

# Remote sensing for on-line temperature estimation in machining: A basic framework

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## Industrial Summary

Remote thermocouple sensing (RTS) is a technique that utilizes analytical and experimental evidence to create models for temperature sensing, thus integrating the advantages of both approaches. The remote solution is an inverse problem of predicting the source based on some remote location temperatures. An accurate forward solution should be available that determines the relation between the source and sink, for estimation of the inverse relationship. Extensive simulation has to be performed on the forward solution, thus establishing a need for fast and accurate computation.

A mathematical formulation for RTS is done using an elliptic partial differential equation. Boundary conditions are formulated to simulate the actual machining situation. Some of these are derived by placing an insulator between the tool and tool holder. For inverse solution, three thermocouples are used for enhancing the accuracy of source prediction. Issues such as steady-state machining, and location of thermocouples are discussed. Overall, a basic framework for RTS in machining is presented.

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## 1. Introduction

In the early years of the twentieth century, it was recognized that temperatures affect tool life. Temperatures depend on tool wear and vice versa for most tool-work combinations. Study of temperatures has hence posed a big challenge to researchers in machining. Several models, analytical and experimental, have been created in the last 70 years. Temperature sensing and

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modelling has stimulated renewed interest in the last 15 years due to the advent of adaptive control, tool wear monitoring, new material development (for work and tool) and multiple sensing. Capability of sensors to predict tool wear on-line using practical, feasible and inexpensive methods is highly desired.

An extensive literature search was conducted to study the various techniques that were used in the past to measure the temperatures in the cutting tool and at the chip-tool interface. It was found that totally analytical models typically lack experimental detail while empirical models generally work only in specific situations.

## **2. Temperature measurements in machining**

One of the early techniques used for temperature determination in metal cutting was done through calorimetry [1]. Other common experimental techniques involve the use of a tool-work thermocouple [2,3], an embedded thermocouple [4,5], thermovision [6,7] and metallographic and microhardness techniques [8–10]. Some of these techniques modified the cutting process for sensing and rendered the process impractical, whilst others required the access of cutting interfaces and hence were restricted to off-line or post-mortem analysis. Overall, all experimental techniques utilized thus far have altered the process significantly and have worked only in specific situations.

Analytical techniques for heat- and temperature-prediction were pioneered by Trigger and Chao [11], this work being followed up by Loewen and Shaw [12] and Boothroyd et al. [13]. The availability of faster computers stimulated interest in the use of numerical techniques in the seventies and eighties. Tay et al. [14] and Stevenson et al. [15] used the finite-element method to estimate temperature distribution, whilst Usui et al. [16] used the method of finite differences. Tay et al. [14] and Stevenson et al. [15] performed extensive analysis to include actual experimental effects in their numerical models, most other analytical and numerical techniques suffering from the lack of proper experimental detail.

A technique that combined the advantages of analytical and experimental techniques for on-line sensing of temperatures, was formulated in the late 60s by Lipman et al. [17]. This technique was called the remote thermocouple sensing (RTS) technique. The main idea of using remotely placed sensors is to overcome the inaccessibility of the cutting interface. This method supposes that there exists a unique relationship between the cutting interface temperature and the remote temperature distribution. Hence by exploiting the inverse relationship, each remotely sensed temperature can be used to extrapolate an unique interface temperature [18]. Since inserts are common at the present time, sensors can be placed underneath the insert in the holder, avoiding the drilling of holes in the cutting tool (Fig. 1). This technique also maintains practicality of use in industry whilst ensuring on-line sensing.

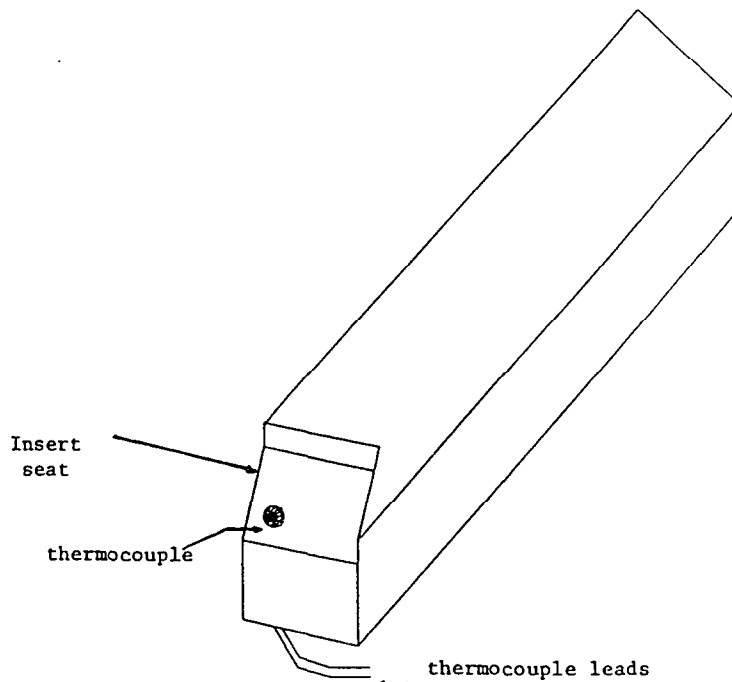


Fig. 1. Remote thermocouple sensing.

As stated earlier, this technique was founded by Lipman et al. [17] and refined by Groover and Kane [19]. Other early efforts in RTS are evident in the work of Stanislaw et al. [20], Levy et al. [21], and Groover et al. [22]. Most of the work in RTS described until now was done from 1967–1977. There was a brief lapse of about 6 years after which interest in this field has revived. Recently, Yen [18] provided an explanation for RTS and developed a new forward solution for the same. He formulated the heat conduction equation in spheroidal coordinates and solved it using a semi-analytical 2-D finite element method. Chow and Wright [23] formulated a 2-D finite-element method and used a ratio curve to estimate the tool interface temperatures.

### 3. Modelling for remote thermocouple sensing

A model is discussed here for studying the tool temperature problem using remote thermocouples. A three-dimensional model is created for representing the heat transfer of a cutting tool. Boundary conditions for the model are then written and a solution is obtained using the finite-difference method. A detailed needs-analysis is performed that discusses the issues of steady-state cutting conditions and thermocouple placement for sensing.

