Proceedings of the
International Conference on Mechanical Engineering 2007
(ICME2007) 29- 31 December 2007, Dhaka, Bangladesh

ICME2007-AM-35

ROLE OF HIGH-PRESSURE JET COOLING ON CUTTING TEMPERATURE IN TURNING STEEL

A.K.M. Bashirul Khoda[1] and N. R. Dhar[2]

[1]Assistant Professor and [2]Professor
Department of Industrial & Production Engineering
Bangladesh University of Engineering & Technology, Dhaka, Bangladesh
Email: nrdhar@ipe.buet.ac.bd

ABSTRACT

The present work deals with development of mathematical model of temperature distribution in chip-tool-work piece interface in turning of medium carbon steel under high-pressure coolant (HPC) condition and subsequently verify it with experimental investigation. Continuous or steady state machining operations like orthogonal cutting are studied by modeling the heat transfer between the tool and chip at the tool rake face contact zone. The shear energy created in the primary zone, the friction energy produced at the rake face-chip contact zone and the heat balance between the moving chip and stationary tool are considered.

To determine the temperature distribution in metal cutting is very laborious and time intense through experiment. By using FEM, the temperature distribution in chip-tool-work piece interface can be achieved very accurately. The mathematical model for cutting temperature has been developed and MSC Nastran / Patran simulation software was used to illustrate the temperature distribution. The results were then verified with the experimental data for dry machining. As the results satisfy with acceptable margin, the model then applied for high-pressure coolant condition to find out the temperature distribution in chip-tool-work piece interface. The mathematical models and simulation results are in satisfactory agreement with experimental temperature measurements reported in the literature.

In high-pressure coolant condition the average cutting temperature reduced by 16% than dry condition. The model provides reasonably acceptable results in terms of deviation from actual result, with 5% deviation for temperature model. Thus the model proves its validity. As the temperature at the chip-tool interface is one of the two most important factor influencing the machining process, so high-pressure coolant condition is complimentary for machining process.

Keywords: Turning, Steel, HPC, Temperature and FEM

1. INTRODUCTION

The growing demand for higher productivity, product quality and overall economy in manufacturing by machining, particularly to meet the challenges thrown by liberalization and global cost competitiveness, insists high material removal rate and high stability and long life of the cutting tools. However, high production machining with high cutting velocity, feed and depth of cut is inherently associated with generation of large amount of heat and high cutting temperature. Such high cutting temperature not only reduces dimensional accuracy and tool life but also impairs the surface integrity of the product. Longer cuts under high cutting temperature cause thermal expansion and distortion of the job particularly if it is slender and small in size, which leads to dimensional and form inaccuracy. On the other hand, high cutting temperature accelerates the growth of tool wear and also enhances the chances of premature failure of the tool by plastic deformation and thermal fracturing. The surface quality of the products also deteriorates with the increase in cutting temperature due to built-up-edge formation, oxidation, rapid corrosion and induction of tensile residual stress and surface micro-cracks. Such problem becomes more acute and serious if the work materials are very hard, strong and heat resistive and when the machined or ground part is subjected to dynamic or shock loading during their functional operations. Therefore, it is essential to reduce the cutting temperature as far as possible.

Conventionally employed cutting fluid fails [1] to penetrate the chip-tool interface and removes heat by bulk cooling [2]. Addition of even extreme pressure additives fails to provide desirable lubrication [3]. Thus, cutting fluids cannot reduce cutting temperature and tool wear that effectively [4]. However if high pressure jet of water soluble cutting fluid is applied at the chip-tool interface, it can reduce friction and temperature to some extent and enable the reduction cutting forces and improve tool life [5].

The machining temperature can also be reduced to some extent by improving the machinability characteristics of the work material by optimizing the process parameters and tool geometry [6]. Proper selection and use of special tools like cBN may also help in reducing cutting temperature and associated problems [7].

However, conventional cutting fluids are still widely used. The major problems of conventional cutting fluid application are [8] (a) environmental pollution due to chemical break-down of the (b) cutting fluid at high cutting temperature (c) biologically hazardous to operator due to bacterial growth (d) requirements of additional system for pumping, local storage, filtration, recycling, chilling and large space and (e) water pollution and soil contamination during final disposal.

