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Heat generation and temperature prediction in metal cutting: A review and implications for high speed machining

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Abstract

Determination of the maximum temperature and temperature distribution along the rake face of the cutting tool is of particular importance because of its controlling influence on tool life, as well as, the quality of the machined part. Numerous attempts have been made to approach the problem with different methods including experimental, analytical and numerical analysis. Although considerable research effort has been made on the thermal problem in metal cutting, there is hardly a consensus on the basics principles. The unique tribological contact phenomenon, which occur in metal cutting is highly localized and non-linear, and occurs at high temperatures, high pressures and high strains. This has made it extremely difficult to predict in a precise manner or even assess the performance of various models developed for modelling the machining process. Accurate and repeatable heat and temperature prediction remains challenging due to the complexity of the contact phenomena in the cutting process. In this paper, previous research on heat generation and heat dissipation in the orthogonal machining process is critically reviewed. In addition, temperature measurement techniques applied in metal cutting are briefly reviewed. The emphasis is on the comparability of test results, as well as, the relevance of temperature measurement method to high speed cutting. New temperature measurement results obtained by a thermal imaging camera in high speed cutting of high strength alloys are also presented. Finally, the latest work on estimation of heat generation, heat partition and temperature distribution in metal machining is reviewed. This includes an exploration of the different simplifying assumptions related to the geometry of the process components, material properties, boundary conditions and heat partition. The paper then proposes some modelling requirements for computer simulation of high speed machining processes.

Keywords: High speed machining; Heat generation; Heat partition; Temperature measurement; FE modelling

1. Introduction

Machining of metals is still not completely understood because of the highly non-linear nature of the process and the complex coupling between deformation and temperature fields. Metal cutting can be associated with high temperatures in the tool-chip interface zone and hence, the thermal aspects of the cutting process strongly affect the accuracy of the machining process. The deformation process is highly concentrated in a very small zone and the temperatures generated in the deformation zones affect both the tool and the workpiece. High cutting temperatures strongly influence tool wear, tool life, workpiece surface integrity, chip formation mechanism and contribute to the thermal deformation of the cutting tool, which is considered, amongst others, as the largest source of error in the machining process [1]. The increase in the temperature of the workpiece material in the primary deformation zone softens the material, thereby decreasing cutting forces and the energy required to cause further shear. Temperature at the tool-chip interface affects the contact phenomena by changing the friction conditions, which in turn affects the shape and location of both of the primary and secondary deformation zones [2], maximum temperature location, heat partition and the diffusion of the tool material into the chip.

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Notation

a_p	depth of cut (mm)	q_{fr}	heat flux of rake face heat source (W/mm^2)
F_V	cutting force in turning (N)	V, V_c	cutting velocity (m/min or m/s as defined)
F_s	feed force in turning (N)	V_{ch}	chip velocity (m/s)
F_{fr}	frictional cutting force at rake face (N)	W_c	rate of energy consumption (W)
L_c	tool-chip contact length (mm)	α	rake angle
Q_r	rate of heat generation at the rake face heat source (W)	λ_h	chip compression ratio
		β	heat partition coefficient
Q_s	rate of heat generation at the shear plane heat source (W)	τ	shear strength

Measuring temperature and the prediction of heat distribution in metal cutting is extremely difficult due to a narrow shear band, chip obstacles, and the nature of the contact phenomena where the two bodies, tool and chip, are in continuous contact and moving with respect to each other.

The ever-increasing demand on cost reduction and improving quality of final products are driving metal cutting research into new areas. As for high speed machining (HSM), it has become a key technology of particular relevance to the aerospace, mould and die and automotive industries. In HSM, cutting speed has a predominant effect on the cutting temperature and the heat transfer mechanism. As cutting speed increases, the cutting process becomes more adiabatic and the heat generated in the shear deformation zone cannot be conducted away during the very short contact time in which the metal passes through this zone. Consequently, highly localized temperatures in the chip occur. Therefore, it appears that in HSM, where the process is nearly adiabatic, the effect of the thermal phenomenon should become more important.

The subject of heat and temperature distribution in metal cutting has received considerable attention from researchers. Several analytical models have been developed based on different simplifying assumptions, which affect the accuracy of these models. Numerical methods have also been used which have enabled the elimination of many of the simplifying assumptions and consideration of more realistic parameters such as, temperature dependent material properties, material flow stress and the boundary conditions. In this paper, several methods for measuring temperature near the cutting tool edge are briefly reviewed and compared. The relative merits of the different techniques are compared by considering accuracy, precision, ease of use and applicable temperature range, etc. Analytical and numerical models, which have been developed over time for the prediction of heat generation, temperature and temperature distributions, are also reviewed. Finally, this paper presents the main requirements for the modelling of high speed metal machining processes based on the review and assessment of the available machining models.

1.1. Heat generation in metal cutting

In the metal cutting process, the tool performs the cutting action by overcoming the shear strength of the workpiece material. This generates a large amount of heat in the workpiece resulting in a highly localized thermomechanically coupled deformation in the shear zone. Temperatures in the cutting zone considerably affect the stress–strain relationship, fracture and the flow of the workpiece material. Generally, increasing temperature decreases the strength of the workpiece material and thus increases its ductility. It is now assumed that nearly all of the work done by the tool and the energy input during the machining process are converted into heat [1,3,4].

The main regions where heat is generated during the orthogonal cutting process are shown in Fig. 1 [5]. Firstly, heat is generated in the primary deformation zone due to plastic work done at the shear plane. The local heating in this zone results in very high temperatures, thus softening the material and allowing greater deformation. Secondly, heat is generated in the secondary deformation zone due to work done in deforming the chip and in overcoming the sliding friction at the tool-chip interface zone. Finally, the heat generated in the tertiary deformation zone, at the tool workpiece interface, is due to the work done to overcome friction, which occurs at the rubbing contact between the tool flank face and the newly machined surface of

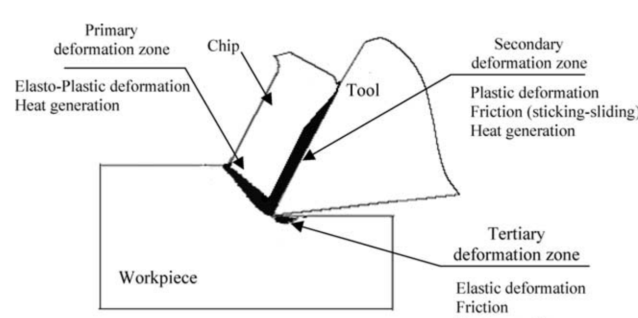

Fig. 1. Sources of heat generation in the orthogonal cutting process.

the workpiece. Heat generation and temperatures in the primary and secondary zones are highly dependent on the cutting conditions while heat generation in the tertiary zone is strongly influenced by tool flank wear.

In summary, the power consumption and the heat generation in metal cutting processes are dependent on a combination of the physical and chemical properties of the workpiece material and cutting tool material, cutting conditions and the cutting tool geometry.

1.2. Estimation of heat generation in metal cutting

Heat generated in metal cutting can be estimated either by calorimetric methods or by measuring the cutting forces. A detailed review of the calorimetric methods is available in literature [6]. Using the knowledge of cutting forces, the rate of energy consumption in metal cutting is given by:

$$W_c = F_V \cdot V \tag{1}$$

where F_V is the cutting force in N and V is the cutting speed in m/s.

Assuming that all the mechanical work done in the machining process is converted into heat, then heat generation, Q_S in J/s in the primary deformation zone may be calculated from the work done as:

$$Q_S = W_c = F_V \cdot V \tag{2}$$

where, F_V is the tangential cutting force or the force in the velocity direction and V is the cutting velocity.

The amount of heat generated due to the work done in the secondary deformation zone along the tool rake face is calculated from the friction energy given by the following equation:

$$Q_r = \frac{F_{fr} \cdot V}{\lambda_h} \tag{3}$$

where F_{fr} is the total shear force in N acting on the rake face, and λ_h is the chip thickness ratio. The force F_{fr} can be calculated by using the following equation:

$$F_{fr} = F_V \sin \alpha + F_S \cos \alpha \tag{4}$$

where F_S is the feed force and α is the rake angle.

Heat is removed from the primary, secondary and tertiary zones by the chip, the tool and the workpiece. Fig. 2 schematically shows this dissipation of heat. The temperature rise in the cutting tool is mainly due to the secondary heat source, but the primary heat source also contributes towards the temperature rise of the cutting tool and indirectly affects the temperature distribution on the tool rake face. During the process, part of the heat generated at the shear plane flows by convection into the chip and then through the interface zone into the cutting tool. Therefore, the heat generated at the shear zone affects the temperature distributions of both the tool and the chip sides of the tool-chip interface, and the temperature rise on the tool rake face is due to the combined effect of the heat generated in the primary and secondary zones.

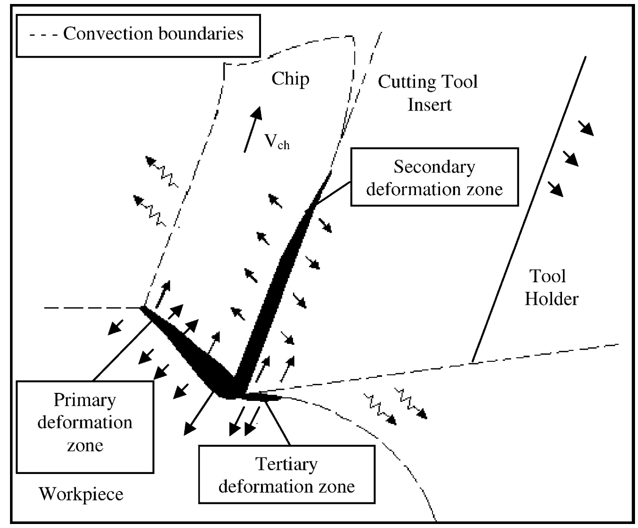

Fig. 2. Schematic representation of a heat transfer model in orthogonal metal cutting considering the combined effect of the three heat sources.

