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SAN CARLOS, CALIFORNIA

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FINAL REPORT

HIGH TEMPERATURE MAGNETRON STUDY

Contract No. NObsr 77568

Index No. NE-110000/S.T. 16.4

Period Covered:

22 April 1959 To 28 February 1962

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NOTICE

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The data and conclusions reported herein are published for information only and do not reflect the opinions or recommendations of the sponsoring agency.

TABLE OF CONTENTS

TABLE OF CONTENTS (Continued)

LIST OF FIGURES

LIST OF FIGURES (CONTINUED)

1. INTRODUCTION.

The purpose of the work performed under this study program was to investigate those factors which affect the stable, arc-free operation of magnetrons; and those which, in general, improve magnetron operation and reliability. Emphasis was placed on the improvement of performance under the extreme conditions of high ambient temperature and high altitude. Additionally, there was an investigation into the financial practicality of tube repair and consideration of special design to enhance repairability.

During the planning stage of this program, a search was initiated for a suitable vehicle with which to reduce theoretical findings to practice. The 4J50 magnetron was finally chosen for this purpose. This is a non-tunable, medium-power, pulsed X-Band magnetron. The peak and average power are 250 KW and 250 watts respectively, and the pulse length for the various versions of this tube varies from 1/4 to [illegible] microseconds. This tube has an extensive history of reliable, long-lived operation under normal ambient conditions. Thus, it provided a firm basis for comparison under adverse environmental conditions. Since the 4J50 had never experienced arcing, gassing, or vane tip erosion under normal environmental conditions, it provided a graphic display for the effects of operation at elevated ambient temperature. It was found that normal production tubes, when subjected to elevated ambient temperatures, experienced catastrophic failure within an hour. The cause was found to be gas (predominately H_2) permeating through

the vacuum envelope.

To convert the 4J50 magnetron to our high temperature version of the tube (which we have designated the L-3458) we have made a number of design changes while keeping the tube outline fixed. All of the glass seals of the normal 4J50 were converted to high density alumina ceramic for the L-3458 design. This was not only to permit higher temperature processing and operation of the tube, but also to prevent permeation of helium which occurs through the glass at high temperatures. To prevent hydrogen permeation through the walls of the tube envelope (especially through the nickel bearing alloys) these surfaces were clad with copper and then nickel plated to inhibit corrosion. The bakeout temperature of the tube was increased from the normal Litton standard of 625° to $750^{\circ}C$, using a double envelope bakeout system described in Section III. The cathode emission surface was also improved, and the cathode support design was changed for better centering as described in Section IV. Since no vane tip erosion previously was experienced with the 4J50 design, no need was felt to change the design of the L-3458 vanes at the power level and pulse length required. However, the theoretical work indicated that the best design for higher power levels would be copper plated tungsten vanes (see Section III C). We recommend this, not because of the sparking problem which can be eliminated in any case, but rather because of impulse heating by collected electrons which can fracture the vane tip surface. This vane tip surface failure causes gas evolution from deep inside the metal, and localized heating of crystal boundaries can result in vaporization of the vane tips.

In addition to the tube design changes listed above which were directly aimed at converting the 4J50 to the high temperature L-3458 magnetron, we also copper-clad the pole pieces and improved their shape to increase the margin of safety on pole piece erosion caused by electrons leaking past the end hats. We also extensively expanded our pre-exhaust schedules to reduce the amount of occluded gas in the parts before assembly.

The above design changes resulted in a tube which operates at 350°C ambient temperature, without an attached ion pump. The normal 4J50 cannot even reach that temperature in an operating condition. After making these design changes, and conducting the studies as described throughout the remainder of this report, we built and delivered six L-3458 magnetrons. Within the limitations of the funding under this program, these tubes incorporated the best high temperature design we have evolved. There are several other design changes which could be made that would either increase the power capability of these tubes or increase the ambient temperature at which they would operate. These proposed changes are pointed out in the remainder of this report and in the recommendations for further work.

An extensive survey and study was made of the economic and technical feasibility of magnetron return and repair programs. This study is summarized in Section VI of this report. It is to be noted that the feasibility depends to a great extent on the initial cost of the tube relative to transportation and handling costs, and on the original design of the tube relative to ease of opening and closing of the vacuum envelope.

Our continuing literature search revealed a similar program at other companies under Air Force Contracts AF 33(616)6563, and AF 33(616)7394. These Air Force programs were considerably larger efforts than ours, and the tube which was redesigned for high temperature operation was very similar to the 4J50 magnetron. The technical emphasis under the Air Force programs were more on the side of gettering the gases which enter the vacuum envelope, while our emphasis has been on eliminating the cause of gas in the tube through work on gas permeation prevention, and on higher bakeout temperatures.

II. GENERAL DISCUSSION OF MAGNETRON RELIABILITY AND LIFE.

The major factors which are important in obtaining good magnetron reliability and long life are maintaining stable electrical and magnetic parameters, and maintaining a good vacuum in the tube.

The maintainance of stable electrical parameters can be further subdivided into control of cathode emission, voltage standoff capability, control of space-charge effects, and spurious mode excitations.

One of the major problems encountered in magnetrons is voltage break-down due to arcing between cathode and anode. The energy of the arc is dissipated in the form of heat on both anode (due to electrons) and cathode (due to positive ions). Arc heating is localized, and can cause differential temperature stresses, crystal growth, evaporation, and sputtering of molten metal. The fissures thus created release intersticial gases from deep in the metal that were not removable during normal fabrication outgassing. Since much of the evaporated and released material is of neutral charge, it remains in the tube vacuum after the arc has extinguished, and thus supplies the atoms necessary for future arcs due to ionization.

In addition, any metalic ions sprayed from the anode surface provide the same means for arcing as gasses. Further, should they become deposited on the cathode, they would restrict emission due to cathode "poisoning."

Another source of ions in an operating tube is the result of electron bombardment of either the cathode end-hats or the pole pieces. This occurs due to space-charge effects in the electron cloud between cathode and anode. Since electrons mutually repel each other in all directions, some of the electrons are pushed out of the interaction space along the dc magnetic field lines (axially). If no cathode end hats or other means of confinement is incorporated into the tube, these electrons will strike pole pieces or tube body parts with considerable kinetic energy. This kinetic energy is picked up by these electrons as soon as they leave the interaction space and become subjected to strong accelerating dc electric fields. The bombardment of pole pieces and body parts by such escaping electrons of high kinetic energy can cause serious outgassing and subsequent arcing problems.

Cathode end hats are provided, in some designs, to conteract these space charge effects. The end hats are held at cathode potential so that most electrons approaching them are repeled back into the interacting cloud. However, these end-hats intercept some electrons which can start secondary emission. These secondary emission electrons enter the interaction space, perturbing the normal electron interaction process, and thus contribute to instability problems.

In some designs where the pole pieces are exposed to electron bombardment, the pole pieces have actually been erroded to such an extent that the magnetic field in the interaction space was distorted.

This, of course, also contributes to instability. As with anode bombardment, the metalic ions thus produced can poison the cathode.

As power levels increase, so do the demands on the cathodes. No matter how good the tube, or how ideal its operating environment, its life will eventually be terminated by the failure of cathode emission. Problems normally encountered with cathodes include poisoning, separation of emitting material from its supporting structure, and in some cases outgassing from the hot cathode. Misalignment is another problem encountered with cathodes. The effect of this is to allow arcing and perturbation of the desired electron interaction process resulting in unwanted mode instability. Since a good vacuum is one of the most important pre-requisites for a good microwave tube, one has to provide tubes with vacuum envelopes which are not permeable at the operating and ambient temperature of the tubes.

One of the weakest physical points in any tube is the ceramic or glass-to-metal seals. A particularly vulnerable area is that of the rf output window. As a result of the rf losses, it becomes heated and expands. Because of differential expansion between the window and its support, stresses are set up which can crack the window. Another problem often encountered with windows is arcing due to localized high rf electric fields in their immediate vicinity. This problem becomes aggravated by the existence in and around windows of trapped modes, ghost modes, or standing waves due to poor matches. When the longitudinal rf voltage exceeds the dielectric capabilities of the window material, an arc occurs through

the window. This arc can crack the window resulting in a leak into the vacuum envelope. The presence of ions may enhance the window arcing problem.

The remaining areas of failure are generally caused by mechanical wear and breakage. Ruggedized designs can reduce the latter to a minimum. Wear is only a problem in mechanically tunable magnetrons where mechanical parts are moved to adjust frequency.

All of the aforementioned factors are adversely affected by increase in temperature. However, gasses within the vacuum envelope appear to be the most critical problem associated with reliability and life. Rates of outgassing and gas permeation accelerate with increase in ambient temperature. In addition, crystal growth, resulting in structural weakness, increases with temperature. Cathode temperature also increases directly as a function of ambient temperature, and all tube surfaces become more susceptable to electron bombardment erosion. Furthermore, the pressure in the tube will generally rise and arcs will become more probable, especially since the secondary emission ratios also generally increase with increased temperature.

A more serious problem with cathodes than gas evolution, is the shortening of their life span at temperatures beyond that normally encountered. The emitting material in most cathodes tends to boil off, and in addition, susceptability to poisoning becomes more pronounced at elevated temperatures.

The problems of mechanical wear and metal fatigue are also increased by high ambient temperature. Two areas that become most critical are the thermal degaussing of the magnet and the corrosive oxidation of the vacuum envelope.

This, then, is the framework upon which the study under this contract is based. Subsequent sections will delineate our experimental and theoretical investigation in terms of these problems and their solution.

III. REDUCTION OF GASSING AND ARCING.

A. Relation of Gassing and Arcing To High Temperature Operation.

Tube operation in high ambient temperature is particularly plagued by the related phenomena of arcing and gassing. Gassing in a tube generally occurs via two mechanisms. One is outgassing of materials that are within the vacuum envelope, and the other is permeation of the vacuum envelope by gasses from the atmosphere surrounding the tube.

1. Outgassing.

Reduction of outgassing can be approached in several ways. A more detailed discussion of the physical phenomena of gas solubility, absorption, and adsorption of metals will be helpful in understanding the reasons behind the techniques used in reducing the outgassing problems.

The crystal structure of a machined part is far from the ordered situation of a single crystal. Imperfections such as vacant lattice sites, dislocations, and foreign atoms either in interstitial or substitutional positions are possible. All of these factors distort the lattice, and produce more unoccupied volume in the metal in which foreign, i.e., gas, molecules may lodge. Different gasses have varying degrees of solubility in a particular metal because of (1) the size of the metal atoms relative to gas atoms, (2) the lattice distance, and (3) the chemical affinity of the gas for the metal. The gasses found in greatest abundance in metals are CO, CO_2, H_2, O_2, and N_2.

The solubility of a gas in a metal usually decreases with increasing temperature (except for special cases where chemical reactions of gasses with metals are involved). As a tube is operated, some of the internal parts become heated, and the gasses within the metal lattice migrate to the surface and are released into the tube vacuum. This results in arcing, possible poisoning of the cathode, and interference with electron trajectories due to the reduction of space charge by positive ions. Therefore, wherever possible, the use of vacuum-processed, high-purity metals should be used. Elimination of gaseous contact with the molten metal produces a much cleaner metal with considerably less metal oxide and nitride formation.

Vacuum firing of all the tube parts before assembly is very important, especially those which are heated by electron bombardment, rf currents, or by the cathode heater during operation. In many cases the tube parts can be outgassed better in pre-exhaust vacuum firing than in the final exhaust. During pre-exhaust, they can be fired at their maximum allowable temperature, whereas in a finished tube the temperature of firing is limited by the least refractory material in the assembly. After vacuum firing, the parts must be stored in vacuum or an inert gas atmosphere. Materials such as stainless steel used for pole pieces, which contains large amounts of N_2, CO, and H_2, can be processed at 1000°C in vacuum, a temperature that could not be realized with the pole pieces in a copper tube.

This type of pre-exhaust eliminates more than the gas that is just adsorbed on the surface; it forces out gas that is deep within the material. The rate of outgassing is an exponential function of exhaust temperature. Therefore for a given standard of "cleanliness" this pre-exhaust is a very favorable process economically, in that the time that the tube will be on exhaust at lower "exhaust" temperature will be considerably reduced. Furthermore, a higher degree of cleanliness may be obtained in this way than could ever be practically achieved by relying solely on final tube exhaust, regardless of time and economics.

Another contributor to gassing is the cathode. Here the gasses may arise as products of chemical reaction as well as from outgassing of the materials. The former will be considered in more detail later. The latter problem can be reduced by pre-glowing the cathode assembly before the tube is exhausted.

There are situations in which it is not practical to pre-exhaust a particular part because of its slow outgassing rate or the large quantity of gas held within it. We felt that this was the case with "405" stainless steel pole pieces used for "high temperature" magnetrons. To solve this problem, we first vacuum fired the pole pieces and then clad them with a thin shield of copper through which gasses diffuse much more slowly than through the stainless steel.

After every effort has been made to buy clean materials, pre-exhaust the parts of the tube, and assemble them under the cleanest of conditions, one must then bake out the tube at as high a temperature as possible during exhaust. Historically, a 625°C exhaust run appears to be adequate for normal ambient temperature operation. However, if the tube will be in a 350°C ambient temperature, the exhaust temperature should be 750°C-800°C.

The combining of all of these practices will produce a tube with a minimum of problems due to gasses contained within its vacuum envelope.

2. Permeation.

a. General Discussion.

The second mechanism by which gas evolves into the interior of a vacuum tube is permeation. This process includes the disolving of gas on the high pressure side of a membrane (with gas dissociation in the case of diatomic gas molecules), diffusion through the metal membrane, and dissolution and recombination on the vacuum side of the membrane.

It is an established fact that glasses are extremely permeable to helium at temperatures above 400°C, and that they are one to two orders of magnitude less permeable to hydrogen at this

temperature[1]. Permeation in glass does not involve gas dissociation. Ceramics are not as susceptible to permeation by hydrogen or helium as are the glasses. Because of this low permeability and higher strength at elevated temperatures, ceramic has replaced all glass parts for severe environment applications.

The metals most commonly used as part of magnetron vacuum envelopes are copper, nickel, monel, kovar, stainless steel, and sometimes other nickel-bearing alloys. It is well known that noble gasses do not permeate metals. It is thought that metallic permeation is quite different than glas permeation, in that metallic permeation involves gas dissociation before diffusion.

Hydrogen represents the most serious problem in gas permeation through metals. At atmospheric pressure, the partial pressure due to hydrogen is 1×10^{-1}Torr. Nickel-bearing alloys are especially permeable to hydrogen. When we first operated standard 4J50 magnetrons at elevated temperatures, the tube became gassy and inoperative before 350°C ambient temperature was reached*. The blue glow that

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*See Section III On Tube Tests

was found in this tube was shown spectroscopically to consist mainly of hydrogen. After this test, it was obvious that the elimination of hydrogen permeation through the nickel-bearing alloys of the tube envelope was one of the most important design changes necessary in converting the 4J50 to a high temperature design.

We first made a literature search to find the permeation rates of typical construction materials. Several important factors came to light during this investigation. First, the surface preparation can greatly affect the permeation rate[2,3]. Also, the heat and mechanical treatment have a pronounced effect, and trace impurities in some cases can change the permeation rate of the bulk solid. However, the lack of information on the type and purity of metal we use convinced us that the only useful information on permeation rates would have to come from our own experiments.

We first attempted to make these measurements with an existing vacuum station. It soon became apparent that the existing equipment would not allow, within reasonable time limits, accurate differentiation between the various low rates of permeation. Consequently, it was necessary to design a special test apparatus.

The following discussion will serve to define the design criteria for this apparatus.

