THERMOELECTRIC COOLING OF A LATHE CUTTING TOOL

by

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INTRODUCTION

Heat generated at the tool tip during metal cutting operations has been recognized as one of the prime factors diminishing tool life. Consequently, the phenomenon of heat generation as well as the methods of reducing it have been studied in detail, in an effort to prolong the periods between regrinds and get more metal removal in a shorter period of time.

One of the simplest and the most widely used methods is to play a jet of cutting fluid at the point on the tool where the cutting is taking place. This method is very effective at low cutting speeds and depths of cut. At higher cutting speeds, however, the fluid would be carried away by the outward flowing chip more rapidly than it was drawn between the interstices between the chip and tool by capillary forces. The force on the tool point likewise increases, resulting in an increase of the real area of contact along the tool face and a decrease in the intersticial volume. Thus making it more difficult for the fluid to find its way to the tool point (1).

It is clear that the cooling efficiency would be increased if the cooling source could be brought closer to the cutting edge in relatively large quantities. This is achieved, to a certain extent, by using a stream of carbon dioxide as a cutting fluid. This method has been found particularly good for turning titanium alloys but fails to be of much use in high speed cutting (1).

The idea of thermoelectric cooling for cutting tools seems to have received little attention and no technical literature has been published so far on the technique. Meyers Electrocooling Products, Inc., a

Manchester, Connecticut firm manufacturing thermoelectric cooling equipment, has put a cooling unit on the market for lathe cutting tools. The author, however, had no access to one of these units due to their prohibitive price and the entire design of the system outlined in the following pages was conceived totally independently, without being aware of the technology employed in the Meyers Electrocooler.

The thermoelectric cooling seems to adapt very well to the task of lowering the temperatures at the tool tips in turning operations. A serious flaw in this kind of cooling is that the lubricating action of the cutting fluid is eliminated. This will result in higher tool-chip interface friction forces and consequently higher heat generation. Nevertheless, on the whole, the system shows considerable promise.

HEAT IN MACHINING

The Occurance of Heat and Its Causes. The occurance of heat in metal cutting operations was studied by Count Rumford and other scientists almost at the inception of the metal working industry. This was as early as 1798, almost 42 years before Joule performed his famous experiments on the mechanical equivalent of heat. Rumford used calorimetric methods, immersing the work, tool and chips in quantities of water and measuring the temperature rise of water (2). However, knowing merely the amount of heat involved in a cutting operation is useful in determining the power required, but does not give much information about the maximum temperature reached.

The three regions in metal cutting where heat is developed are shown in Figure 1 as follows (3);

1. Along the shear plane OA, the heat associated with plastic deformation of the material being machined raises the temperature of the chip. Part of this heat is carried away by the chip as it leaves contact with the tool.

2. As the chip moves upward along the tool, the friction at the tool-chip interface OB causes a further rise in the temperature of the chip which may cause boundary lubrication to deteriorate toward metal-to-metal contact.

3. If the tool is not sharp, the wear land OC will cause friction along the tool-workpiece interface and further generate heat.

The amounts of heat that are conducted from the chip to the tool and workpiece depend on the temperature differential between these elements, their masses, and the length of time in contact with one another. Heat lost to the surrounding atmosphere is another factor. It has been observed

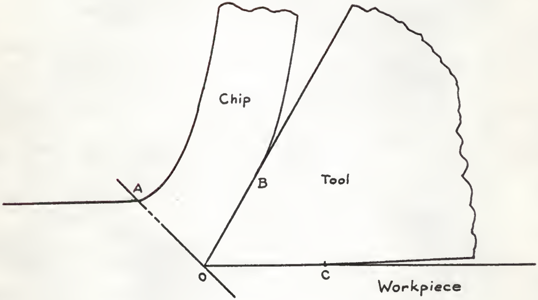


FIGURE 1

REGIONS OF HEAT GENERATION (3)

that the higher cutting speeds show greater amounts and percentages of heat in the chips because the heat has less time to be conducted from the chip to the tool and workpiece. Thick chips have a lower temperature than thin chips, but the percentage of total heat in the chips is greater for thicker chips. Because a thick chip has a greater mass and a lower temperature, the rate of conduction of heat away from a thick chip is less than it would be for a thin chip (2).

As cutting speeds increase up to about 200 feet per minute, they entail higher chip temperatures and lower workpiece temperatures. Above 200 feet per minute chip temperatures remain practically constant. Generally, tool temperatures increase, however, if machining is continuous or prolonged, or the cutting speed is increased since the heat in the tool accumulates faster than it can be dissipated in most instances. For the same reason the heat in the tool becomes concentrated near the cutting edge and the temperature of this portion of the tool is considerable higher than in the surrounding portions. Higher cutting speeds especially tend to accelerate this condition. Cutting speed is often limited by the ability of the tool to conduct heat away from the immediate vicinity of the cutting edge (2).

The actual temperature reached during the cutting operation by the tool tip itself, that is by the cutting edge, and the immediately surrounding tool material, is of primary interest because the ability of the tool edge to stand up decreases with increasing temperatures. Temperatures of a steel workpiece and chips measured in milling by A. O. Schmidt are plotted in Figure 2. The temperatures in the graph are based on data arrived at by Professor Trigger, of the University of Illinois,

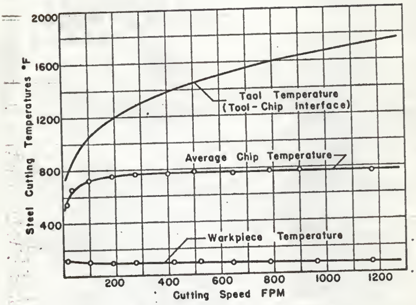


FIGURE 2

AVERAGE TEMPERATURE IN MILLING OF
STEEL WORKPIECE AND CHIPS IN RELATION TO CUTTING SPEED (2)

in tests with a tool-workpiece thermocouple arrangement. The tests showed that the major portion of the heat in a metal cutting operation is carried away by the chips. In the range of 200 feet per minute cutting speed the percentage of heat in the chips will vary with the cutting speed and chip thickness. The higher the cutting speed and thicker the chip, the higher will be the percentage of heat in chips. Above a cutting speed of 200 feet per minute the amount of heat in the chips, and also the average chip temperature, acquire a constant character for otherwise identical cutting conditions.

At low cutting speeds (around 10 feet per minute) the amount of heat in the chips is about 40 to 50% of the total heat generated, depending upon the chip thickness. At higher cutting speeds (200 feet per minute and above) the amount of heat in the chips varies between 60 and 80% of the total heat depending upon the chip thickness. The remaining heat is distributed almost equally to the tool and workpiece. Since the workpiece is usually of larger mass than the tool, its temperature tends to be low while the heat in the tool is of necessity concentrated in a zone near the cutting edge which at high cutting speeds or under continuous machining reaches a high temperature. This is a frequent contributing cause of tool failure (2).

An increase in feed does not increase the temperature as much as an increase in speed. In practice, it is therefore recommended by Dr. Kronenberg to increase the feed rather than the speed for increasing production and tool life; provided, of course, that a powerful and rigid machine is available so that the increase in cutting force does not produce excessive deformation of the machine and work. Since it is known also that the value U (specific cutting force) decreases with an increase in feed rate. This is another reason for increasing feeds, since the temperature drops as U drops (4).

Analytical Determination of Tool-Chip Interface Temperatures. Many researchers have tried to develop a means of analytical evaluation of tool-chip interface temperature distribution, most noteable among this literature are Chao and Trigger of the University of Illinois, (5); Loewen and Shaw of M.I.T., (6); and Wu and Meyer of the University of Wisconsin (7). The analysis are far from accurate and make use of several assumptions which may not all be justified. However, the correlation between results obtained by such methods and by actual measurements (which again cannot be very accurate) is strikingly good.

Loewen and Shaw were the first to publish an iterative approach based on the theory of moving heat sources. It takes into account the portions of the heats generated at the shear plane and at the tool-chip interface due to friction, that flow into the tool face. A value of the mean temperature at the tool-chip interface can be found by this method if the various machining variables like cutting speed, depth of cut, width of cut, chip contact length, chip length ratio and cutting forces are known.

The values of the mean temperatures obtained agree very well with those measured by the tool-work thermocouple method.

Trigger and Chao (5) presented in 1955, an iterative method from which the temperature distribution at the tool-chip interface could be computed. Later in 1958, (8) they improved upon their previous work and put forth a non-iterative method for computing the distribution both at the tool-chip and tool-workpiece interface (Figure 3) and drew some interesting conculsions. It was noted that except for the initial drop, the calculated tool-chip interface temperature changes little as flank wear increases, although a definite upward trend can be noticed. The temperature at the tool-work interface, however, is greatly affected by the development of flank wear.

Professor Wu and Meyer have recently put forth a first-order, five-variable cutting tool temperature equation,

$$\theta = 165V^{0.392}\, f^{0.244}\, d^{0.072}\, A^{0.217}\, R^{0.104}$$

where V is the cutting speed

f is the feed

d is the depth of cut

and R is the nose radius.

According to Dr. Kronenberg (4)

$$\theta_t = \frac{V^{0.5}\, t^{0.5}\, \tau\, \gamma^{0.5}}{K^{0.5}\, (\rho C)^{0.5}\, J}$$

where V is the cutting speed

t is the depth of cut

τ is the shear stress

γ is the shear strain in shear plane

k is thermal conductivity

ρC is the volume specific heat

J is the Mechanical Equivalent of heat.

All these methods are, of course, approximations involving assumptions about variables of which little is known. Further research is needed in this area.

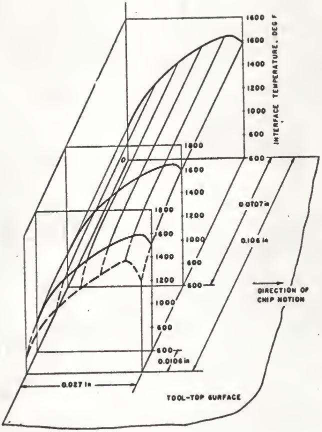


FIGURE 3

THREE DIMENSIONAL TEMPERATURE DISTRIBUTION
AT TOOL-CHIP INTERFACE (8)

THERMOELECTRIC COOLING

Thermoelectric phenomenon. In 1822-1823 Seebeck described in the Reports of the Prussian Academy of Sciences a phenomenon which he named, "the magnetic polarisation of metals and ores produced by temperature difference," (9). It is understood from the description of his experiments that Seebeck discovered thermoelectric currents arising in a closed circuit made up of different conductors at different junction temperatures. This was an epoch when the discovery by Oerstead of the effect of an electrical current on a magnetic needle was followed by a series of investigations by Ampere, Biot, Savart, Laplace and others who explained the interaction between electric currents and magnetic fields.