### 3.1. Assumptions and an introduction to modelling

The thermal response of a cutting tool can be separated into two components [21,22]: (i) pure heating; and (ii) wear heating. Since these components are very closely related to each other, their separation is non-trivial. The main premise of this paper is that, making a few assumptions, the two effects can be separated. The tool achieves a steady state within seconds of cut [17], and the response of the thermocouple is delayed until a time when the applied temperature actually reaches it. If the heat is retained within the insert and not conducted to the other parts of the tool sub-system, then a steady state is reached within the entire insert very rapidly. The cutting conditions (speed, feed, depth of cut, material of tool and work, type of process, geometry of tool) influence the rate of conduction and the time needed to achieve this steady state.

The heat can be retained within the insert if proper insulation is provided [17]. Thus if the tool insert is made to fit into an insulator pocket, the heat is retained within the insert as far as pure conduction is concerned. An alumina insulator is considered in this research, being assumed to be a perfect insulator. The actual contact area of the tool with chip is of a very small magnitude compared with the total surface area or the area of the insert exposed to air. Therefore, the thermal heating effect can be assumed to be caused by a point heat source, and the rest of the contact area can be assumed to be convecting to air. Air is a thermal insulator and its thermal conductivity is negligible, hence this also assists in heat retention. However, there is forced convection about the mean surface of the tool (the surface not covered by the insulator) and heat is lost through this forced convection. Heat is also lost through other faces perpendicular to this face. The insulator covers three of the faces of the insert and the heat flux in these directions is considered to be zero. The heat-transfer coefficient of the air and the thermal conductivity of the tool determine the heat retention within the tool to a large extent.

The heating effect is assumed to be caused by a point source on the rake face of the tool. This point source of heat is a result of friction in machining. This high temperature causes a certain wear at the point applied. Thus the position of the maximum temperature is the position at which crater wear begins and this position is dependent on cutting conditions. Experimental results presented by Trent [8] support this assumption. This position is assumed not to vary during the cutting operation. Further, this assumption is different from that of Lipman et al. [17], who use an average tool-chip temperature as the source for temperatures. From Trent's discussions [8], it can be said that for most materials there is a unique hot point on the rake face some distance away from the tool tip. A second point to note is that the tool-chip interface cannot be approximated by rectangles. The distribution of the source is critical in determining the heat intensity that goes into the tool. Assuming the point source hence more closely approximates the actual situation of cutting rather than using rectangular sources. Further, it is also assumed that this temperature is transmitted to the tool flank before actual flank heating takes place. For ease of analysis, flank heating is neglected in this investigation.

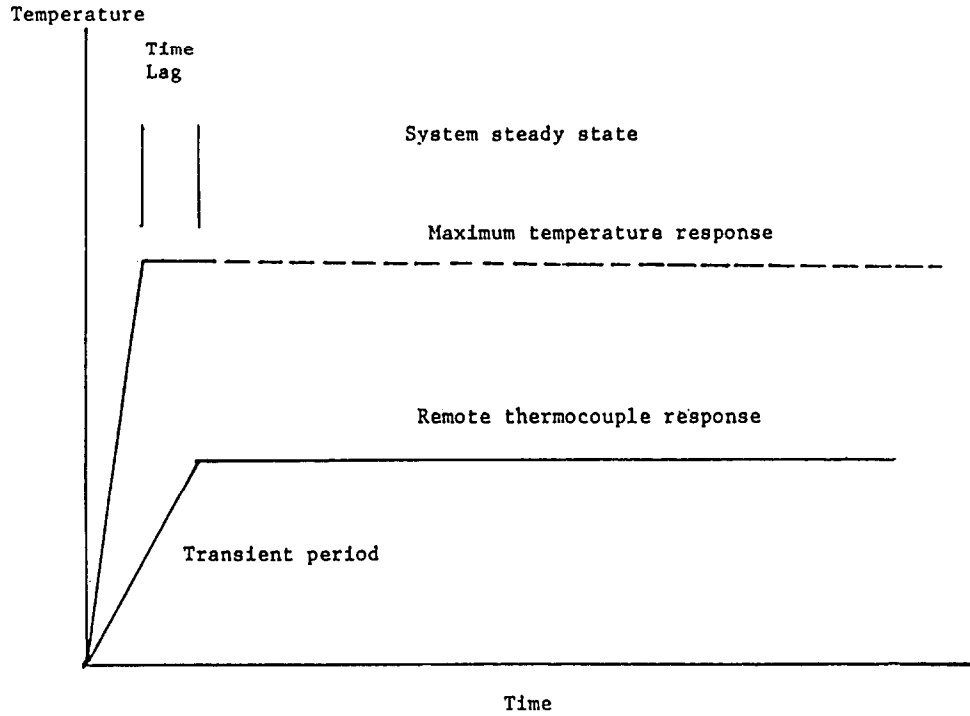


Fig. 2. An ideal steady-state system.

The steady state of maximum temperature causes a steady state in the system. Since steady state in machining occurs relatively quickly (of the order of seconds), the thermal response can be approximated as shown in Fig. 2. This response is true of ideal conditions, i.e., in the absence of wear. In such a case, for every maximum temperature there will be a unique remote thermocouple response. Thus it can be stated that

$$T(r_1) = f(T_{\max}) \quad (1)$$

where  $T(r_1)$  is the temperature at a remote thermocouple and  $T_{\max}$  is the maximum temperature on the rake face.

If wear proceeds, this steady state is impaired. In the case where the wear starts and remains constant thereafter, the maximum temperature will reach a second steady state along with the remote thermocouple as shown in Fig. 3. Thus at steady state the wear effect can be used to study temperature response on a wear domain rather than a time domain. This logic can be extended to generate a relationship between temperature and wear as shown in Fig. 4. Therefore at a particular wear value, there is still a one-one relationship between the remote thermocouple and the maximum temperature. For different wear values, there will be different maximum temperatures and hence