Barrer[1], and Jost[4] discuss the permeation of solids by gasses in some detail, and the results of their work are used here. The solution of the permeation equation for a hollow-walled cylinder, with a diameter large compared to the wall thickness, is essentially the same as the solution for a flat plate. For the planar case, it is found that S, the rate of flow of gas (atm-cm^3-sec^{-1}) permeating through an area, A, at a constant temperature, is given by:

$$S = - AD \frac{C_1 - C_2}{d} \tag{1}$$

where D is the diffusion constant, C_1 and C_2 are the concentrations of gas on the low pressure and high pressure sides of the membrane respectively, and d is the thickness in mm. The concentration of gas is proportional to the square root of the pressure for diatomic gasses.

Eqn. (1) then becomes:

$$S = AkD \frac{\sqrt{p_2} - \sqrt{p_1}}{d} \tag{2}$$

where:

$$C_1 = k\sqrt{p_1}, \text{ and } C_2 = k\sqrt{p_2}$$

Letting kD = P, the permeation constant, and continually pumping the gas at pressure p_1 on the low-pressure side of the membrane at speed "s" (cm^3/sec.),

we have equilibrium conditions for $\frac{\partial p_1}{\partial t} = 0$.

This leads to

$$s\,p_1 = \frac{AP\left(\sqrt{p_2} - \sqrt{p_1}\right)}{d} \tag{3}$$

or:

$$P = \frac{sp_1 d}{A\left(\sqrt{p_2} - \sqrt{p_1}\right)} \tag{4}$$

Eqn. 4 assumes that all of the gas that permeates the envelope is pumped by the pump and none is pumped by the walls of the system.

The reported permeation constants P_o are related to the experimental P by:

$$P = P_o e^{-E/RT} \tag{5}$$

E = Activation energy for entire process of permeation

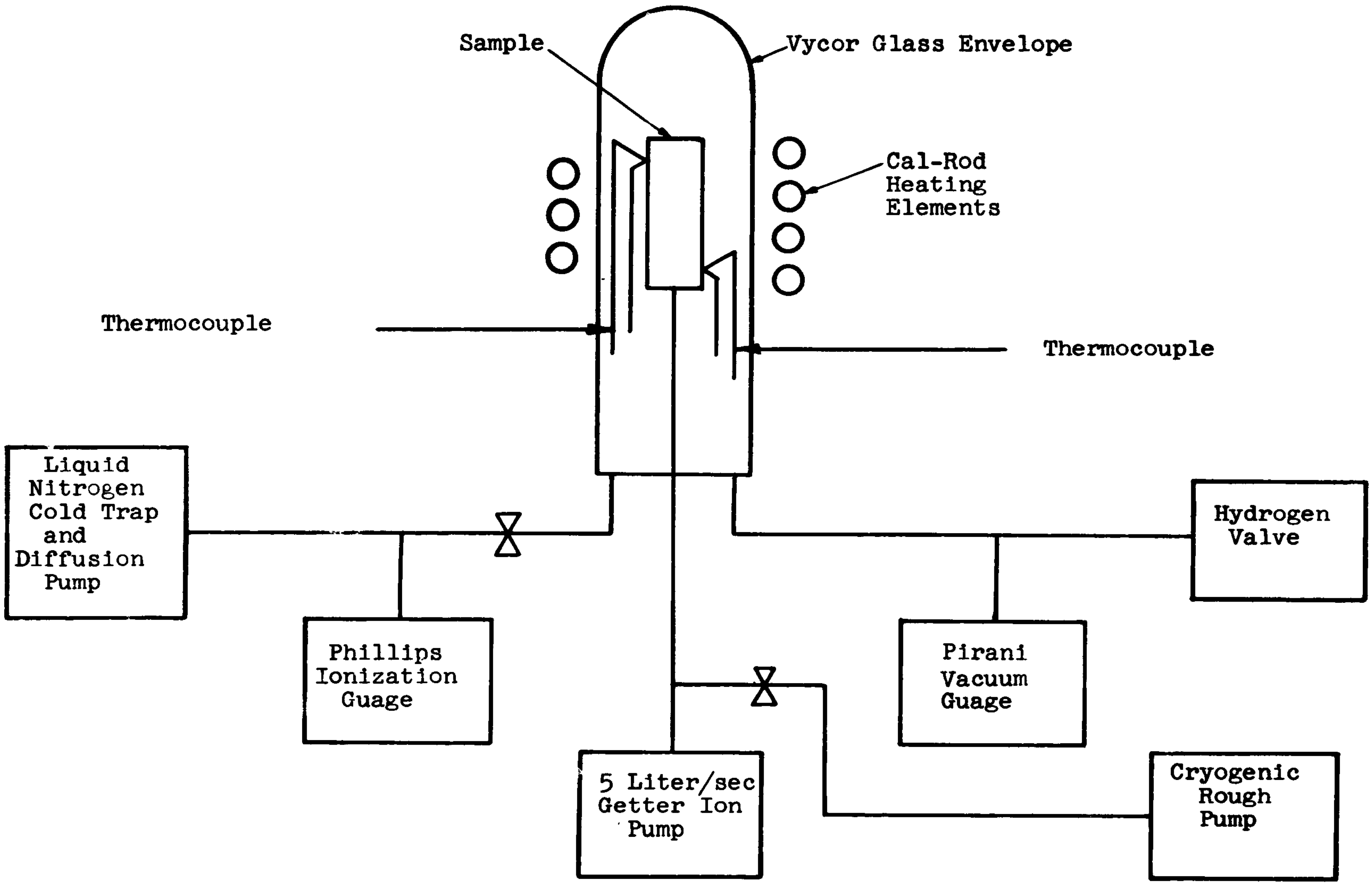
R = Gas constant (cal/mol/^{o}K)

T = Temperature (^{o}K) of the membrane

b. Experimental Apparatus.

The apparatus used is shown schematically in Fig. 1. It is composed of two separate vacuum systems.

The internal system is composed of a 5 liter/sec. gettering type pump connected to the sample by a manifold and tubulation. This system is valved to a cryogenic rough pump by a stainless steel valve



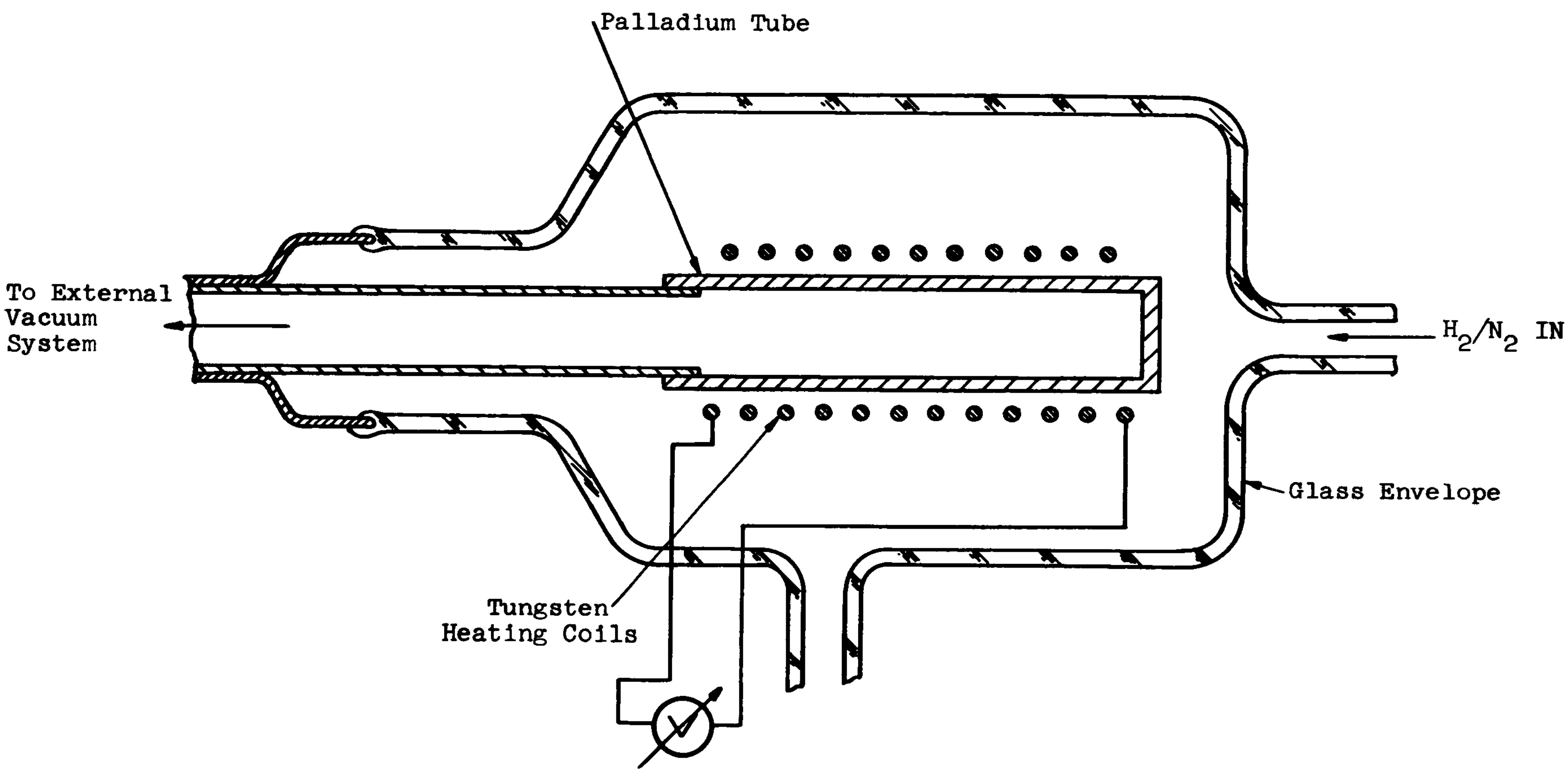
SCHEMATIC OF PERMEATION TEST APPARATUS

Fig. 1

with VITON-A "O" ring gaskets. The pressure at the pump in the internal system is measured by the current drawn from the pump power supply. The entire internal system up to the valve was bakeable to 300°C. The sample was heated to 800°C by a cal-rod oven which does not distort the thermocouple reading.

The external system consisted of a liquid nitrogen-trapped diffusion pump and ionization gauge valved to a port in the flange supporting the Vycor glass envelope. The sample and its tubulation separated the internal and external vacuum systems in the Vycor envelope. A hydrogen valve and Pirani gauge for supplying hydrogen and measuring its pressure were also ported to the flange. The Vycor envelope was sealed to the flange by a water-cooled, VITON-A gasket. The temperature of the sample was measured by two alumel-chromel thermocouples brought through the manifold by special ceramic insulators with alumel and chromel center conductors. The connecting tubulation, excluding the diffusion pump, ionization guage, and valve, were bakeable to 300°C. The envelope and flange were bakeable to 800°C.

The hydrogen valve (see Fig. 2) consisted of a palladium tube vacuum sealed to the external system.



HYDROGEN DIFFUSER

Fig. 2

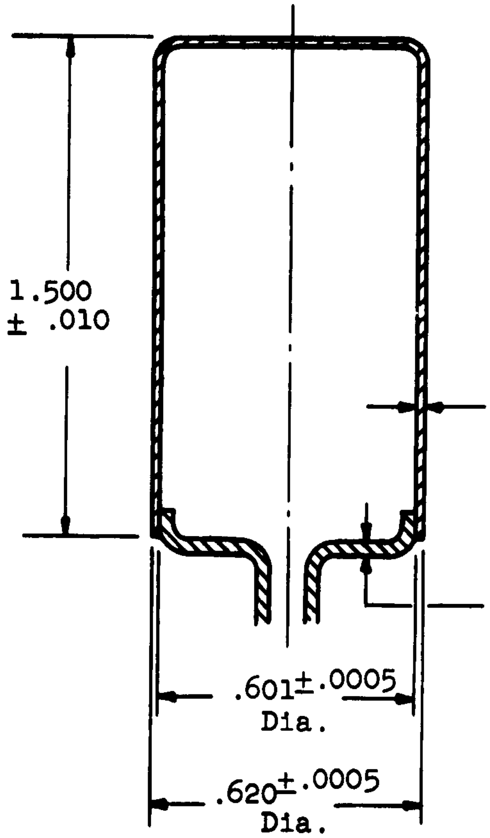
When this tube was heated by a tungsten filament in a nitrogen and hydrogen atmosphere, spectroscopically pure hydrogen was admitted to the external system by permeation. The rate of flow could be controlled by the temperature. With this apparatus and the sample at $650^{o}C$, the internal pressure could be lowered to less than 1×10^{-8} Torr and the external pressure to 1×10^{-6} Torr.

c. Sample.

The sample geometry must satisfy two requirements. The wall thickness must be small compared to the diameter of the sample so that the planar diffusion equation may be used. Our sample (Fig. 3) satisfys this condition. The other condition is that the wall thickness be uniform as noted in Fig. 3.

All samples, copper, copper-clad kovar, kovar, 403 monel, nickel, and gold-plated copper were attached to a copper tubulation whose wall thickness was .030 minimum. They were then attached to the apparatus by a crush seal on a heavy copper flange brazed to the end of the tubulation. All parts were chemically cleaned before sealing.

Fig. 3 is representative of the geometry of all the samples except the gold-plated copper sample. Onto a copper sample, conforming to Fig. 3, was plated .002 inch of gold on the outside of the cylinder.



PERMEATION SAMPLE
Fig. 3

The copper-clad kovar sample, had the same wall thickness as Fig. 3. The wall was .002 inch copper on the inside, .008 inch kovar in the middle, and .002 inch copper on the outside.

d. Experimental Procedure.

The sample was sealed to the apparatus and the internal and external systems were evacuated. The manifold was then baked at 300°C, and the sample at 750°C, to completely outgas the sample and apparatus. When the internal pressure asymptoted and the external pressure was less than 1×10^{-6} Torr, the manifold was cooled, keeping the sample hot. This processing generally took 48-60 hours. If the internal pressure, i.e., the pressure inside of the sample was greater than 1×10^{-8} Torr, the bakeout was repeated.

Once the internal pressure was below 1×10^{-8} Torr and the external pressure was below 1×10^{-6} Torr, the diffusion pump, cold trap, and ionization guage were valved off and hydrogen was admitted to the external side of the sample to a pressure greater than 2 Torr for 30 minutes. This saturated the walls of the apparatus as well as the sample with hydrogen so there would be no pumping due to the walls of the apparatus.

The external hydrogen pressure was then reduced in three steps to known pressures, keeping the temperature of the sample constant. At each step, the hydrogen pressure was held constant until the internal pump pressure asymptoted, but for no less than 30 minutes. The internal pressure, external hydrogen pressure, and the sample temperature were recorded. The temperature was then lowered 80^{o} - $90^{o}C$, the external system evacuated and when the internal pressure was less than 1×10^{-8} Torr, the procedure was repeated. For each sample we took measurements at three different temperatures, and for four different external hydrogen pressures at each temperature.

e. Experimental Errors.