According to Tay et al. [7] and Trent [8] the total heat generation due to plastic deformation and frictional sliding in the secondary deformation zone, for continuous chips produced from a non-abrasive material at medium cutting speed, can be assumed to be between 20 and 35% of the heat generated in the primary zone. This implies that, when considering the temperatures on the cutting tool, the heat source in the primary zone should also be taken into account in addition to the direct effect of the heat generation on the rake face. Vernaza et al. reported that 17% of the primary heat zone flows into the workpiece [9]. However, for very low metal removal rates this amount of heat usually assumed to be as high as 50% [9]. Moriwaki et al. [10] assumed that half of the heat generated due to the friction between the tool and workpiece is supplied to the workpiece and another half to the tool as a heat flux. It also said that 10–30% of the total heat generation enters the tool [1].

Most of the analytical models for temperature prediction used Blok's [11] energy partition analysis or Jaeger's [12] friction slider solution, which differ significantly in heat partition phenomenon.

Blok's partition principle has been widely used in the analytical investigation of temperatures generated in metal cutting. Blok's principle is only valid for two bodies in relative motion; one stationary while the other is moving with a relative velocity. Therefore, it is only valid to solve the problem of heat partition in the tool-chip interface. Blok's partition principle can be summarised as follows: the rate of heat generated per unit area at the interface is conducted into the tool and the chip at λ and $(\lambda - 1)$ rates, respectively, assuming that all heat losses are neglected. The author then derived an expression for the average temperature over the tool-chip contact by considering a stationary heat source of intensity βq on the rake face of the tool. Another expression is then derived for the average temperature over the same contact by considering a moving heat source of intensity $(1-\beta)q$ with chip-velocity at the separating surface of the chip. The two values of the mean temperature can be then equated to determine the single unknown β (the fraction of the heat flowing into the tool). The main concern about Block's procedures is the unrealistic assumption of a uniform distribution of heat flux over the contact region (considering the existence of sticking and sliding contact). This assumption makes it less relevant especially when the temperature distribution is considered. In addition, the use of this model to predict the temperature rise in the shear zone, where there is one body involved, can also be questionable.

Also, most of these models, assumes steady state conditions when partitioning the heat generated at the primary and secondary zones. This means that the heat fluxes flowing into the process components are constant during the process. This leads to an underestimation of heat partition into the cutting tool especially in transient period.

However, there is a lack of consensus in the methodology for accounting of heat partition and the effect of the remote primary heat source on rake face temperature field. In HSM, it could be argued that the low interaction time diminishes the significance of the primary heat source on rake face temperatures.

2. Temperature measurement in metal cutting

Much effort has been made to measure the temperature at the tool-chip interface zone and the temperatures of the chip, tool and the workpiece, as well as, obtaining the temperature distributions in the cutting tool. A review of the most common experimental techniques for temperature measurement in metal cutting processes reveals that these techniques can be classified as: direct conduction, indirect radiation, and metallographic. These techniques have been reviewed by Barrow [3], Da Silva et al. [5], Herchang et al. [13] and more recently by Komanduri et al. [14], O'Sullivan [15], and Sutter et al. [16]. Generally, these techniques include: tool-work thermocouples, embedded thermocouples, radiation pyrometers, metallographic techniques and a method of using powders of constant melting point.

The tool-work (dynamic) thermocouple technique is based on the fact that the tool and the workpiece are two different materials. The contact area between them forms a hot junction, which produces an electromotive force (emf) while the tool or the workpiece material themselves form the cold junctions. Stephenson used this technique for measuring the temperature in cutting tests on grey cast iron and aluminium with WC tools [17]. This technique was also used by Alvelid [18] and Lezanski et al. [19]. However, the main concerns stated by different researchers about this method are that: it only gives a mean value of the temperature along the whole tool-chip interface and high local temperatures which occur for short periods can not be observed; it gives incorrect results if a built up edge is formed; a coolant cannot be used; both workpiece and the tool should be electrical conductors, the thermocouple pair requires accurate calibration and produces significant noise in the signal. Furthermore, this technique's application in HSM could be limited by the brittleness and electrical resistance properties of the tools. In particular, its inability to capture the transient aspects of temperature distribution makes it less ideal for HSM. Indeed this technique is not suitable for temperature measurements when the workpiece material melts. This is one of the possible reasons for the inaccuracy of Salmon's 1931 theory of the evolution of cutting temperature in HSM.

Embedded thermocouple technique uses thermocouples, which are inserted and mounted into the cutting inserts to measure temperature either at a single point or at multiple points along the tool rake face. This method requires drilling of several holes into the tool or the workpiece for the thermocouples to be inserted. For good accuracy the depth of the holes needs to be as close as possible to the surface where the temperature is to be measured. Kitagawa et al. employed this technique to study the effect of the cutting temperature on the tool wear in high speed turning of Inconel 718 and milling of Ti-6Al-6V-2Sn alloy [20]. Tungsten micro-thermocouples were embedded in the tool. Temperatures of 1200 and 1100 °C were reported for the ceramic–Inconel tool material pair at 150 m/min and the carbide-Ti-6Al-6V-2Sn tool-material pair at 500 m/min, respectively. Chen and Tsao calculated the heat flowing into the rake face of the cutting tool by using inverse heat conduction method (IHCM) based on the interior temperature variations measured from a thermocouple [21]. Although this technique has been widely used, especially for the estimation of the temperature of the tool using the IHCM, there are a number of limitations and questionable aspects concerning the placement of the thermocouple, since they alter the heat flow, as well as, limit the strength of the tool. Other limitations include the slow response time, difficulties to drill holes in hard tool materials such as ceramics, as well as, an inability to predict the transient response.

The radiation techniques are non-contact thermographic methods to measure the surface temperature of the body based on its emitted thermal energy. It is available for both temperature field measurement (infrared thermography) including photo cameras with films sensitive to infrared radiation and infrared cameras, and for point measurement (infrared pyrometer). The radiation technique has many advantages over the thermo-electric technique including: fast response; no adverse effects on temperatures and materials; no physical contact; and allowing measurements on objects, which are difficult to access. This technique is probably the most suitable in HSM applications where high temperatures can be captured easily as there is no direct contact with the heat source. However, the measurement position has to be selected carefully as the accuracy may be significantly affected by chip obstruction. Chip obstruction also makes it difficult to measure the temperature at the tool-chip interface. In addition, the exact surface emissivity should be known as it strongly affects the measured temperature. Sullivan and Cotterell applied a coating of known emissivity on the workpiece surface in order to eliminate any measurement problems [22]. There are many different emissivity values reported in literature for different cutting tools and workpiece materials. However, it is important to note that the emissivity of most of the metals with clean surface or thin oxide layers varies with wavelength and temperature. Therefore, the pyrometer-operating wavelength and band data must match with the spectral emissivity values. In addition, measuring the emissivity is also complicated by the fact that the emissivity of an object significantly changes with the change in the surface conditions such as oxidation, etc. In most cases, to overcome the uncertainty of the emissivity measurement it is important to measure the emissivity of the object prior to the temperature measurement in the cutting test.

Lin et al. used an infrared pyrometer to measure the tool-chip temperature for carbide and ceramic tools at cutting speed of 600 m/min [23]. Dewes et al. employed an IR camera and the thermocouple technique to measure the chip temperature when machining H13 hardened steel in high speed machining [24]. Young used the IR camera to measure temperature of the chip back section and the interface temperature in orthogonal cutting of AISI 1045 steel [25]. These results were used to investigate the effect of the flank wear on the cutting temperature. Yourong et al. used the infrared camera to investigate the temperature distribution on the flank face of ceramic tools used for turning operation [26]. More recently, Muller et al. developed a two-colour fiber-optic pyrometer for the measurement of temperatures on surfaces of unknown or varying emissivity [27]. The pyrometer was also used for the measurement of the chip and the workpiece temperature in an oblique turning process of AISI 1045 steel. Darwish et al. used an IR camera to measure the tool-chip interface temperature in orthogonal cutting tests in a study to compare the heat flow through brazed and bonded tools [28]. IR pyrometer was also used as a remote sensor to measure the temperature at the tool-chip interface in high-speed milling [29]. The measured temperatures were used as an input for the inverse heat transfer model to calculate the heat flux and temperature distribution on the tool-chip interface.

In metallographic techniques, the temperature measurement involves the analysis of the microstructure and/or the microhardness of the heat-affected zone within the cutting tool. It requires calibration curves, which give the hardness of the tool material against known temperatures and heating time. Typically the accuracy of the temperatures measured by this method is in the range of $\pm 25\,^{\circ}\mathrm{C}$ [30]. However, this technique is mainly limited to HSS tools, which experience a change in the structure/microhardness with temperature in the range from 600 to 1000 °C. It has been reported that temperature maps can also be constructed for the iron-bonded cemented carbide tools where cobalt is replaced by iron [31]. The accuracy of this method for the determination of metal cutting temperatures is limited. This method cannot be used to record the temperature field in the cutting tool as a function of time.

The fine powder method can be used to predict the temperature gradients on the rake face of cutting tools [32]. The temperature distribution on the rake face is estimated by observing the boundary line formed by the melted and unmelted powder scattered on the tool surface. Lo Casto et al. used a remote temperature sensing technique to determine the isothermal lines on a plane parallel to the rake face based on the use of various constant melting point powders [33]. However, the main limitation of this method is that the used powder requires a long time to be completely melted.