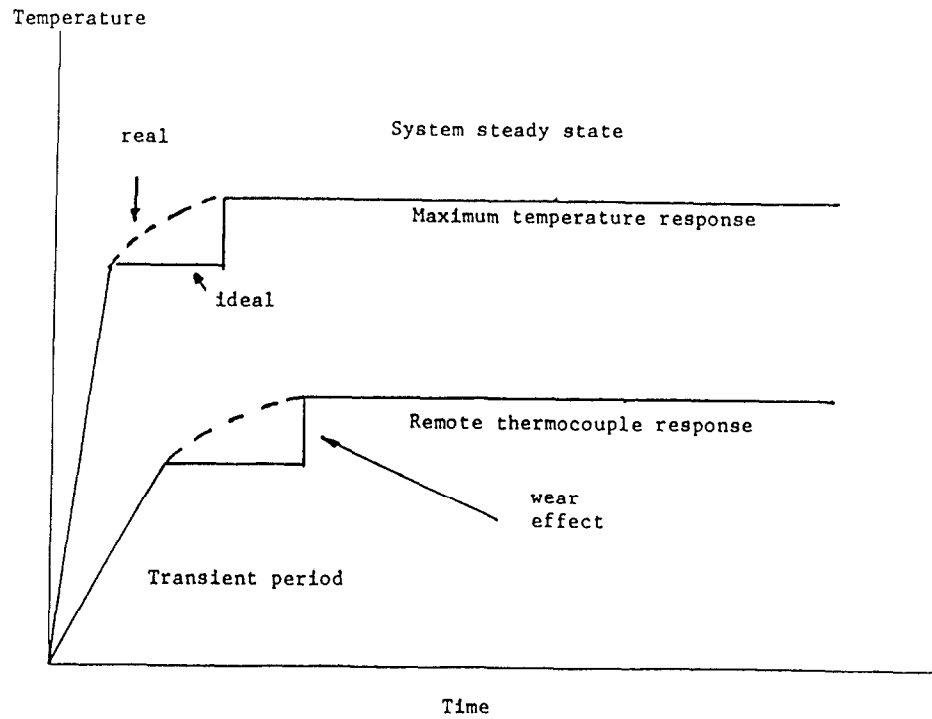


Fig. 3. Wear effect on temperature.

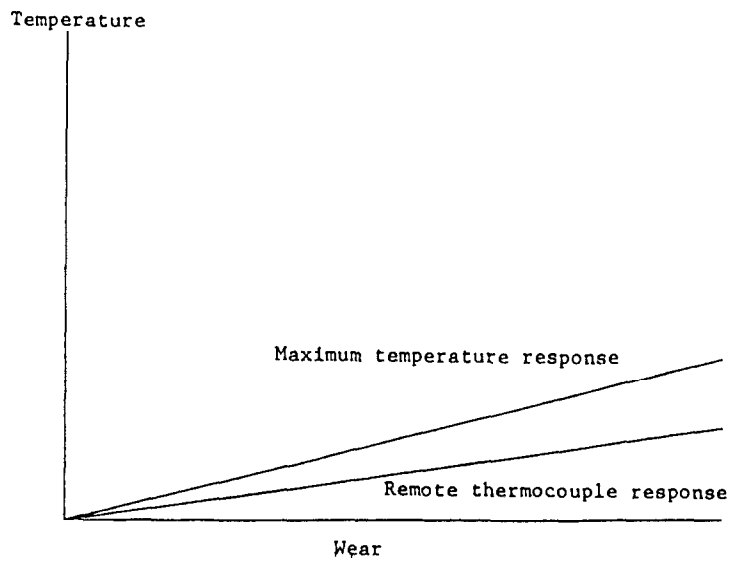


Fig. 4. Temperatures in the wear domain.

different remote temperatures. Thus a starting equation to estimate the maximum temperature based on a remote temperature is needed. To find this relationship, the nature in which the heat travels from the source to the sink (remote thermocouple location) is to be studied: this is known as the forward solution of heat transfer for RTS. The present paper attempts to develop an accurate model and then perform a needs analysis for RTS using the elliptical heat-transfer equation in three dimensions. Once the forward behavior is established, the inverse heat behavior or the prediction of the source with different remote temperatures can be estimated totally off-line. This inverse solution can be utilized with subsequent remote sensors to predict heat distribution in metal cutting.

Thermal radiation is neglected in the forward temperature modelling. The variation of thermal properties with temperature is also neglected, to keep the analysis simple.

### 3.2. Forward solution

For the problem defined (Fig. 5) with the necessary boundary conditions, the heat-transfer equations are written.

Governing equation:

$$\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} + \frac{Q'''}{k} = 0 \quad (2)$$

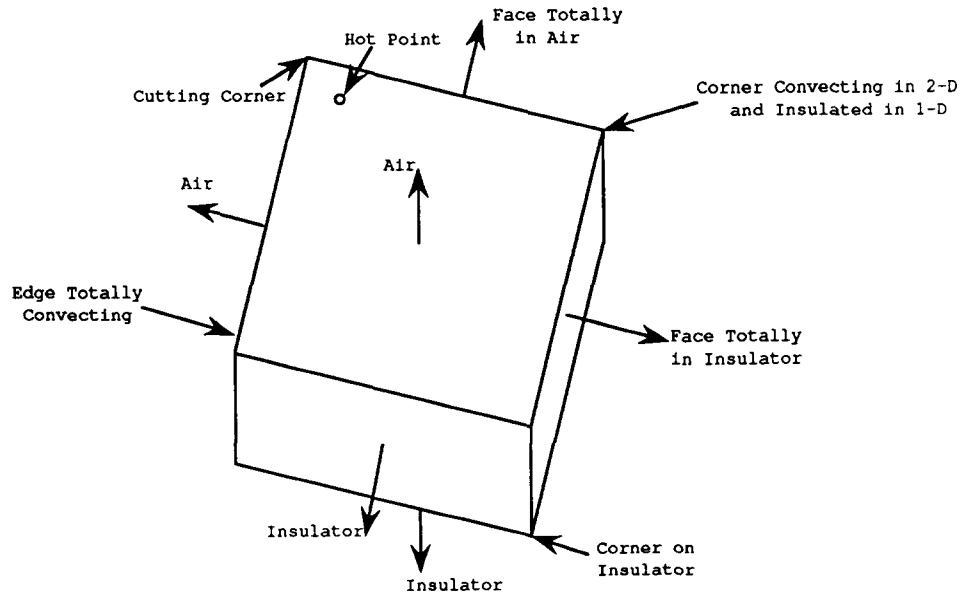


Fig. 5. Major boundaries of the cutting tool insert: only some representative boundary conditions are shown.