Seebeck effect essentially is that when two dissimilar metals are welded together at one end and this junction is heated, a voltage is developed on the free ends proportional to the temperature difference between the welded and free ends (10). The extensive thermoelectric series compiled by Seebeck for dissimilar metals is even now of interest. The thoroughness of Seebeck's investigations may be illustrated for example, by the fact that in an article by Maria Telkes published in an American Journal 125 years later the best couple was given ZnSb and PbS, which were the first and the last members in Seebeck's series (9).

Twelve years after Seebeck's discovery, Peltier, a watchmaker, published in the French Journal, "Annal. Phys. Chem., in 1834 an article on temperature anomalies observed in the vicinity of the boundary between two different conductors when a current was passing through them. The phenomenon first observed by Peltièr and therefore given the name of Peltier effect, consists in reality of the generation or absorption (depending on the direction of the current) of heat, at a rate Q, at the junction between two different conductors when a current I flows through them:

$$Q = \Pi I$$

where Π is the Peltier coefficient.

The Peltier effect is very closely related to the Seebeck effect, and the coefficient Π to the thermoelectric power α:

$$\Pi = \alpha T$$

where T is the absolute temperature of the junction.

A temperature difference produces an electric current in a closed circuit made of different materials, while in turn, a current flowing through such a circuit produces a temperature difference. A few more years passed, during which Becquerel and other investigators tried to explain the time nature of the Peltier Phenomenon, until in 1838 the St. Petersburg Academician Lenz put an end to all doubts with a simple experiment. Lenz made a small hole in the junction of a metal thermocouple and succeeded in freezing a drop of water. He found that he could change the water from liquid to solid and back again at will by reversing the current. This showed that he could pump 80 calories/ gram of water, which is the heat required to freeze or thaw it at freezing temperature. Later Lord Kelvin formulated a set of equations which explained the thermoelectric effects (9).

These early workers, however, used metals and simple alloys for their experiments and were unable to proceed very far towards applying the cooling effect in useful devices. The properties of the metals were not suitable; the thermal conductivity was large which allowed heat to leak through the material itself, and the Peltier effect obtained with them was small to begin with (11).

Thermoelectric cooling has, over the past few years seen the development of many practical devices. One of the first was an icemaker for hotel rooms and several hundred were installed. This unit makes one tray of ice cubes in several hours. The production being exactly tailored to the needs of the guest. The facility of designing the system to suit the particular application in mind is peculiar to thermoelectricity. Thermoelectric designs can be tailored to kilowatts of cooling down through tenths of watts to the smallest amount of heat removal that is required. With mechanical systems, this range can only be handled with considerable difficulty (11).

Advantages of Thermoelectric Cooling. Some of the many advantages of thermoelectric cooling can be defined from the work accomplished to date and are mentioned below (11):

1. Units are tailored exactly to size,
2. They are silent,
3. Solid state devices and hence no moving parts,
4. The latest units are capable of maintaining a differential of 70 C and more, depending on where in the temperature spectrum the device operates.

The "Frigistor" Thermoelectric Cooling Module. A thin sandwich of two special semiconductor alloys, one N, the other P type, arranged so that current flows in a zig-zag pattern from one face to the other and back again, through the special alloys, constitutes the basic multimodule cooler configuration. At each face a set of junctions occurs where the current leaves an N block and enters a P block via a bus-bar. On one side, the conventional current always enters a P block, and this side

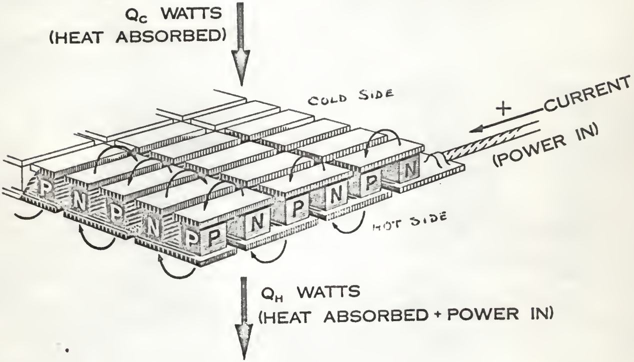


FIGURE 4

TYPICAL ARRANGEMENT OF A "FRIGISTOR" (11) becomes cold. On the other side, the current always leaves a P block and enters an N; this side, becomes hot, and dissipates all the heat that flows through the unit, including that generated by I^2R losses: (Figure 4) (11).

The side that cools in the explanation just given will subsequently be called the "cold" side and is defined as the side on which "conventioanl current enters" the P block. This will be termed "cold" even when the conditions of operation dictate that the heat flow causes it to be otherwise.

The side that heats is defined as the side where "conventional current leaves" the P block and will be referred to as the "hot" side. This side by convention is also the location of the power leads, so that power is not led into the cold area when the module is cooling.

Basic Equation of Thermoelectricity. From the laws of thermodynamics an equation can be written which represents the heat flows in a Frigistor, and shows at what rate heat can be removed from an objectto maintain a given temperature difference. The concept of cooling with an active device is best approached by considering the module as a thermal conductor with a negative thermal resistance. A copper bar of given dimensions has a thermal resistance of say x degrees centigrade per watt. That is to say, if one watt of heat is passed through the bar, the temperature drop across the bar will be x degrees. With a thermoelectric cooler, however, when current is caused to flow, there is a virtual negative resistance of y^o centigrade per watt. This means that with $-y^o c$ across the module, one watt of heat will be caused to flow from the cold region to the hot region through the module. Because the device is active, heat is caused to flow "uphill." But when the current is reduced to zero, it becomes passive and behaves like any other thermal conductor. It then loses its negative resistance and reverts to its positive thermal resistance of y^0 centigrade per watt (11).

In a perfect cooler, the amount of cooling available at each cold junction is given by:

$$Q = (\alpha_p - \alpha_n)\ I\ Tc \qquad (1)$$

where α_p and α_n are the Seebeck voltages in volts per degree centigrade in each of the two materials, I is the current in amperes and Tc is the absolute temperature in ^{0}K(273 ^{0}K = 0^{0}C). This term is reversible and is discussed later.

In a practical Frigistor, however, the circuit includes electrical resistance, and this resistance represents a change of electrical energy into heat. This conversion is not reversible and is in addition an effect which occurs throughout the bulk of the conductor. It can be shown, that providing the cross-section is uniform, half the heat generated goes to each junction. Thus, on the cold side;

$$Q_R = \frac{1}{2}\ I^2R \qquad (2)$$

where R is the resistance of the couple.

Since the module is made of a physical substance across which there exists a temperature gradiant, heat will flow through it from the "hot" side to the "cold" side. This leakage heat has the value:

$$Q_L = K\Delta T \qquad (3)$$

where K is the total thermal conductivity of the couple and ΔT is temperature differential between "hot" and "cold" sides.

Now the module will pump back to the "hot" side all the resistance heat and leakage heat that reaches the "cold" side, so that the net amount of heat that is available for cooling per couple is given by:

$$Qc = Q - QR - QL$$

$$Qc = (\alpha_p - \alpha_n)\ I\ Tc - \frac{1}{2}\ I^2R - K\Delta T. \tag{4}$$

In this equation three properties of the material appear, the first is α, the Seebeck voltage, as we have already discussed and the second is ρ contained in R since the resistance R is given by:

$$R = 2\rho_{pn}\frac{L}{A} \tag{5}$$

Here the second material property ρ is the average electrical resistivity of the two materials in ohms-cm. The geometrical shape is a uniform L/A, and the constant 2 is present because R refers to both the N and P branches.

The third property appears in the last term, in K, since:

$$K = 2k_{pn}\frac{A}{L} \tag{6}$$

where k_{pn} is once again, the average thermal conductivity of the two branches, and A/L the geometrical shape of the branch. It is inverted since K is a conductivity term.

For the present it will be assumed that both branches are identical, so that:

$$\alpha_{pn} = (\alpha_p - \alpha_n) \tag{7}$$

$$\rho_{pn} = \frac{\rho_p + \rho_n}{2} \tag{8}$$

$$k_{pn} = \frac{k_p + k_n}{2} \tag{9}$$

Each of the three terms of the basic equation is a physical effect which involves a basic property of the material.

A fourth effect involving a basic material property is the absorption or liberation of heat in a single homogenous conductor which has a temperature gradient along it. It is known as the Thompson effect, and is normally neglected in cooling. In fact, it is zero if α is constant with temperature since it is given by;

$$QT = \tau\Delta\ TI \tag{10}$$

and

$$\tau = \frac{d\alpha}{dT} \tag{11}$$

where τ is the Thompson coefficient.

For the purposes it will be considered negligible.

When written out in full, to show all the variables, the basic equation is:

$$Qc = (\alpha_p - \alpha_n)\ I\ Tc - \frac{1}{2}\frac{L}{A}\ (\rho_p + \rho_n)\ I^2 - (k_p + k_n)\frac{A}{L}\,\Delta T \tag{12}$$

Within our simplifying assumptions, it is:

$$Q_c = \alpha_{pn}\ ITc - \frac{L}{A}\,\rho_{pn}\ I^2 - 2k_{pn}\,\frac{A}{l}\ \Delta T \tag{13}$$

and also the heat rejected on the hot side:

$$QH = \alpha_{pn}\ ITc + \frac{1}{2}\ I^2R - K\Delta T. \tag{14}$$

In a multicouple unit Qc is n times the value for a couple as given above, where n is the number of couples. The designer has, therefore, five variables with which to operate: Qc, Tc, I, ΔT, and A/L. The basic material accounts for three (α, ρ, and k) a total of eight in all. Obviously, as values are given to these variables in a given problem, some dependence of one on another becomes apparent.