There is no general agreement on the results obtained by using the various temperature measurement techniques discussed above. The complexity of the machining process makes it extremely difficult to compare the results from different techniques. This difficulty is illustrated by the fact that different results are obtained by the same measuring method from the experiments, which are performed under the same cutting conditions and on the same tool and workpiece materials. In terms of the temporal and spatial resolutions of the measurements of temperatures in metal cutting, it has been reported that the best temporal resolution of 50 μm was obtained by the pyrometer while the best spatial resolution of 5 μm was obtained by thermal video cameras [34].

3. Temperature in high speed machining

The high temperatures encountered in HSM are due to many reasons: more heat is generated; heat generation is concentrated over a small area; and, the adiabatic nature of the process where less heat is dissipated away from the cutting zone due to a short contact time.

This section presents the results of extensive cutting tests performed to measure temperature along the tool-chip interface line when machining BS 970-709M40EN19 (AISI/SAE-4140) high strength alloy steel using uncoated cemented carbide inserts. The temperature was measured as a function of cutting speed. The cutting speeds used in the tests were 750 and 925 m/min while the feed rate and depth of cut were kept constant at 0.15 mm/rev and 0.1 mm, respectively. Temperature was measured using an infrared thermal imager FLIR ThermaCAM SC3000, a long wave, self-cooling analysis system with a cool down time of $<$ 6 min. It has a temperature range of -20 to $+2000\,^{\circ}\mathrm{C}$ with

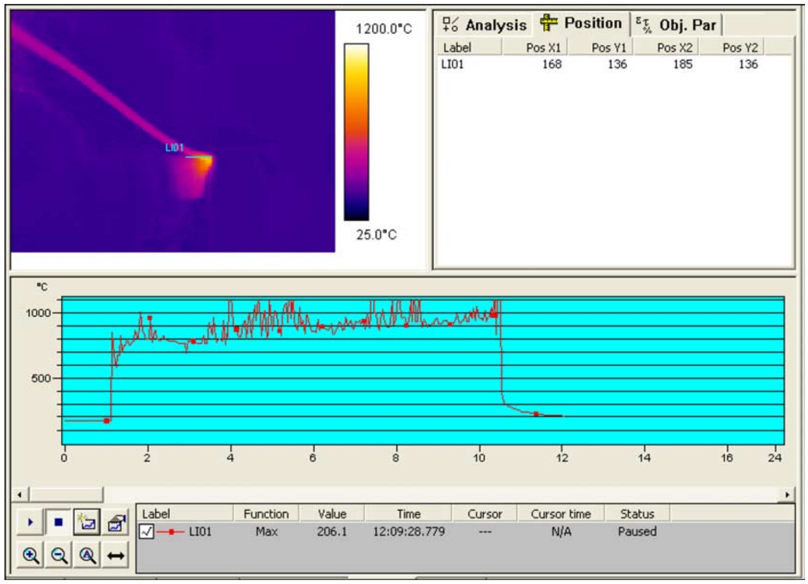

Fig. 3. The ThermaCAM™ researcher main window.

an accuracy of $\pm 2\%$ or 2 °C for measurement ranges above 150 °C. This camera can acquire images and data at high rates of up to 750 Hz PAL/900 Hz NTSC with ThermaCAM ResearcherTM HS package. This package allows extensive analysis of highly dynamic objects and events typically found in metal machining research applications. The emissivity of the workpiece material was measured using the direct emissivity measurement method. The surface temperature was measured locally by a thermocouple. The emissivity was evaluated using the ThermaCAM researcher software by matching the measured temperature with that of the thermocouple.

The ThermaCAM SC3000 was set up and the location of the analysis area was chosen to be on the chip. The camera was positioned at a distance of 10 mm from the tool workpiece interface in order to avoid any damage by the chips.

The stored images were recalled and analysed by using the 'flying spotmeters and the line' in the software, see program interface in Fig. 3. When placed on the image the spotmeter gave the temperature value at a particular point and was used to predict the position of the maximum temperature, while the line gave the maximum temperature along the tool-chip interface. The line was positioned at a distance of 40 μm from the interface into the chip as shown in Fig. 4.

The results obtained give a clear idea about the values of temperature achieved at the tool-chip interface at high cutting speeds. The thermography of Figs. 4 and 5 shows that as the insert gets in contact with the workpiece the temperature raises rapidly. For the cutting speed of 750 m/min, it can be seen that the maximum temperature is rising during the whole cutting period. The maximum temperature has a value of approximately 950 °C at the end of the cutting test. For the higher cutting speed of 925 m/min, the temperature reached the steady state conditions and a nearly constant value of 1000 °C after the heat up period. The temperature then increased and reached approximately 1150 °C at the end of the test. The curves in Fig. 6 represent the temperature evolution according to the spotmeters located on the chip, parallel to the tool-chip interface at a distance of 40 μm. It can be seen that the maximum temperature on the chip was recorded at a distance of 120 μm to the left from the tool tip. This clearly shows the predominance of the frictional heating component.

4. A review of temperature prediction and distribution models

Researchers have long investigated heat generation and cutting temperatures in metal cutting. They have applied different analytical and numerical methods with

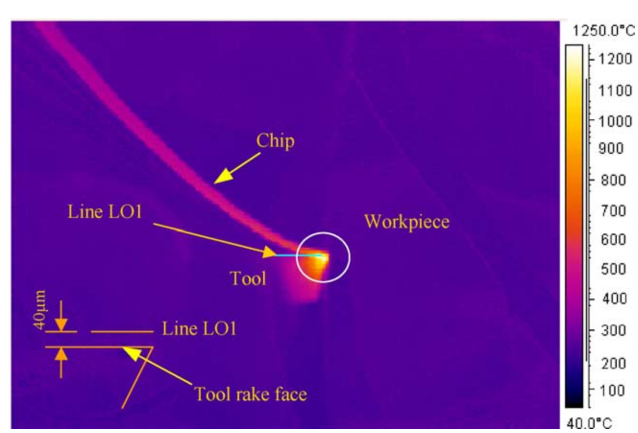

Fig. 4. Infrared photograph of the cutting process ($V = 750$ m/min).

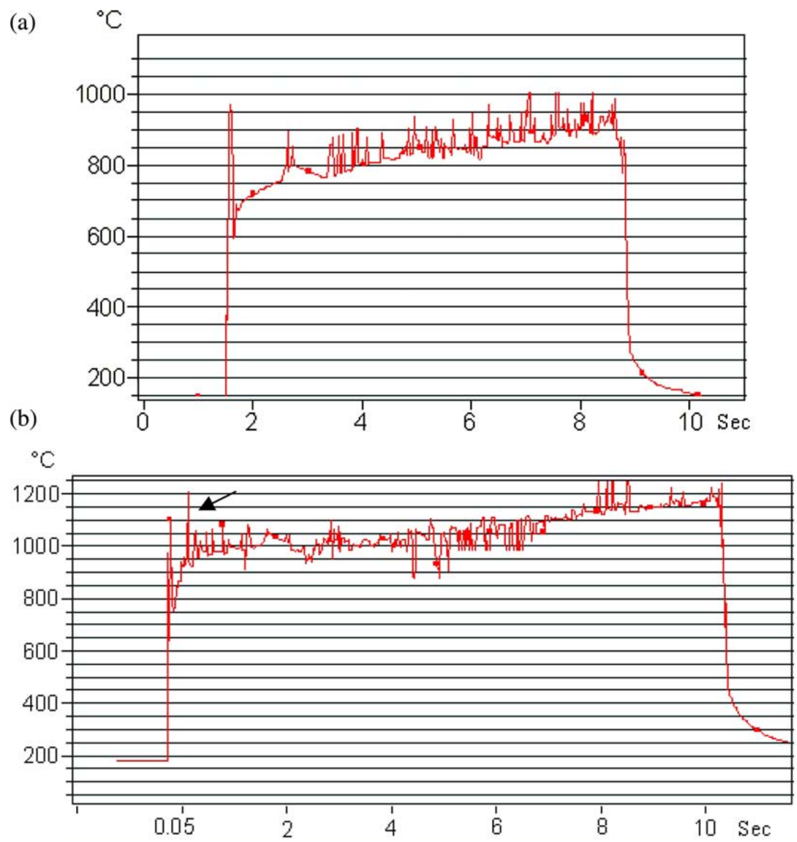

Fig. 5. Maximum temperature profile along line LO1 on the chip near the tool-chip interface at a distance of 40 μm, for cutting speeds of (a) 750 m/min, (b) 925 m/min.

the objectives to calculate the peak and average temperatures in the shear zone and on the tool rake face, determine the temperature distribution on the tool rake face, investigate the heat partition phenomena and obtain the temperature fields within the cutting tool, chip and the workpiece. These methods involve analysis of heat conduction with moving or stationary heat sources together with the kinematics, geometry and energy aspects of the machining process. The inverse heat conduction method has also been employed where the unknown boundary values of heat flux are obtained from a known interior temperature distribution in a heat conduction system.