Some alternative methods have been studied in an attempt to effectively control the cutting zone temperature. Friction and temperature at the work-tool interface and the cutting forces were found [9] to considerably decrease when steels are machined by carbide inserts tribologically modified by depositing soft bearing materials on their working surfaces. However at higher cutting speed the lubricating layers were depleted very fast. Application of CO2 in the form of liquid (-79°C) jet under high pressure (60 bar) showed some success, if used properly, in the case of some specific work-tool materials [10]. High pressure coolant injection techniques reportedly [10] could reduce cutting fluid consumption by more than 50 percent as well as cutting forces, tool wear and surface roughness to some extent. Some work has recently been done [12,13] on high-pressure coolant jet in machining some commonly used steel. Compared to dry and wet machining with conventional fluid, high-pressure coolant provides much better surface integrity, lesser cutting forces and longer wheel life, though in different degrees for different steels, mainly through reducing temperature, preventing wheel loading

The concept of high-pressure coolant presents itself as a possible solution for high speed machining in achieving slow tool wear while maintaining cutting forces/power at reasonable levels, if the high pressure cooling parameters can be strategically tuned. It has the benefits of a powerful stream that can reach the cutting area, it provides strong chip removal, and in some cases, enough pressure to deburr. High-pressure coolant injection technique not only provided reduction in cutting forces and temperature but also reduced the consumption of cutting fluid. The aim of the present work is primarily to explore and evaluate the role of high-pressure coolant on machinability characteristics of commonly used tool-work combination mainly in terms of cutting temperature and chip-forms, which govern productivity, product quality and overall economy.

2. EXPERIMENTAL INVESTIGATIONS

Continuous turning of medium carbon steel bar was carried out on a heavy duty lathe, 10 hp under dry and high-pressure coolant (HPC) conditions. The coolant jet was applied at the cutting zone along the auxiliary cutting edge at a pressure 70 bar as shown in Fig. 1. Cutting temperature has been measured by a tool-work thermocouple technique with due care to avoid generation of parasitic emf and electrical short circuit. The cutting forces were monitored by a dynamometer and recorded in a PC. The conditions of the present experiments are given in Table-1.

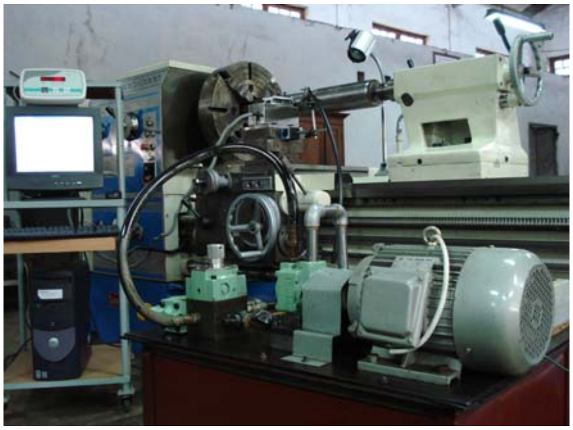


Fig 1: Photographic view of the experimental set-up

Table 1: Experimental conditions

Machine tool	: Lathe Machine (China) 10hp
Work material	: Medium carbon steel (φ176 X 580 mm)
Cutting tool	
Cutting insert	: SNMM 120408, Sandvick
Tool holder	: PSBNR 2525M12
Working geometry	: -6°,-6°,6°, 15°, 75°, 0.8 (mm)
Process parameters	
Cutting velocity	: 93, 133, 186 and 266 m/min
Feed rate	: 0.10, 0.14, 0.18 and 0.22 mm/rev
Depth of cut	: 1.0 mm
High-pressure coolant	: 80 bar, Coolant: 6.0 l/min through external nozzle
Environments	: Dry and High-pressure coolant

Fig.2 shows the calibration technique employed for the thermocouple used in the preset investigation. The work-tool thermocouple junction was constructed using a long continuous chip of the work-material and a tungsten carbide insert to be used in actual cutting. To avoid generation of parasitic emf, a long carbide rod was used to extend the insert. A graphite block embedded with a electrically heated porcelain tube served as the heat sink. A chromel-alumel thermocouple was used as a reference in the vicinity of the thermocouple for measuring the temperature of the graphite block. The junction temperature measured by the reference thermocouple was recorded using a temperature controller (Eurotherm, UK) while, the emf generated by the tool work thermocouple was recorded by a digital multimeter.

Fig.3 shows the calibration curve obtained for the tool-work pair with tungsten carbide (P30 Grade, WIDIA) as the tool material and medium carbon steel as the work material. In the present case, almost linear relationship is obtained between the temperature and emf. A multiple correlation coefficient of 0.994 was obtained.