Figure of Merit "Z". Minimum temperature differential across the hot and the cold side of the module occurs when Qc is forced to be zero. This condition occurs when the cold face is perfectly insulated. Io the current for which maximum temperature difference occurs is called Optimum Current and it varies with the changes in temperature on cold side.

$$Io = \frac{\alpha_{pn} Tc}{R} \tag{15}$$

By substituting this in equation (12) we get

$$\Delta T \max = \frac{1}{2} \frac{\alpha_{pn}^{2}}{\rho_{pn} k_{pn}} \tag{16}$$

The three material properties which appear as a group in equation (16) are given the special name of "Z" also called "Figure of Merit."

Thus,

$$Z \quad \frac{\alpha_{pn}^{2}}{\rho_{pn} k_{pn}} \tag{17}$$

Application of Thermoelectricity in Temperature Measurement. The Peltier and Thomson e.m.f.s. combine to produce a potential difference across the cold junctions of a thermocouple. This p.d. is proportional to the temperature of the hot junction and can be used for temperature measurement if the cold junction temperature can be maintained constant. Various metals are available but the right combination would depend on the temperature range for which the couple will have to be used. For temperatures up to 600^{o}c Copper-Constantan couples are preferred (12).

The thermocouple is calibrated by knowing the p.d. for various temperatures at the hot junction and constant cold junction temperature. The measurement of the p.d. is done by either a potentiometer or a millivoltmeter. For accurate results, however, the potentiometer is preferred as it does away with error of resistance change of thermocouple on the leads. The potentiometer method, however, takes more time to record readings as it is difficult to balance the e.m.f.s. at each reading.

In metal cutting the tool tip temperature is measured either by the embedded thermocouple method or the tool-workpiece thermocouple method. In the former the thermocouple is brought as near to the cutting edge as possible and the e.m.f. measured as is convenient. The later method utilizes the small e.m.f. generated between tool and workpiece. The tool-chip interface develops high temperatures (13). The tool as well as the workpiece are electrically insulated from the rest of the machine. The tool-workpiece thermocouple method is the most accurate and practical available to date, but it has serious limitations due to the fact that the workpiece as well as the tool should have their cold ends at the same constant temperature. Also, it is not always possible to electrically isolate the tool from the rest of the machine.

DESIGN OF THERMOELECTRIC COOLING SYSTEM FOR CUTTING TOOL

Design Objectives.

1. To increase the heat conductivity of the tool shank so as to promote fast drainage heat from the tool-chip interface to the main body of tool, thereby reducing tool-chip interface temperature.

2. To increase the temperature gradient across the path of heat flow, so as to accelerate the transfer of heat through the path. This will be achieved by lowering the shank temperature to a value appreciably below ambient temperature.

3. To provide a means of removing heat from the shank to the atmosphere. The thermoelectric cooling module will perform this function.

4. To insulate in all possible ways, parts which will be at below atmosphere temperatures (during operation of unit) from the ambient.

5. To provide a means of renewing the cutting edge without distributing the shank and its arrangements for cooling. This will be achieved by using disposable inserts for cutting tips.

6. To design the shank from the strength viewpoint, using a material of high thermal conductivity to satisfy objective (1) and compensating by increased cross-sectional area the possible reduction in strength due to usage of weaker material (e.g., aluminum).

Design of Thermoelectric Cooling Capacity Requirements.

1. The temperature rise across the heat sink from water at ambient temperature to the hot junction of the module is estimated at $8^{o}c$. Thus,

$Th = 22 + 8 = 30^{0}c.$

We need a temperature of about $-30^{0}c$ on cold side of the module. This is also the maximum achievable value (i.e., ΔT max = $60^{0}c$) according to the manufacturer of the modules.

2. $\therefore$ $\Delta T = Th = Tc = 30-(-30) = 60^{0}c.$

3. To determine the capacity requirements we shall make use of the graphs and data from "Sintered Frigistors Performance Data," a leaflet supplied by Frigistor Ltd., Montreal, Canada, whose modules were used in this project. The graphs are reproduced in Figures 5, 6, and 7. Drawing a verticle line at $\Delta T = 60^{0}c$ we find on Figure 6 that (14) at this ΔT the COP (coefficient of performance of the module) is the highest for 20 amps. We, therefore, select an operating current value of 20 amps.

4. From Figure 5, cooling capacity Qc per couple = 1.6 watts, and for 20 amps at $\Delta T = 0$, the cooling capacity (denoted by Q_c^1) per couple = 1.35 watts.

5. The corrected value of Q_c for $T_c = -30^{0}c$ is, therefore, (14) given by:

$$Qct = Q_c + Q_c^1 \;\; 0.006\, T_c$$

$$= 1.6 + 1.35 \times 0.006 \times (-30^{0})$$

$$= 1.6 -.02430$$

$$= 1.5757 \text{ watts/couple.}$$

6. Our heat generated at the tool tip is at a rate of about 50 watts. This wattage is to be extracted by the cooling unit.

7. The number of couples to be used,

$$n = \frac{50}{Qct} = \frac{50}{1.5757} =$$

We will use three 1FB - 12 - 030 - D1 Frigistor modules,

$\therefore$ Number of couples used actually = 12 x 3 = 36.

From Figure 7 for 60^0 temperature difference voltage required is 100 millivolts/couple.

Correcting this for $T_c = -30^0c$

$$\begin{aligned} V_t &= 100 + 0.034 \text{ I } T_c \\ &= 100 + 0.034 \times 20\ (-30) \\ &= 100 - 20.4 \\ &= 80 \text{ mV.} \end{aligned}$$

For 36 couples in series $V_{total} = 80 \times 36 = 2.880$.

Thus, a power supply of about 3 to 4 volts capacity will be required. The three modules that will be used have the following specifications (14),

Manufacturers Number: 1FB - 12 - 030 - D1

Cooling Capacity/module at Th - 27^0c, $\Delta T = 0^0c$ and I = 15A, is 22.3 watts or 76 BTU/hour, that is 1.85 watts/couple or 6.3 BTU/hour/couple.

Cooling capacity/module at Tc = 0^0c, $\Delta T = 0^0c$, and I = 15A, is 19.2 watts or 65 BTU/hour, that is 1.6 watts/couple or 5.5 BTU/hour/couple.

Total number of couples per module = 12. The cold and hot sides of the module are bonded with anodized aluminum plates.

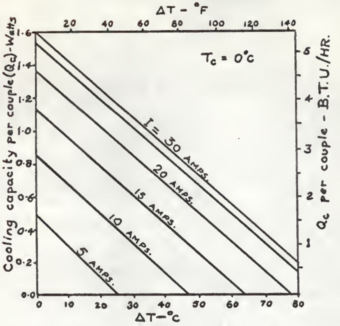


FIGURE 5 (14)

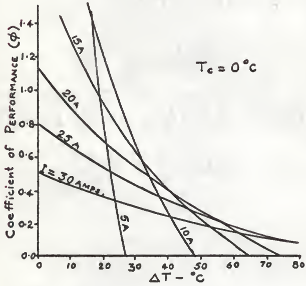


FIGURE 6 (14)

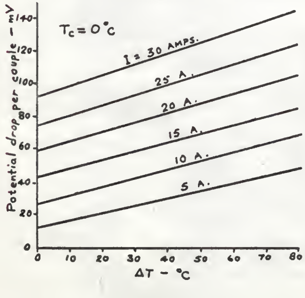


FIGURE 7 (14)

The anodized plate has a surface film of aluminum oxide which acts as an electrical insulator but maintains the heat conducting properties of aluminum. (20) This is very essential to prevent leakage of electricity from the module to the heat sink or the tool shank and at the same time to leave an unobstructed flow of heat across the hot and cold sides of the module.

Design of Heat Sink. A fundamental aspect of the Seebeck effect is that when current flows through a bimetallic circuit, one junction gets cold whereas the other gets heated up. This heating effect is a major problem in thermoelectric cooling device applications. The heat generated in the module as well as that pumped over from the cold side has to be dissipated on the hot side (19). A further property of the modules is that lower the hot side temperature, the lower is the cold side temperature. This is an obvious advantage and points out the necessity for careful design of heat extraction method at the hot side of the module.

The three modules in our design are water cooled on their hot side since water supply is readily available near the lathe. The water cooled heat sink has been shown to be more efficient than air-cooled types, which normally consist of a row of fins clamped on to the hot side of the module with or without air being forced over them.

Care must be taken to see that the water used for cooling does not get heated up after several times of recirculation over the hot side. If possible, a continuous flow from the faucet, with water exhausted from the cooling units being discarded must be used, as it will result in a more constant temperature of the cooling water. In the experiments conducted for testing the cooling of the cutting tool, a centrifugal pump was used to circulate water in the heat sinks (Figure 10). A large

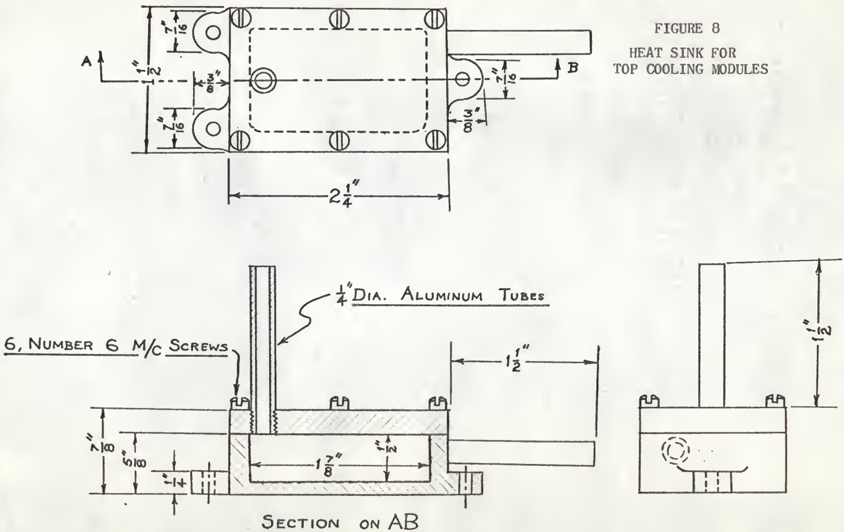


FIGURE 8

HEAT SINK FOR
TOP COOLING MODULES

water container was used under the suction pipe of the pump, and the water in it was changed after every 20 minutes of pump operation.

The heat sinks themselves were made of aluminum so as to get good heat conductivity, as well as for the ease of manufacture. The design is shown in the Figures 8 and 9. To facilitate heat transfer across the aluminum, the portion of the heat sink in contact with the hot side of the thermoelectric module has to be made as thin as is possible without sacrificing mechanical strength. This thickness was approximately assumed to be $\frac{1}{8}$". The temperature gradient across this portion of the aluminum between the hot side of the module and the cooling water at ambient temperature was estimated to be 8^{0}c.