4.1. Analytical models

Trigger and Chao [35] developed a steady state two-dimensional analytical model for the prediction of average temperature in metal cutting. They calculated the average temperature rise of the chip as it leaves the shear plane due to the shear plane heat source and the average tool-chip interface temperature in orthogonal machining based on the existence of two heat sources, one on the shear plane and the other on the tool-chip interface. The latent heat stored in the chip was approximated to be 12.5% of the total heat generation. They also assumed that 90% of the heat flow into chip and 10% flows into the work material. They assumed plane heat sources at the shear plane and at the tool-chip interface and that the shear and frictional energy are distributed uniformly. Additionally, they assumed that there is no redistribution of the thermal shear energy going to the chip during the very short time the chip was in contact with the tool. They used Blok's [11] partition principle to compute the distribution of thermal energy at the shear plane. The frictional heat source at the tool-chip interface was considered as a moving band heat source in relation to the chip and a stationary plane source in relation to the tool. Moreover, Trigger and Chao assumed the work surface and the machined surface as adiabatic boundaries. The authors were able to calculate the average heat partition into the chip

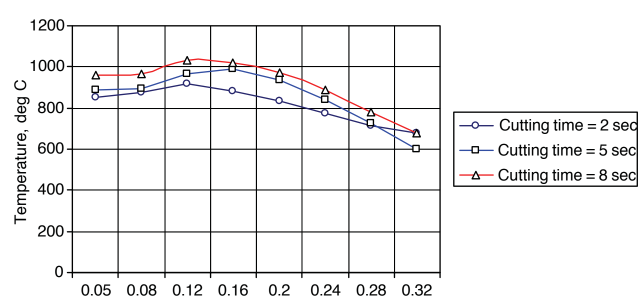

Fig. 6. Variation of maximum interface temperature with the distance from the tip of the cutting edge ($V = 925$ m/min).

and the tool and consequently the resulting average temperature at the tool-chip interface zone.

Loewen and Shaw [36] used a similar approach to calculate the average temperature rise at the tool-chip interface by making the same assumptions. The authors considered the chip and the work material as two bodies in relative motion at the shear plane. Relative to the shear plane the chip was considered as stationary body while the work material is a moving body moving at the velocity of shear instead of the cutting velocity. They applied Blok's heat partition principle to their analysis. Two temperature solutions were obtained for each heat source, one for each side of the plane heat source. Similar to Trigger and Chao, Loewen and Shaw considered that tool-chip interface is adiabatic and therefore, the shear plane contributes only to the temperature rise in the chip including the tool-chip interface on the chip side but not on the tool.

Although the models developed by Trigger and Chao and Loewen and Shaw provide a relatively straightforward solution for the prediction of the average temperature of the shear plane and the tool-chip interface, the main questionable aspect is about the use of Blok's partition principle and the assumption of a uniform heat flux at the tool-chip interface zone. Chao and Trigger pointed out the difficulties, which arose from the use of this assumption [37]. It was concluded that with this assumption it was impossible to match the temperatures on the two sides of the heat sources and that to bring the two temperature distribution curves to near coincidence; the heat flux distribution must necessarily be non-uniform. To solve this problem Chao and Trigger used two approaches. Firstly, they proposed an approximate analytical procedure in which the heat flux was assumed as an exponential function. Although they were able to achieve a more realistic interface temperature distribution, they pointed out that this approach required the use of a time consuming cut-and-try procedure to find the values of the constants in their equation. The second approach used by the authors was a discrete numerical iterative method, which involved a combination of analytical and numerical methods. The authors were able to calculate the temperature rise at the tool-chip interface on the tool and chip sides using Jaeger's solution for the moving and stationary heat sources.

Weiner developed another analytical model and obtained the average temperature at the tool-chip interface [38]. The shear plane was considered as an inclined plane with the heat source moving with a speed equal to the cutting speed. In order to simplify the geometry of the problem, Weiner assumed that the chip flow was normal to the shear plane and the heat conduction in the direction of tool motion was negligible. Moreover, it was assumed that the intersection between the shear plane and the workpiece free surface remained at ambient temperature. Rapier, assumed the primary deformation zone to be a plane heat source of uniform strength [39]. It was also assumed that there were no heat losses from the free surfaces of the chip and the workpiece and that the thermal properties of the workpiece were constant and independent of temperature. At the tool rake face the heat source was assumed as a plane uniform heat source with no heat transferring into the tool. The author was able to calculate temperature distribution in the workpiece, chip and tool and compute the temperature at any distances from the shear plane. Results however showed an overestimation of the chip temperature. Also the maximum temperature on the tool rake face occurred at the end of the tool-chip contact length. Boothroyd [40] and Wright et al. [41] developed two similar temperature models using Wiener's energy partition analysis. These were non-iterative models, which considered the workpiece thermal properties to be independent of temperature. Boothroyd assumed that the heat source on the tool rake face was uniform and the fraction of the total heat entering the tool was constant. Boothroyd also assumed that the heat conduction in the direction of the chip flow is negligible. Wright, on the other hand, used a different assumption for the rake face frictional heat source. His model computed the temperature of specially ground HSS inserts based on the relative size of the sticking and sliding regions with the assumption that 80% of the frictional heat source flows into the chip. Venuvinod and Lau [42] proposed a three-dimensional iterative model for the distribution of the average tool-chip interface temperature in free oblique cutting based on Jaeger's friction slider solution [12]. Their analysis assumed the tool to be ideally sharp and unworn, the chips produced to be continuous and the chip moves as a rigid body relative to the tool. The shear plane source was assumed to be uniformly distributed heat source while the heat generation at the tool-chip interface was assumed to be uniformly distributed over the sticking zone but decreasing linearly over the sliding zone and becoming zero at the separation point.

Young and Chou presented an analytical model to predict the tool-chip interface temperature distribution during orthogonal metal cutting [43]. They assumed plane strain and steady-state conditions. The shear plane and frictional heat sources were assumed as plane heat sources of a uniform strength. Furthermore, they assumed that the shear plane was inclined to the chip velocity; heat conduction in the direction of motion could be neglected; and heat generated along the shear plane was at a uniform rate. Their results show that the maximum temperatures occur along the interface where the chip starts to separate from the tool face as a consequence of the assumption of a plane source of uniform strength at the tool-chip interface. The location of the maximum temperature, expressed as a fraction of the contact length, was found to shift from 1 to 0.55 and 0.65 when the triangular and trapezoidal distributions of the heat source were used, respectively.

Radulescu and Kapoor presented a three-dimensional analytical model for the prediction of temperature fields in continuous and interrupted metal cutting processes [44]. Their analytical model can be used to calculate the transient cutting temperatures based on the cutting forces as inputs.

The authors assumed steady-state conditions and imposed convection boundary conditions on the boundaries exposed to the environment. The values of heat flux into the tool, chip, and the workpiece were determined from an energy balance on the control volume comprising of the chip formation zone. The tool-chip interface temperature was calculated by solving a heat conduction problem using the predetermined heat fluxes. Their results were found to be in good agreement with the experimental observations for the workpiece materials.

Stephenson et al. developed a model for calculating the tool temperatures in contour turning under transient conditions with a time varying heat flux and tool-chip contact area [45]. Unlike Radulescu and Kapoor, Stephenson et al. used insulated conditions for the faces exposed to the air in order to reduce the computing time and simplify the input requirements. Besides this, the ambient and insulated boundary conditions were applied at the bottom surface of the insert. Moreover, the computational domain was selected to have the same size as the tool insert, which means that any heat penetration to the holder was neglected. Heat flux was determined from measured cutting forces using Loewen and Show's model [36]. The average temperatures were calculated with two sets of boundary conditions for the bottom of the insert i.e. ambient temperature and insulated contact. Generally, the predictions obtained with the ambient temperature boundary condition at the bottom of the insert were found to agree well with the measured temperatures.

More recently, Komanduri and Hou developed an analytical model for the temperature rise distribution at the tool-chip interface in metal cutting which considered the combined effect of the shear and the frictional heat sources [46]. The model was based on Hahn's work [47] of an oblique moving heat source for the shear plane heat source, and Chao and Trigger [37] on the frictional heat source at the tool-chip interface and the heat source method developed by Jaeger. The authors assumed that only the backside of the chip was an adiabatic boundary and were able to predict the temperature rise distribution at the tool-chip interface by considering a non-uniform moving-band heat source for the chip and a stationary rectangular heat source for the tool. The model was applied to conventional machining of steel with a carbide tool. The results obtained are very interesting and show the combined effect of the shear and the frictional heat sources on the temperature rise as can be seen in Fig. 7. It was concluded that, although, the contribution of friction heat source is predominant, the shear plane heat source accounts for an increase in the temperature at the interface of about 200 °C.

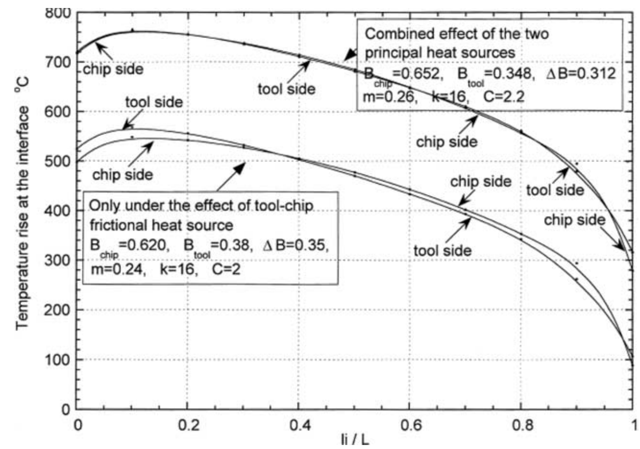

Fig. 7. Temperature rise at the tool-chip interface due to the combined effect of the shear and the frictional heat sources.

4.2. Numerical models

Finite element simulations have been successfully applied for modelling orthogonal metal cutting processes. They have significantly reduced the simplifying assumptions of the analytical models. However, the use of FEM in metal cutting research requires a large number of input parameters which needs to be determined through an extensive experimental work and mechanical property tests. These include material models for large deformation, high strain rate, temperature effects, tool-chip contact and friction models, and the separation criterion.

Generally, application of finite element modelling to cutting processes involves two types of formulations; Eulerian or an updated Lagrangian. The analyses employ different material models, such as rigid-plastic or elasto-plastic. The meshes are either structured or adaptive. Chip separation criteria, friction and contact conditions at the secondary zone and coupled thermomechanical models are also considered. Recently, general codes and commercial implicit finite element codes such as Deform 2D–3D, MSC.Marc have also been used for the simulation of metal cutting processes.