Convective:

$$-k \frac{\partial T}{\partial n} = h(T - T_{\infty}) \quad (n \text{ is a dimension}) \quad (3)$$

Adiabatic:

$$\frac{\partial T}{\partial n} = 0 \quad (4)$$

where  $T$  refers to the temperature,  $x, y, z$  are the spatial coordinates,  $Q'''$  is the heat generation,  $T_{\infty}$  the ambient temperature,  $k$  the thermal conductivity and  $h$  the heat-transfer coefficient. Using finite differences the problem is reduced to a non-uniform grid. As the maximum gradients of heat exist close to the source, it is better to use a non-uniform grid with higher resolution close to the source. This grid is very much larger than the thermal balance grid utilized by Lipman et al. [17]. Finite-difference analysis involves the termination of higher order terms (in a Taylor series expansion). This termination is valid only if the grid size is very small. The accuracy of results is hence expected to improve by using a large grid (small grid size). Further, the computational facilities available today are not prohibitive of the use of such grids. The finite-difference method is used here because it is felt that this method is better suited to solving heat-transfer problems. Using a north-south convention as per Croft and Lilley [24], the basic equation is reduced to

$$\begin{aligned} & \frac{T(i+1, j, k) + T(i-1, j, k) - 2T(i, j, k)}{(\Delta x)^2} + \frac{T(i, j+1, k) + T(i, j-1, k) - 2T(i, j, k)}{(\Delta y)^2} \\ & + \frac{T(i, j, k+1) + T(i, j, k-1) - 2T(i, j, k)}{(\Delta z)^2} + \frac{Q'''}{k} = 0 \end{aligned} \quad (5)$$

where symbols have their usual meaning.

The six boundary conditions are expanded to yield 27 sets of equations. The boundary conditions are modelled by introducing fictitious points (as per Croft and Lilley [24]) as shown in Fig. 6. Some of these are summarized in Table 1.

Computer formulation of the algorithm was done in Fortran 77 on a Vax cluster (Vax 11/780, Vax 11/750 and Vax 8550). The heat-transfer coefficient was estimated from standard handbooks while simulating a flat plate subjected to convection. A successive relaxation method was used to solve the equations resulting for each node of the finite-difference mesh. This method was iterative (Fig. 7).

### 3.3. Simulation analysis

This program was used to study the forward behavior of the temperature. Typical plots of the rake face and flank temperatures were generated for different source temperatures (maximum temperatures). Two examples are shown in Figs. 8 and 9.

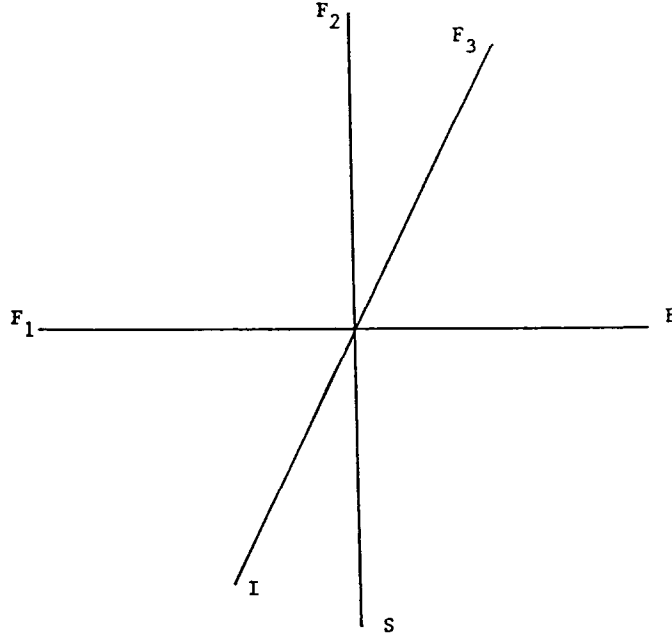


Fig. 6. PDE replacement at corner node.

Table 1

Formulation of equations for solution at boundary points (only 5 cases out of 27 are shown)

| Type of boundary                                  | Equations                                                                                                                                                                                                                    |
|---------------------------------------------------|------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| (1) face (totally convecting) $x$ - $y$ direction | $T_F - T_I = \frac{-2h \Delta x}{k} (T_B - T_A)$ $T_E + T_W + T_S + T_N + T_F + T_I - 6T_B = 0$                                                                                                                              |
| (2) edge (totally convecting) $x$ - $y$ direction | $T_{F_1} - T_S = \frac{-2h \Delta x}{k} (T_B - T_A)$ $T_{F_2} - T_I = \frac{-2h \Delta x}{k} (T_B - T_A)$<br>$T_{F_1} + T_{F_2} + T_E + T_W + T_I + T_S - 6T_B = 0$                                                          |
| (3) vertex (totally convecting)                   | $T_{F_1} - T_E = \frac{-2h \Delta x}{k} (T_B - T_A)$ $T_{F_2} - T_S = \frac{-2h \Delta x}{k} (T_B - T_A)$<br>$T_{F_3} - T_I = \frac{-2h \Delta x}{k} (T_B - T_A)$ $T_{F_1} + T_{F_2} + T_{F_3} + T_I + T_S + T_E - 6T_B = 0$ |
| (4) face insulated $x$ - $y$ direction            | $T_F - T_I = 0$ $T_E + T_W + T_S + T_N + T_I + T_F - 6T_B = 0$                                                                                                                                                               |
| (5) edge (1D insulated 1D convecting)             | $T_{F_1} - T_N = \frac{-2h \Delta x}{k} (T_B - T_A)$ $T_{F_2} - T_I = 0$<br>$T_{F_1} + T_{F_2} + T_E + T_W + T_N + T_I - 6T_B = 0$                                                                                           |