The inlet and outlet tubing were of plastic and connected to the heat sink by slipping them on to 2" lengths of ¼" aluminum tubings which were threaded into the heat sink material and glued on water tight. The top cover of the heat sink was held in place by six number 6 machine screws and had impregnated paper gasket at the joining faces to make the entire joint completely watertight. The inlets and the outlets were so positioned that the water would enter at the lowest and leave at the highest possible points of the heat sink. This would let the hottest water (which tends to rise to the top) to be excluded rapidly via the rightly located outlet connection.

Design of Tool Shank. In order to be able to renew the cutting edge on the tool tip without disturbing the shank and its arrangements for cooling replaceable carbide inserts must be used. The design of the tool shank must, therefore, be such that the shank will rigidly hold the cutting tip during the cutting operation, yet be easily removeable. This was

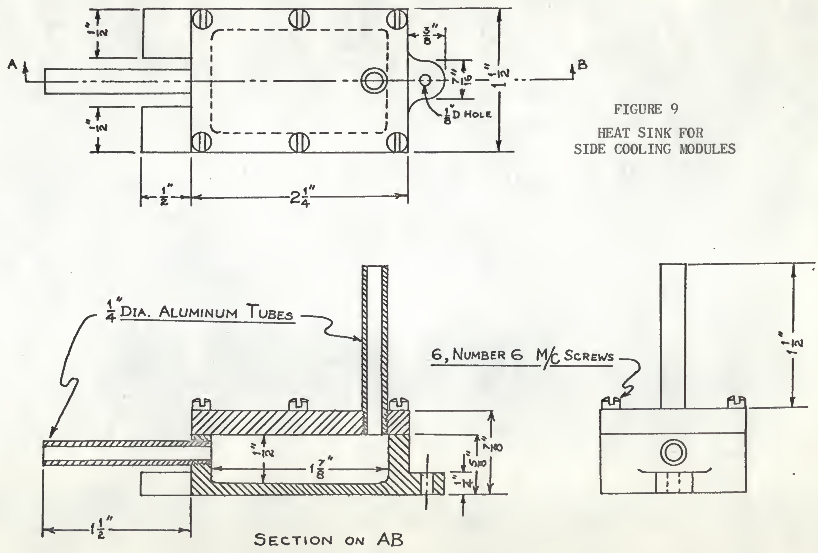


FIGURE 9

HEAT SINK FOR
SIDE COOLING MODULES

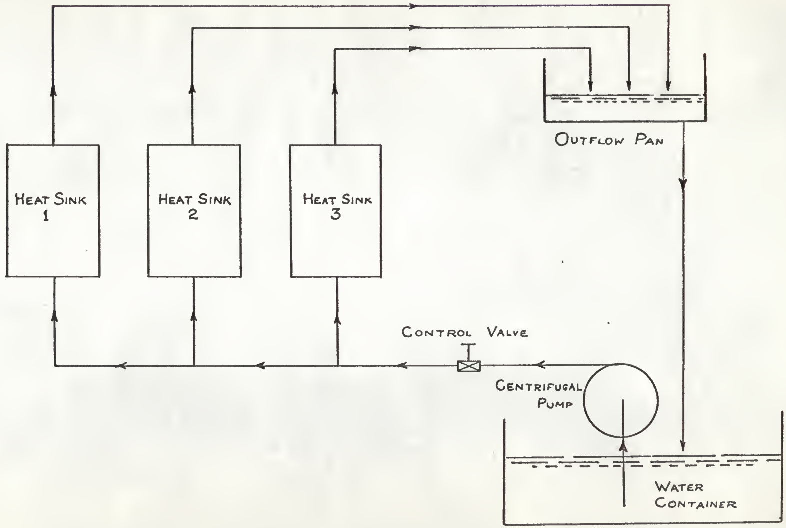


FIGURE 10

SCHEMATIC DIAGRAM OF WATER FLOW PATH

achieved by using the General Electric's Carboloy inserts with the company's holding arrangement (Carbo-O-Lock). This was manufactured in place by Metallurgical Products department of General Electric Co..

The cross-section of the shank was restricted to 1½" x 1½", as the thermoelectric cooling units which were to mounted on it had a width of 1½". The tool was to be clamped down rigidly and the only feasible solution was to use bolts through the body of the shank as all the three faces of the 1½" x 1½" block (top and two sides) were reserved to be placed in contact with the cold sides of the thermoelectric modules. The cutting tip was General Electric' Carboloy Insert.

The shank therefore constituted of an 1½" x 1½" aluminum block about 2¼" long (length of the thermoelectric modules) with an elongation of $\frac{3}{4}$" in the front in the shape of a triangular prism which would be the portion to mount the tool tip on. The details of shape and dimensions are given in the Figure 11.

The sides and the top of the tool shank was made into as truly plane a surface as was possible and then polished by hand on fine emery cloth. This was essential to form a good thermal joint between the cold side of the module and the tool shank. While assembling the coolers on the shank the mating surfaces were greased with Silicone grease. This provides a good interface contact and facilitates flow of heat across the joint.

The tentatively determined dimensions of the section of the tool 1½" x 1½" were varified from the strength viewpoint. The loading on the shank is as shown in the Figure 12. The forces F_t and F_f, the tangential and feeding forces respectively are assumed to be 400 pounds approximately from the following analysis.

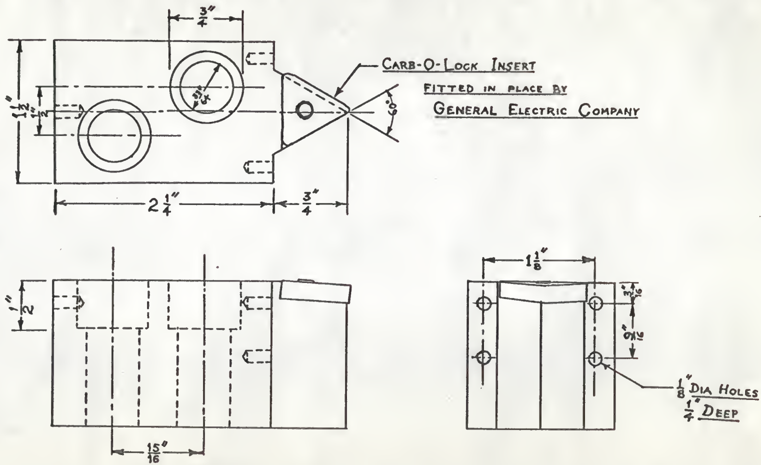


FIGURE 11

TOOL SHANK WITH CARBIDE INSERT

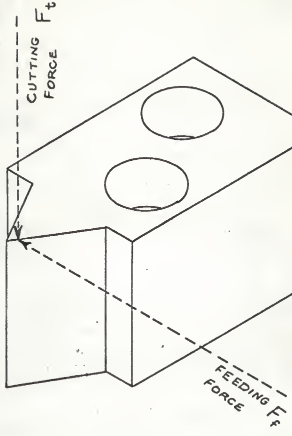


FIGURE 12
CUTTING FORCES AT TOOL TIP

The turning horsepower is a product of the tangential tool force and the cutting speed and it constitutes approximately 99% of the total power required by the machine tool. The h.p. rating of the Reed and Prentice lathe to be used for experimentation is 5 h.p. Assuming a top speed of 400 surface feet per minute the tangential cutting force can be computed from (15);

$$\text{Force } F_t = \frac{\text{h.p. x } 33000}{\text{speed}}$$

$$= \frac{5 \times 33000}{400}$$

$= 412$ approximately 400 pounds for design purposes.

By running trial runs with a dynamometer the maximum value of the side feeding force were obtained as 392 pounds. This was rounded off to 400 pounds also.

The weakest sections of the tool shank would be the one where the triangular prism shaped portion joins the rectangular block. At this section the forces of 400 pounds will cause bending of the horizontal and vertical planes of the tool material. The bending will, therefore, be a combined bending and will tend to incline the neutral axis of the section which can be considered the end of a cantilever beam with combined horizontal and vertical loading. A rigidly accurate analysis of the stresses at the section would therefore be extremely complicated and unnecessary for our purposes. Therefore, an approximation will be used by considering the stresses at the section to be those which would exist if a force of 400 pounds were acting vertically at the tool tip and a force of 600 pounds ($400 \times \frac{3}{2}$) acting at the section causing shear.

The section under consideration has the dimensions of $\frac{7}{8}$" x $1\frac{1}{4}$", the $\frac{7}{8}$" being the bredth and $1\frac{1}{4}$" being the depth. The bending moment due to the force of 400 pounds would be 400 x $\frac{3}{4}$" = 300 lb. in. = M .

The tensile stress caused by this bending moment at the section

$$= f_t = \frac{M}{Z} = \frac{M}{\frac{1}{6}\,bd^2}$$

where f_t = maximum tensile stress

M = bending moment

b = width of section

d = depth of section,

$$f_t = \frac{300}{\frac{1}{6} \times \frac{7}{8} \times (\frac{5}{4})^2}$$

$$= 9.13 \text{ lbs./square inch.}$$

The shear stress due to a force of 600 pounds at the section is given by,

$$= \qquad f_s = \frac{600}{\text{area}} = \frac{600}{\frac{7}{8} \times \frac{5}{4}}$$

$$= 34.3 \text{ lbs./square inch.}$$

The combined stress at the section in shear (16),

$$S_{smax} = \frac{1}{2}\sqrt{f_t^{\,2} + 4f_s^{\,2}}$$

$$= \frac{1}{2}\sqrt{(9.13)^2 + 4\,(34.5)^2}$$

$$= 69.4 \text{ lbs./square inch.}$$

The maximum permissible shear strength of aluminum for design purposes is very much higher than this value. Therefore, the design is safe. In addition, the extra margin of strength will impart rigidity to the tool shank which is so very essential in cutting with carbide tools. This will also enable the hole to be drilled in the tip of the shank to accommodate the cam mechanism for holding the carbide inserts.

Holes $\frac{1}{8}$" diameter were drilled in the shank as shown in the Figure 11 to accommodate clamps, etc., for attachment of thermoelectric modules and the heat sinks to the shank. This is discussed under design of clamping arrangements.