The pressure and pumping speed readings of the ion pump on the low pressure side of the system were corrected for hydrogen. The Pirani guage calibration used on the high pressure side of the system was also corrected for hydrogen. The greatest difficulty of our measurement was due to the background pressure of gases other than hydrogen on the high pressure side of the system. Much time was involved in processing the sample to achieve the low background pressure on the high vacuum side, so that the hydrogen pressure, due to permeation, wouldn't be

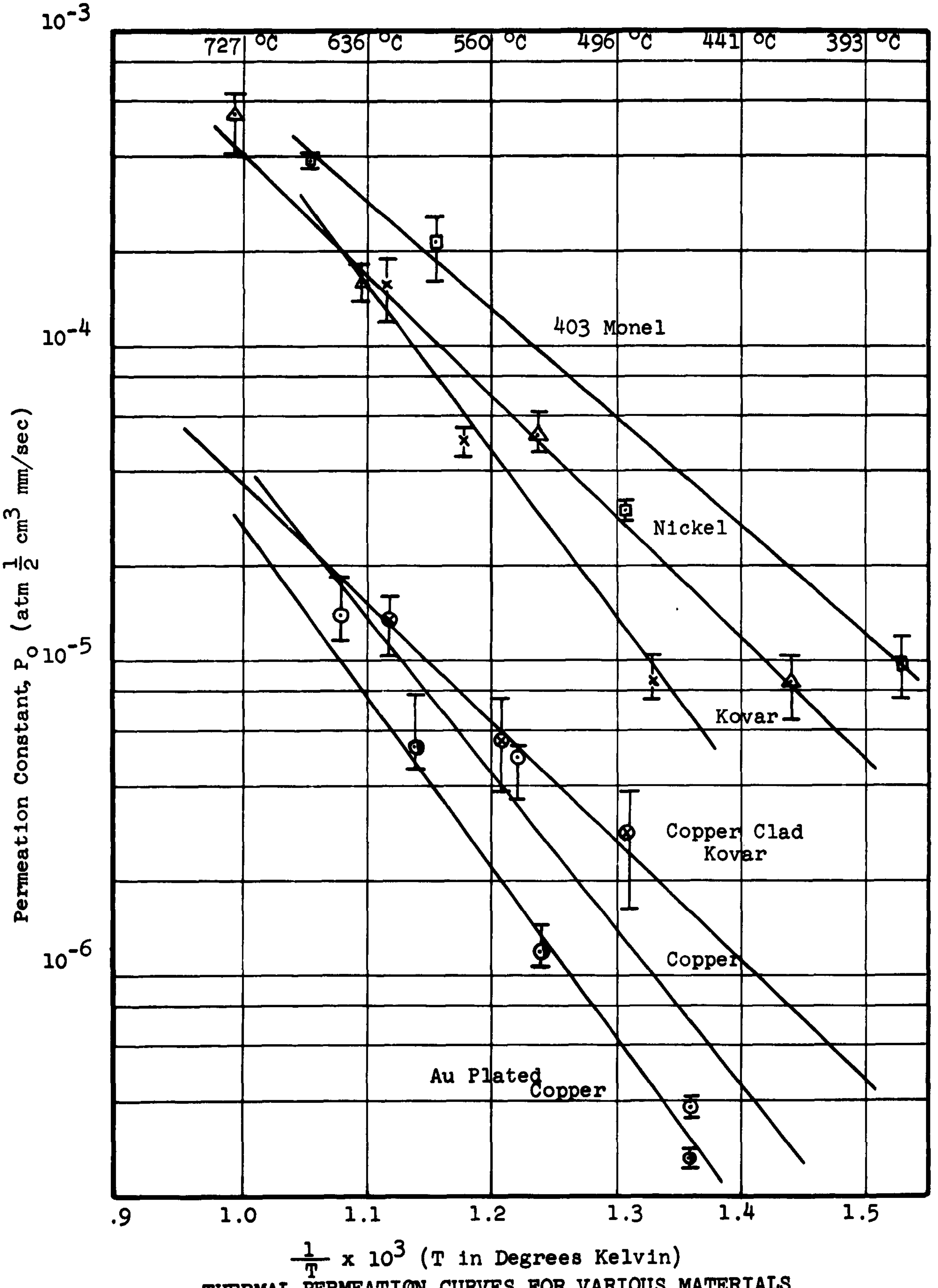
masked by the partial pressure of other gas emanating from the envelope.

It is to be emphasized that hydrogen gas permeation readings were taken on samples as near the size and thickness of material as would actually be used on tube envelopes. It is well known that permeation rate measurements depend, to a large extent, on the detailed size, shape, and environmental conditions of the sample used.

Our absolute pressure readings may be inaccurate by 1/2 Torr scale. However, the relative pressures from one test to another were much closer. We believe that the gas permeation constants measured represented the most accurate that could be obtained without extreme expenditures for more sophisticated measurement systems.

f. Experimental Results.

Fig. 4 shows the results of the permeation studies. The materials tested fall into two categories with regard to their hydrogen permeation rates. The metals of the group with high permeation by hydrogen (nickel, 405 monel, and kovar) are all nickel or nickel-bearing alloys. The material with low permeation constants (copper, gold-plated copper, and copper-clad kovar) all have copper as a common material.



THERMAL PERMEATION CURVES FOR VARIOUS MATERIALS

Fig. 4

The advantage of cladding kovar with copper is very clearly shown. Kovar, because of the ceramics sealing techniques used, is often the thinnest part of the vacuum envelope. This makes it very susceptible to permeation. Cladding the kovar gives it the permeation properties of copper or about 1/10th the permeation rate of the unclad alloy. Monel appears to be worse than kovar; however, it is seldom that the cross-sectional thickness is as thin as that of kovar in actual tube applications. Gold plating the copper also appears to reduce the permeation. Further work on the application of gold to copper would optimize this reduction.

B. Sparking and Vane Tip Erosion.

Sparking and vane tip erosion have been among the most troublesome problems encountered in many pulsed magnetron developments, although Litton has never experienced this problem in the 80,000 medium and high-power magnetrons manufactured and delivered. In general, sparking and vane tip erosion problems go hand in hand. We have found that the presence of gas is usually the predisposing factor in creating this problem. The sequence of events is that gas is evolved either at the cathode or at the anode; a few arcs are drawn slowly at first, then at an accelerated pace because the arc liberates more gas from deep under the surface of the metal of the vane tips.

The continued arcing vaporizes the metal from the vane tips and erodes them. The greatest heat dissipation occurs at the leading edge of the vane tip (i.e., where the electrons strike) so that, during the pulse, the temperature at that point is higher, causing a preferential occurance of arcs. That is why the erosion occurs at the leading edge of the vane.

1. Discussion of a Typical History of Vane Tip Erosion.

A very important case of vane tip erosion occurrence and its elimination took place at the Services Electronic Research Laboratory, Harlow, England, during the development of the 1 megawatt X-Band magnetron based on the Columbia Radiation Laboratory tube AX-9 (Ferranti type VF 10)*. In this high power tube development, a cermet cathode was initially used consisting of 80% tungsten and 20% thoria. After some tens of hours of operation, tubes using these cathodes developed a tendency to spark severly. It was after this sparking had occurred that it was found that vane tip erosion had been severe. By modifying the cathode mixture and employing 88% tungsten, 10% tungsten carbide (W_2C), and 2% thoria, sparking and vane tip erosion were improved. The logical explanation

*This information was conveyed to the authors of this report by private communication by A. J. Monk and C. P. Lea-Wilson of S.E.R.L., Harlow, England; and by A. D. Cooper of Elliott-Litton Ltd., Borehamwood, Herts, England.

for this is that the gas evolution from the cathode was reduced because of the reduction of the amount of thoria in the mix. Further improvement was achieved by replacing the thoria by thorium or thorium hydride. This eliminated the gas evolving chemical reaction, and vane tip erosion was eliminated.

In this very important case history, it is clear that gas evolution from the cathode is typically a prime factor in the problem of sparking and vane tip erosion. Two very important elements in the cathode design in this regard are: (1) the elimination of gas evolutions from the cathode due to normal chemical reactions or due to release of absorbed gasses in the parts, and (2) the capability of the cathode to withstand the electron spark bombardment without overheating and thus causing gas evolution. As discussed in the section on cathodes, another cause for sparking is cracks in the cathode surface which, among other things, results in melting of the metal at the edges of the cracks, and the concommitant gas evolution.

Of course, there are other sources of gas within the tube besides the cathode which can also start the sequence of gassing, arcing, and vane tip erosion; namely: absorbed gas liberation from the vane tips and other internal parts, permeation of gasses into the vacuum envelope, and leaks. These matters are discussed in other parts of

this report, and their minimization represents major efforts under this program. The high temperature exhaust techniques with the double envelope system, and the redesign of the tube to prevent gas permeation, represent steps taken to exclude gas from the vacuum envelope.

2. The Relation Between Sparking and Vane Tip Erosion.

The detailed cause of vane tip erosion is evaporation of the metal caused by localized heating by the arc. This is a form of impulse heating which has such a high dissipation density that the metal is locally vaporized during each arc. The area affected by the arc is so small and the current is so large that it is practically impossible to avoid erosion if sparking occurs. There are other forms of impulse heating which can also cause damage to the vane tips, the most common of which is electron bombardment heating due to the electron current as discussed in the next section. This heating is not so localized and, if sparking does not accompany it, the damage to the vane tips is not initially vaporization but rather a fracturing of the crystal boundaries.

C. Pulse Dissipation Metal Fatigue.

Failure of high power tubes due to impulse heating by electron bombardment has long been reported in the industry as the result of surface melting of structures or outgassing of components under high pulse temperatures. However, it has been found that failure may occur when conditions do not predict melting. The vanes of such tubes show a marked grain structure on their surface with cracks which penetrate along grain boundaries. It is shown in this section that the surface has failed in fatigue from the alternating stresses produced by the impulses of temperature. Procedures for determining this limit are presented. Anode failure may be the result of improper anode design at either the surface of the anode where electrons bombard, or the surface where the coolant carries the heat away. At the surface exposed to the electrons, the factors influencing peak temperature are developed, including corrections for the affect of high speed electron penetration into the copper, thus permitting proper design evaluation at this boundary.

At the liquid cooled boundary, vaporization must be avoided or vapor lock can result. Thermal smoothing is provided by thickening the anode to at least a limiting value (minimum) which can be calculated using the procedures reported in this section. The heat exchanger design is not included (see reference 5).

1. Summary Of The Impulse Heating Analysis.

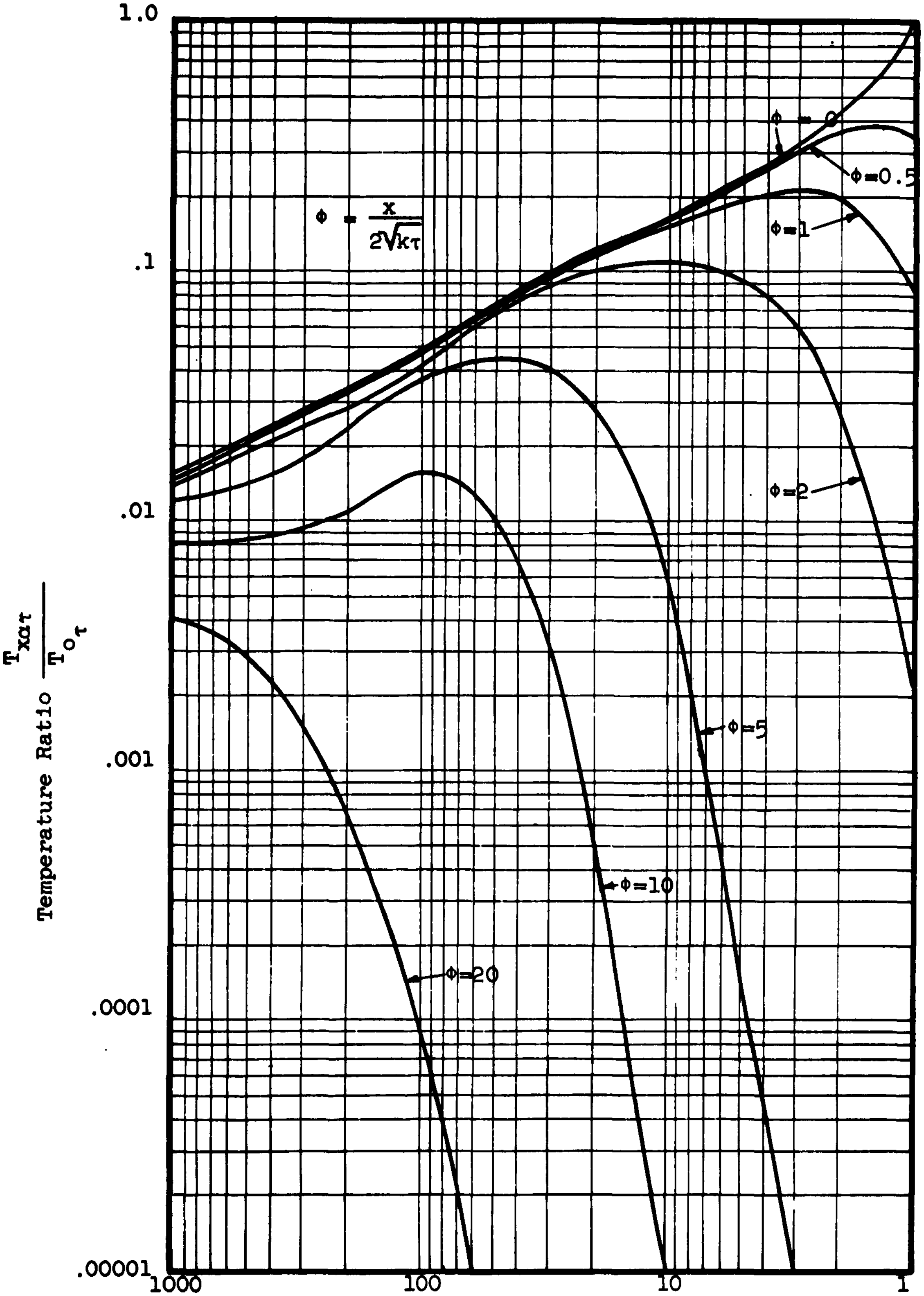
The analysis clearly shows that the problems of high power anode design must be as much directed toward stress reduction as toward avoidance of excess temperature.

The general analysis has resulted in several expressions or dimensionless design curves which can be used to determine anode temperature under a variety of conditions. Included are the following: (1) impulse heating of the outer surface of a semi-infinite thick slab (see Fig. 5); (2) impulse heating of a semi-infinite thin slab when bombarding electrons penetrate appreciably into the slab (see Fig. 6), a universal curve of penetration vs. voltage is given in Fig. 7); and (3) design of a moderately thick slab as a filter to prevent overloading at the heat exchange outer surfaces. An organized method for calculating the thermal stresses in anodes is included for convenience.

The methods outlined in the general analysis are applied to the magnetron problem, particularly the 4J50 pulsed magnetron; and recommendations for an improved design for high temperature operation are developed.