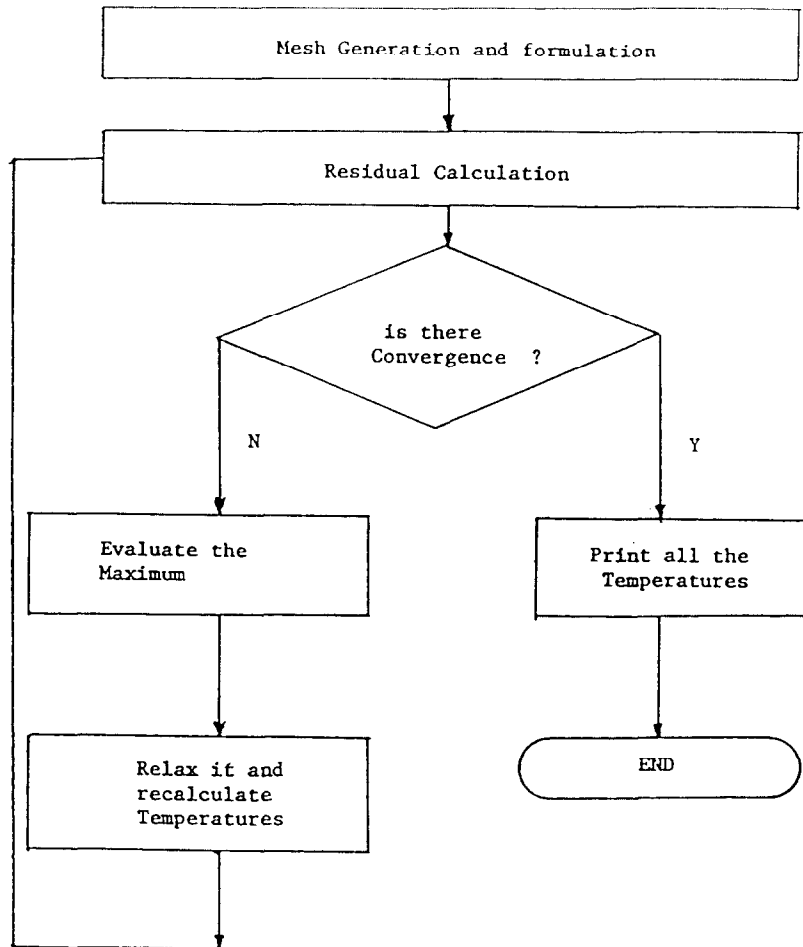


Fig. 7. Flow chart of the algorithm.

Two experimental techniques were tried out (metallographic and microhardness testing) to verify temperatures, as a separate exercise. However, the calibration data of these techniques in themselves, did not serve to provide conclusive trends. Hence they could not be applied to verify temperatures of this numerical model. An experimental verification of temperatures is thus not provided in this research.

The basic nature of the temperature distribution pattern and isotherms were verified experimentally using a basic computer vision system (camera-frame grabber-computer apparatus). Discoloration patterns on the rake face and flank were used as indicators for plotting the isotherms. It can be seen that the basic shapes of the isotherms on the rake face and flank (Figs. 10 and 11) obtained using vision resemble those of the numerical model (Figs. 8 and 9). The vision

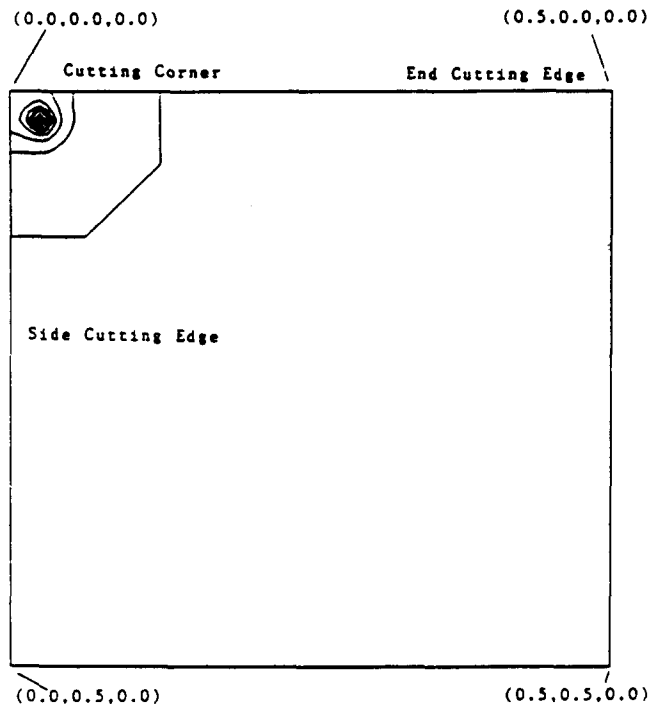


Fig. 8. Temperature distribution on the rake face. The maximum temperature is applied at the position (0.025, 0.025, 0.0); the magnitude of the maximum temperature is  $280^{\circ}\text{C}$ ;  $h=12.0 \text{ BTU/h/ft}^2/^{\circ}\text{Temp}$ ;  $k=0.10 \text{ BTU/h/ft}^2/^{\circ}\text{Temp}$ ; the convergence tolerance is 0.0001.

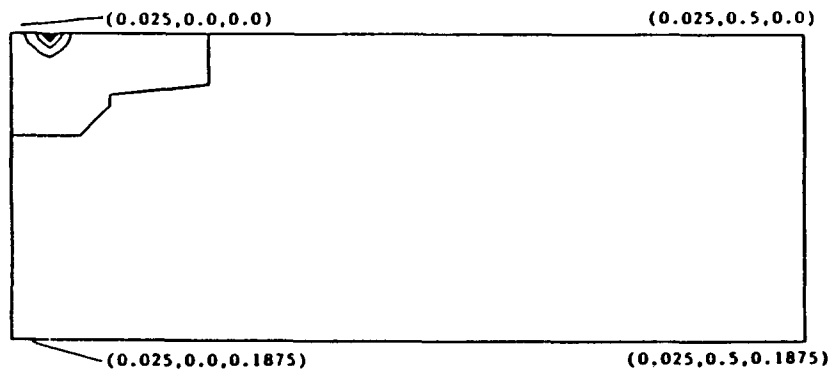


Fig. 9. Temperature distribution on the flank plane (section along maximum temperature). The maximum temperature on this plane is  $280^{\circ}\text{C}$ ; isotherms are plotted every  $25^{\circ}\text{C}$ ; the convergence tolerance is 0.00025.

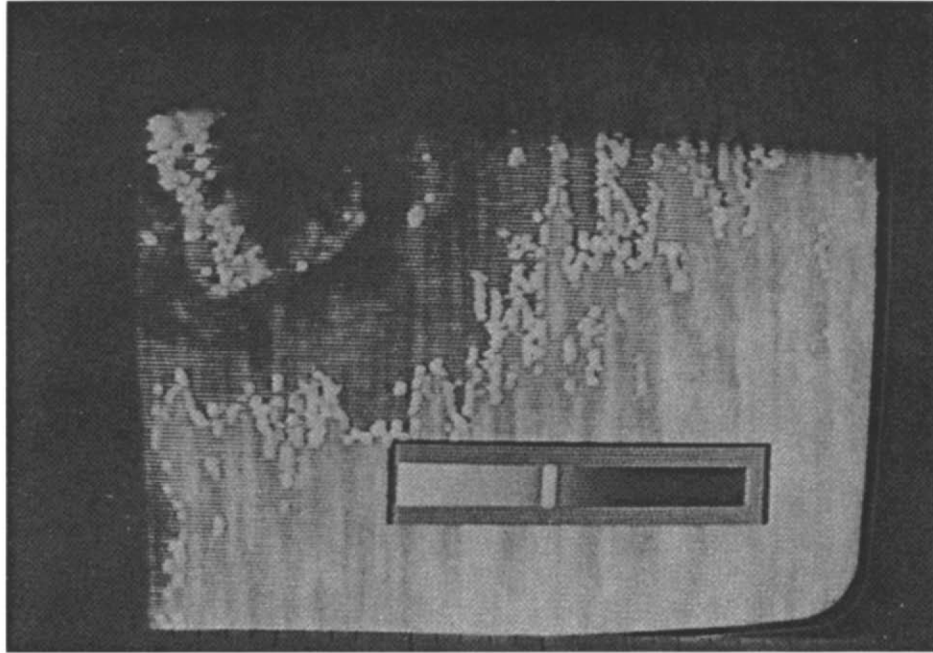


Fig. 10. Thin band isotherm of a typical temperature discoloration on the rake face. The thin band (white) demarcates the colour patterns on the plane. The cutting corner is in the top left-hand corner of the picture. The box in the lower right-hand corner is the grey level indicator. The shiny corners and grind marks are also picked up.