Design of Holding Bolts. The holding arrangement for the shank consisted of two bolts which were fastened through the shank on to the tool post. The bolt holes were located for convenience of seating as shown in the Figure 11.

The bolts were of the Allan type so that the head would go below the top face of the shank,leaving an uninterrupted area for cooling purposes. The dimensions of the bolt were arrived at by the following analysis.

The bolts are subjected to two kinds of loading, (1) the shearing load due to side thrust of 400 pound and (2) the tensile force holding the shank down against the toppling action of 400 pound vertical cutting force about point C.

Let X_A = Shearing load on bolt A,

X_B = Shearing load on bolt B,

Y_A = Tensile load on bolt A,

Y_B = Tensile load on bolt B,

Y_C = Load at point C around which toppling action takes place.

The forces X_A and X_B can be found by taking moments about pt. B (Figure 13),

$$400 \times 2\frac{3}{8} = X_A \times \frac{15}{16}$$

$$X_A = 1150 \text{ lb.} \qquad (1)$$

$$X_B = 1150 \text{ lb.} - 400 = 750 \text{ lb.} \qquad (2)$$

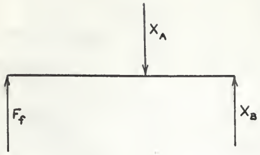


FIGURE 13
FORCES IN HORIZONTAL DIRECTION

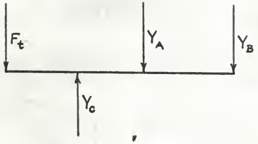


FIGURE 14
FORCES IN VERTICAL DIRECTION

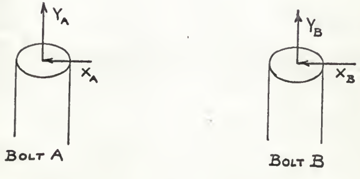


FIGURE 15
FORCES ACTING ON BOLTS A AND B

Forces Y_A and Y_B can be determined by taking moments about pts, A,B, and C (Figure 14),

$$400 + Y_A + Y_B = Y_C \qquad \Sigma Y = 0 \tag{3}$$

$$400 \times \frac{3}{4} = \frac{11}{16} Y_A + \frac{26}{16} Y_B \text{ .... moments about C} \tag{4}$$

$$400 \times 1\frac{7}{16} = \frac{11}{16} Y_C + \frac{15}{16} Y_B \text{.. moments about A} \tag{5}$$

$$400 \times 2\frac{3}{8} = \frac{26}{16} Y_C - \frac{15}{16} Y_A \text{.. moments about B.} \tag{6}$$

The above set of equations are insolvable for the three variables Y_A, Y_B, and Y_C, and result in redundency. To arrive at a solution an assumption viz. $Y_A = Y_B$ will have to be made.

$$\therefore \text{ from (4) } 300 = \frac{11}{16} Y_A + \frac{26}{16} Y_B$$

$$Y_A = Y_B = \frac{300 \times 16}{37} = 129.7 \text{ lb.}$$

The bolts, A and B, are therefore subjected to loadings X_A, Y_A and X_B and Y_B respectively as shown in Figure 15. The stresses in the bolts due to these loadings will be as follows.

For the design purposes only the more heavily stressed bolts will be considered. This is obviously bolt A. Let A = area at core dia. of bolt.

Direct shear stress = $S_s = \frac{1150}{A}$

Direct tensile stress = $S_t = \frac{129.7}{A}$

$$\text{Maximum principle shear stress} = \frac{1}{2}\sqrt{S_t^2 + S_s^2}$$

$$= \frac{1}{2}\sqrt{(129.7)^2 + (1150)^2}$$

$$= \frac{577}{A} \text{ lbs./square inch.}$$

Now the maximum permissible shear stress for bolt material is 8000 lbs./square inch (16),

$$\therefore \; 8000 = \frac{577}{A}$$

$$\therefore \; A = .0722$$

$\therefore$ core diameter of bolts dc $= \sqrt{\frac{.0722 \times 4}{\pi}}$

$= .301$ inches,

Thus actual nominal diameter $= \frac{.301}{.8}$

$= .377$ inches.

To ensure regidity in the system a size of $\frac{1}{2}$" diameter of bolt is selected and installed in the positions shown in Figure 11.

Design of Clamping Arrangement. A necessary prerequisite for satisfactory heat transfer across a thermal conductive joint is that the interfaces must be in intimate contact. In a poor joint, even with the best thermally conductive electrical insulator, practically all of the thermal resistance is at the interface. This is partly because the surfaces in many instances are in contact only at the peaks or asperities, reducing the actual area. High bolting pressures, very flat surfaces, soft foil, heat conductive silicon grease, etc., help increase conductivity across the joint many times over (20).

From this, it is obvious that the clamping arrangement holding together the heat sinks, thermoelectric modules and the tool shank be properly designed. The anodized aluminum surfaces of the hot and cold side provide effective electrical insulation while preserving good thermal-conductive joints with the shank and the heat sink. The thermal conductivity of anodized coating is 2 to 10 btu/hour/feet^2/F^o/feet. Whereas, it has a useful dielectric strength of 500 to 1000 volts/mil. The bearing surfaces of the shank and the heat sink were ground flat and hand polished.

To hold the module and heat sink on to the shank, it is not advisable to put screw lugs on the module plates themselves, since the stress on a plate screwed to another surface may result in breakage of joints or branches in the thermoelement (21). The attachments toward the front of the tool shank have to be as small in size as possible to involve less interference with the rotating workpiece. Therefore, L shaped bolts were used to clamp the shank and the heat sink front ends with the module sandwiched in between as shown in Figure 17. At the rear end the space available was ample and C clamps of the type shown in Figure 16 were used. The L shaped bolts were avoided as this alternative was available, since the bolts result in a direct thermal short circuit between the hot and cold sides.

The rear of the shank which is an area of square shape $1\frac{1}{2}$" x $1\frac{1}{2}$" must be insulated from the ambient to prevent heat input from the ambient to the low temperature shank. This was done by introducing a piece of foam insulation also held in place by C clamps. The C clamp themselves acted on the extensions of heat sinks of the side modules and held the entire assembly in place from the rear. The clamping arrangement can be seen in photographs on pages 49 and 50.

The shank itself is to be held down as was decided earlier by two Allen bolts ($\frac{1}{2}$" diameter). The bottom face of the shank would constitute a heat leak source if it were to rest directly on the metal tool post. Therefore, the tool shank was clamped to the post with a $\frac{3}{8}$" sheet of Bakelite in between, Bakelite being a good thermal insulator with high mechanical strength.

The Allen bolts were screwed into a mild steel plate which had a shape conforming with the slot in the tool post. The plate and its dimensions are shown in Figure 18. The tapped holes for screws were so located

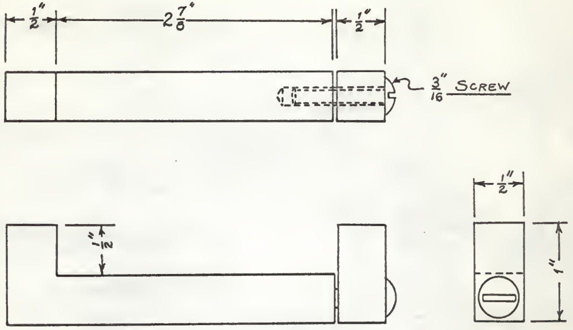


FIGURE 16

C - Clamps

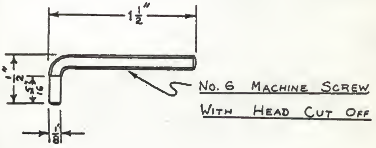


FIGURE 17

L - SHAPED BOLTS

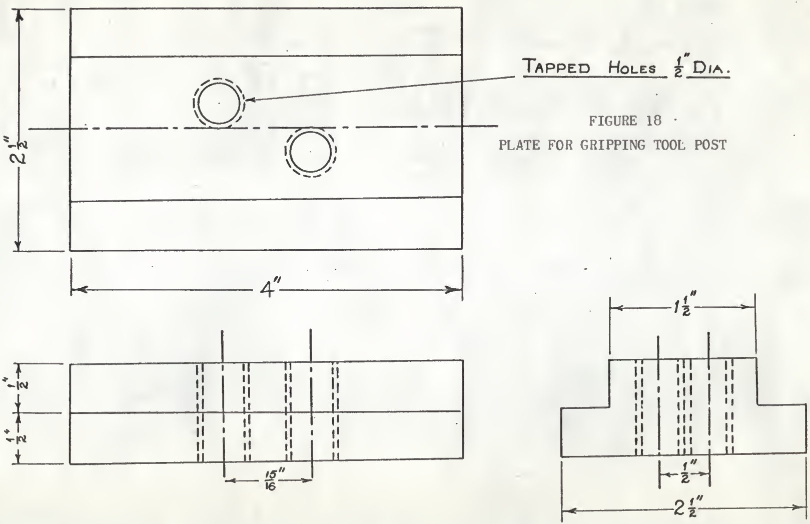


FIGURE 18

PLATE FOR GRIPPING TOOL POST

that the shank was inclined to the normal position of the tool post to an angle that would bring the cutting edge of the carbide insert perpendicular to the axis of the rotating workpiece. This would ensure orthogonal cutting conditions required for the experiment to be performed for purposes of testing.

Design of A.C. to D.C. Power Supply. The D.C. power requirements of thermoelectric heat pump devices are generally characterized by high input current and low voltage drop. The temperature difference (ΔT) of the module is influenced by the ripple of the D.C. input current. Any r.m.s. value of alternating component of the input current added to the average value would produce Joule heating but would not pump heat. Thus, if the ripple factor is high, Joule heating would tend to decrease the temperature difference. It has been found that the highest temperature difference reached by thermoelectric heat pump devices is unaffected and can be maintained when the ripple of the input current is equal to or less than ten percent. When the ripple is greater than ten percent, the temperature difference will begin to decrease (17).

Due to the high current and low voltage power requirements of thermoelectric units, the major power loss in the circuit occurs as rectifier dissipation. The full-wave center tap circuit is used in preference to the full-wave bridge circuit to reduce rectifier diode power loss. In addition, the center tap circuit permits mounting of all rectifier diodes on a common heat sink.

The D.C. voltage requirements of the three frigistors was determined at approximately three volts. A higher value of four volts was chosen to be on the safer side, for the capacity of the power supply.