Fig 2: Tool-work thermocouple calibration set up

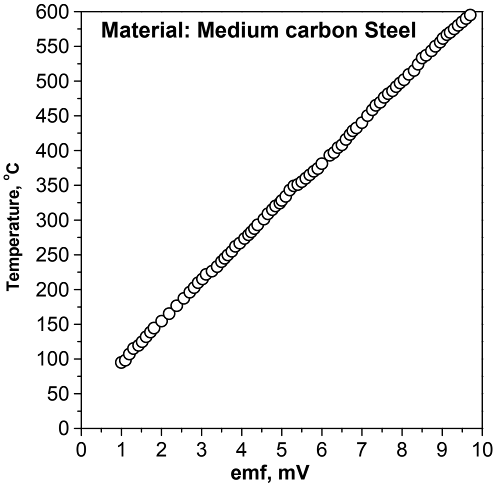


Fig 3: Tool-work thermocouple calibration curve

3. COMPUTATIONAL EVALUATION OF MACHINING TEMPERATURE

Under both dry and high-pressure coolant conditions, the average chip-tool interface temperature could be effectively measured very reliably throughout the experimental domain. However, the distribution of temperature within the tool, work and chip cannot be determined effectively using experimental techniques. This necessitated development of a finite element model of machining temperature, its validation using present experimental results and finally determining complete temperature distribution in the tool, work and chip under both dry and high-pressure coolant environment.

Earlier researchers [14-16] used Jeager's model of moving heat sources and block partition principles to estimate average temperature at the shear plane and at the chip-tool interface. However, this model could not take into account variation in thermal properties of work and tool material with temperature, the elastoplastic nature of chip-tool interaction, work-tool interaction at the wear land in flank, etc. The aforesaid shortcomings of analytical approach were overcome [17] by using finite element modeling of machining temperature. The refinement of the FEM models took place over the years [18-21] with increasing reliability. Three dimensional FEM models are marginally more accurate than two dimensional models, but are complex to develop and require more computational effort [18-21].

In machining, where the chip thickness is much smaller than the chip width, the problem is reduced to a two-dimensional steady-state heat transfer [19, 20]. In the present work a two dimensional model has been developed for computational evaluation of temperature distribution in turning tools for which the heat developed at the auxiliary flank has been neglected. In machining, heats is generated primarily in three different zones, namely primary shear plane, chip-tool interface and wear land of the principal flank. The mass and heat transport are governed by formation and separation of the chip, speed of the work materials and heat conduction and convection. The two-dimensional section at which the temperature distribution is calculated includes three regions: one is the workpiece moving at the cutting velocity, the second is the chip moving at the bulk chip velocity and the third is the tool which is stationary. The basic equation to be solved for the temperatures of this domain is of the type:

$$\rho c_p(T)\left(u\frac{\delta T}{\delta x}+v\frac{\delta T}{\delta y}\right)-k(T)\left(\frac{\delta^2 T}{\delta x^2}+\frac{\delta^2 T}{\delta y^2}\right)=\dot{Q} \quad \text{..............} \quad (1)$$

where ρ is the mass density, c_p r is the specific heat, k is the thermal conductivity, $\dot{Q}$ is the internal heat generation rate, u & v represent the velocity components in the x and y directions and T is the temperature. The workpiece, chip and insert domain are discretized for analyzing them for the steady-state heat transfer using finite element analysis. The problem domain is defined with three types of material. Starting from the shear plane, in the downstream direction, the whole length of the chip is treated as one type of material, the workpiece is treated as a second type of material and the insert is treated as the third type of material. This facilitates the specification of different mass flow rates. The velocity of flow of the chip has been treated as uniform throughout and is used to specify the mass flow rate in the chip region. The mass flow rate in the workpiece region is specified using the cutting velocity. These conditions would make the solution procedure simulate the thermal phenomenon in the cutting process more or less close to the actual cutting conditions with specified assumptions.

For all the cases, the shear plane and the chip-contact length were divided into 14 elements and the wear land was divided into 3 elements as shown in Fig.4. Since the chip-contact length varied with the cutting speed and feed, the element size for all the cases were not the same.

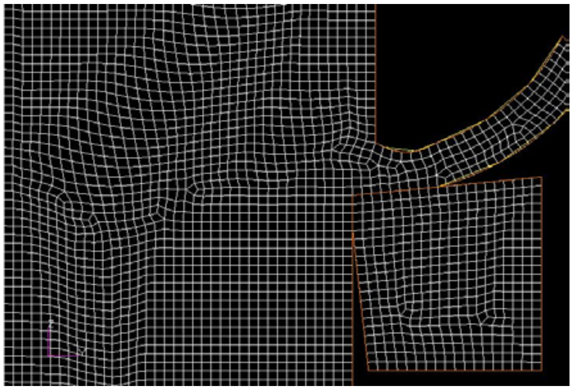
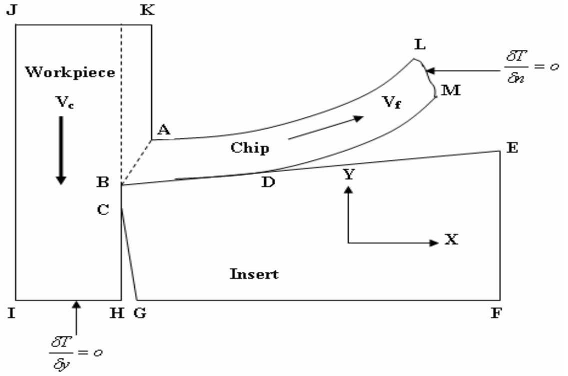


Fig 4: Meshing used for Finite Element Model (FEM)

The dimensions of the work part are so selected that the far boundaries are almost negligibly affected by the sources of heat. A constant gradient condition is imposed on these boundaries.