system unfortunately picked up some noise as well. It is to be noted also that the comparison cannot be one-one as the test conditions were different. Further, Fig. 9 shows a flank plane through the maximum temperature whereas Fig. 11 shows the actual flank. Extensive work with this thermal vision apparatus can be utilized to develop new experimental techniques for the future. Microhardness testing presented by Trent [8] also verified the basic shape of isotherms and hence the selection of the model and boundary conditions.

Simulations were also performed to determine the location of thermocouples. Figure 12 shows the temperature response of the nearest and farthest neighbor of the source for a given set of conditions. The source of error will be lower if the thermocouple is placed closer to the nearest neighbor. In embedded thermocouple sensing, it is possible to place the thermocouple close to the nearest neighbor, but this will modify the cutting tool significantly and render the process impractical. However, in RTS, thermocouples will be generally placed closer to the farthest neighbor and the majority of the temperature gradient is close to the source. It is felt that an ideal situation to sense remote temperatures will be to use some kind of thermally activated film sensor

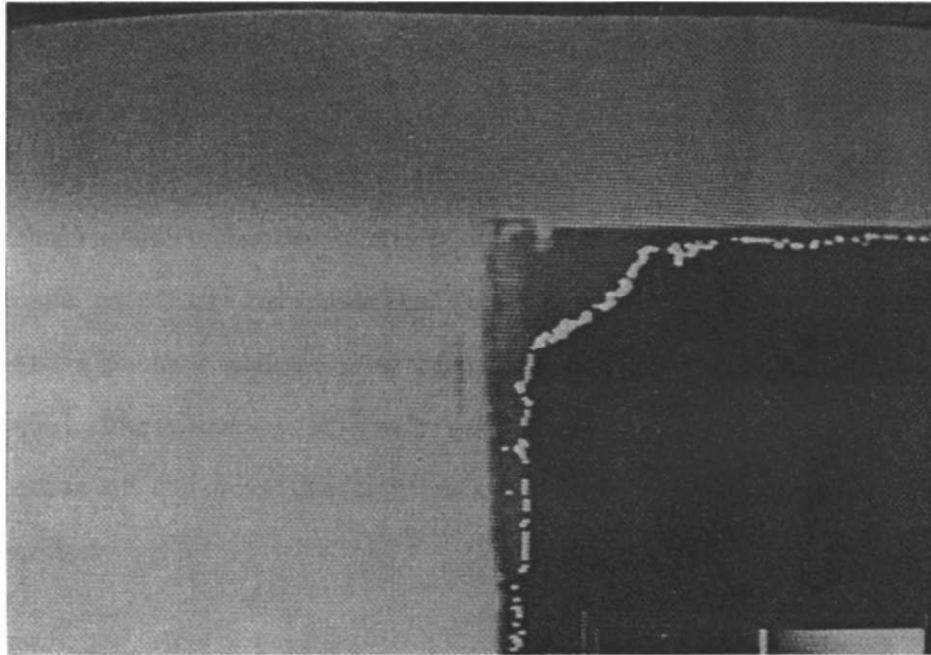


Fig. 11. Thin band isotherm of a typical temperature discoloration on the flank. The thin band (white) demarcates the colour patterns on the plane. The cutting corner is in the top left-hand corner of the picture. The box in the lower right-hand corner is the grey level indicator (partly hidden). The shiny corners are also picked up.

placed underneath the insert. This will allow for an entire distribution of temperatures to be studied. Since no such sensor is available, more than one sensor should be used to improve reliability of sensing. All the same, a thermocouple placed close to the source will provide a better response.

Analysis is still underway in determining optimal location of thermocouples. Other considerations for placement include the rigidity of the holder, the non-interruption of cut, etc. For rigidity and insert positioning considerations, it is desirable to place thermocouples in a flat portion of the holder touching the insert. Though much study was conducted on these issues, only the result is presented here.

The thermocouple arrangement selected after much research (and experimental and analytical evidence) is illustrated in Fig. 13. An insulator pocket was designed to sit within the tool holder. Three recesses were designed to serve as the locations for sensing of the temperatures within this pocket. Thus, the thermocouples will go through the holes provided, and touch the insert at three distinct points. Any standard ungrounded thermocouple (K type) can be used.

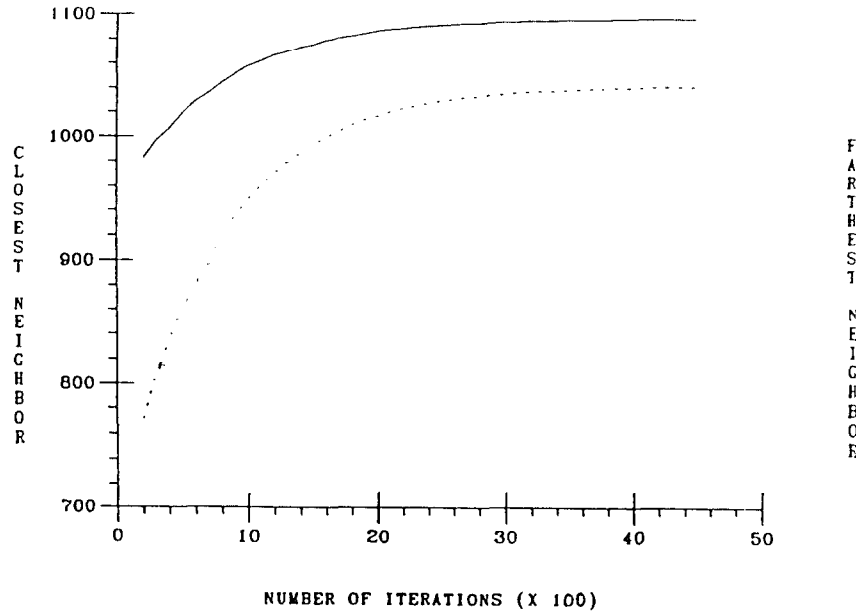


Fig. 12. Temperature response at bounding points.