Tay et al. used the finite element method to compute the temperature field in the tool, the chip, and the workpiece in orthogonal cutting process [7]. Their approach was to develop a model based on the knowledge of the strain-rate field from experimental data. This procedure requires the strain-rate field being available for each set of conditions; quick stop tests printed with fine grids and the flow stress of the work material as a function of strain, strain rate and temperature. The authors were able to obtain an acceptable complete two-dimensional temperature distribution. Maximum tool-chip interface temperatures were found to be 700 and 1000 °C for cutting speeds of 122 and 244 m/min, respectively and a depth of cut of 0.25 mm. The location of the maximum interface temperatures occurred approximately at the mid of the contact length. However, the main limitation of this method is that the inputs should be obtained from the cutting tests for the specific combination of the tool geometry and workpiece. More simplified version of this finite element method was described later in order to reduce the computer run time and to eliminate

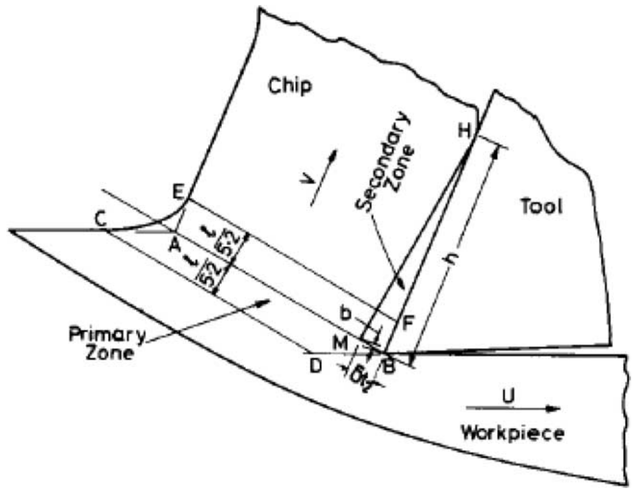

Fig. 8. Assumed deformation zone shape and dimensions used in [48].

the need for an experimental strain rate field for each set of conditions [48]. The secondary plastic zone was assumed to extend over the entire contact length while the elastic region, which occurs just before the separation point, was neglected. Moreover, the secondary plastic zone was considered to have a triangular shape as shown in Fig. 8.

Fig. 9 shows temperature distribution obtained with this model. The author's results were compared with those obtained by Rapier and Boothroyd for the same cutting conditions. The tool-chip interface temperatures of Rapier were found to be higher than those obtained by the authors. This was attributed to the assumption of plain heat source made by Rapier. The primary zone temperatures of Rapier and Boothroyd were also found to be higher than the values of the authors.

Muraka et al. also used the finite element method to investigate the effect of the process variables such as cutting speed, flank wear rate, coolant water, etc. on the temperature distribution on the tool flank and rake faces [49]. The rate of heat generation was calculated from the experimentally measured strain-rates in the primary and secondary shear zones. In addition, empirical expressions were used to relate the flow stress of the workpiece material in the primary zone

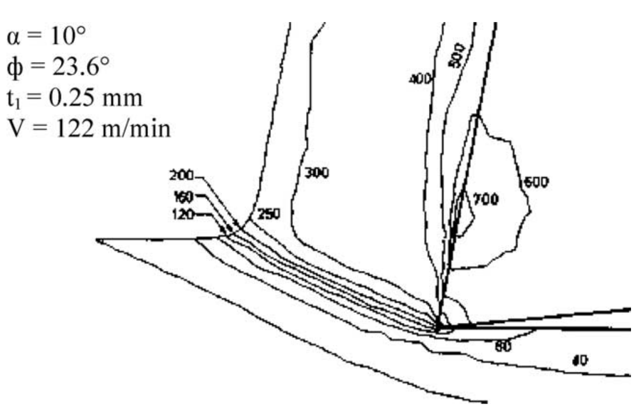

Fig. 9. Temperature distribution [48].

to the strain, strain rate and temperature. Unfortunately, the input requirements for this model have to be determined experimentally, which may influence the accuracy of the obtained results. Stephenson et al. presented a further development of Tay's finite element model and extended its range of application [50]. Modifications were applied to the procedures for calculating the strain rate field, the mesh and the flow stress. The resulting procedure avoided the need for the flow field as an input and was able to accommodate a wide range of shear angles and tool-chip contact lengths.

Dawson and Malkin [51] presented a finite element solution for the heat transfer problem for the shear plane temperature. They suggested that the band heat source did not move along the shear plane relative to the workpiece as had been assumed by Loewen and show [36]. Instead, the band heat source was moved with the cutting velocity directly into the workpiece material to be removed ahead of shear plane. Therefore, the preheated material directly ahead of the shear plane was removed. From the chip side at the shear plane it was assumed that all of the heat dissipated at the shear plane is conducted into the workpiece. The authors also considered the heat convection from the workpiece into the chip across the shear plane.

Kim and Sin developed a thermo-viscoplastic cutting model by using finite element method to analyse the mechanics of steady-state orthogonal cutting process [52]. The authors assumed that the workpiece is a thermo-viscoplastic material and the flow stress is a function of strain, strain rate and temperature. In the temperature analysis, the authors applied the full upwind scheme to overcome the spurious oscillations in the solution arising from the standard discretization of the heat transfer equation involving both diffusion and convection terms. The experimental data comparison included the cutting force and temperature analysis. Validation of the cutting temperature was performed by comparing the simulated temperature distributions, maximum temperature and the location of maximum temperature with Tay et al.'s results and was found in good agreement. Ceretti et al. proposed a finite element model to simulate the orthogonal cutting process with continuous and segmented chip flow [53]. They used the commercial code DEFORM 2D to simulate the process. Despite the simplifying assumptions, related to friction conditions, properties of the workpiece material, limited work hardening, strain rate, and temperature effects, their results were in good agreement with the experiments in terms of estimating chip geometry, tool workpiece contact length and chip and tool temperatures.

Moriwaki et al. [10] developed a rigid-plastic finite element model to analyse the mechanics of the orthogonal micro machining process and examined the effect of the tool edge radius to depth of cut ratio on the micro machining process. They also analysed the flow of cutting heat and temperature distribution in both the workpiece and the tool based on stress, strain and material flow within the workpiece. The steady state heat conduction analysis was performed assuming that the flow stress is independent on the temperatures and the nodal velocities in workpiece and chip and therefore, their model was for low cutting speeds.

Strenkowski et al. [54] presented a steady-state Eulerian finite element model for orthogonal metal cutting in which the workpiece and chip are treated as a control volume. For this model, the cutting process was treated as a large deformation process involving a viscoplastic material. The authors assumed that the elastic effects are negligible. The model incorporates a procedure for predicting the chip geometry and contact length which allows the determination of the thermal conduction path of the heat generated by plastic work of the workpiece and sliding friction into the tool which allows for calculation of the tool temperatures. Since, the Eulerian cutting model is a control volume approach the free surface is not known in advance, the authors calculated the position of the free surface by considering the material velocity normal to a free surface is zero. Their model does not require an empirical cutting data such as material velocity or measured contact length to predict the tool temperatures and therefore, no companion cutting experiments are required.

More recently, Lei et al. presented a thermomechanical plane strain finite element model for modelling orthogonal cutting process with continuous chip formation. The authors considered a steady state plane strain conditions. They assumed that friction forces at the tool-chip interface are neglected. Instead the authors applied a uniform heat flux directly into the chip to account for the frictional heat transfer at the interface [55].

Liu and Guo proposed a thermoelastic–viscoplastic FE model to investigate the effects of the friction at the tool-chip interface and sequential cuts on residual stresses in machined layers [56]. The authors assumed adiabatic conditions and calculated temperature rise in the workpiece based on the heat generated by plastic deformation while the friction heat generation was neglected. The FE analysis of the orthogonal cutting process conducted by Shet and Deng was based on a modified Coulomb's friction model at the tool-chip interface and a stress-based chip separation criterion and the assumption of adiabatic heating conditions [57]. The authors were able to estimate the local temperature rise in the primary and the secondary deformation zones.

Levy et al. [58] applied a two-dimensional finite difference approach to determine the transient temperature variations in the chip and the tool in orthogonal cutting. In order to simplify the use of square grids in their study, the authors considered a tool of zero rake and clearance angle. The results were obtained under idealized cutting conditions with a zero wear rate, a normal wear rate and an accelerated wear rate. Smith and Armarego presented a three-dimensional temperature analysis in orthogonal cutting processes based on the thin shear zone model, popular shear zone temperature equations and a finite difference scheme [59]. The heat generated at the rake face was uniformly distributed on the contact area and no frictional heat was transferred into the workpiece. All heat losses other than conduction and mass heat transfer were neglected. The model required the temperature at the back of the tool to be known. Boothroyd also applied the finite difference technique to measure and calculate the heat generation rates in the primary and secondary deformation zones based on the temperature variations in preheated workpieces using an IR photography technique [60].

Boundary element method has also been used to analyse the metal cutting process. Chan and Chandra [61] used this method to analyse the heat transfer problem in orthogonal machining processes based on the BEM formulation of Tanaka et al. [62]. The method was applied to solve for heat transfer in the tool, chip and the workpiece regions separately due to the different velocities in each of these regions and assuming that the thermophysical properties were same in the three regions. The complete heat transfer model was then obtained by matching the boundary conditions at the shear plane and the tool-chip interface.