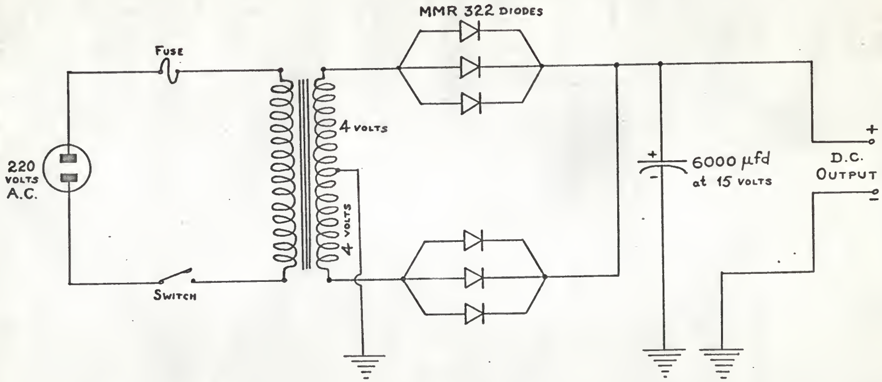


FIGURE 19

CIRCUIT OF AC - DC RECTIFYING UNIT

A transformer was made available to step down 220 volts 60 cycles per second, A.C. supply down to 8 volts, so that the voltage obtained at the center tap on the secondary was 4 volts. Since the use of silicon rectifier diodes results in high reliability, long life and minimum cost, they were incorporated in the design to take care of the rectification junction. Six diodes were used as shown in the circuit diagram, all sides being mounted on a common heat sink (Figure 19).

The ripple had to be reduced below 10% and since high capacity condensers were available more readily than comparable chokes, a capacitor type filter was chosen. The minimum required capacity value of capacitor filter for a full wave single-phase rectifier is given by the following formula (18),

$$r = \frac{1}{4\sqrt{3}\, fCR_e}$$

where f = frequency in cycles/second,

C = capacity in farads,

R_e = load resistance.

The load resistance R_e of the three Frigistors was about 0.2 ohms. Therefore, the available capacitor of 600 μfd at 15 volts was found to be of ample capacity. In actual operation the ripple obtained was as low as 6%.

The modules themselves were connected in series as shown in the circuit diagram (Figure 20) and a voltmeter and ammeter were incorporated.

To be able to get variable voltages at the output terminals, a variac was used on the primary of the transformer.

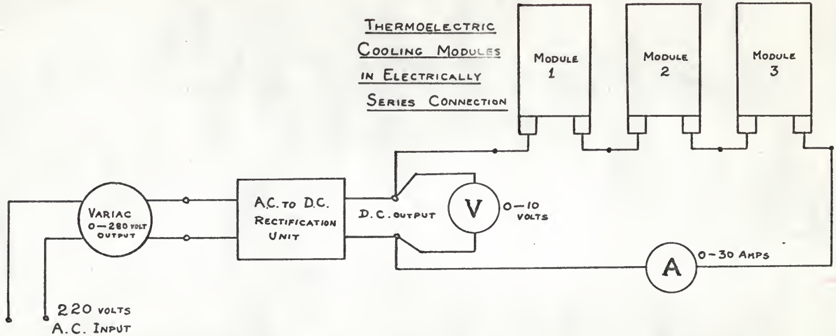


FIGURE 20

CIRCUIT DIAGRAM OF POWER SUPPLY TO MODULES

EXPERIMENTAL PERFORMANCE OF THE SYSTEM

The design shown in the foregoing pages was manufactured into a complete unit and was then tested for the efficiency in operation.

There are several variables involved which affect the performance of the unit. The three basic ones were the machining constants, namely cutting speed, feed and depth of cut. Besides these are the variables of the cooling system itself viz., current through the cooling modules, the quantity and temperature of water flowing through the heat sink. Thus, the temperature at the tool tip is a function of the effect of the interaction of these variables.

The temperature is a uniformly increasing function of the three machining constants, so that an increase in either the cutting speed or the feed or the depth of cut would result in an increase in temperature. The upper limits of these variables is fixed either by the tool capacity, determined by its strength, rigidity, and life, or by the capacity of the lathe, determined by its power capacity as well as its accuracy of alignment and degree of vibration-free operation. Whereas, carbide tools can cut at high efficiency at higher speeds, an inherent requirements is that the entire setup be vibration free and the workpiece rotate without transient deflections about its axis.

The workpiece selected for the purpose of testing the tool was a mild steel pipe (extra-heavy) with a mean diameter of $4\frac{1}{4}$" and a wall thickness of $\frac{5}{16}$ ". The cut was to be taken on one end of the length of pipe. The other end being grasped in the chuck of the lathe. Thus, an orthogonal cut with a depth of cut of $\frac{5}{16}$ " was possible. The lathe steady rest was used to provide support to the pipe in the center. The maximum safe speed achievable with this kind of a setup was 212 revolutions per minute when cutting with a feed of 0.015 inches per revolution. This was the maximum load condition under which the cooling unit can be tested. The unit was also tested under two other cutting conditions - a low load condition of 58 revolutions per minute and a feed of 0.005 inches per revolution and an intermediate load condition of 110 revolutions per minute and feed of 0.010 inches per revolution. The depth of cut remaining constant at $\frac{5}{16}$". The speeds in surface feet per minute under these conditions were therefore 64.5, 122.3 and 236, calculated at a mean diameter of $4\frac{1}{4}$".

The cooling water through the heat sinks was maintained at the same steady flow rate. This was achieved by keeping the control value at the same position throughout the entire experiment except when the cooling was switched off. The current through the modules was 20 amps according to our design for maximum coefficient of performance. Maximum cooling conditions were, therefore, fixed at 20 amps. Two other cooling conditions were investigated at 0 amps, and at 10 amps that is at no cooling and half-cooling capacity.

At each of these conditions - totalling 9 according to a combination of 3 cutting conditions and 3 cooling currents - the temperature at the tool tip was measured.

The measurement of temperature at the tool-chip interface exactly was impossible as the tool-chip thermocouple method could not be used. The temperature of the tool shank was variable according to the rate of transfer of heat from the tool tip to the cold side of the modules. This eliminated any possibilities of using the shank as one of the arms of a tool-work thermocouple. The insert itself could also not be used

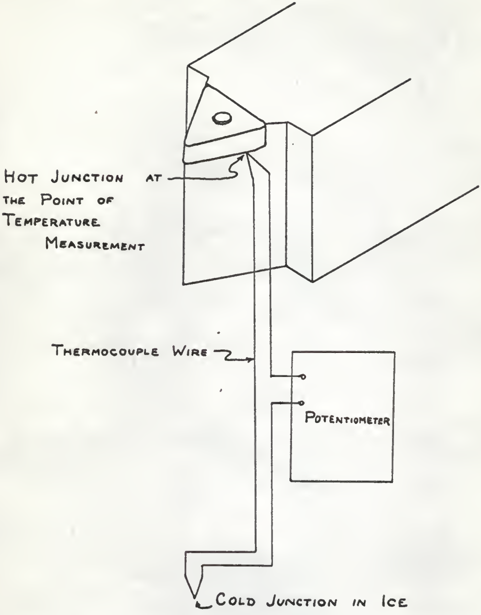


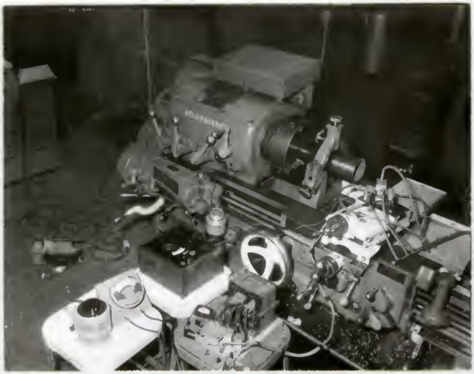
FIGURE 21
TEMPERATURE MEASUREMENT CIRCUIT

for the same reason and also for the fact that the insert would have to be electrically insulated from the rest of the shank to prevent leakage of thermoelectric current into the shank. This insulation, if attempted, would defeat our purpose of good thermal contact with the shank for purposes of rapid heat drainage from it. Thus, the temperatures were measured by an external thermocouple junction of copper-constantan wires and the aluminum shank at a point shown in the Figure 21. The temperatures were enumerated below:

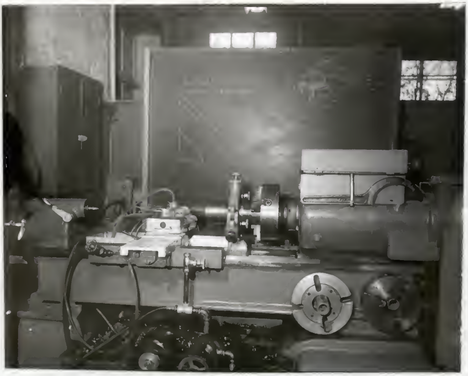
Condition Number	Cutting Condition	Current Through Cooling Unit
1	64.5 surface feet/minute .015"/revolutions	0 amps
2		10 amps
3		20 amps
4	122.3 feet/minute .010"/revolutions	0 amps
5		10 amps
6		20 amps
7	236 feet/minute .015"/revolutions	0 amps
8		10 amps
9		20 amps

The lathe was started and the temperatures were noted at 20 minute intervals timed by a stop watch, starting immediately at start of cut and noting readings until a steady state was established and the temperature settled down to a more or less constant quantity. The results are shown in tables and graphs.

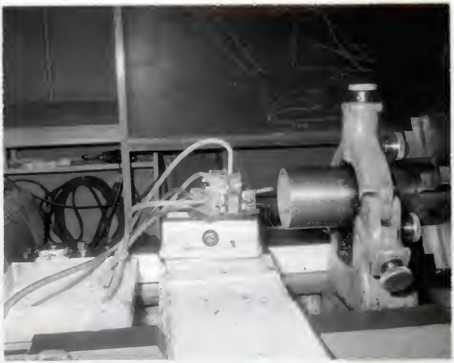
As can be noticed from the graph, the nature of temperature rise is experimental and it achieves a constant value when the heat generation rate and the cooling rate are balanced.