#### 3.4. Inverse solution

A standard Valenite tool holder was modified to include a ceramic insulator pocket to verify the feasibility of sensing. A T-15 HSS cutting tool ground to negative geometry (SNG 432) was seated on this insulator pocket. Three K-type thermocouples were spring-loaded underneath the insert through the holes in the pocket and tool holder. Temperatures were sensed using instrumentation (calibration, linearization, amplification, electronic cold junction compensation), a white box interface and a PC. More details on this are presented by Raman and Cohen [25].

After deciding the location of thermocouples, a simple formulation was used for studying the inverse problem. Suppose  $(r_1)$  represents a  $n \times 1$  matrix vector of remote thermocouple response 1 and similarly  $(r_2)$  and  $(r_3)$  the respective remote responses at other locations and if  $y(\max)$  represents the  $(n \times 1)$  vector of maximum temperatures, then they can be written as satisfying the relation:

$$Y = X(r) \times [R] \quad (6)$$

where  $Y$  is the response matrix ( $n \times 1$ ) and  $R$  is the relationship (least-squares coefficients) matrix of dimension  $4 \times 1$  and  $X(r)$  is the variable (remote thermocouple response matrix of dimension  $n \times 4$ ).

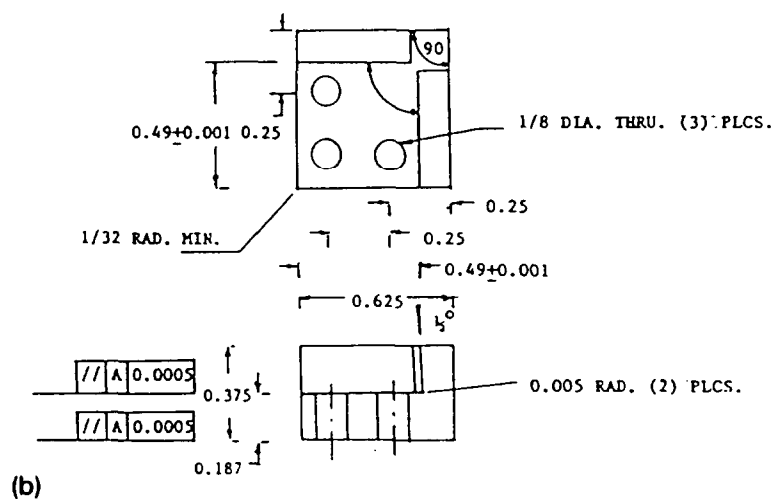
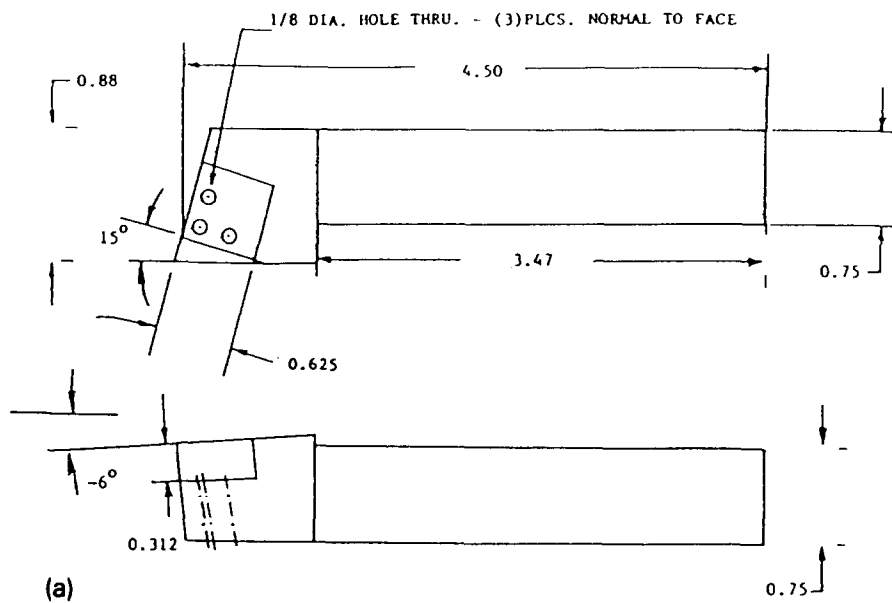


Fig. 13. Details of modified tool holder that allows for alumina insulator pocket and remote thermocouples: (a) final design of tool holder; (b) final design of insulator pocket.

Multiplying both sides of eqn. (6) by  $X'(r)$  as per Neter et al. [26]:

$$X'(r) Y = X'(r) X(r) [R]$$

which can also be written as

$$\begin{aligned} \sum Y_{T_{\max i}} &= d n + a \sum X_{T_{r1}} + b \sum X_{T_{r2}} + c \sum X_{T_{r3}} \\ \sum X_{T_{r1}} Y_{T_{\max i}} &= d \sum X_{T_{r1}} + a \sum X_{T_{r1}} X_{T_{r1}} + b \sum X_{T_{r1}} X_{T_{r2}} + c \sum X_{T_{r1}} X_{T_{r3}} \\ \sum X_{T_{r2}} Y_{T_{\max i}} &= d \sum X_{T_{r2}} + a \sum X_{T_{r2}} X_{T_{r1}} + b \sum X_{T_{r2}} X_{T_{r2}} + c \sum X_{T_{r2}} X_{T_{r3}} \\ \sum X_{T_{r3}} Y_{T_{\max i}} &= d \sum X_{T_{r3}} + a \sum X_{T_{r3}} X_{T_{r1}} + b \sum X_{T_{r3}} X_{T_{r2}} + c \sum X_{T_{r3}} X_{T_{r3}} \end{aligned}$$

where  $a, b, c, d$  can be estimated solving this system of simultaneous equations.

The inverse formulation will hence relate the remote temperatures ( $T_{r1}, T_{r2}, T_{r3}$ ) to the source temperature ( $T_{\text{source}}$ ) as:

$$T_{\text{source}} = a T_{r1} + b T_{r2} + c T_{r3} + d$$

Note that this is only a simple methodology and has been presented here only to capture the essence of inverse solution.