The length of the chip on the down stream side, from the point of separation from the shear plane has been taken to be equal to 12 mm. At such a distance from the frictional heat source, the temperatures in the chip are found to stabilize in the flow direction. Thus, a zero thermal gradient condition could be specified at the outlet boundary of the chip. All the surfaces exposed to air are assumed to be insulated as the heat loss through these surfaces is negligibly small.

In the domain of the cutting insert, some surfaces will conduct the heat into the tool holder. A constant temperature (200°C) boundary condition has been imposed at all the nodes on these surfaces [6].



Boundary	**Dry condition**	**HPC**
IJ &JK	ambient temperature boundary	ambient temperature boundary
KA, AL,MD & DE	insulated boundary	convective boundary
EF & FG	200^{O}C temperature boundary	60°C temperature boundary
GC & CH	insulated boundary	insulated boundary

Fig 5: Problem regions showing thermal boundary conditions

In dry cutting conditions, the boundary condition as shown in Fig.5 can be categorized into three distinct groups, namely STα, where the temperature is known to be ambient temperature (40°C), ST, where the temperature is assumed to be a constant T, and So , which is assumed to be thermally insulated. T is set to 200°C for dry cutting. A zero temperature gradient $\left(\frac{\delta T}{\delta n} = o \text{ and } \frac{\delta T}{\delta y} = o\right)$ is imposed on LM and HI and the heat fluxes for AB, BD, and BC are given by the determined heat-generation rates along AB, BD, and BC respectively. Therefore, the following applies to the classification of the boundary for dry cutting conditions:

$$\left.\begin{cases} i. \quad S_{T_\alpha} \in IJ, JK \\ ii. \quad S_T \in EF, FG \\ iii. \quad S_n \in KA, AL, MD, DE, GC, CH \end{cases}\right\} \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (2)$$

In high-pressure coolant conditions, the boundary as shown in Fig.5 can be categorized into four distinct groups, namely STα , where the temperature is known to be ambient temperature (40°C), ST, where the temperature is known to be a constant TN (TN should be set to the cutting oil temperature 40°C for high-pressure) Sn, which can be assumed to be thermally insulated and Sh, which is assumed to be convective heat transfer boundary by introducing cooling heat-transfer coefficients (0.02 W/mm2) with temperature 40oC into the boundary conditions.. The convective heat transfer coefficient has been calculated assuming the flow of cutting oil over a flat plate maintained at a particular temperature [22]. The thermo-fludic properties of the cutting oil have been taken from the handbook. The heat fluxes for AB, BD, and BC are given by the determined heat-generation rates along AB, BD, and BC respectively. The surface HI has been assumed to have a zero temperature gradient$\left(\frac{\delta T}{\delta y} = o\right)$. The thermal conditions at the boundaries of the solution region are thus, in the most general terms, are:

$$\left.\begin{cases} i. \quad S_{T_\alpha} \in IJ\ , JK \\ ii. \quad S_T \in EF\ , FG \\ iii. \quad S_n \in GC\ , CH \\ iv. \quad S_h \in KA\ , AL\ , LM\ , MD\ , DE \end{cases}\right\} \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (3)$$

Furthermore, by merging the boundaries of the tool and workpiece on the chip-tool interface BD and tool-work interface BC, it is implicitly assumed that the tool rake and the chip face are subjected to the same temperature in that area. This is justified by the fact that high normal contact stress on the chip-tool interface produces only negligible thermal resistance.

The variation of work material properties with temperature for different work materials has been considered in the present model [23]. The mass density, thermal conductivity and specific heat of the work material used in the analysis are given in Table-2. It was assumed that within an element, *k* and *c* r were constant at values corresponding to the average temperature of the element. As a consequence of this temperature dependence, it was necessary to perform the calculations iteratively. The thermal properties of the tool material were taken to be temperature independent as given in Table-3.