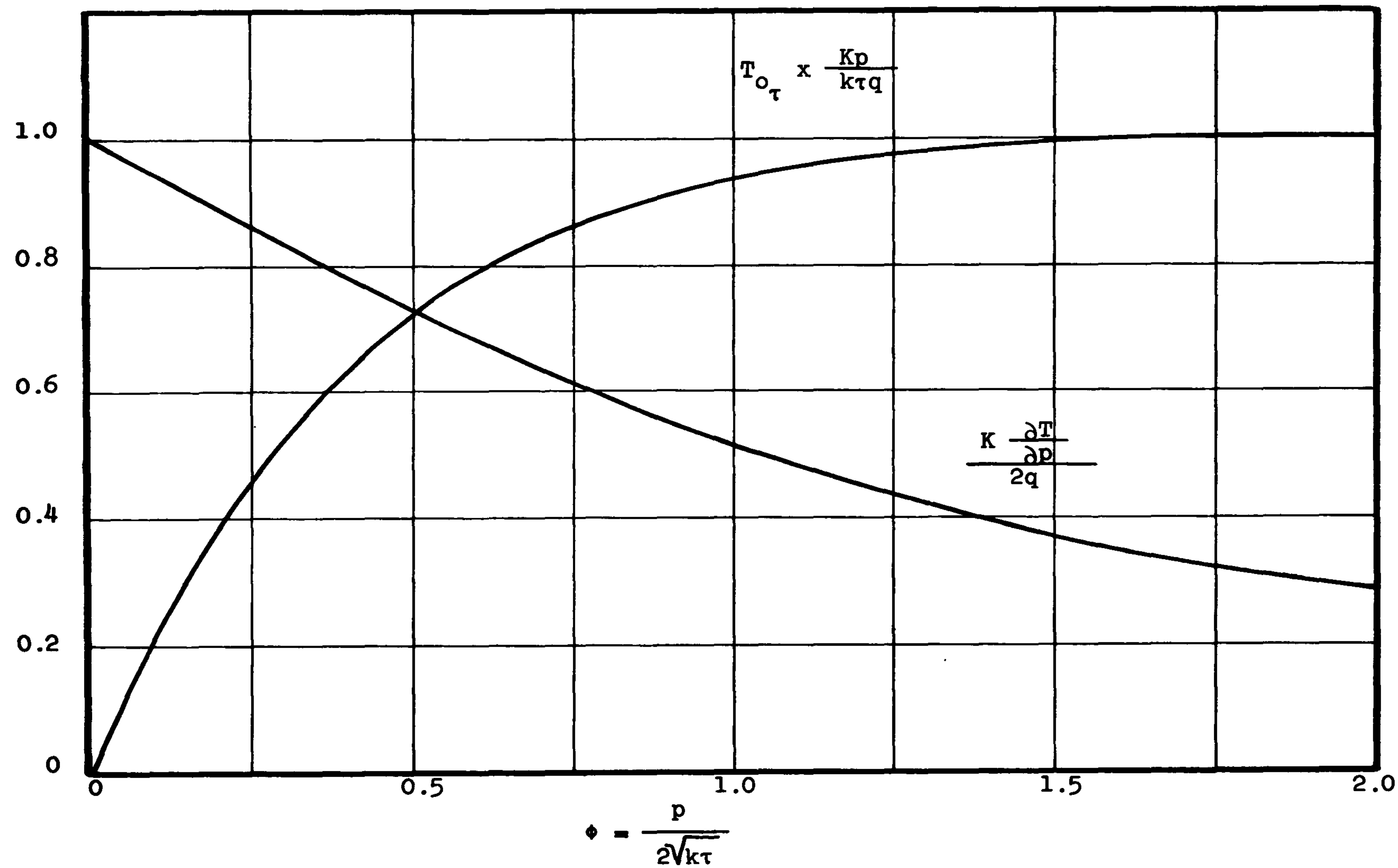
2. Analysis Of The General Impulse Heating Problem.

The general impulse heating problem is naturally divided into temperature distributions and thermally induced stresses which do not couple with one another.



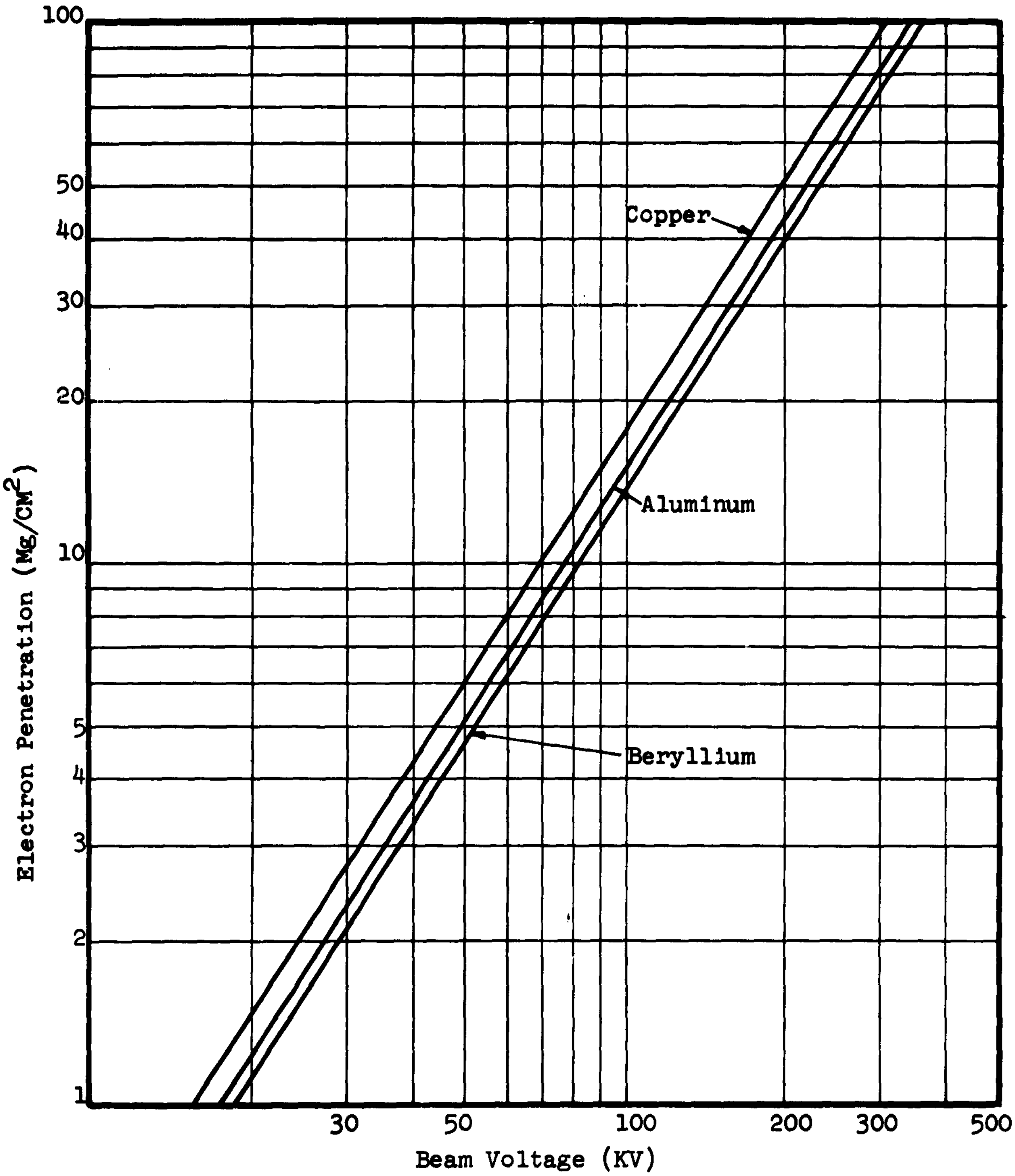
IMPULSE HEATING SEMI-INFINITE THICK SLAB

Fig. 5



IMPULSE THERMAL GRADIENT (WITH ELECTRON PENETRATION)

Fig. 6



CURVES OF ELECTRON PENETRATION
AS A FUNCTION OF BEAM POTENTIAL

Fig. 7

Analysis leading to calculation of temperature in various parts of the anode body are presented in order as follows: (1) general case of heat input to a semi-infinite anode leading to universal design curves; (2) case of heat supplied to a depth, p, within the outer surface of the body leading to curves for maximum temperature and temperature gradient at the end of the pulse; (3) case of a moderately thick anode leading to establishment of thickness criteria and design curves; (4) case of sequential pulsing, a modification of cases (1) and (2) above.

Using the temperatures derived from these analyses, the thermal stresses are shown to be quite large; thus, proving the nature of the anode breakdown fatigue leading to loss of vacuum.

<u>Assumptions.</u>

(a) The mediums are homogenous throughout.

(b) Heat flow is laminar throughout.

(c) Boundaries can handle the heat brought to them either as defined or as though the body extended beyond the boundary.

(d) Planar geometries.

<u>The Differential Equations of Transient Heat Flow.</u>

A very comprehensive treatment of transient heat flow is given by Carslaw and Jaeger in their book,

"Conduction of Heat in Solids", by Oxford Press; 1959. To establish the basis of the conclusions which can be drawn, the differential equations will be derived and solutions presented.

In a differential volume dx dy dz, the heat flow into face 1 is: (See Fig. 8)

$$q_1 = -K dydz \left.\frac{\partial T}{\partial x}\right|_1$$

and from face 2 is:

$$q_2 = -K dydz \left(\frac{\partial T}{\partial x} + \frac{\partial^2 T}{\partial x^2} dx\right)$$

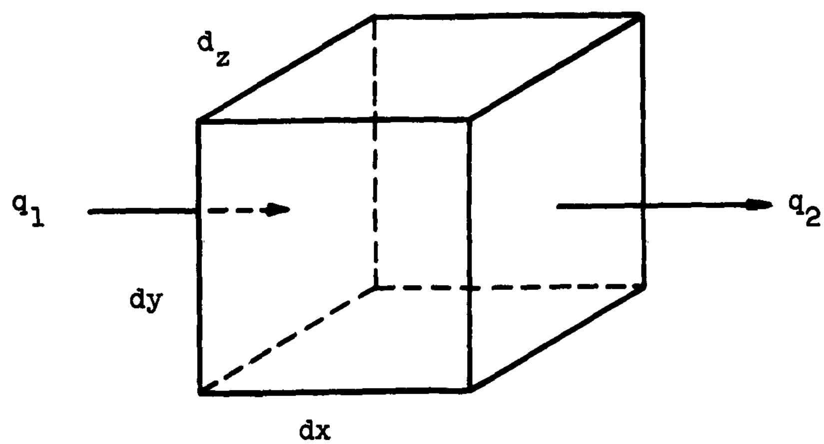
The difference between the input and the output represents stored energy which is given by $C_p \rho$ dxdydz $\frac{dT}{dt}$ hence we have:

$$\frac{K\partial^2 T}{\partial x^2} = C_p \rho \frac{\partial T}{\partial t} \text{ or } \frac{k\partial^2 T}{\partial x^2} = \frac{\partial T}{\partial t} \qquad (6)$$

where $k = \frac{K}{\rho C_p}$ is the thermal diffusivity.

Solutions to these partial differential equations are numerous. Any solutions which fit the same boundary conditions will yield the same results but, at each boundary certain forms of solution are more tractable than others.

<u>Case 1.</u> Semi-infinite solid with heat flow into a face at x = 0. For a single pulse the heat flux q is:



SCHEMATIC OF HEAT FLOW THROUGH DIFFERENTIAL VOLUMN

Fig. 8

0 for $t < 0$

q_o for $0 < t < \tau$

0 for $\tau < t < \infty$

As shown in Ref. 6, (p. 75 and 76) this differential equation is satisfied by an error function solution which very conveniently describes conditions at the input and throughout the body. Thus, for $0 < \alpha < 1$

$$T = \frac{2q_o\sqrt{\alpha k\tau}}{K} \text{ ierfc} \left(\frac{\phi_o}{\sqrt{\alpha}}\right) \tag{7}$$

and for $\tau < t < \infty$

$$T = \frac{2q_o\sqrt{\alpha k\tau}}{K} \text{ ierfc}\left(\frac{\phi_o}{\sqrt{\alpha}}\right) - \sqrt{\alpha - 1} \text{ ierfc}\left(\frac{\phi_o}{\sqrt{\alpha - 1}}\right) \tag{8}$$

where:

$$\alpha = \frac{t}{\tau}$$

$$\phi_o = \frac{x}{2\sqrt{k\tau}}$$

The dimensionless variables defined here have been introduced to permit plotting a family of design curves (see Fig. 5). The special case of $\alpha = 1$ is of importance since the temperature is maximum on the skin at that time. At $x = 0$ and $\alpha = 1$, both the above equations reduce to

$$T_{o_\tau} = \frac{2q_o}{K}\sqrt{\frac{k\tau}{\pi}} \tag{9}$$

Case 2. Electrons penetrate the surface of the semi-infinite solid to a depth, p, and produce uniform heating throughout the solid to that depth.

Since the electrons are penetrating the outer surface, the thermal gradient at that point is zero, and becomes a maximum at the depth of penetration, p. The added mass (heat capacity) at the input face integrates the energy and peak pulse temperatures are reduced. The differential equations need be modified by the addition of a term which adds, to the energy stored by heat transfer, the amount which is directly introduced into the differential element, A_o dx dy dz. The differential equation thus becomes

$$\rho\, C_p \frac{\partial T}{\partial t} = K \frac{\partial^2 T}{\partial x^2} + A_o \qquad (q_o = A_o p) \tag{10}$$

Ref. 6, Eqn. 9, p. 80 presents a solution to this equation valid during the pulse interval. When x = 0 (inner surface) the temperature is

$$T_o = \frac{k q_o \tau}{pK} \left(1 - 4 \ \mathrm{ierfc}^2 \frac{p}{2\sqrt{k\tau}} \right) \tag{11}$$

and

$$\left.\frac{\partial T}{\partial x}\right|_{x = p} = \frac{2 q_o \sqrt{k\tau}}{Kp} \left(.5643 - \mathrm{ierfc} \frac{p}{2\sqrt{k\tau}} \right) \tag{12}$$

The temperature distribution throughout the body is not significantly different from that of Case 1 at distances beyond 5p into the metal and for $\alpha > 1$ throughout the

metal so that additional solutions for that case are not deemed necessary in design. Design curves have been calculated from these equations and are presented as Fig. 6.

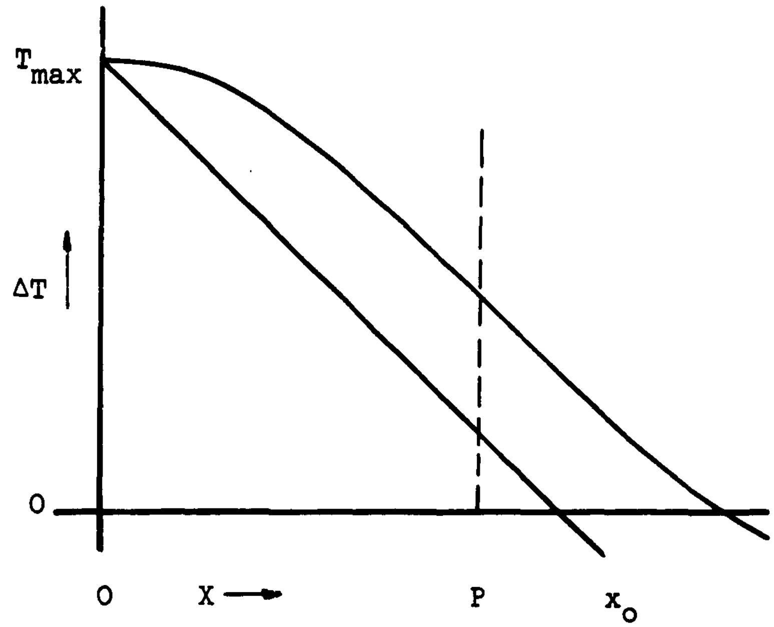
The thickness of the plate subjected to thermal stresses can be adequately approximated using the results in Fig. 6. The outer surface temperature is shown, and so is the gradient at the depth of penetration. As shown in Fig. 5, the wave front of the heat impulse is very steep when $\alpha = 1$. Thus, as shown in Fig. 9:

$$\frac{T\ max}{X_o} = \left.\frac{\partial T}{\partial x}\right|_{x=p} \tag{13}$$

and "X_o" is of proper magnitude.

<u>Case 3.</u> An anode with moderately long heat path which acts as a ripple filter to prevent vapor lock at the coolant heat exchange boundary.

