fraction $\lambda$ of the maximum grinding zone temperature $\theta_m$:

$$\theta_{max} = \lambda \theta_m$$  \hspace{1cm} (50)

Results for $\lambda$ in Figure 34 show how an increase in the thermal number or inclination angle reduce the maximum temperature on the finished workpiece surface at $B$ (Figure 32).

![Figure 34: Maximum temperature at the finished surface as a fraction of the maximum grinding zone temperature.](image)

In order to calculate the temperature at the grinding zone and maximum temperature on the finished workpiece surface, it is necessary to estimate the energy partition $\kappa$ at the grinding zone. For this purpose, the single grain model can be applied as in Section 4-2. A further reduction in the energy partition due to additional heat being removed with the chips should also be considered [100]. While this effect was neglected up to now when applying the single grain energy partition model, it could have a significant influence, especially if the specific grinding energy is low. For HEDG grinding of a low alloy...
steel, the specific grinding energy was found to decrease down to about 10-15 J/mm² at very high specific material removal rates of 750 mm²/s [97], and a smaller specific energy of only 7 J/mm² has been reported at an extremely high specific removal rate of 3000 mm²/s [99].

To illustrate the implication of these results, consider the case of HEDG grinding of a steel with a CBN wheel of diameter dₘ = 150 mm, wheel speed vₘ = 150 m/s, workspeed vₙ = 250 mm/s, and wheel depth of cut aₜ = 3 mm corresponding to a specific removal rate of 750 mm³/mm²/s. According to the single grain model, the energy partition for a grit with an assumed wear flat area of 0.3% would be ε = 15.5% for grinding with a water-based soluble oil ((kₐc)₀ = 2.72 × 10⁻⁶ J/m² K s) and ε = 26% with a straight oil ((kₐc)₀ = 0.246 × 10⁻⁶ J/m² K s). These energy partition values, which take into account both conduction to the grains and cooling by the fluid, would apply if the grinding zone temperature is maintained below the fluid burnout temperature, which is about 130 °C for water-based fluids and 350 °C for straight oils. For a specific energy of 12 J/mm², the maximum grinding zone temperature would reach about 676 °C for water-based fluid and 1125 °C for straight oil, which means that burnout would occur in both cases. The energy partition would be 36.5% if cooling is not considered ((kₐc)₀ = 0), and the maximum grinding zone temperature would reach 1575 °C. The inclination angle is about 10 degrees and the thermal number L is 92.6 for this case. The corresponding C factor is 0.79 (Figure 33) and λ is 0.56 (Figure 34). Therefore, the maximum temperature at the finished workpiece surface (Point B in Figure 32) would be only 44% of the maximum grinding zone temperature, or θ_max = 690 °C.

Some results are presented in Figure 35 which show the effect of both wheel depth of cut and workpiece velocity on the maximum temperature on the finished surface. For these calculations the specific grinding energy was varied from 27 J/mm² at the lowest specific removal rate down to 12 J/mm² at the highest in accordance with experimental measurements [97]. It is especially interesting to note that the temperature reaches a maximum value and then progressively decreases as the removal rate is raised by increasing either the wheel depth of cut or workpiece velocity. Beyond a certain point, the thermal situation with HEDG becomes better with faster removal rates.

![Figure 35: Maximum temperature at finished surface.](image)

5 CONCLUDING REMARKS

This objective of this paper has been to present an overview of analytical methods to calculate grinding temperatures and their effect on thermal damage to the workpiece. Thermal damage is one of the main limitations of the grinding process, so it is especially important to account for the grinding temperatures.

A critical factor needed for calculating the grinding temperatures and controlling thermal damage is the energy partition to the workpiece. Past research has shown that the energy partition can vary significantly depending on the type of grinding, abrasive grain material, and grinding fluid. For regular shallow cut grinding with vitrified aluminum oxide wheels, the energy partition is typically 60% - 85%. Much lower energy partition values are obtained for creep feed grinding due to cooling by the fluid at the grinding zone, and also for grinding with CBN abrasive wheels due to heat conduction to the grains and cooling by the fluid. For HEDG processes using CBN wheels with large depths of cut and fast workpiece velocities, the removal of preheated material with the chips may also remove a significant portion of the heat from the grinding zone, thereby leading to lower temperatures at the finished workpiece surface.

Thermal models have been developed which take all of these factors into account, although further refinement and experimentation could enhance their accuracy. Grinding temperatures can be predicted quite well, but how the grinding temperatures affect the workpiece surface is not well understood. Much more research is needed to better understand and quantify how the grinding temperatures, with their extreme gradients in time and space, affect the surface integrity of the finished workpiece. A comprehensive solution to this challenging problem would seem to require coupling of the thermal analyses together with models which describe the kinetics of metallurgical phase transformations and the evolution of the microstructure, macrostructure, and residual stresses in the near-surface layer.

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7 REFERENCES


[54] Anon, 1960, Grinding Stresses - Cause, Effect, and Control, Collected Papers, Grinding Wheel Institute, Cleveland, Ohio.