Table 2: Thermal properties of the work materials

Work material	**Mass density, ρ (Kg/mm^3)**	**Thermal conductivity, k (W/mm-K)**	**Specific heat, c_p (J/Kg-K)**
Medium carbon steel	$7.2X10^{-06}$	$k=0.052-1.9X10^{-5}\ T$	$c_p=420+0.66\ T$

Table 3: Thermal properties of tool materials

Tool material	**Mass density, (Kg/mm^3)**	**Thermal conductivity, k (W/mm-K)**	**Specific heat, c_p (J/Kg-K)**
SNMM 120408 TTS	$12 X 10^{-06}$	0.047	251.00

The knowledge of the normal and tangential stress distributions on the tool rake face is essential for the analysis of temperature distributions in the cutting tool. The magnitude and distribution of the frictional forces involved in a cutting process largely controls the temperature distribution over the rake surface.

Previous investigations suggested that [24-26] both the normal stress and the tangential stress remained uniform up to the plastic contact portion of the chip tool contact area and from there linearly reduced to zero at the point of separation of the chip from the rake surface, as shown in Fig.6. Though there has been some difference of opinion regarding the apportionment of plastic to elastic contact length, in this work, the plastic contact length is considered as 60 percent of the total contact length [27].

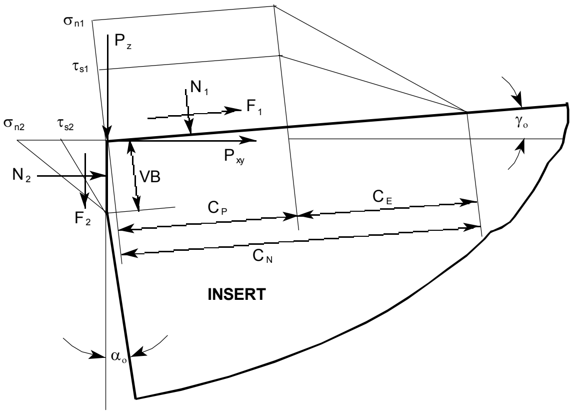


Fig.6:Stress distribution along the chip-tool and work-tool interface.

In the actual machining conditions, the tool never remains perfectly sharp and a small wear land almost always exists on the flank surface. Considering such a wear land would always make the analysis represent more realistic conditions. A wear land of length of 0.2 mm [6] has been taken in all the cases studied in this work. Based on the experimental results reported [19] earlier, the frictional force on the wear land is assumed as 20N and the normal force as 40N. A triangular distribution is assumed for these forces over the wear land as shown in Fig.6.

The chip velocity at the vicinity of the rake surface is usually less than the bulk chip velocity, V_f due to drag force. The best approximation of such drag-back is to assume the velocity at the tool tip to be $V_f /3$ and then increasing to V_f with uniform acceleration within half the contact length [17]. However in the present analysis such effect has been neglected and the chip velocity has been taken to be uniform through the chip contact length.

The chip-tool contact length is difficult to measure accurately, it can be assumed to be equal to a constant multiple of either the chip thickness [17] or product of the chip thickness and tool-chip friction coefficient. Conventionally, the chip-tool contact length C_N, for given rake angle and feed becomes almost proportional to a_2 as

$$C_N = a_2\left[1+\tan(\beta-\gamma)\right] \qquad (4)$$

where, β is the shear angle.

In machining, the major portion of the deformation energy gets converted into heat and a small portion remains frozen in the strained chips. The percentage of such frozen energy gradually decreases with the increase in cutting velocity. Hence, it may be assumed that the entire work done during the deformation process gets converted into heat. Further, in the present work exothermic oxidation of the chips has also been neglected. Therefore, for the present analysis, the total heat generation, Q and the heat generation at the rake surface, Q_f per unit time have been evaluated as,

$$Q = P_z.V_c \ \ and \ \ Q_f = F_1.V_f \qquad (5)$$

where, F_1 is the total frictional force on the rake surface which could be determined from the known values of the tangential force P_z and the axial force P_x by experimental force measurement.

The rate of heat generated in the shear zone Q_s is therefore,

$$Q_s = Q - Q_f \qquad (6)$$

Q_S is assumed to be uniformly distributed over AB (Fig.3.1) both under dry and high-pressure cooling condition and specified over a strip of nodes along the shear plane. The total heat generated per unit time, Q_W due to friction at the flank wear land has been evaluated from,

$$Q_w = F_2.V_c \qquad (7)$$

where, F_2 is the total frictional force at the wear land. The distribution of rate of heat generation at the chip-tool interface, Q_f and wear land, Q_w follow the shear stress distribution pattern as shown in Fig.6.

Unlike the previous approaches using apportionment coefficient for calculating thermal loads on the cutting tool and analysis of only the tool [28, 29], the present analysis uses an integrated approach with a control volume consisting of work, tool and chip. The MSC NASTRAN/PATRAN software is used for implementing the model.