A typical simulation of the forward procedure for different source temperatures (for a coarse mesh) is shown in Table 2 to demonstrate the computation of constants  $a, b, c$  and  $d$ . The thermocouple responses at the appropriate selected locations is also shown. Since a symmetrical source was used, temperature response is symmetrical about the main diagonal of the top face. Thus  $T_{r2}$

Table 2

Results of forward simulation under identical conditions

| $T_{r1}$ (°C) | $T_{r2}$ (°C) | $T_{r3}$ (°C) | $T_{\max}$ (°C) |
|---------------|---------------|---------------|-----------------|
| 508.07        | 496.88        | 497.011       | 600.00          |
| 526.0         | 499.83        | 501.38        | 700             |
| 553.0         | 510.0         | 510.0         | 800             |
| 621.0         | 573.0         | 572.0         | 900             |
| 694.36        | 641.23        | 641.04        | 1000            |

Table 3

Coefficients of least-squares approximation

| Coefficient | Value      |
|-------------|------------|
| $a$         | 5.951489   |
| $b$         | -9.387945  |
| $c$         | 4.56099067 |
| $d$         | 4.283633   |

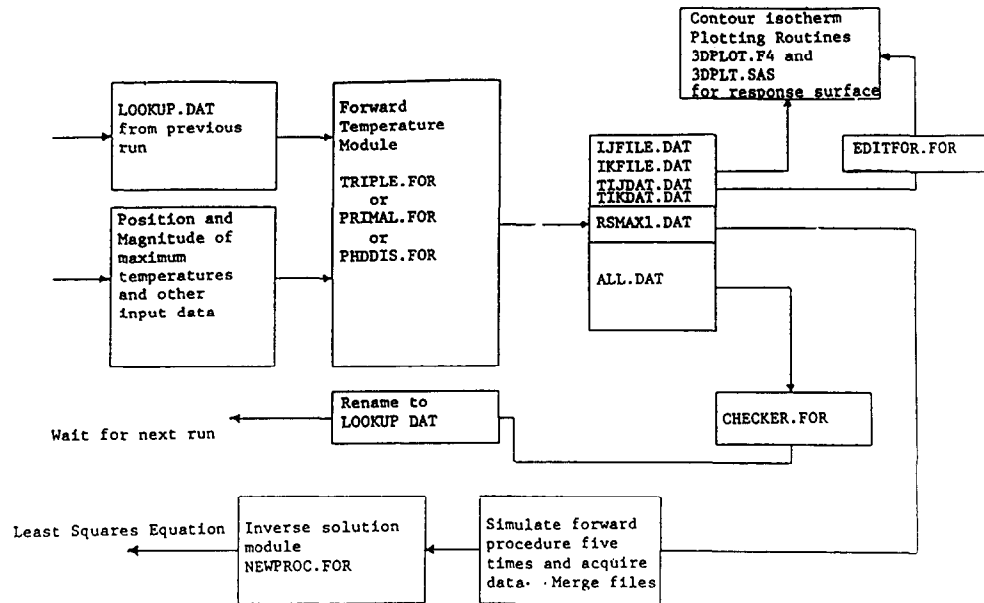


Fig. 14. System software structure.

and  $T_r$  are approximately the same.  $T_{r_1}$  is the closer thermocouple and picks up a better response. The coefficients of the least-squares approximation were estimated using the above set of equations which were coded using the Fortran language. These are illustrated in Table 3. Accordingly, the overall system structure developed for this research is shown in Fig. 14.

Several other simulations were also conducted to study the forward thermal behavior. Overall, a basic framework for RTS in machining is created. Simple models are implemented to verify the feasibility of RTS in machining. Since RTS is one of the very few on-line methods for temperature sensing in machining, more work based on the present research is required. The three most obvious extensions to this work are:

- (1) development of an empirical/analytical model that identifies the position of maximum temperature at different cutting conditions for different tool-work combinations;
- (2) a verification of the forward thermal behavior, using experiments;
- (3) a sensitivity analysis to study the impact of thermal properties. The thermal properties will affect the temperature propagation and hence the sensed values at the thermocouples.

### 3.5. Limitations and other general observations on RTS

Since accuracy is very important in inverse prediction, very large meshes (for forward simulation) were used. The use of such grids results in large computational times, which may be viewed as a disadvantage. It is to be noted

that very small source temperatures result in very small remote temperatures. Change in source temperatures by a small amount will invariably result in a very small change in the remote location temperatures, in such cases. Hence, the experiments have to be very carefully performed to detect changes in source. On the other hand, temperatures at the source are very large while machining with carbides and RTS can be used effectively to detect changes in the rake face temperature. The time lag between the source and remote sensing may prohibit the use of RTS in situations where tool wear/fracture is very fast. Smaller inserts (SNG 322) will show better response at the remote thermocouples than larger inserts (SNG 432). This is because the source comes closer to the sensing interface. The use of cutting tools such as ceramics (insulators) may change the heat models significantly. Coated cutting tools, with two or three (micro) layers of coatings (with significantly different thermal properties) may also pose problems in modelling.

#### **4. Summary and conclusions**

An attempt has been made to verify the findings of different researchers while developing a framework for RTS. Some analysis is performed and presented here citing prospective models and approaches for implementation of RTS. Most data, results and observations presented in this paper are simulation generated. Possible extensions to the present research are also suggested. The features of this research, its conclusions and recommendations are presented:

- (1) The machining situation is represented, using assumptions and facts derived from past research, for remote thermocouple sensing. RTS is one of the very few techniques available for on-line temperature estimation in machining.
- (2) A three-dimensional forward temperature simulator for RTS is developed.
- (3) Boundary conditions are formulated to simulate the actual machining situation.
- (4) A design configuration for placement of thermocouples is presented. The feasibility of sensing with this configuration is verified experimentally.
- (5) An inverse formulation is presented for RTS based on location of thermocouples and the forward solution.
- (6) Initial results justified the feasibility of using this technique for on-line sensing of temperatures (and hence wear), though more analysis needs to be performed.
- (7) Simulations of the forward behavior also allowed for a needs (for the future) analysis. Overall, a basic framework for RTS is provided.
- (8) Experimental verification of forward modelling is desired, but has not been performed in this study: future work should concentrate on this issue. In fact, the development of an accurate experimental technique, for sensing temperatures for a wide variety of tool and work materials and over a large

range of cutting conditions, in itself will constitute a mammoth contribution to machining.

(9) The use of faster and more accurate techniques such as the method of differential quadrature should be investigated for forward modelling in the future. Researchers working in other areas have proven the high accuracy of this technique.

(10) The practicality of using RTS in a multiple sensing environment should be investigated.

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