The inverse heat conduction techniques have been employed in the analysis of machining process. The unknown boundary condition i.e. the actual heat flux is obtained from known interior temperatures measured at certain positions in a heat conduction system. Chen and Tsao used a remote method to determine the temperature distribution on the tool rake face [21]. The authors used three-dimensional boundary element method to solve the heat transfer model of the cutting tool and then they employed the inverse heat transfer technique to estimate heat flux at the rake face for the corresponding transient measurement of a thermocouple embedded in a HSS tool. The model was also used to determine the temperature fields on the tool rake face.

5. Discussion on heat generation and temperature distribution in metal machining

Some conclusions can be drawn from the study of the work published on heat generation, temperature measurement and temperature distribution in metal cutting. Firstly, the temperature measurement techniques employed in the experimental work have certain limitations, which make it difficult to predict the results in a precise manner. A large part of research on thermal aspects of the metal cutting operations has concentrated on predicting the temperature of the interface zone and obtaining the temperature distributions in the cutting tool and the chip. However, there is no general agreement on the procedures for estimating the mean temperatures on the shear plane and the tool-chip interface, and for predicting the intensity and distribution of heat sources in machining operations. This is due to the complexity of the machining mechanics and the heat transfer phenomena involved.

There is a wide range of analytical models based on certain simplifying assumptions, and developed from differing levels of experimental work. Most of these models have been developed for orthogonal cutting processes in order to reduce the problem to a two-dimensional case. A few consider the process as three-dimensional. The simplification of these models relate to the geometry of the tool, the chip, and the workpiece, as well as, some of the boundary conditions and material data. In reality, heat is generated at the shear plane, at the tool-chip interface, and at the flank face of the cutting tool. Each of these sources affects the heat distribution in the tool, the chip and the workpiece. Most of the models, however, consider the effect of each source separately. Consequently, each model is subject to some difficulties in obtaining the complex temperature distribution.

The main concerns about the accuracy of the analytical models, and their limited predictive capability, emanate from the simplifying assumptions related to the nature of heat sources and the boundary conditions. Most models assume that the heat sources on the shear plane and on the tool-chip interface are plane heat sources. This assumption is made mainly to simplify the analysis, since modelling the deformation zones with distributed heat sources would require the workpiece material velocity (or strain) distributions in these zones to be determined experimentally. In reality, the shear zone and the frictional zone extend slightly into the workpiece and the chip, respectively, and in both cases the heat is generated over a larger finite volume. The assumption of plane sources, therefore, usually leads to an overestimation of the temperature. Another assumption made in some studies, when considering the effect of the shear plane heat source on the temperature rise in the chip, is that the chip boundaries, the back and the interface sides, are adiabatic. This means that there is no heat flowing from the chip into either the environment or the cutting tool. This assumption also implies that the chip temperature is independent of the heat generation in the secondary deformation zone and consequently the cutting speed does not have a significant effect on the chip temperature rise. In reality, there is always heat conduction occurring within the workpiece, the chip and the tool. The heat generated in the secondary deformation zone flows into the tool and the chip and hence should be included in the total temperature rise of the chip. Therefore, the adiabatic heating is just an assumption which leads to non realistic results. The effect of this assumption is more important when cutting within the conventional speeds region.

The assumption, used by some investigators, of a band heat source moving relative to the workpiece along the shear plane does not appear to be realistic in HSM. This is because the heat source moves directly into the workpiece at the cutting velocity and implies that the maximum heat is generated in the material ahead of the shear plane. This considerably alters the heat transfer behaviour during the process. In conventional machining, most of the heat flows into the workpiece. Part of this heat is then transferred to the chip along the shear plane through convection from the workpiece. In HSM, on the other hand, most of the heat conducts into the chip, as there is no sufficient time for the generated heat to flow into the workpiece.

In metal cutting, the tool-chip interface is divided into two regions. At any instant during the cutting process a part of the interface experiences full seizure whilst the remaining part undergoes inter-facial sliding [63,64]. Seizure (or full sticking friction) can be defined as a solid phase weld between the primary atomic bonds of absolutely clean metallic surfaces. Interfacial sliding, on the other hand, is due to the relative movement between last layer of chip material (atoms) and the tool rake face surface. These interfacial conditions are highly dependent on the cutting conditions and the properties of the tool and workpiece materials. Therefore, the heat generated along the secondary shear zone is directly influenced by the tribological conditions of the contact region, whilst the tool-chip contact length greatly affects the thermal conduction path between the chip and tool. For the calculation of heat generation and temperatures in the secondary deformation zone, these factors should be taken into consideration. The assumption of a uniform plane heat source is inadequate in conventional machining where sliding and sticking contact phenomenon is obvious. Thus, in conventional machining the uniform band source results in an overestimation of the calculated temperatures. In high speed machining the contact phenomenon is different. The friction model at the tool-chip interface plays a rule of importance in the modelling of machining process; it is probably one of the major limitations of the machining models developed so far.

Chao and Trigger [37] found that the temperature rise on either side of the tool-chip interface could not be matched at any position along the contact length if the heat source was assumed to be uniform. They pointed out the need for the consideration of a distributed heat source. This was also recognised by Boothroyd [40], who did consider the rake face frictional contact as a distributed source over a very thin region. This led to a significant reduction in the estimated rake face temperature. Another consequence of the 'uniform heat source' assumption is the unrealistic prediction of the position of the maximum temperature along the cool-chip interface, which, in some studies, has been found to occur at the tool-chip separation point. It is well known that the chip heats up as it moves along the rake face and hence, the vicinity of the maximum temperature cannot possibly be the edge of the cutting tool. It cannot be at the tool-chip separation point either as the intensity of the heat source decreases along the friction region gradually and reaches a minimum at the end of the contact length, as will be seen later. Therefore, the hottest point along the tool-chip interface occurs within the contact length, as shown in Section 3.

Wright and Trent [65] suggested that the contact phenomenon along the tool-chip contact length consists of sticking and interfacial sliding regions and the distribution of heat follows the same trend as the frictional stress given by Zorev [66], and as shown in Fig. 5(a). The frictional stress is distributed uniformly from the cutting edge to the end of the sticking region and then decreases gradually to the end of the contact length. Based on the assumption that all the energy resulting from friction is converted into heat, the authors concluded that heat generation could not be uniformly distributed along the rake face. Tay et al. [7] reported that the material which is in contact with the cutting edge slides at a cutting speed of $V_c/3$, where V_c is the bulk chip speed. The interfacial speed increases and reaches the bulk chip speed at the end of the sticking region. Tay et al. also suggested that the heat generation in the sticking region comes from two sources, as shown in Fig. 10(b). First, the heat generated in the secondary zone, Q_S, due to the shearing of the material. This is a function of the material shear strength and the relative velocity across the zone thickness which varies from a value of $2V_c/3$ at the cutting edge to zero at the end of the sticking region. The second source is due to the heat generated through the sliding of the material at $V_c/3$ at the cutting edge, with the amount of heat generated per unit area given by $Q_f = \tau_S V_c/3$.

Considering the generation of the shear and frictional heat and the distribution of temperature along the tool-chip interface, some of the analytical models use Wiener's energy partition analysis, whilst others use Jaeger's friction slider solution. These solutions differ significantly in heat partition; Wiener's solution assumes a less amount of heat flowing into the workpiece than Jaeger's solution. Therefore, the models derived based on Wiener's solution overestimate the temperatures of the chip and tool rake face. Also, with these models, the assumption of a constant heat partition factor lead to an overestimation of the calculated temperatures. In summary, there is no general agreement on a partition criterion for the heat transfer into the tool, the chip, and the workpiece, in conventional machining.

The intensity and distribution of heat sources are extremely difficult to predict in machining operations. This is because the dynamic mechanical and thermal processes are coupled together and influence each other to a large degree. The elevated temperatures associated with the cutting process significantly alter the material mechanical and thermal properties during the process. For example, as the temperature along the tool-chip interface zone increases, the material strength and hence, the cutting forces decrease, thereby reducing the heat generation. Any model, which does not take this into account, would predict higher temperatures. It can be concluded, therefore, that the temperature solution in metal cutting needs to be an iterative solution with temperature dependent material properties.

Different analytical models are based on the assumption of different heat partitions into the workpiece. As a result, the temperature distributions obtained from these models differ significantly. For example, the assumption of steady-state cutting conditions and fixed heat partitioning between the process components imply that the contact area, the input heat area, and the tool geometry remain constant during the process. In reality, the cutting parameters vary significantly with time before reaching the steady-state conditions. This is due to many factors such as tool wear, and change in contact conditions. The contact area, which determines the heat input area for the model, changes with the cutting time due to the fact that crater wear modifies the tool rake angle leading to a longer contact length. Without considering this, the analysis may lead to an underestimation of the heat flowing into the cutting tool and hence, yield lower tool temperatures.

In some studies for the prediction of the temperature on the shear plane and at the tool-chip interface, the intersection between the shear plane and the workpiece free surface is maintained at ambient temperature. This boundary condition is another source of error as ambient conditions may not be prevail at the end of the shear plane due to heat convection into the chip along the shear plane. This is particularly so in HSM where the chip takes away most of the heat.