4. RESULTS AND DISCUSSION

The correlation between the average chip-tool interface temperature obtained directly by measurement and that obtained by finite element analysis under dry machining have been depicted in Fig.7. The estimated values of average chip-tool interface temperature appeared to be higher than the measured values expectedly for ignoring the part of the cutting energy that remains frozen in the chips as strain energy. However, the average deviation between the estimated and measured temperature throughout the domain is around 5.4 percent which is within the acceptable range from an engineering viewpoint.

The finite element analysis provided distribution of temperature assuming steady state heat transfer in the workpiece, chip and tool. Such analyses have been carried out for all the combinations of work material-tool-V_c-S_o environment undertaken. Fig.8 and to Fig.9 are typically showing the temperature distribution in case of machining the medium carbon steel under dry and high-pressure cooling condition respectively by SNMM inserts.

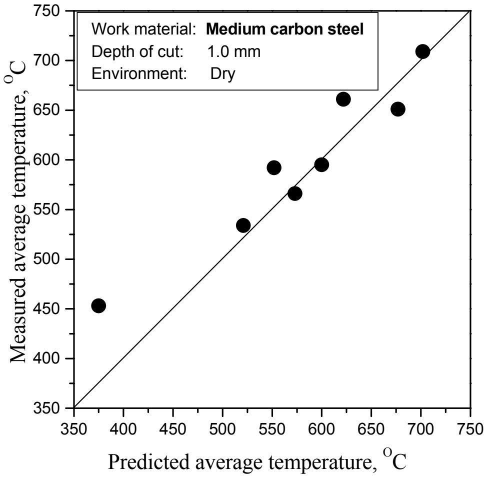
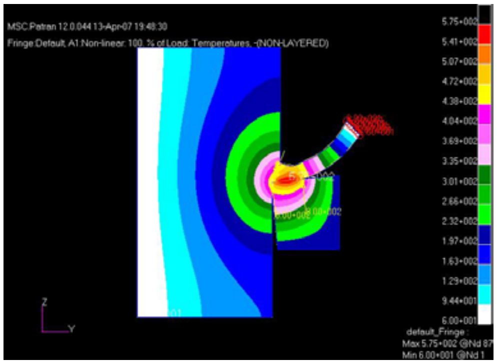
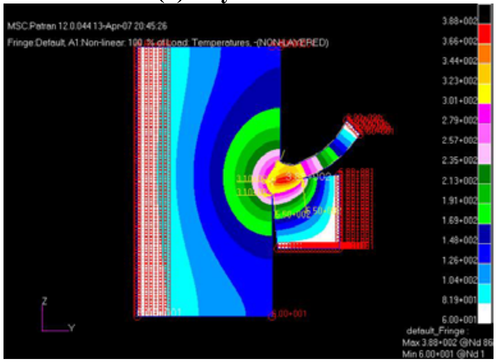


Fig 7: Comparison of measured and predicted average chip-tool interface temperature attained in dry machining

The contact side of the chip is found to be at a higher temperature as compared to the open side of the chip. Further at the wear land of the flank the increase in temperature is not very appreciable as compared to the chip-tool interface. This may be attributed to higher heat carrying capacity of the large workpiece and mostly the elastic nature of contact between the wear land and the finished work surface. The average shear plane temperature under dry machining conditions appeared to be around 300°C which is much less than that at the chip-tool interface.

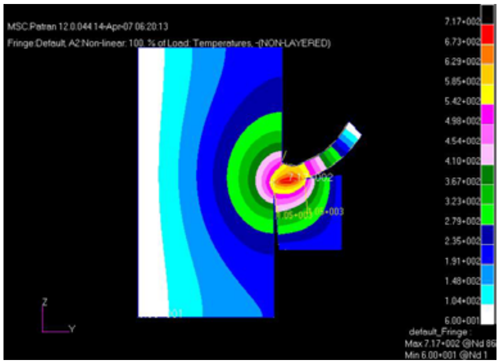


(a) Dry condition

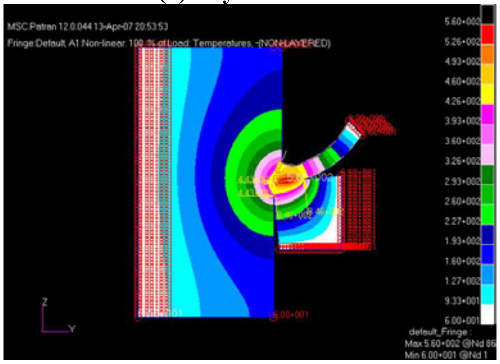


(b) High-pressure condition

Fig 8: Computed temperature distribution in chip, tool and workpiece under (a) dry and (b) high-pressure coolant condition [V_c= 93 m/min, S_o=0.10 mm/rev, t=1.00 mm]