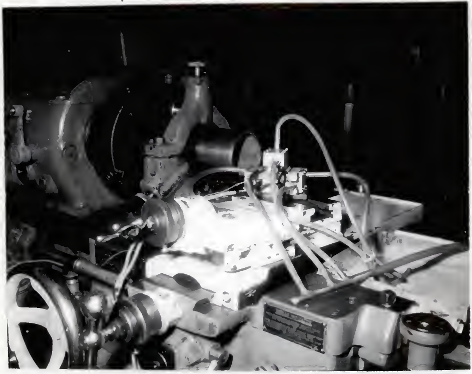
PHOTOGRAPH I: FRONT VIEW OF THE EXPERIMENTAL SETUP
SHOWING POTENTIOMETER CIRCUIT AND SOME OF THE COMPONENTS OF COOLING SYSTEM



PHOTOGRAPH II: REAR VIEW OF THE SETUP SHOWING WATER
CIRCULATING PUMP AND CONTROL VALVE



PHOTOGRAPH III: CUTTING HEAD ASSEMBLY
AND WORKPIECE, VIEWED FROM THE CARBIDE INSERT MOUNTED END



PHOTOGRAPH IV: REAR VIEW OF CUTTING HEAD
ASSEMBLY SHOWING WATER INLET AND OUTLET TUBING LEADING
TO THE HEAT SINKS AND THE ELECTRIC POWER SUPPLY LEADS TO THE MODULES

RESULTS AND DISCUSSIONS

The tables and graphs shown on pages 53 through 58 indicate the performance of the system under actual experimentation.

The plots of temperature versus time are exponential in nature and settle down to a steady value after a certain period of time has elapsed.

The effectiveness of the cooling system can be observed from the variation in the steady state values of the temperature with different rates of cooling viz., 0 amps, 10 amps, 20 amps. These values of temperature are, of course, the values at the base of the carbide insert as was shown in Figure and are of little significance as far as the tool life is concerned. The actual temperature, which does affect the wear rate of the tool and the period between regrinds, is the temperature at the tool-chip interface.

In order to get an idea of the actual temperature at the tool-chip interface, the following experiment was run. The relationship between the temperatures at the cutting point and the temperatures at the base of the insert where the measurements were made is shown in the Figure 22.

To simulate the cutting conditions exactly, (i.e., having a moving heat source, etc) would be extremely difficult, if not impossible. The only alternative, therefore, would be to utilize a steady heat source which would feed heat at a predetermined rate and cause the temperatures to rise both at the tool tip and at the base of the insert. Since no interface from chip movement etc., is to be encountered the temperatures can be conveniently measured at the tool tip, with the help of a thermocouple junction.

The heat source selected for the experimental calibration curves shown in Figure 22 was the heating element of an ordinary 250 watt electric soldering iron. The heating tip of the iron was filed to a rectangular shape, with the tip area approximately equal to the chip contact area. A small cavity was prepared at the edge of the face to accommodate the seating of the temperature measuring thermocouple. A separate potentiometer was used to measure the thermoelectric potential in millivolts generated due to the temperature difference between this junction and the cold junction which was dipped in ice.

The temperature rise curves shown in the graphs indicate during actual cutting the variation of the temperature with respect to time until a steady state is achieved. However, to duplicate the transient portion of the curve would need an exactly identical heat source with the rate of input of heat exactly identical to that under cutting conditions. This would be impractical due to the fact that it is not possible to exactly estimate the magnitude of the heat input when the chip glides over the tool face. Thus, the only reliable duplication of the cutting conditions can be the steady state operation. In other words, the only possible calibration that can be performed would be to apply the heat source, and wait for the temperatures at both the tool tip and the insert base to be steady. These values can be obtained for different heat inputs at the cutting tip. The heat inputs were varied by varying the voltage to the soldering iron with the help of a variac. Thus, a plot of the temperatures in millivolts at the tool tip versus those at the insert base can be obtained. In order to be able to predict the temperatures for all three cooling rates, this calibration plot was obtained with the currents in the cooling modules of 0, 10, 20 amperes. The curves are shown in Figure 22.

Referring to these curves the actual values at the tool tip under steady state conditions was obtained and is shown in the tables. The same were not obtainable for the 212 R.P.M. .015 feed set because the values of the insert base temperatures lie outside the range of the caliberation curves. This would necessitate the use of a very much stronger heat source. The measurement of temperature at the tool tip would in that event also be difficult as it would require a different thermocouple of higher capacity.

As such, the effectiveness of the cooling system is strikingly apparent both from the temperature size curves as well as the calibrated steady state values of the temperature at the tool tip with cooling, without cooling, and at half cooling current.

The temperature reduction is larger for the heavier cutting conditions. This is probably because of higher absolute temperatures at the tool tip and a resulting higher temperature gradient between the tool tip and the thermocooling units.

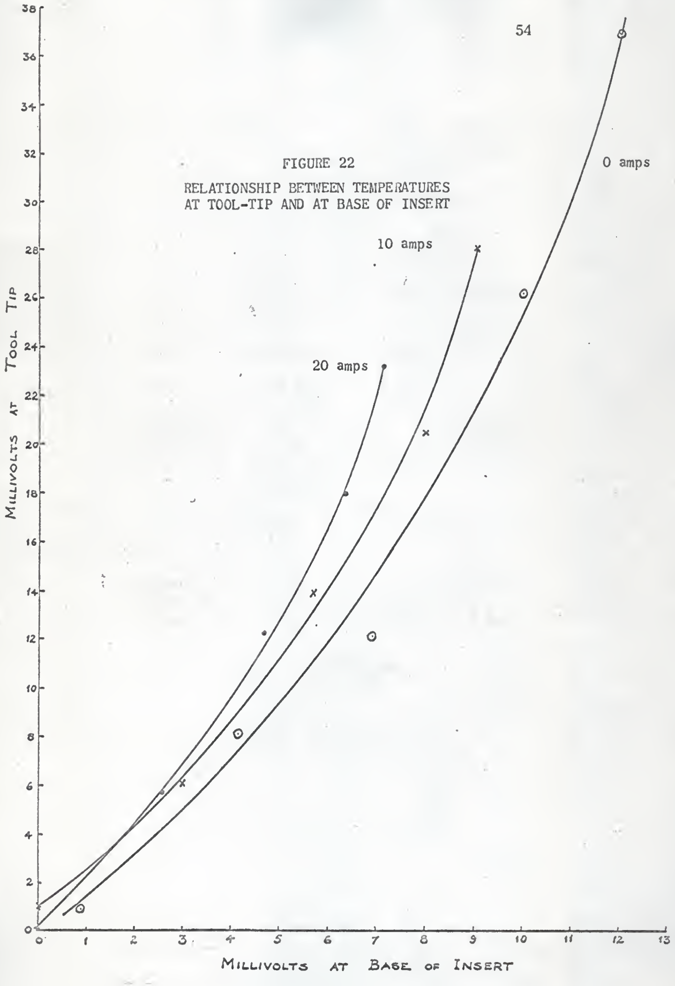


FIGURE 22
RELATIONSHIP BETWEEN TEMPERATURES AT TOOL-TIP AND AT BASE OF INSERT
0 amps
10 amps
20 amps
MILLIVOLTS AT TOOL TIP
MILLIVOLTS AT BASE OF INSERT
0 2 4 6 8 10 12 14 16 18 20 22 24 26 28 30 32 34 36 38
0 1 2 3 4 5 6 7 8 9 10 11 12 13

SPEED 64.5 SURFACE FEET/MINT. FEED .005/REVOLUTION

	Time in Minutes	Potentiometer Reading in Millivolts	Temperature in ^{0}C at Insert Base	Temperature in ^{0}C at T-C Interface
I = 0 amps (1)	0	.88	22	
	.20	3.79	89	
	.40	4.95	114	
	.60	5.45	124.5	
	.80	5.69	129.75	
	1.00	5.78	131.5	
	1.20	5.825	132	11 m V
	1.40	5.825	132	$223^{0}C$
	1.60	5.825	132	
I = 10 amps (2)	0	0	0	
	.20	2.14	52.5	
	.40	3.32	79	
	.60	3.36	86.25	
	.80	4	95	
	1.00	4.075	95.5	
	1.20	4.075	95.5	8.3 M V
	1.40	4.075	95.5	182 ^{0}C
	1.60	4.075	95.5	
I = 20 amps (3)	0	0	0	
	.20	1.04	26	
	.40	1.46	36.5	
	.60	2	49	
	.80	2.36	57.5	
	1.00	2.05	51	
	1.20	2.33	57	5.5 m V
	1.40	2.33	57	125 ^{0}C
	1.60	2.33	57	

FIGURE 23

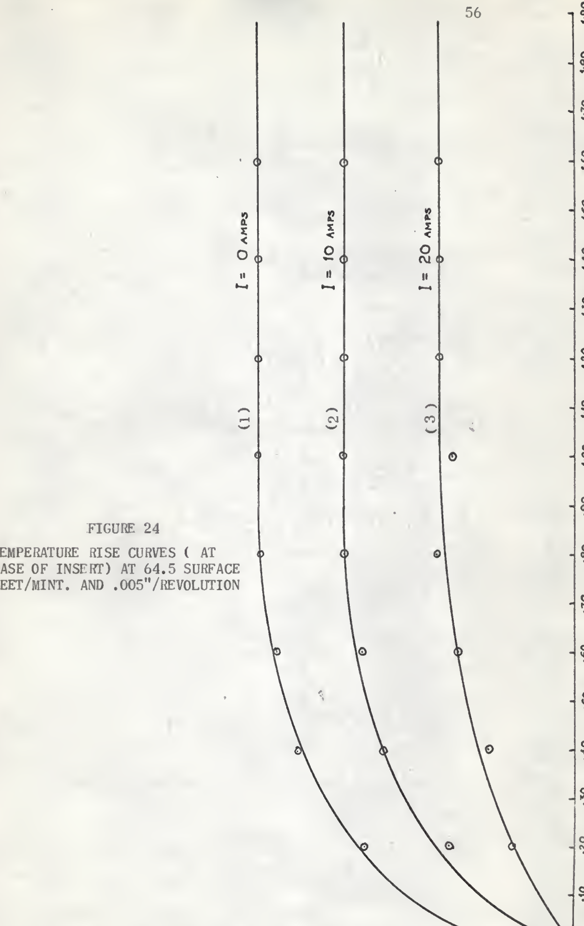


FIGURE 24

TEMPERATURE RISE CURVES (AT BASE OF INSERT) AT 64.5 SURFACE FEET/MINT. AND .005"/REVOLUTION

SPEED
122.3 FEET/MINUTE

FEED
.010"/REVOLUTION

	Time in Minutes	Potentiometer Reading in Millivolts	Temperature in ^oC at Insert Base	Temperature in oC at T-C Interface
I = 0 amps (4)	0	.88	22	
	.20	4.2	98.5	
	.40	6	136	
	.70	7.5	166	
	1.00	8	175.5	
	1.20	8.2	179	
	1.40	8.15	178	18.2 m V
	1.60	8.15	178	356 ^oC
	1.80	8.15	178	
I = 10 amps (5)	0	0	0	
	.20	2	49	
	.40	3.52	85.5	
	.60	4.225	99	
	.80	5.225	120	
	1.00	5.7	129.5	
	1.20	6	136	
	1.40	6.7	138	
	1.60	6.175	139.5	14.9 m V
	1.80	6.175	139.5	301 ^oC
I = 20 amps (6)	0	0	0	
	.20	1.14	28.75	
	.40	1.91	47.25	
	.60	2.9	70	
	.80	3.3	79	
	1.00	3.7	87.5	
	1.20	3.725	88	
	1.40	3.75	89	8.8 m V
	1.60	3.75	89	192 ^oC
	1.80	3.75	89	