The finite element method has been frequently used for modelling machining processes. The main objective of FE studies is to derive a computational model for predicting deformations, stresses and strains in the workpiece, as well as the loads on the cutting tool including forces,

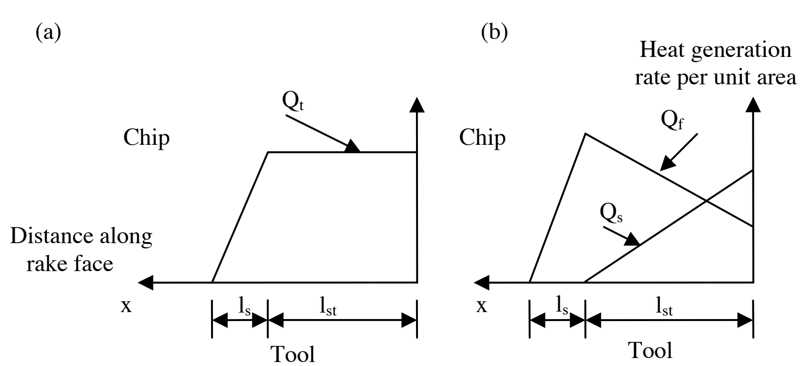

Fig. 10. Heat generation rate per unit area along the contact length according to: Wright and Trent (a), and Tay et al. (b).

temperatures and temperature distribution under different cutting conditions. Similar to the analytical modelling, FEM require material flow stress data for the workpiece material as a function of strain, strain rate and temperature. However, the accuracy of the FE simulation of orthogonal cutting process is dependent upon the effects of the input friction model, chip separation and formation criterion and material data (material flow stress), material properties of the process components. Due to the combined effect of these factors, the accuracy of the cutting temperatures and temperature distribution cannot be examined independently. All other predictable process parameters; such as cutting forces, chip thickness and contact length, must be assessed and validated by the experimental results until a good correlation is achieved for the predictable parameters.

Machining process is mainly a deformation process where deformation is highly concentrated in a very small zone. However, the workpiece material flow data at the conditions encountered during machining process; at large plastic strain up to 3, temperature up to 1200 °C and strain-rate up to 10^4 1/s, are still not widely existent. This lack of complete material properties and friction parameters are a major limitation in accurate FE simulation of metal cutting.

Most of finite element simulation of the orthogonal cutting assumes a plane strain relying on the fact that cutting width is much larger than the undeformed chip thickness. Furthermore, in view of the large elastic modulus of the tool material relative to that of the workpiece, perfect rigidity of the cutting tool is commonly assumed. This is rarely the case but this approximation is quite reasonable, since the elastic deflection of the cutting tool is small compared to the large plastic deformations of the workpiece material.

The contact phenomenon in metal cutting is associated with very high conditions of pressure, strain rate and temperature. The state of technology for interactions at the tool-chip interface is mainly based on assumptions, not on scientific understanding of the underlying physics.

Based on the review of the above studies, it can be concluded that finite element work done so far has not successfully modelled the machining process. The uncertainty in FE modelling of machining is illustrated by inaccuracies in the following aspects: mesh and boundary conditions, material property data, friction properties at the tool-chip interface, temperature consideration and chip separation mechanism.

To summarise: there is no general agreement on various modelling issues in conventional machining. The problem becomes even more complex in high speed machining where material properties, heat sources, and heat partition become dependent on temperature and cutting speed.

6. Numerical modelling of high speed machining

In metal cutting process, the nature of the deformation process in the primary and secondary deformation zones is quite different. In the primary deformation zone, the chip and the work material should be considered as one body. The deformation is a material flow process in which a certain amount of the work material continuously flows through the shear plane to form the chip. On the other hand, in the secondary deformation zone, the chip and the tool are two different bodies in relative motion. This fact needs to be emphasised because it highly influence the heat partition phenomena at the chip-work and tool-chip interfaces.

As mentioned above, cutting metals at high speeds results in highly localised temperatures, stresses and strain rates resulting in a decrease in chip thickness and a corresponding decrease in cutting forces. Under such conditions it is obvious that the assumption of plane-strain deformation of the work material during conventional orthogonal cutting simulation for continuous chip formation cannot be used in HSM. The mechanical behaviour of metallic materials at conventional cutting speeds should not be directly transferred to the high-speed region. In HSM, material behaviour during the deformation process at high strains is characterized by increased strain sensitivity, by adiabatic nature of the deformation process and by increasing effects of mass inertia forces [67]. The material flow characteristics, or flow stress (the resistance of the workpiece material to cutting, which is always considered as the major mechanical characteristic of the work material), are highly affected by the temperature increase that is associated with the deformation process in HSM. The flow stress reaches a maximum at certain stress value. Any further drop in flow stress strongly influences the deformation process leading to a deformation localization combined with heat concentration in very small region [67]. The material data for the severe deformation conditions that exist in HSM are not widely available. This is probably due to the unavailability of the necessary experimental setup. However, the available data are mainly obtained by using impact compression tests for various materials at the moderate deformation rates. For example, the strain rates in the standard tensile tests and the rapid impact type tensile test are 10^{-3} and $10^3\ s^{-1}$, respectively, which is considerably below than even those of conventional machining [68]. In addition, it is important to mention that at strain rates greater than $10^4\ s^{-1}$ the role of material internal forces cannot be neglected [68]. Therefore, there is a strong need to obtain the material data for wide range of strain rates and temperatures for the numerical simulation of HSM process.

As shown above, due to the adiabatic nature of the HSM process, temperatures at the tool-chip interface could be as high as 1200 °C in a very short time. This will considerably affect the properties of tool and workpiece materials. The thermal properties of both the cutting tool and the workpiece, such as thermal conductivity, specific heat cannot be expected to remain constant when the temperature difference within the cutting process is more then 1000 °C. Therefore, the simulation of the HSM must be considered as an iterative process and should incorporate variable thermal properties for the workpiece and tool materials.

Another difference of great importance between the conventional and high speed machining is the physical contact phenomenon and the friction characteristics at the tool-chip interface. Nevertheless, it is undoubted that the friction model at the tool-chip interface in metal cutting, especially in HSM, plays a vital role in influencing the modelling process and the accuracy of the predicted cutting forces, stresses, temperatures and temperature distribution. In the earlier works on FE modelling of conventional machining, the frictional condition at the interface is often neglected or assumed to be constant and usually a constant Coulomb's friction, along the tool-chip contact area is used. This is mainly due to the difficulty in implementing accurate friction and contact conditions which arises from the fact that the contact phenomenon and friction parameters are influenced by different factors such as cutting conditions, especially the cutting speed, and tool geometry, etc. However, it is will known that Coulomb's Law is capable of describing only friction effects between effectively rigid bodies and gross sliding of one body relative to another which is not the case in HSM. For HSM, it would appear that using Coulomb's frictional model will lead to an overestimated shear friction stress far beyond the local yield shear stress.

HSM is characterized by high chip velocity, friction and temperature at the tool-chip contact area and consequently high tool wear rate. This clearly means that the steady state conditions may not be reached in HSM process. It has been reported that the contact phenomenon changes from conventional to high speed machining. While sliding friction is common in conventional machining, in HSM seizure takes place and sticking contact length is the mode of heat transfer [69]. Also, both sticking and sliding has been reported to occur simultaneously at the tool-chip interface. Though there has been difference of opinion regarding the ratio of plastic to elastic contact lengths. As a consequence, the mean friction stress and friction coefficient on the tool rake face is expected to differ substantially according to the contact phenomenon present at the interface. Therefore, under this unique tribological phenomenon it is clear that for HSM modelling and simulation, it is not possible to use the empirical values of the coefficient of friction found from ordinary sliding test conditions.

Different approaches have been taken to tackle the problem including applying a constant shear friction at the entire chip-tool contact, a constant shear friction in the sticking region and Coulomb's friction in sliding region, variable Coulomb friction at the entire contact area, variable friction in sliding region and variable shear friction at the entire tool-chip contact [70]. However, there is no general agreement on a friction model at the tool-chip interface in conventional machining.

Another added complexity is the use of coatings on cutting tools where very limited knowledge is available in regard to the friction behaviour of various hard and soft coatings under metal cutting conditions. The problem becomes more complicated in HSM due to the extreme conditions of deformation of the work material under locally high hydrostatic pressure. Obviously, more research is needed to develop more realistic and accurate friction model for HSM. The prediction of friction model for HSM needs to be dependent mainly upon the cutting conditions, especially cutting speed and the tool and chip materials.

Another point of great importance in the FE modelling of orthogonal cutting process is the design of the finite element mesh and the boundary conditions. Meshing the CAD models in their original forms is problematic due to overlaps, gaps and other inconsistencies in the model surfaces. Manual fixing of the model surfaces to be properly meshed is a tedious and time consuming process. The simulation package must have the capability to mesh the complex geometry of the CAD model. In general, the accuracy of the finite element analysis depends on the number of elements involved in the simulation process. However, it is worth mentioning that the efficient and accurate solution of the HSM modelling problem is not merely the number of elements and nodal points but also their placement and the individual element relationships [71]. The deformation zone within the work material is exposed to steep thermal gradients which give rise to thermal stresses in addition to the combined effect of the thermo-mechanical stresses. Therefore, a high mesh density is required especially in the regions of high deformation.

In HSM modelling, considering the accuracy enhancement close to the region of interface between the tool, chip and workpiece is essential. Adaptation of mesh in FE simulation of HSM is required in order to achieve high accuracy while maintaining local details of the deformation. Thus, frequent remeshing is necessary to ensure that the elements never become too distorted and to reduce error in the deformation zones. On the other hand, quiescent regions, less detail and accuracy are required and hence can be meshed using a comparatively coarser mesh. This means that regions of the mesh undergoing less change will be resolved at lower resolutions. However, the main difficulty with adaptive meshing in FE simulation of the HSM is that the regions of interest where refinement is necessary, i.e. size of the deformation zone within the work material and the heat affected zone within the cutting tool, are not precisely known a priori. In terms of the element size, the narrow shear bands formed in HSM process requires an element sizes of the order of 1 μm or less in order to resolve the generated stress and strain gradients and model stable tearing under elastic–plastic conditions [72]. As large plastic strains, without volume change, are expected during the simulation of the process, elements with additional degree of freedom are preferable. It may be worth mentioning that in general, better results and greater accuracy could be achieved with higher order elements, however these elements require more computational time.