(a) Dry condition



(b) High-pressure condition

Fig 9: Computed temperature distribution in chip, tool and workpiece under (a) dry and (b) HPC condition [V_c= 133 m/min, S_o=0.10 mm/rev, t=1.00 mm]

Fig.9 clearly show that the machining temperature significantly increased with the increase in cutting velocity and feed, though in different degrees, under all the conditions undertaken expectedly for increased energy input. High-pressure coolant has enabled a significant reduction in machining temperature, though in different degrees for different levels of process parameter undertaken. Such effects may be reasonably attributed to reduction in chip-tool contact length, reduction in forces due to restricted contact cutting effect [6] and also by enhanced heat transfer under high-pressure cooling. The benefit of high-pressure cooling has been more predominant at lower cutting velocity expectedly because at lower velocity a large portion of the chip-tool contact remains elastic in nature which is likely to allow a more effective penetration of cryogen at the interface. However, it is evident from Fig.9 that the average chip-tool interface temperature has decreased by about 20 percent to 35 percent when medium carbon steel has been machined under high-pressure cooling conditions.

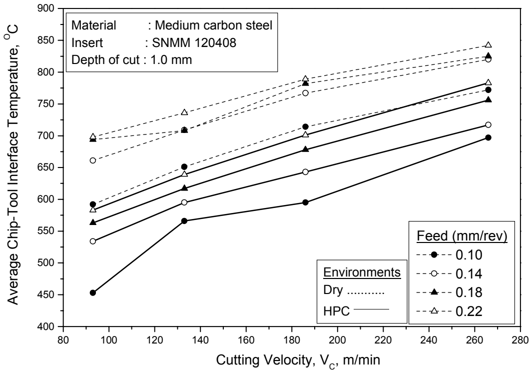


Fig 9: Variation in chip tool interface temperature with V_c at different S_o under dry and high-pressure coolant conditions

5. CONCLUSIONS

Detailed experimental study along with finite element analysis provides the following conclusions:

i. The measured chip-tool interface temperature and the predicted temperature are in close agreement with an average deviation of 5 percent, which is, thereby, validating the present two-dimensional finite element model.
ii. The FEM slightly overestimated the average chip-tool temperature especially at low speed-feed combination possibly for neglecting the residual strain energy, though small, retained in the chips.
iii. The high-pressure coolant jet provided significant reduction in temperature; the level of benefit seemed to be higher at lower feed and lower cutting velocity.
iv. The reduction in temperature is attributed to reduction in chip contact length, favorable chip-tool interaction, better chip breaking, reduced forces and enhanced heat transfer situation under high-pressure cooling conditions.

6. ACKNOWLEDGEMENT

This research work has been funded by Directorate of Advisory Extension and Research Services, BUET, Dhaka, Bangladesh. The authors are also grateful to the Department of Industrial and Production Engineering, BUET for providing the facilities to carryout the experiment.

7. REFERENCES

1. Shaw, M. C., Pigott, J. D., and Richardson, L. P., 1951, ‘‘The Effect of Cutting Fluid upon Chip-Tool Interface Temperature,’’ Trans. ASME, 71, pp. 45–56.
2. Merchant, M. E., 1958, ‘‘The Physical Chemistry of Cutting Fluid Action,’’ Am. Chem. Soc., Div. Petrol Chem., Preprint 3, No. 4A, pp. 179–189.
3. Cassin, C., and Boothroyed, G., 1965, ‘‘Lubrication Action of Cutting Fluids,’’ J. Mech. Eng. Sci., 7~1!, pp. 67–81.
4. Kitagawa, T., Kubo, A., and Maekawa, K., 1997, ‘‘Temperature and Wear of Cutting Tools in High Speed Machining of Inconel 718 and Ti-6V-2Sn,’’Wear, 202, pp. 142–148.
5. Mazurkiewicz, M., 1989, ‘‘Metal Machining with High-Pressure Water-Jet Cooling Assistance-A New Possibility,’’ ASME J. Eng. Ind., 111, pp. 7–12.
6. Singamneni, S. B., 1993, ‘‘Role of Controlled Contact Cutting on the Mechanism and Mechanics of Chip Formation and the Induced Stresses in Turning Carbide Inserts,’’ Doctoral Dissertation, Mech. Eng. Dept., IIT Kharagpur.
7. Grabner, T., and Tonshoff, H. K., 1984, *Proc. 5th ICPE*, Tokyo, pp. 326–343.
8. Ecologically Improved Manufacturing Processes at home page http://www.ifw.uni-hannover.de/BEREICH3/Forschen/310–2e.htm
9. Farook, A., Varadarajan, A. S., and Philip, P. K., 1998, ‘‘Machinability Studies on Steel Using Hard Metal Inserts with Soft Material Deposit,’’ *Proc. 18th All India Conf. MTDR*, pp. 152–155.
10. Thoors, H., and Chandrasekaran, H., 1994, ‘‘Influence of the Cutting Medium on Tool Wear during Turning,’’ Swedish Institute for Metal Research, Report No IM-3118.
11. Rashid, M. H., Dhar, N. R. and Siddiqui, A. T., “Effect of High-Pressure Coolant on Chip, Roundness Deviation and Tool Wear in Drilling AISI-1040 Steel”, Proceedings of the International Conference on Manufacturing Automation (ICMA-2007), Singapore, pp.453-463, 2007.
12. Dhar, N. R. and Kamruzzaman, M., “A Study of Effects of High-Pressure Coolant on Chips, Temperature and Cutting Force in Turning AISI-8740 Steel” Proceedings of the International Conference on Manufacturing Automation (ICMA-2007), Singapore, pp.159-168, 2007.
13. Dhar, N. R., Kamruzzaman, M. and Rashid, M. H.,