As mentioned during the general analysis, there may be an infinity of solutions to a differential equation which fit all the boundary conditions, but which converge more or less rapidly, and thus can have varying degrees of engineering significance. It has been found that the condition of the exchanger boundary is best represented by a solution in terms of the Fourier components of the input pulse



RELATIVE THERMAL GRADIENT
FROM IMPULSE HEATING

Fig. 9

$$q = \sum_{n=o}^{\infty} q_n \sin n\omega t \text{ (See Ref. 7)} \tag{14}$$

where:

$$q_n = 2q_{ave} \frac{\sin n\pi d}{n\pi d}$$

$$q_{ave} = qd$$

Then, as shown on p. 68 of Ref. 6, the differential equation is also satisfied by

$$T = T_o x + \sum_{n=1}^{\infty} A_n e^{-x\sqrt{\frac{n\omega}{2k}}} \sin\left[n\omega t - x\sqrt{\frac{n\omega}{2k}}\right]$$

$$+ \sum_{n=1}^{\infty} B_n e^{-x\sqrt{\frac{n\omega}{2k}}} \cos\left[n\omega t - x\sqrt{\frac{n\omega}{2k}}\right] \tag{15}$$

where the values of A_n and B_n can be obtained by differentiating (15) with respect to x and matching term for term when x = 0. Since q contains no cosine term, it is found that:

$$A_n = B_n = \frac{q_n}{2K}\sqrt{\frac{2k}{n\omega}} \tag{16}$$

and then

$$T_o = \frac{A_o x}{K} + \sum_{n=1}^{\infty} \frac{q_n}{2k}\sqrt{\frac{2k}{n\omega}}\, e^{-x\sqrt{\frac{n\omega}{2k}}} \left(\sin \theta_n - \cos \theta_n\right) \tag{17}$$

and

$$q = A_o + \sum_{n=1}^{\infty} q_n e^{-x\sqrt{\frac{n\omega}{2k}}} \sin \theta_n \tag{18}$$

in which

$$\theta_n = \left(n\omega t - x\sqrt{\frac{n\omega}{2k}} \right) \tag{19}$$

By inspection, one sees that the velocity of the wave of constant θ is

$$v = \sqrt{2nk\omega}$$

and that

$$\frac{T_n}{q_n} = \frac{1}{K}\sqrt{\frac{2k}{n\omega}}\,(1-j) \tag{20}$$

in which the real term describes the heat carried away by conduction and the imaginary term describes the heat passing through the boundary destined to be stored in the heat capacity. Any boundary which represents continuity in T_n / q_n will be reflectionless for that harmonic.

As this complex wave of heat passes through the materials, the higher frequency components $(n > 1)$ attenuate more rapidly. Since, in usual high power tube design, the designer is as much limited by handling the heat exchange problem as by the pulse temperature problem, it is felt that the wave of heat should be almost completely attenuated when it arrives at the cooled boundary or the boundary will vapor lock and reflections taking place there will return to the anode face and cause its temperature to be higher than that given by cases 1, 2 or 3.

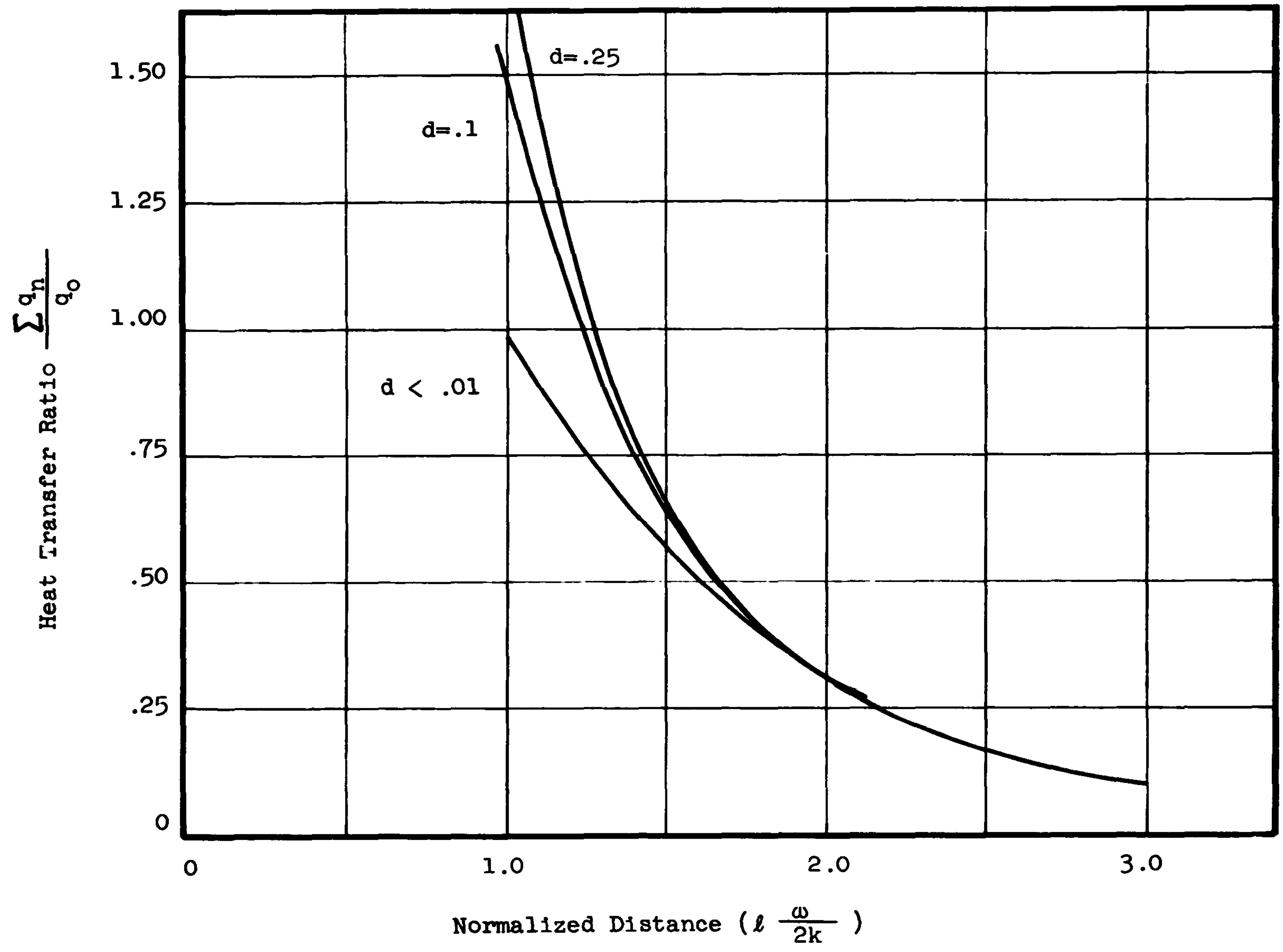
The time of arrival of the crest of the fundamental is seen to be $t = \frac{\ell}{\omega}\sqrt{\frac{\omega}{2k}}$. At this same time the crest of the higher harmonics have advanced in phase by $(n-1)\sqrt{\frac{n\omega}{2k}}$, as can be developed from (17) and the input conditions. Summation of all fluctuating heat flow at the heat exchange boundary is needed to determine peak values of heat to be transferred there, thus:

$$R = \frac{1}{q_{oo}} \sum_{n=1}^{\infty} \left\{ \frac{2 \sin n\pi d}{n\pi d} e^{-\ell\sqrt{\frac{n\omega}{2k}}} \cos\left[\gamma + \sqrt{\frac{n\omega}{2k}}(n-1) \right] \right\} \quad (21)$$

in which γ is the phase of the peak fluctuation at the heat exchange boundary compared with the crest of the fundamental fluctuation. Since the waves attenuate rapidly when ℓ is large, γ is approximately zero and curves as in Fig. 10 can be prepared. When the anode is thin (ℓ small) an individual summation must be made assuming various γ to obtain the real peak amplitude since ordinary maximizing procedures are hopelessly complex.

Case 4. Modifications of the above solutions to accommodate repetitious pulses and/or cylindrical geometry.

In case 3, the assumption was made that a series of pulses were producing the heat in the anode, not just one. Since the differential equations are linear, a steady state solution can be superimposed on the transient solution directly and the steady state temperature everywhere in the body is given by the correction term



HEAT FLOW CURVES

Fig. 10

$$T = \frac{qd}{K} x \qquad (22)$$

<u>Thermally-Induced Stresses.</u>

Thermal stresses are developed in materials at elevated temperatures when they are confined and cannot expand as they normally would. This is particularly true of the surface of the anode which reaches high peak temperatures during the impulse heating. The layers of anode material near the electron bombardment surface are subjected to high temperatures but are not allowed to expand, or at least their expansion is limited by the deeper layers of metal which do not get as hot. The electron bombarded surface of the anode is free to grow in a direction normal to the surface but, not in a direction parallel to the surface. For an infinite planar geometry, every imaginary layer of the anode is completely confined in the direction parallel to the anode surface. For a finite size anode, such as the vane tip of a magnetron, the outer edges are not so confined, and only the inner parts of the anode surface area layers experience confinement.

Let us consider a differential volume element from a planar anode of infinite extent, and examine it as a free body with known applied forces. The body is free to expand in the direction normal to the anode direction

(z-direction), but is confined on all four other faces. The tendency is for each face to expand by an amount

$$\Sigma = 6T$$

Studying the forces required to push the cube back to its original x and y-dimensions tell us what the thermal stresses are, for which

$$\frac{S_x}{\Delta T} = \frac{S_y}{\Delta T} = \frac{bE}{1-\sigma} \text{ (See Ref. 9)} \qquad (23)$$

where ΔT is the temperature difference (hot or cold) in the body. These compressive forces are parallel to the surface. When these compressive stresses become too great compared with the compressive stress limit, the surface cracks at the crystal boundaries since that part of the metal is the weakest.

If the surface of the anode is diced (thin parallel, equally-spaced grooves cut in the x and y-directions, intersecting each other) to a significant depth with a square dice of side a, the stresses will be reduced: Consider a thin slice (X_o thickness) cut out of the rod so formed. If a steady flow of heat is passing through the plate, it is found that the plate has become a portion of a sphere whose radius is given by

$$r = \frac{1}{b\frac{\partial T}{\partial x}} \qquad (24)$$

When forces are applied to straighten out the curved plate, the stress caused by thermal gradient in a constrained body is reproduced. These stresses are undoubtedly of the form shown on the sketch of Fig. 11. Ref. 9 does not include any stress/strain relations under such conditions of loading on a flat plate, but an approximate order of the magnitude of reduction due to dicing is obtained when one assumes edge support and uniform load over the circle d = .6a (pg. 197, Case 37 of Ref. 9). Then:

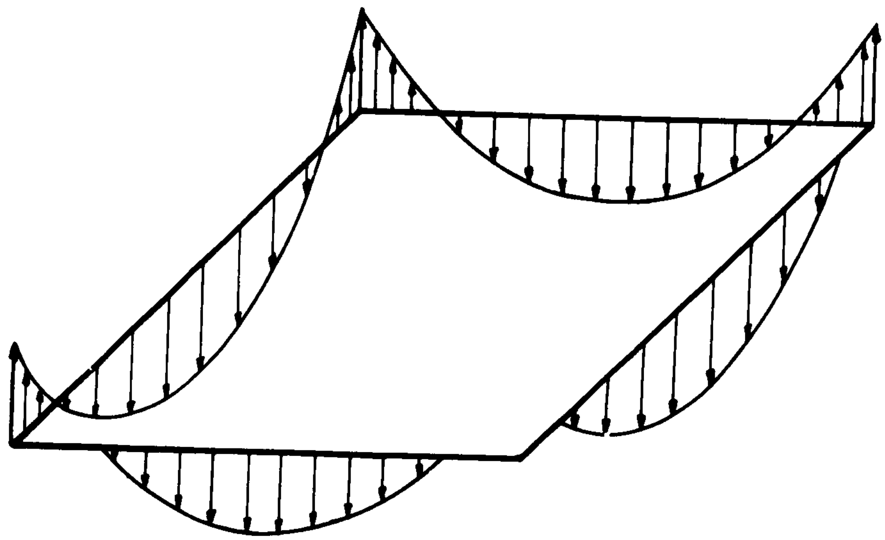
$$S_{max} = \frac{1.14\ mEX_o}{(m^2-1)r} \qquad \text{where} \quad m = E/G \tag{25}$$

which is applied uniformly whenever $0 < a < 0.5\ r$. For larger a, the solution asymptotes on Eqn. (23). If the relation between temperature, temperature gradients, and temperature coefficients presented in Eqns. (13), and (24) are combined, (25) becomes

$$\frac{S}{\Delta T} = \frac{1.14\ mbE}{m^2 - 1}$$

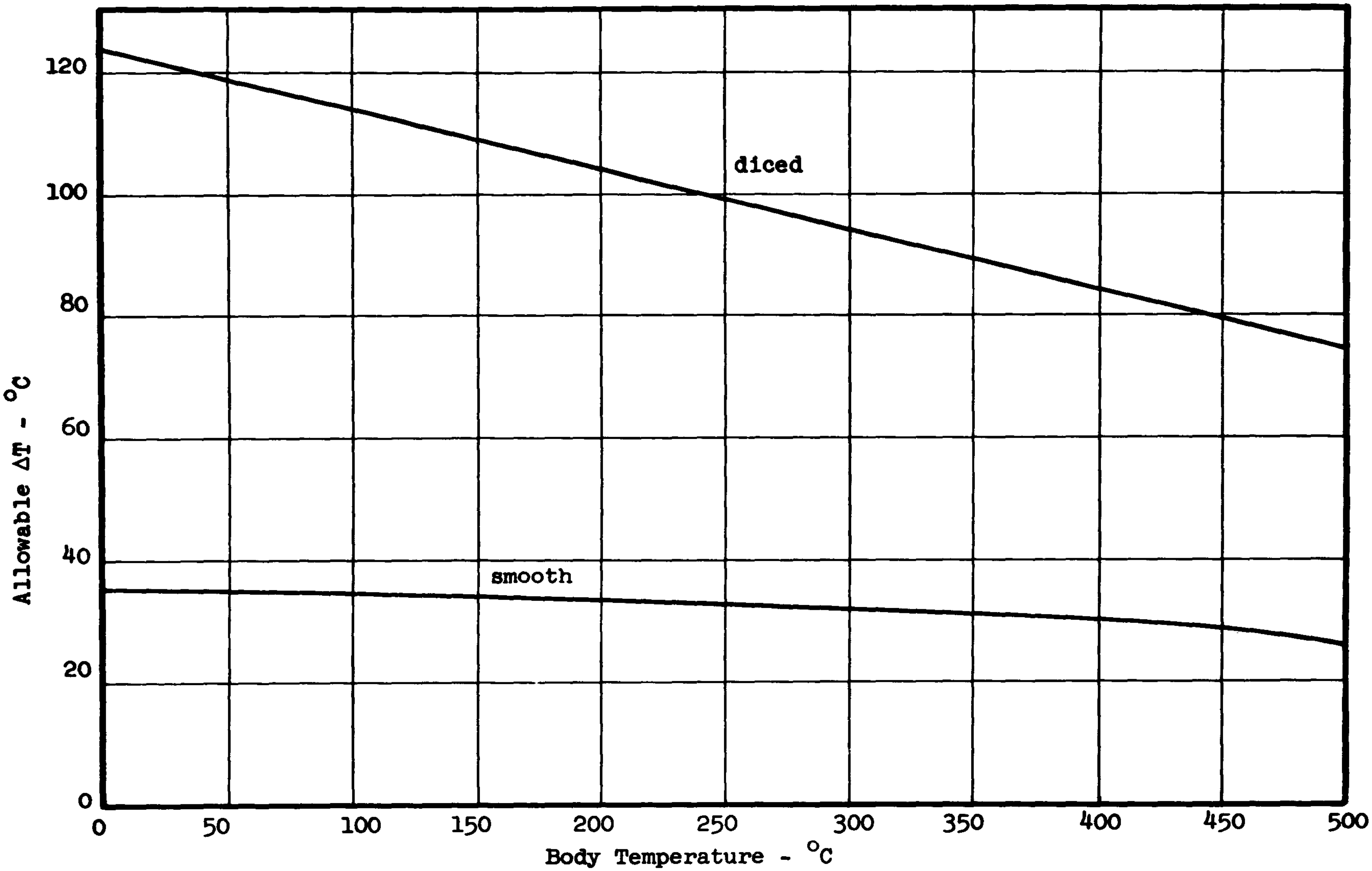
which is a 3:1 reduction due to the dicing (see Fig. 12).