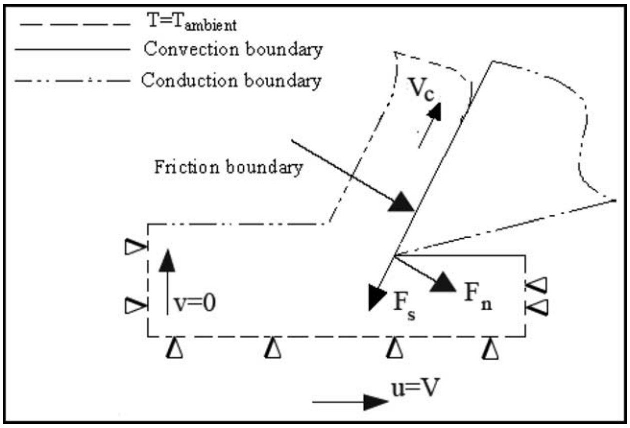

Fig. 11. Thermal and elastic–plastic boundary conditions in FE simulation.

Another added complexity to the modelling and simulation of HSM is the meshing of the coating layers when coated tools are considered. For modelling the coating layers of the coated tools, using a single solid element through the thickness of each coated layer would generate elements with large aspect ratios and would lead to a high mesh density. Also having a single solid element through the thickness would produce poor temperature gradients. Therefore, shell elements should be used. The main benefits of using shell elements for thin walled applications are: an increase in computational speed, very thin aspect ratios and with the layers it is simple to incorporate material or orientation changes through the thickness. In addition, these elements also have an option that allows them to be meshed directly to solid thermal elements. However, when shell elements are used, it is important to ensure that the temperatures obtained within the elements can be

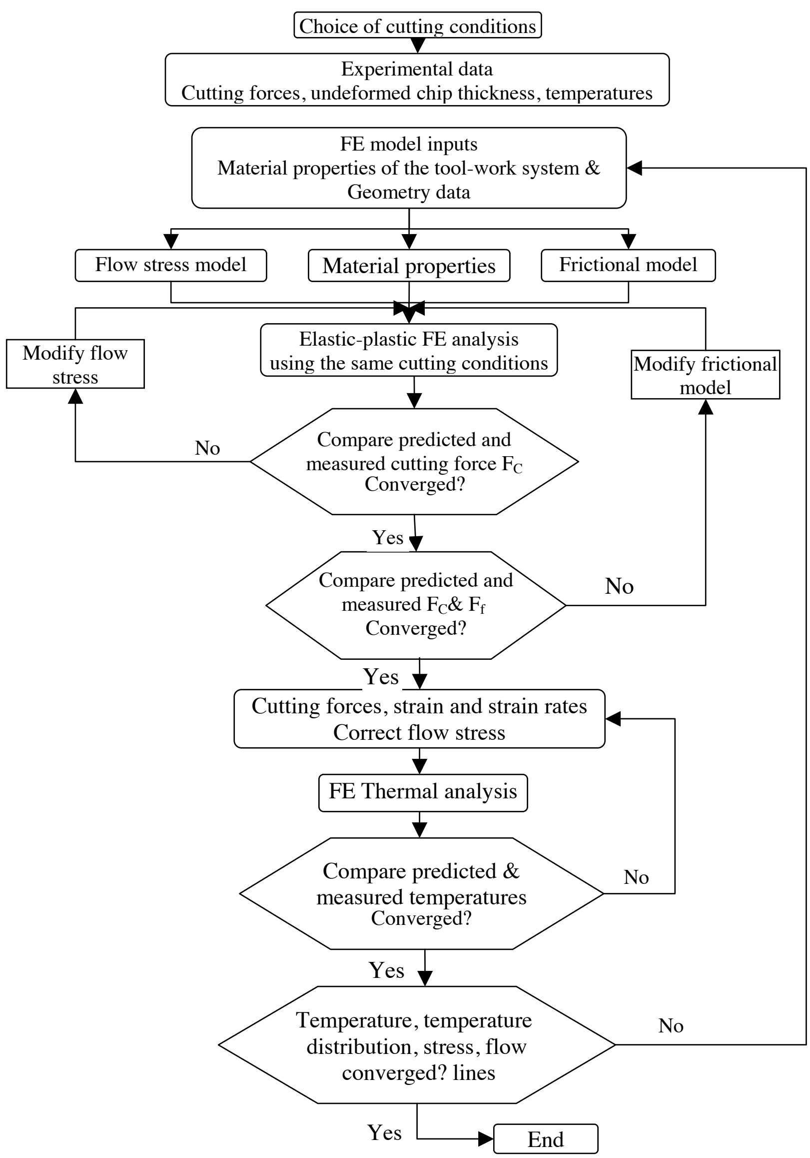

Fig. 12. Flow chart for computer simulation of orthogonal machining process for prediction of the tool-chip interface temperature and temperature distribution.

transferred directly to the surrounding structural elements. In terms of the thermal boundary conditions, the pressure at the contact interface is very high; therefore, heat transfer at the contact is thermally perfect. The other boundary conditions are shown in Fig. 11.

To summarize, FE modelling and simulation of large deformations, high temperatures, and high stress and strain rates in HSM processes requires a coupled thermal and elastic–plastic analysis. The precision of such simulation can be enhanced by considering the influences of the non-linear behaviour of the material as well as the large deformation and the dynamic contact conditions. The finite element model for the analysis of the HSM process must account for dynamic effects, heat conduction, mesh-on-mesh contact with friction, and full thermo-mechanical coupling. In addition, a fracture model which allows arbitrary crack initiation and propagation in the case of shear localized chips is necessary. The FE software to be used for HSM simulation needs to have several types of analysis capabilities including; static, dynamic, buckling and crack extension, as well as, material non-linear and geometric non-linear behaviours.

It needs to be emphasised that the material properties are temperature dependent and thus not constant. As an example, it is not logical to assume that the thermal conductivity of the workpiece remains constant during the process. This introduces a high source of uncertainty in the process. For accurate process modelling these uncertainties must be taken into account by considering the thermal effects on input material properties. FE modelling of HSM requires a friction model that accounts for the non-local and non-linear frictional presents in the contact between metallic bodies. However, it can be said that FE modelling results are only as good as the material and frictional models used and never better. A methodology for FE simulation of HSM process is proposed in Fig. 12. The precision of the simulation results can be determined by comparing them to the experimentally measured parameters of the process. Calibrating the numerical simulation in terms of, the cutting forces, the chip thickness, size of deformation zones and the temperatures is a fundamental requirement for the evaluation of the model, as well as, for any further work based on simulation.

7. Conlusions

This paper reviews the temperature measurement methods and the analytical and numerical models for the prediction of temperature and temperature distribution in metal cutting. Based on this investigation, the following general statements can be made:

Prediction of cutting temperatures is a major challenge in metal cutting. This is due to numerous practical difficulties involved in the process. However, for temperature measurement of the high speed cutting process the most promising candidates are the fibre-optic pyrometers and infrared thermography techniques. Compared to other methods these techniques could measure temperature, as well as, the cooling rate easily, accurately and with fast time response. In addition, they enable the prediction of the transient behaviour during the warm up period, as well as, the cooling period. However, design of higher magnifying and resolution systems capable of predicting temperatures at the cutting zone on a micro scale is a major challenge. The lack of emissivity values for different materials is a major limitation in the use of the infrared thermography techniques. Specifically, emissivity values of hard and soft coatings deposited on the cutting tools are very limited.

Review of the works on analytical modelling of the metal cutting process show that these models suffer mainly due to the simplified assumptions. However, the superiority of the numerical techniques over the analytical solutions can be easily established.

Due to the complexity and the severity of the deformation in HSM process, it is clear that purely analytical approaches are severely restrictive. However, as the deformation zone is non-analytic and non-linear, it is essential to have confidence in any proposed numerical model, which relies on a limited representation of the deformation shape.

In metal machining, the workpiece material can be considered as a material for which the flow stress of the workpiece is a function of strain, strain rate and temperature. Therefore, the analysis and finite element simulation of the machining process requires the coupling of the metal flow and heat transfer.

While FEM has been successfully applied for two-dimensional analysis of the machining process with reasonable accuracy, the transition to three-dimensional analysis is still problematic.

The inability to validate existing conventional machining simulation models and extend them to HSM with confidence is a main problem.

Modelling the extreme conditions presented in the HSM process, which includes highly inhomogeneous plastic flow and thermal fields, complex contact conditions and high temperatures and pressures, is extremely difficult.

The state of technology for consideration of interactions at the tool/chip interface is mainly based on certain assumptions and not on a precise understanding of the underlying physics.

FE modelling and simulation of machining processes is mainly suffering from a lack of the fundamental input data. Therefore, further effort is required to provide a clear, consistent, well measured and relevant data on material behaviour under conditions of interest. In addition, more research is required to develop realistic and accurate friction models for the tool-chip interface.

One of the very important findings of this paper is that accuracy and reliability of HSM machining simulation models is crucially limited by uncertainties in material friction models. Therefore, more research is needed to develop numerical models to predict the contact geometry and separating contours between the sticking and sliding zones. Furthermore, ongoing research on the investigation of the relationship between the tool-chip contact phenomena and geometry with other process parameters should be incorporated into machining simulation researches. These models tend to minimise the uncertainties associated with the FE models for HSM simulations.

Benchmark information, which clearly defines the ability of FE codes to accurately predict machining process performance is missing. Therefore, there is a need to assess and benchmark the predictive capabilities of the currently available FE codes for the determination of conventional and HSM process parameters. This will help in establishing the direction and focus for future research efforts. However, due to the complexity of the process and inability to validate existing models, an experimental validation is still the only available route to assess FE simulation of the machining processes.

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