“On Effect of High-Pressure Coolant on Tool Wear and Surface Finish in Turning AISI-8740 Steel” Proceedings of the International Conference on Manufacturing Automation (ICMA-2007), Singapore, pp.695-703, 2007.

14. Hahn, R. S., 1951, “On the Temperature Developed at the Shear Plane in the Metal Cutting Processes,” *Proc. U. S. Nat. Congress of Applied Mechanics*, ASME, pp. 661.
15. Loewen, E. G., and Shaw, M. C., 1954, “On the Analysis of Cutting Tool Temperature,” Trans. ASME, 76, pp. 217–231.
16. Weiner, J. H., 1955, “Shear-Plane Temperature Distribution in Orthogonal Cutting,” Trans. ASME, 77, pp. 1331.
17. Tay, A. O., Stevenson, M. G., and Davis, G. V., 1974, “Using the Finite Element Method to Determine Temperature Distribution in Orthogonal Machining,” Proc. Inst. Mech. Eng., 188, pp. 627–638.
18. Tay, A. O., Stevenson, M. G., Davis, G. V., and Oxley, P. L. B., 1976, “A Numerical Method for Calculating Temperature Distribution in Machining from Force and Shear Angle Measurements,” Int. J. Mach. Tool Des. Res., 16, pp. 335–349.
19. Stevenson, M. G., Wright, P. K., and Chow, J. G., 1983, “Further Developments in Applying the Finite Element Method to the Calculation of Temperature Distributions in Machining and Comparisons with Experiment, ”ASME J. Eng. Ind., 105, pp. 149–154.
20. Muraka, P. D., Barrow, G., and Hinduja, S., 1979, “Influence of the Process Variables on the Temperature Distribution in Orthogonal Machining using the Finite Element Method,” Int. J. Mach. Tool Des. Res., 21, pp. 445.
21. Strenskovski, J. S., and Kyung-jin, Moon, 1990, “Finite Element Prediction of Chip Geometry and Tool and Workpiece Temperature Distributions in Orthogonal Cutting,” ASME J. Eng. Ind., 112, pp. 313–318.
22. Nashchokin, V. V., 1979, *Engineering Thermodynamics and Heat Transfer*, MIR Publisher, Moscow, USSR.
23. Rothman, M. F., 1988, *High Temperature Property Data: Ferrous Alloy*, Metal Park, OH 44073, USA.
24. Kato, S., Yamaguchi, K., and Yamada, M., 1972, “Stress Distribution at the Interface Between Tool and Chip in Machining” ASME J. Eng. Ind., pp. 683.
25. Barrow, G., Graham, W., Kurimoto, T., and Leong, Y. F., 1982, “Determination of Rake Face Stress Distribution in Orthogonal Machining,” Int. J. Mach. Tool Des. Res., 22, pp. 75.
26. Chen, N. N. S., and Pun, W. K., 1988, “Stresses at the Cutting Tool Wear Land,” Int. J. Mach. Tools Manuf., 28, No. 2, pp. 79.
27. Stephenson, D. A., and Agapiou, J. S., 1997, *Metal Cutting Theory and Practice*, New York, pp. 425.
28. Bhattacharyya, A., 1965, “On the Friction Process in Metal Cutting,” *Proc. 6th Int. Conf. on MTDR*, Manchester, UK, pp. 491–505.
29. Lo Casto, S., Lo Valvo, E., and Micari, F., 1989, “Measurement of Temperature Distribution within Tools in Metal Cutting. Experimental Tests and Numerical Results,” J. Mech. Work. Technol., 20, pp. 35.