The properties of materials are rapidly varying functions of temperature in part of the region of interest. To avoid overstressing the material, it is well for the designer to prepare curves of S, p, b, E and σ (see Fig. 13) vs. temperature and, from them plot a curve of



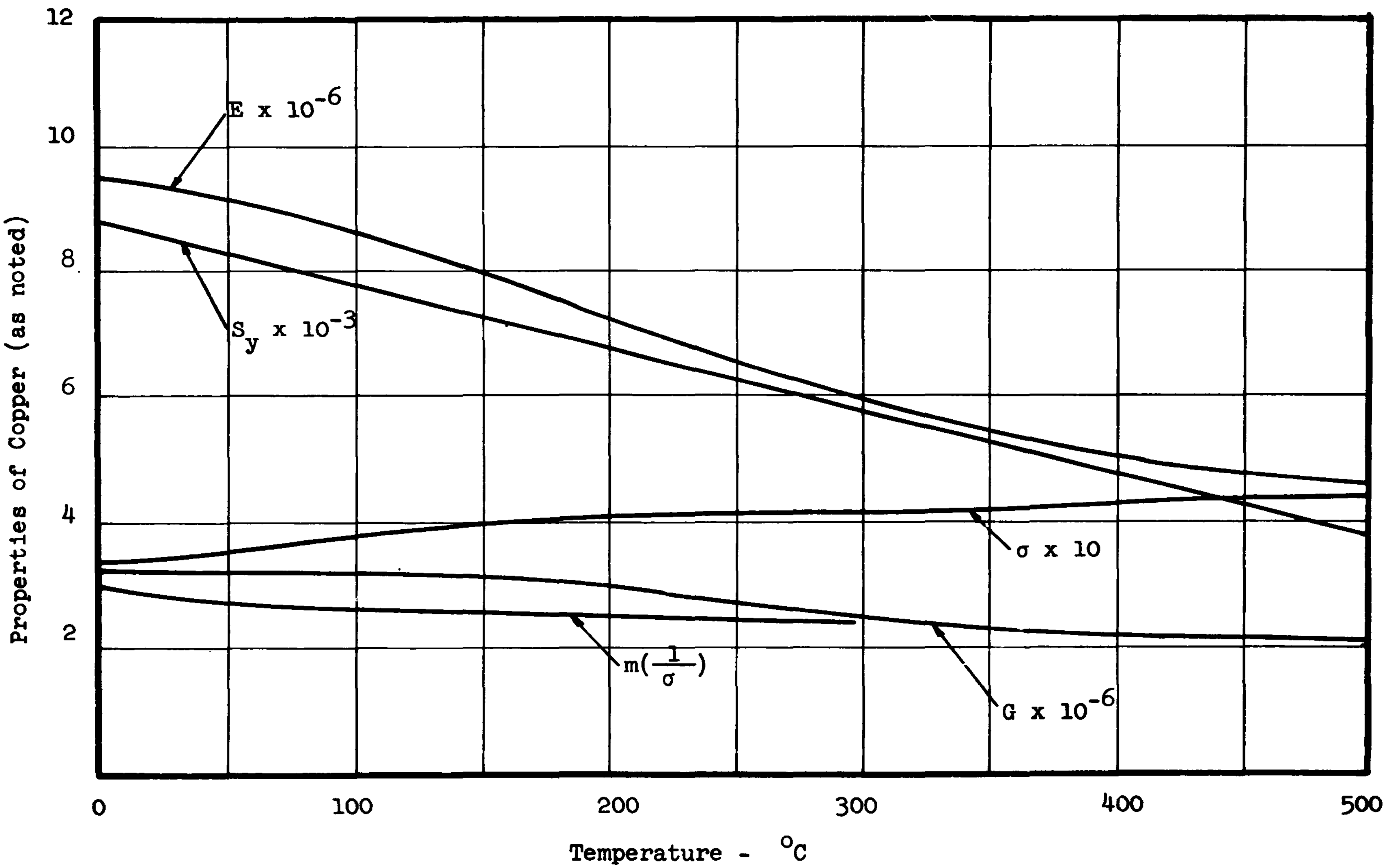
STRESS PATTERN IN
DEFORMED DIFFERENTIAL AREA

Fig. 11



ALLOWABLE PULSE TEMPERATURE RISE FOR COPPER
(TO AVOID THERMAL STRESS CRACKS)

Fig. 12



THERMAL CHARACTERISTICS OF COPPER

Fig. 13

allowable ΔT vs. temperature. See Fig. 12 for typical curves for copper. The peak temperature determined from calculation must be consistant with the value from Fig. 13 or stress cracking will take place and the size of the cracked islands can be determined from Eqn. (25).

3. General Design Procedure.

Since a multitude of considerations determine the adequacy of a design and no analytical expressions relate them, design takes the form of successive approximations approaching the desired result. First, using Fig. 7, determine the beam penetration at the design beam voltage. Then using Fig. 6, and the material constants, determine the ratio of the peak surface temperature to the peak watts per square inch. If a thermally non-cracking design is desired (not practical in many cases with copper as the material) this value can be used in conjunction with Fig. 12 to obtain the peak heat flux input consistent with stress factors. If one assumes a pumping capacity to absorb gases resulting from stress cracks, one then can select an upper temperature as the basis of design and thus determine q. From q, one can calculate q_{oo}, and the heat exchanger. After evaluating the heat exchanger design, a consistent value of peak to average heat flux is determined, and the anode thickness is found by using Fig. 10. The steady state term (see Eqn. 22) is added to the value from Fig. 6 to obtain the final value

of temperature rise.

This result is compared with design goals for temperature, stress relief, and dice size (if used) and the processes can then be repeated to obtain an improved solution.

D. Application of the General Impulse Heating Theory To The L-3458 Magnetron.

The L-3458 magnetron is a high ambient operating temperature version of the 4J50. It is an X-Band, fixed-tuned, pulsed magnetron with a minimum peak power of 250 kw. The pulse length varies with the application of the tube from 1/2 microsecond or less to 5 microseconds. The tube body is cooled by means of an air blower diverted through cooling fins, and the cathode insulator bushing is convection cooled. The pertinent operating parameters are:

Frequency	9375 MC
$V_{cathode-anode}$	23 KV
I_{peak}	27 amps
Duty Cycle	0.001
P_o (Peak)	250 kw min.
P_o (Average)	250 watts min.
Cooling	Air
Tube Body Temperature Rise	$100^{\circ}C$ (Normal Operation)

The pertinent internal dimensions and other data are:

Anode Diameter	0.874 cm
Anode Length (Axial)	0.635 cm
Vane Thickness	0.089 cm
Vane Length (Radial)	0.417 cm
Number of Vanes	16
Effective Anode Area	0.905 cm^2
Electronic Efficiency	61%

The total peak power which must be dissipated on the ends of the vanes is the peak input power multiplied by the factor $(1-\eta_e)$. Likewise, the average power dissipated on the ends of the vanes is the average input power multiplied by the same factor. Therefore, the peak and average power density dissipation on the vane ends are:

Dissipation Power Density (Peak)	266 KW/cm^2
Dissipation Power Density (Average)	266 $watts/cm^2$

The synchronous velocity of the electrons, and the approximate velocity with which the electrons strike the anode, corresponds to 3,000 electron volts which, as may be seen in Fig. 6, gives a depth of penetration of electrons which is so small as to be essentially negligible in this problem.

The pertinent thermal properties of some of the different materials is shown in Table I. The most significant quantity for impulse heating is $\sqrt{k/K}$. The lowest value of this quantity

TABLE I

Table of Useful Thermal Properties (Temperature Range 0 - 600°C)

Material	K $\frac{W/cm^2}{^{\circ}C/cm}$	Cp Joules/gm/°C	Density gm/cm³	k^* cm²/sec	$\sqrt{k / K}$	b (Coeff. of Expansion)
Ag (silver)	4.16	.232	10.5	1.70	.31	19.6
Au (gold)	2.95	.127	19.3	1.2	.37	14.1
Be (beryllium)	.59	2.72	1.85	.118	.585	8.0
Cu (copper)	3.82	.392	8.94	1.10	.272	17.6
Fe (iron, 1%C)	4.85	.448	7.88	.138	.77	14.2
Ni (nickel)	.856	.428	8.90	.154	.67	12.5
Mo (molybdenum)	1.46	.261	10.2	.54	.51	5.6
Ta (tantalum)	.543	.138	16.6	.24	0.90	6.4
W (tungsten)	1.60	.136	19.3	.61	0.488	4.46

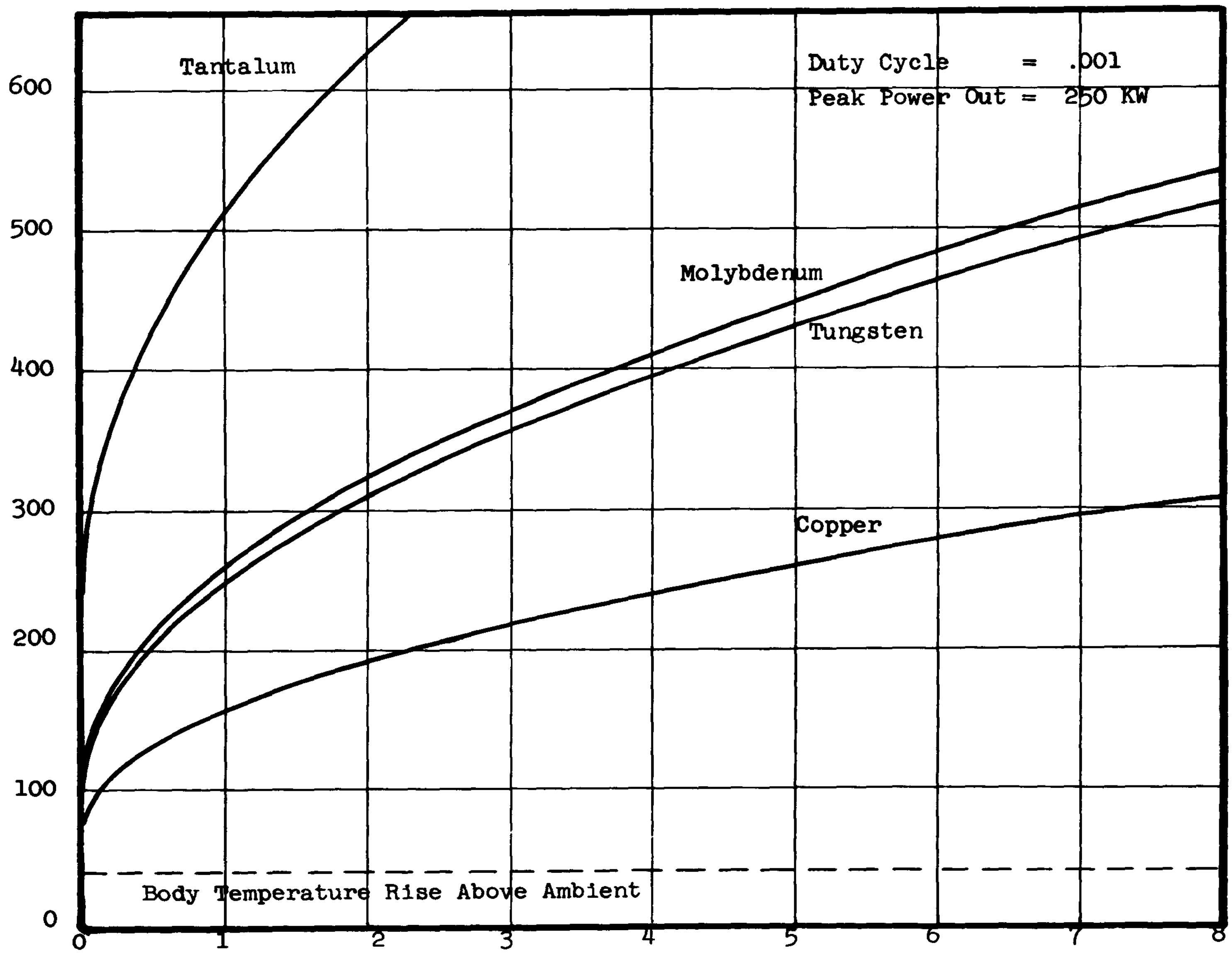
* $k = \frac{K}{C_p \rho}$

gives the lowest peak temperature under a given condition of impulse heating. It is to be noted that copper is the lowest, followed by silver and gold. However, the latter two metals are not useful for vane material since they would alloy with the copper body when brazed. Besides copper, the most interesting metals are the refractory metals of which tungsten is the best, molybdenum next, and tantalum last.

The peak and average increase in temperature of the vane tips above the ambient temperature for the most interesting vane materials are shown in Fig. 14. The peak temperature rise of the vane tips above the body is a function of the pulse length, while for the fixed .001 duty cycle and peak power, the average temperature rise is not.

Also, the temperature rise of the magnetron body above the ambient temperature is only a function of the average power output. These temperature rises are indicated on the figure. This peak temperature rise penetrates only a little way into the vane tip. Fig. 15 shows the depth into which the peak temperature rise is reduced to 1/10 that of the surface for various vane materials and pulse lengths. It is to be noted that the depth of impulse heating penetration is very small, in all cases less than a few thousandths of an inch.

It is to be noted from Fig. 14 that the copper vanes have the least temperature rise of all the various types plotted.

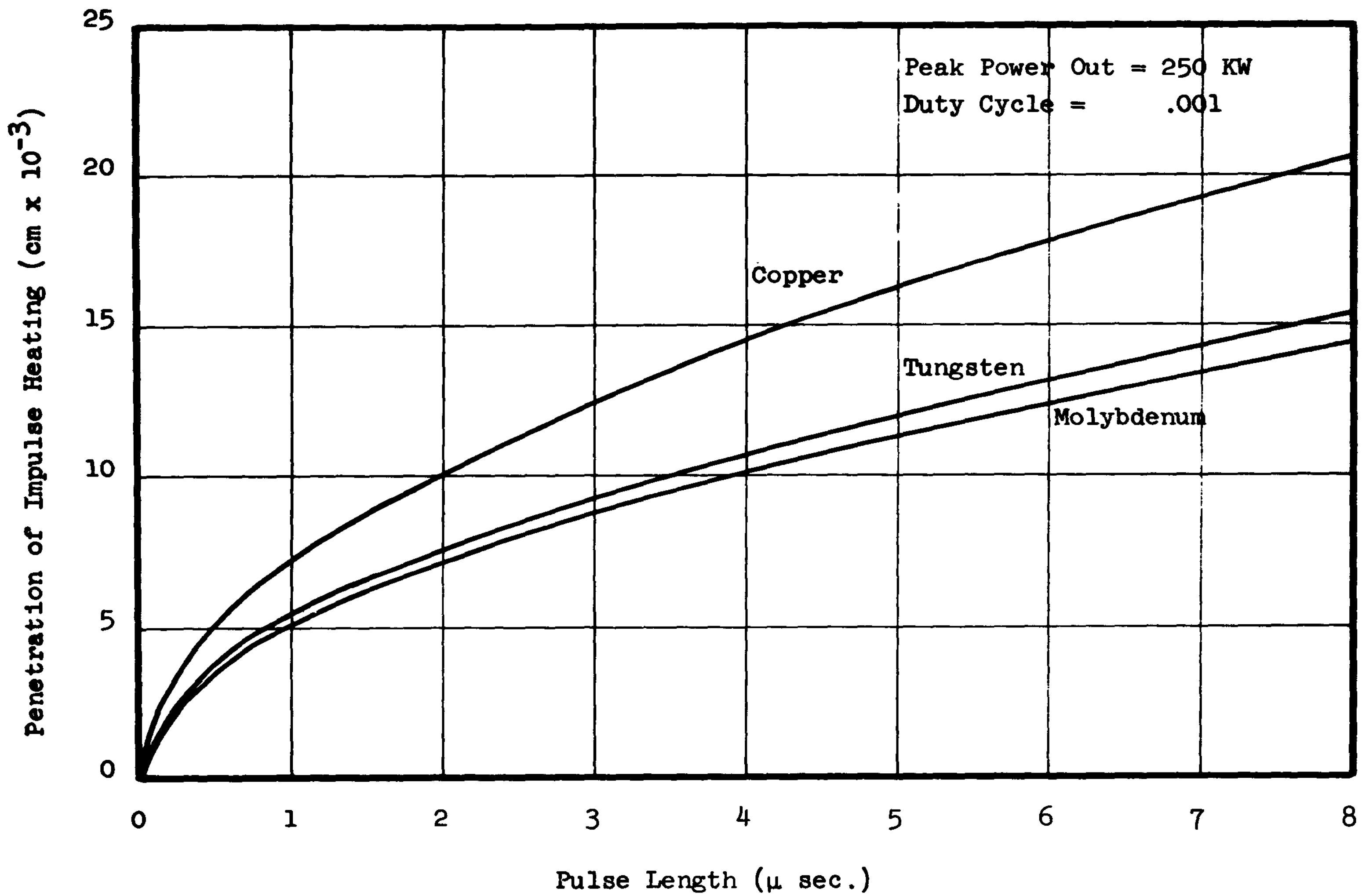


Peak Vane Tip Temperature Rise Above Ambient in ^{o}C

Pulse Length (Microseconds)