FIGURE 25

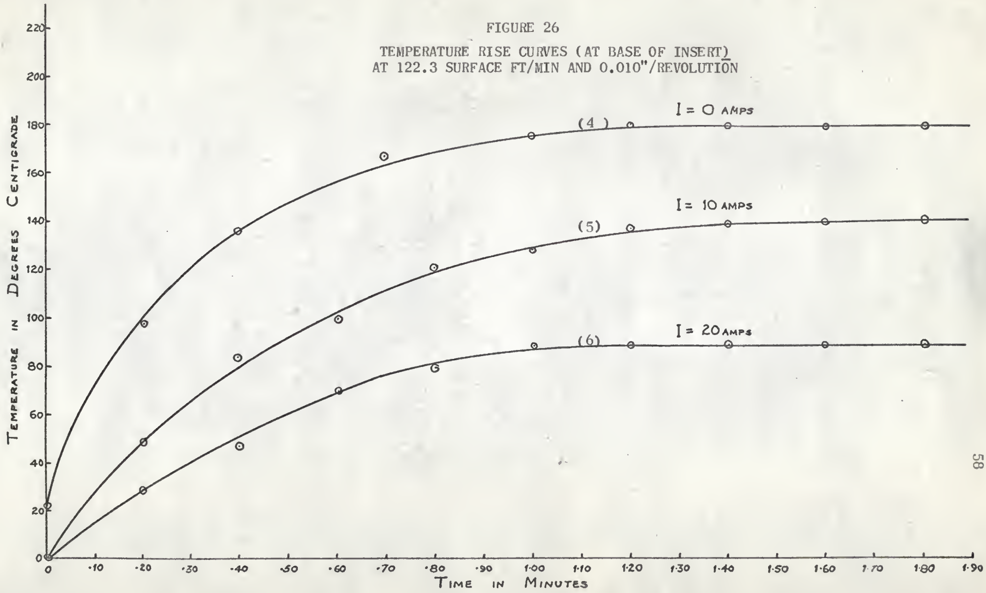


FIGURE 26

TEMPERATURE RISE CURVES (AT BASE OF INSERT)
AT 122.3 SURFACE FT/MIN AND 0.010"/REVOLUTION

SPEED
236 SURFACE FEET/MINUTE

FEED
.015/REVOLUTION

	Time in Minutes	Potentiometer Reading in Millivolts	Temperature in ^{0}C at Insert Base	Temperature in ^{0}C at T-C Interface
I = 0 amps (7)	0	.85	22	
	.20	7.17	159.5	
	.40	11.2	236	
	.60	14.15	287.5	
	.80	15.95	328.5	
	1.00	18.25	357	
	1.20	18.75	372.5	
	1.40	20.5	391	
	1.60	20.7	397	Not
	1.80	20.7	397	Determinable
I = 10 amps (8)	0	0	0	
	.20	3.8	89.5	
	.40	5.85	133	
	.60	8.8	191	
	.80	9.3	200.5	
	1.00	11	232	
	1.20	11.66	243.75	
	1.40	12.1	252	
	1.60	12.575	260	Not
	1.80	12.575	260	Determinable
I = 20 amps (9)	0	0	0	
	.20	2.15	53	
	.40	3.75	88.25	
	.60	5.6	128	
	.80	5.8	132	
	1.00	6	135	
	1.20	7.7	170	
	1.40	7.6	168	
	1.60	7.77	171	Not
	1.80	7.77	171	Determinable

FIGURE 27

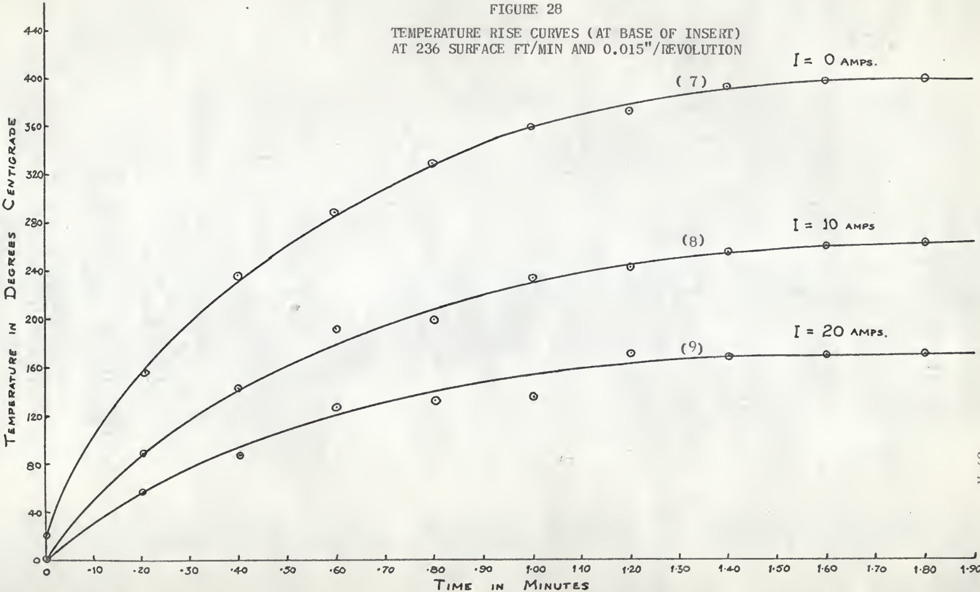


FIGURE 28

TEMPERATURE RISE CURVES (AT BASE OF INSERT)
AT 236 SURFACE FT/MIN AND 0.015"/REVOLUTION

CONCLUSIONS

The thermoelectric cooling system seems to work very effectively in reducing the temperature at the tool point. The cooling seems to be more pronounced at higher speeds and feed in so far as the absolute temperature reduction is concerned.

The rate of cooling can be varied by varying the current flow through the thermoelectric modules. A step wise variation of the current can be used to incorporate a control system which can maintain the tool tip at a predetermined temperature.

The temperatures at the tool tip can be further reduced by using more cooling modules in a cascade arrangement, resulting in lower cold side temperatures. In that event, the cold portions of the system will have to be better insulated from the ambient.

Whereas, thermoelectric cooling system does away with cutting fluids and takes over the function of cooling the cutting tool, it also eliminates the lubricating function performed by the cutting fluid. Thus, an additional tendency for temperature rise is introduced. It would be of interest to use a cutting fluid in conjunction with thermoelectric cooling and determine if any marked improvement is obtainable. At higher speeds, however, the cutting fluid may not have much usefulness.

Thermoelectric cooling appears to be especially suitable for automated or long production runs. Longer tool life obtained due to lower temperature should result in larger cost reductions and higher turning speeds. It should also prove useful in machining hard-to-machine alloys where intense heat is generated at the cutting point.

A disadvantage, although not too serious, is the bulkiness of the unit. It may be difficult to use the system with tools which have to work at corners and at hard-to-get-at places.

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THERMOELECTRIC COOLING OF A LATHE CUTTING TOOL

by

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The most common method used for cooling the tool tip in metal cutting operations is the use of a jet of cutting fluid. This method, however, is not sufficient at higher speeds and feeds. Consequently, high temperatures restrict the full utilization of machine tool capacity and reduce tool life.

The idea of thermoelectric cooling for cutting tools has received little attention and no technical publications exist on the technique of application. The system utilizes Peltier effect phenomenon which consists of generation or absorption (depending on the direction of the current) of heat, at the junction between two different conductors when a current flows through them. "Frigistor" modules were used on an aluminum tool shank on which was mounted the carbide insert.

The objectives for the design of the system were; to increase heat conductivity of tool shank; to increase the temperature gradient across the path of heat flow; to provide an effective means of removing heat from the shank to the atmosphere; to insulate all cold parts from the ambient; to provide a means of renewing the cutting edge without disturbing the shank and its arrangement for cooling; and finally to design the shank itself to be of sufficient strength inspite of the use of weaker material viz. aluminum.

The design procedure is completely outlined in this thesis. A low voltage, high current power supply was also designed and the means for supplying the cooling water to the module heat sinks was provided.

The system was experimentally checked for effectiveness in operation at three cutting conditions, viz., (1) 64.5 s.f.p.m.; .005 inches/rev., (2) 122.3 s.f.p.m.; .010 inches/rev., (3) 236 s.f.p.m.; .015 inches/rev. Each test was run for three different cooling rates viz., (1) no cooling, (2) 10 amps through modules, (3) 20 amps. through modules. Temperatures were measured at 0.20 seconds intervals with a copper-constant thermocouple located at base of carbide insert until the readings settled down to a constant value. Using a soldering iron as a heat source to simulate the heat input under cutting conditions, the relationship between temperatures at the tool-tip and at the base of carbide insert was found.

The results of the test seem to indicate that the system is very effective, with cooling more pronounced at higher speeds and feeds. The cooling is proportional to the current fed to the modules.

The lubricating function of the cutting fluid is absent in the system and may be construed to be a disadvantage, as also is the bulkiness of the unit which may not permit all machining operations.

Thermoelectric cooling seems especially suitable for automated or long production runs and for machining hard-to-machine alloys where large amounts of heat are generated.