THERMAL PROPERTIES OF VARIOUS VANE TIP MATERIALS

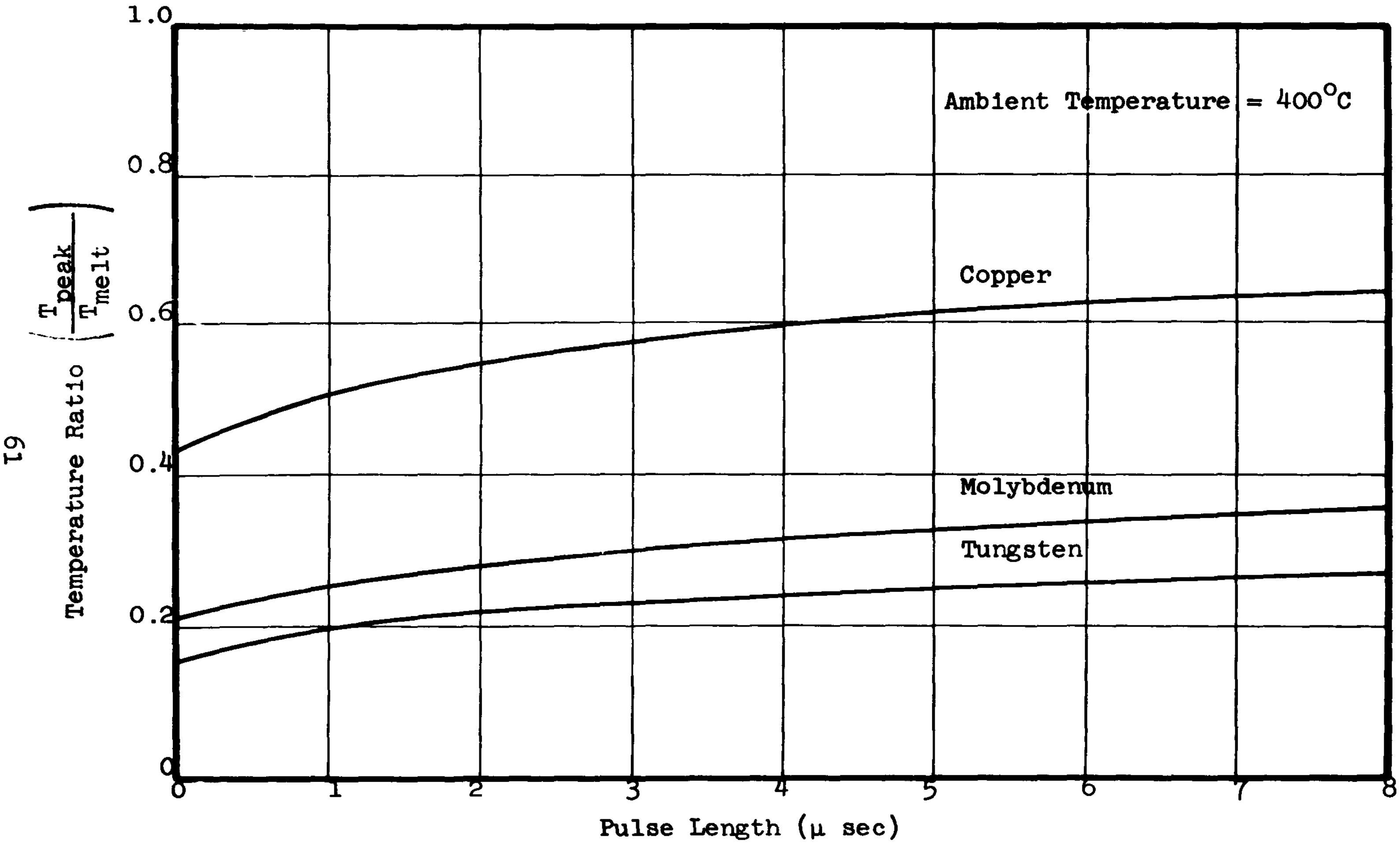
Fig. 14



CALCULATED IMPULSE HEATING PENETRATION INTO VANE TIPS OF L-3458 FOR VARIOUS MATERIALS

Fig. 15

The tungsten and molybdenum vanes are the next best, and the tantalum vanes reach the highest temperature. A more significant number, however, is the ratio of peak temperature to the melting point of the metal, since this gives the relative deterioration index of the material. Fig. 16 shows curves of peak temperature rise over melting temperature for copper, tungsten, and molybdenum vanes for various pulse lengths. In this calculation, a 400°C ambient temperature was assumed. It is to be noted that tungsten has the lowest peak temperature relative to melting temperatures. Also, the coefficient of expansion of this material is smallest, and its mechanical strength is best of all the materials. Therefore it is concluded that tungsten is thermally the best material for the vanes for the L-3458 magnetron. However, the electrical resistivity is about three times that of copper, so that its rf surface resistivity is about 1.7 times that of copper. Hence tungsten vanes would reduce the loaded Q to about .6 that of copper vanes. This is too much rf circuit loss for this tube, so the tungsten vanes must be copper clad. This would be accomplished by copper plating, sintering, and then polishing the copper. The straps could be made of either copper-clad molybdenum or copper-clad monel. The vanes could be fabricated from a high-grade, cross-rolled tungsten sheet with the spark erosion technique.



CURVES OF RATIO OF IMPULSE HEATING PEAK TO MELTING TEMPERATURE
AS A FUNCTION OF PULSE LENGTH

Fig. 16

For very small sized vanes, such as are used in the L-3458, it is easy to make the whole vane out of tungsten. For larger vanes, such as would be used in S or L-Band tubes, this would not be convenient because of their large size. For large vanes, it would be better to fabricate them out of copper, and then braze a thin sheet of tungsten on the dissipation surface.

Another fabrication operation that would help the impulse heating metal fracture problem would be to "dice" the surface as described in the previous section. This would require cutting very thin cross grooves in accordance with Eqn. 24.

E. Ion Pumping Considerations.

Since the operation of microwave tubes at high ambient temperatures will inherently cause somewhat poorer vacuum in the tubes than at normal ambient temperature, it is desirable to have these tubes supplied with a continuously operating ion getter pump (Penning discharge type). The most common material used in such pumps is titanium which has extremely good getter properties. Since most microwave tubes utilize applied dc magnetic fields, it would be possible, in principle, to build an ion getter pump, directly into the main body of the tube, by making certain tube parts out of titanium. Unfortunately, titanium is a structurally difficult material;

that is, it shows very low heat conductivity, is difficult to match in expansion with other materials, crystallizes at high temperatures, is impossible to hydrogen fire, and outgases heavily at high temperatures. Titanium can also easily poison cathodes. There is also evidence that any Penning discharge close to an rf circuit would tend to load this circuit to an undesirably high degree. It is therefore not recommendable to locate any ion getter pumps in the main body of tubes unless they are very well shielded from cathode and rf. Considering this fact, together with the constructional and processing problems, it seems more desirable to place the ion getter pump into an appendage to the tube. This would allow a separate processing schedule for the getter pump and would not compromise the choice of structural materials and processing methods in the tube.

There is, of course, also the possibility of using titanium as plain getter material in the tube; that is, not incorporated into a Penning discharge pump. The problems encountered here are again possible cathode poisoning and the heavy outgassing of titanium at high temperatures such as those which will be prevailing in the ambient temperature range of interest. This method appears actually less desirable than the separate getter ppmp which is more controllable.

The separate ion pump could utilize fringing magnetic fields of the magnetron permanent magnet. Further, in some

cases, it might be possible to operate the pump from the magnetron power supply, thus obviating the need for a separate power supply. These modifications would be relatively straightforward and should obtain a considerable extension in tube life under a high ambient temperature environment.

F. Double Envelope Exhaust.

Another method for obtaining "clean" tubes is to utilize a double envelope exhaust. The tubes which were shipped under this program were exhausted at 750°C to get maximum outgassing. The second vacuum envelope surrounding the tube prevented severe oxidation which would have otherwise taken place at this temperature. It also prevented atmospheric gases from entering the tube by permeation and saturating the tube parts.

The internal or tube vacuum was obtained with a getter ion pump, thus eliminating the possibility of contamination by oil. The exhaust schedule took about twelve hours with the tube at 750°C for a minimum of four hours. This allowed pinch-off in the 10^{-9} torr range with minimal oxidation.

IV. DISCUSSION OF CATHODES FOR HIGH POWER PULSE MAGNETRONS.

The types of cathodes for pulse magnetrons which have had wide-spread use during recent years are nickel base oxide cathodes, nickel mush cathodes, barium aluminate impregnated tungsten matrix cathodes, and the so-called cermet cathodes.

A. Oxide Cathodes.

The ordinary oxide-coated nickel base cathode is useful only for cases where the electron back-bombardment power density is low and where the emission density requirements are modest. The oxide layer of this cathode acts like a thermal insulator between the base metal and the emission surface. This layer also causes hot spots to form when very high current densities are involved, which leads to arcing. Therefore, for high frequencies, this type of cathode is confined mainly to low power pulsed magnetrons, and for high power pulsed tubes it is confined mainly to very low frequency magnetrons. In high-frequency, high-power magnetrons, the oxide cathode has not been useful because of the destructive action of the high density dissipation caused by the electron back-bombardment power and the destructive action of the high emission density.

B. Nickel Mush Cathodes.

The most commonly used cathode for pulsed high-power, high-frequency magnetrons is the nickel-mush cathode. In this type cathode, a hollow cylindrical sleeve has a layer of nickel

powder mixed with barium and strontium carbonates (5-10%) molded and sintered to its outer surface. The nickel powder plays a multiple role in this cathode. It provides the base metal for a low work function emission surface; it provides for good thermal and electrical conductivity at the emission surface; and it minimizes sputtering of cathode material by positive ions.

One of the problems with the nickel mush cathode is the difference in expansion between the powdered mush material and the cathode sleeve -(usually molybdenum) onto which it is sintered. Repeated heating and cooling often causes the mush layer to crack and the edges to lift up from the molybdenum sleeve. (This is often called "mud-flatting.") The lifted-up edges of the mush layer cause concentrated high electric fields, poor heat transfer and increased bombardment, which results in localized arcing and melting. This problem has probably caused more terminations of life of pulsed magnetrons than any other cause.

One method of relieving the "mud-flatting" problem in nickel mush cathodes is to put the mush material into circumferential grooves in the molybdenum sleeve, instead of coating the entire surface of the molybdenum. This not only relieves the problems of differential expansion (because it reduces the lengths over which the bond between different materials exist),

but also tends to lock the mush material to the molybdenum base along the sides of the grooves. This technique, however, doesn't completely eliminate the "mud-flatting" problem. This is because of the long bonding surface length along the groove, which eventually causes cracks at right angles to the groove to form in the mush material. Although these crack edges do not, in general, lift up, it is still desirable to eliminate them.

One way of doing this is to use closely spaced holes drilled radially into the molybdenum sleeve instead of grooves. These holes should not go through the sleeve, and should have flat bottoms. The holes would be filled with mush material and sintered, after which the surface would be machined smooth. If the diameter and depth of the holes were to be 1/32" there probably would be no mud-flatting effects at all.

There is one other minor problem with the nickel mush cathode which must always be contended with, even though one "designs-out" all the undesirable problems previously mentioned. This problem is the slow evolution of gas from the mush material during the life of the cathode. CO gas is normally evolved along with metalic barium from the mush material at the proper operating temperature of this cathode. Without such a chemical reaction, the primary emission of the cathode would not be maintained. This gas is absorbed by the gettering action of the walls of the tube, or by a getter ion pump attached to the

tube envelope. There is not enough gas evolution to interfere seriously with the operation of the tube, but the fact that it originates in the immediate vicinity of the interaction space decreases the resistance of the tube to arcing and sparking. It is for this reason that we feel that a better cathode for pulsed magnetrons is the tungsten matrix-barium aluminate impregnated cathode which is described in the next Section.

C. Tungsten Matrix Barium Aluminate Cathode.

This cathode is made by impregnating a tungsten matrix with barium and calcium aluminate. For magnetrons, the tungsten matrix is usually in the form of a hollow cylindrical sleeve. The matrix sleeve is often brazed at its inner diameter to a longer molybdenum supporting sleeve. This type of cathode has several advantages over the nickel mush type cathode for high power pulsed magnetrons. (a) the tungsten matrix is a better thermal conductor, is more refractory than the nickel mush surface, (b) it can operate at higher temperatures without evaporation of material which is important when the back-bombardment power density is high, (c) it is much more resistant to emission poisoning than the nickel mush cathode, and (d) there is no gas evolution from the cathode under normal operation. In the past, one of the disadvantages of this cathode has been the difficulty of heating it to its required temperature by the use of an ordinary alumina coated heater. The main

problem here has been overheating of the heater insulation coating. However, in our development of these cathodes for other tubes we have overcome this problem by using alumina "packed" heaters, where alumina powder is packed into the entire heater region and sintered, thus creating a very good thermal conduction path between the heater and the cathode. With this good thermal conduction path the heater temperature is kept down to very reasonable limits.

A proposed design for this cathode is shown in Fig. 17. In this design the end hats are made of molybdenum and are thermally isolated from the cathode. They could be treated to suppress secondary emission either by coating with chromium or by carburizing them. The heater is packed with alumina as previously discussed. The cathode emitting temperature will run at about 950°C for optimum operation of the tube. This is sufficiently high temperature for enough primary emission for starting the magnetron pulse.

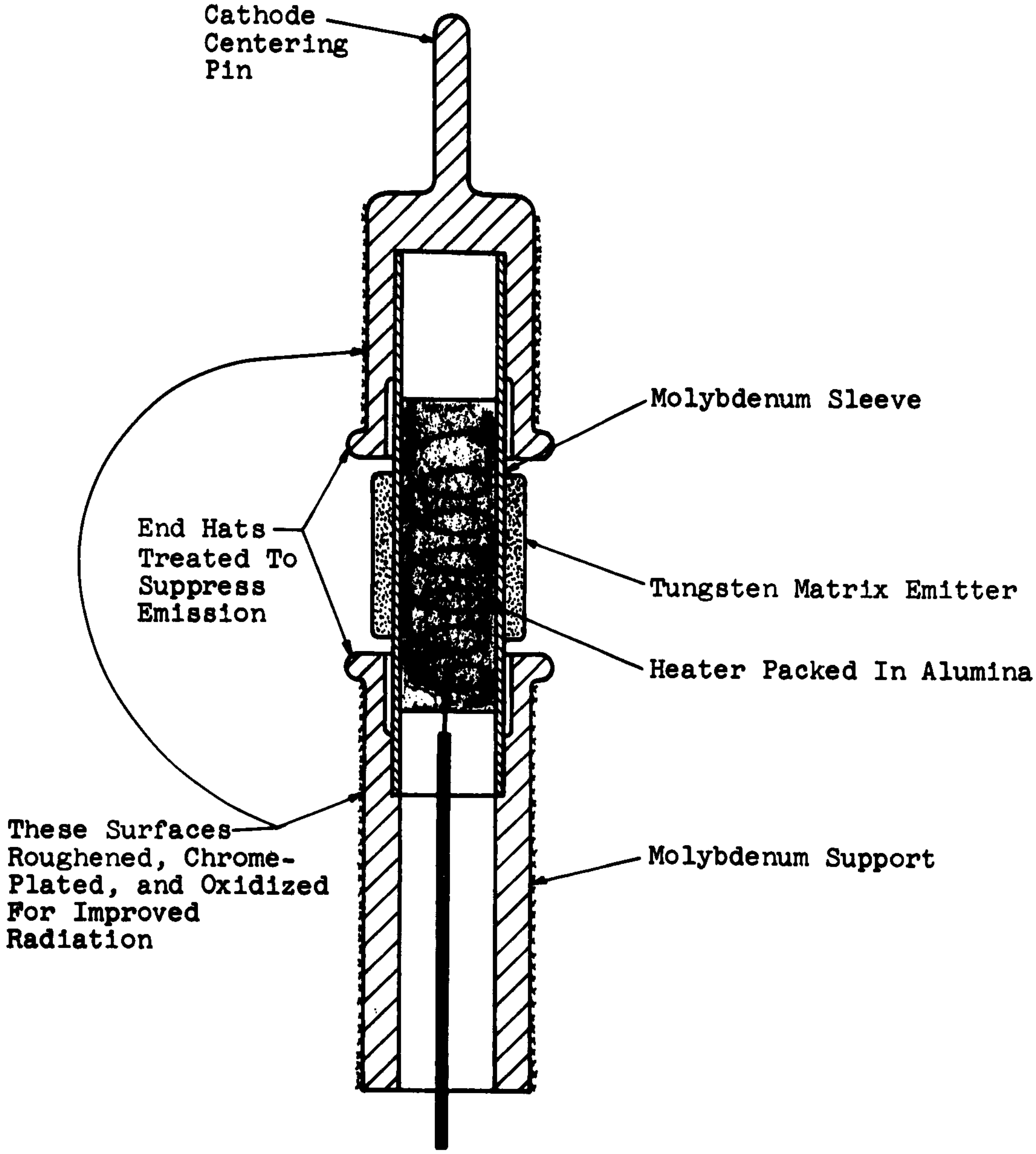
The chief source of emission is, of course, the secondary emission caused by back-bombarding electrons. The secondary emission rates of the cathode surface is kept sufficiently high by migration of barium aluminate to the surface at 950°C. It has been found from our experience with other cathodes that the tungsten matrix emitting sleeve does not crack with repeated heating and cooling even when it is brazed to a molybdenum sleeve as in Fig. 17. This is because its mechanical strength

sufficiently exceeds that of the molybdenum sleeve so that the molybdenum yields and the tungsten matrix is not stressed.

A barium-aluminate impregnated tungsten matrix cathode was used in some early versions of the L-3458, but they were not very reliable. This is because the tungsten matrix was merely slid over the molybdenum sleeve and mechanically locked in place. Thus a serious thermal impedance existed between the heater and the emitting surface which required excessive heater temperature. Also, the heaters were not packed with alumina for good thermal conductivity, which further raised the heater temperature. For these reasons, we used the grooved versions of the nickel mush cathodes for the six tubes delivered under the contract. However, the design of Fig. 17 would eliminate all the problems previously encountered with the barium aluminate impregnated tungsten matrix cathode, and improved tube operation would result.

D. Cermet Cathodes.

Another type of pulsed magnetron cathode which has had limited use in high power tubes is the so-called cermet cathode. It generally consists of a mixture of tungsten powder and thoria, (or thorium or thorium hydride) which is compressed into a sleeve and sintered. The operating temperature of such a cathode is in the neighborhood of 1500°C, thus precluding the use of alumina coated heaters. An early method of heating



MODIFIED CATHODE DESIGN

Fig. 17

this cathode was to run heating current through the mixture, but this was found to be unreliable because a localized band of high resistance would form and burn out the sleeve. Therefore, this method of heating has been abandoned in favor of either heating with uncoated tungsten heaters, or by the use of axial electron bombardment heating.

One very important thing has been learned about the effect of the constituency of the cermet mix on arcing and vane tip erosion in a 1 MW X-Band pulsed magnetron. Mark and Lea Wilson of Services Electronics Research Laboratory,Baldock, Herts, England found that when thoria was used in the cermet cathode of this tube, the evolution of gas from the cathode caused severe vane tip erosion. When thorium, or thorium hydride was used the vane tip erosion was eliminated. (See Section III, B for a discussion of this problem). A complete discussion of the fabrication of this type of cathode may be found in the literature.

E. Discussion of the Relative Merits of The Various Types of Cathodes.

It is our conclusion that the barium-aluminate impregnated tungsten matrix cathode is the best and most practical type for high-power, pulsed magnetrons for good reliability and high ambient temperature operation. This is particularly true where the cathode is called upon to operate under the strongest conditions of back-bombardment due to various rf load VSWR conditions, etc. at high power density. For somewhat less stringent

operating conditions, the nickel mush type cathode is also a very good design, particularly if trapping holes are used in the molybdenum sleeve to lock in the emissive material so that "mud-flatting" is avoided.

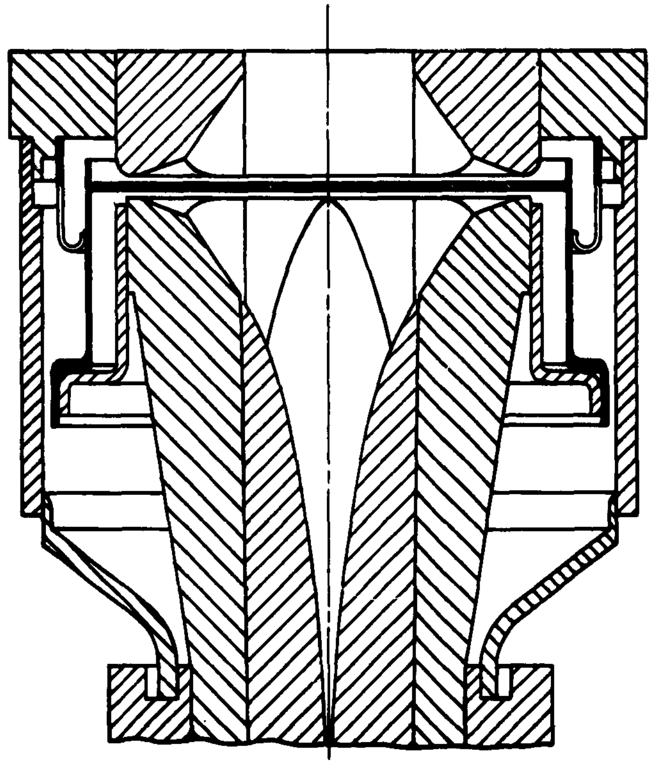
Cermet type cathodes should be used only if extreme back-bombardment power densities dictate the use of a very high temperature cathode. The primary heating of this type cathode is the main problem in its use. Indirect heating by an uncoated tungsten heater is the easiest method, but unreliability because of the necessary high heater temperature is a problem, particularly if the mechanical vibration environmental condition is severe. The only practical alternative method of heating the cathode is by the use of axial electron bombardment by an electron gun located at one end of the interaction space. It is possible to place a convergent Pierce type gun in one of the pole pieces. The practical problem that this technique raises is the extra power supply that the electron gun would require.

V. IMPROVED WINDOW DESIGN.

As previously stated, one of the major modifications necessary to convert the 4J50 to high temperature operation was that of changing the output window structure to ceramic. The basic conversion problems thus incurred were change in window seal techniques and the change in the rf match.

Theoretically, an extremely thin ceramic would provide the best rf characteristics. However, calculations of strength considerations indicated that the minium length of the ceramic-to-metal seal should be .030" to .060". On this basis, several configurations of seals in the thickness range were tested. The resulting design is shown in Fig. 18.

This design, in cold test, proved to be reasonably good electrically. It provided broadband VSWR of less than 1.2:1. However, when it was incorporated into a tube, it proved to be quite susceptable to breakage. Another problem with this window was that of arcing. In the actual tube, where relatively large power levels are transmitted, coupler arcing occurred when the finger stock was not in good contact with the window frame, and a high reflected impedance resulted between the window and coupler. Arcing also occurred if the tube operated in other than π-mode. The output coupler had a circular wavelength mode which was excited by the $(\frac{N}{2} + 1)$ mode of magnetron operation. In either case, the ultimate result was generally arcing through the window and puncture.

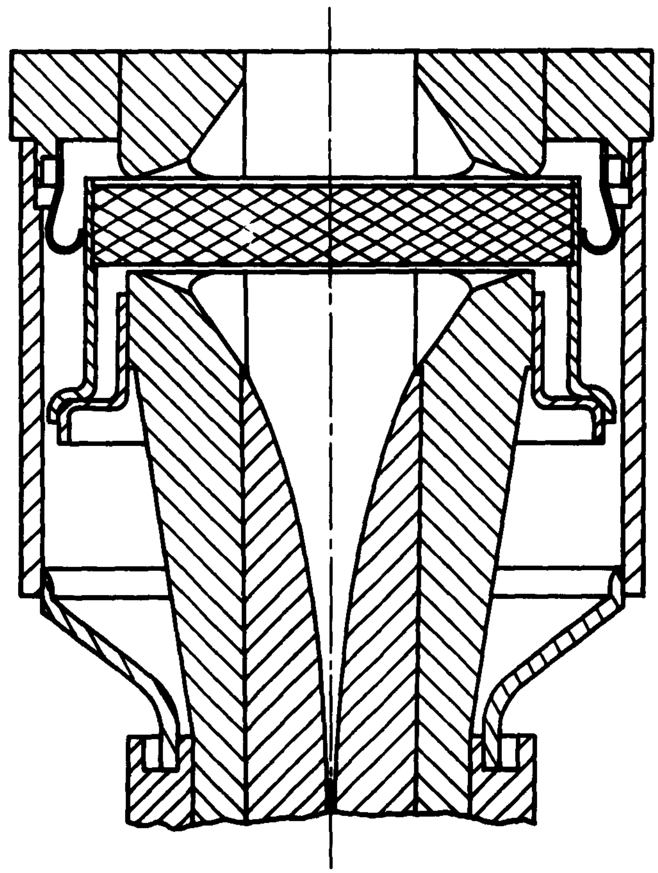


L-3458 OUTPUT STRUCTURE

Fig. 18

These results indicated that a thicker window material was necessary in the output structure. From microwave transmission considerations, the next most desirable thickness was one half wavelength. Fig. 19 shows this final design which was incorporated into the L-3458. This structure proved to be strong physically and quite acceptable electrically (1.03 to 1 VSWR at 9375 Gc).

The use of high purity alumina (AD-74) along with the foregoing design changes evolved a very reliable output structure for the L-3458.



HALF-WAVE LENGTH OUTPUT WINDOW

Fig. 19

VI. HIGH TEMPERATURE EFFECTS ON TUBE PARTS OUTSIDE THE VACUUM ENVELOPE.

There are two basic adverse effects, on tube components external to the vacuum envelope, associated with high temperature operation. The effects are thermal demagnetization of permanent magnets, and accelerated corrosive oxidation.

A. Thermal Demagnetization.

1. Effects of Demagnetization.

The variation in magnetic field strength caused by change in temperature is quite small. In the case of Alnico V, the change is about .02% per degree centigrade, with the magnetic field decreasing as the temperature rises. There are also non-reversible changes, but they occur only once during the first temperature cycle, and therefore can be compensated for by proper design.

A brief review of some of the electrical parameters in crossed-field devices will help in understanding the problem of thermal demagnetization. All rf tubes extract power from moving electrons. Crossed-field, or "M"-type devices employ crossed electric and magnetic fields to accelerate the electrons. Electron velocity is governed by the following relationship:

$$v_e = E/B$$

Thus, in order to maintain uniform velocity, any change in B requires a corresponding change in E, which is proportional to the cathode-anode voltage, v, in the case of the magnetron. In the case of the magnetron, the cathode

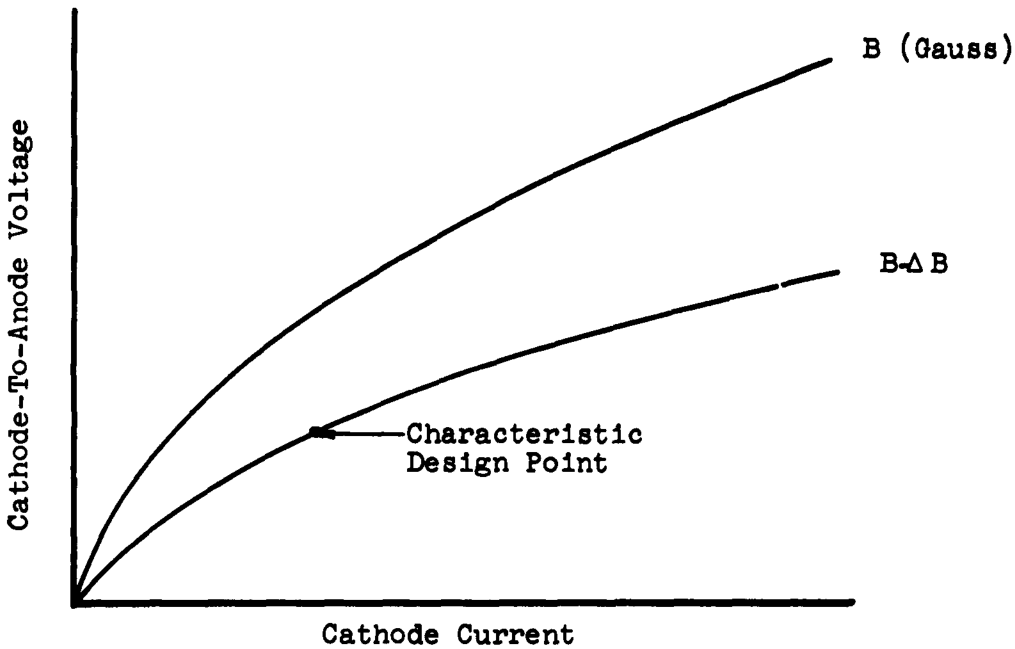
current is also a function of v and B. Fig. 20 is a typical set of cathode-anode voltage vs. cathode current (or V-I) curves, at various magnetic fields, for a magnetron. It is obvious that, at a constant magnetic field, a small change in v (or E) causes a considerable change in I. Also, at constant v, a small change in B causes a considerable change in I.

From the output power curves, (Fig. 21) it may be seen that a change in either v or B alone causes a change in power output. If B varies (as it would in case of temperature change in the magnet) the power output would vary unless v were also varied in the right way (v must be kept proportional to B).

Since control of E (i.e., v) requires additional complexity in the system, it would be more desirable that the magnet be less temperature sensitive or that it be temperature compensated so that the tube operates at proper voltages and currents over a wide range of temperatures. Some of our effort has been concentrated on reviewing new magnetic material, and on temperature compensating methods.

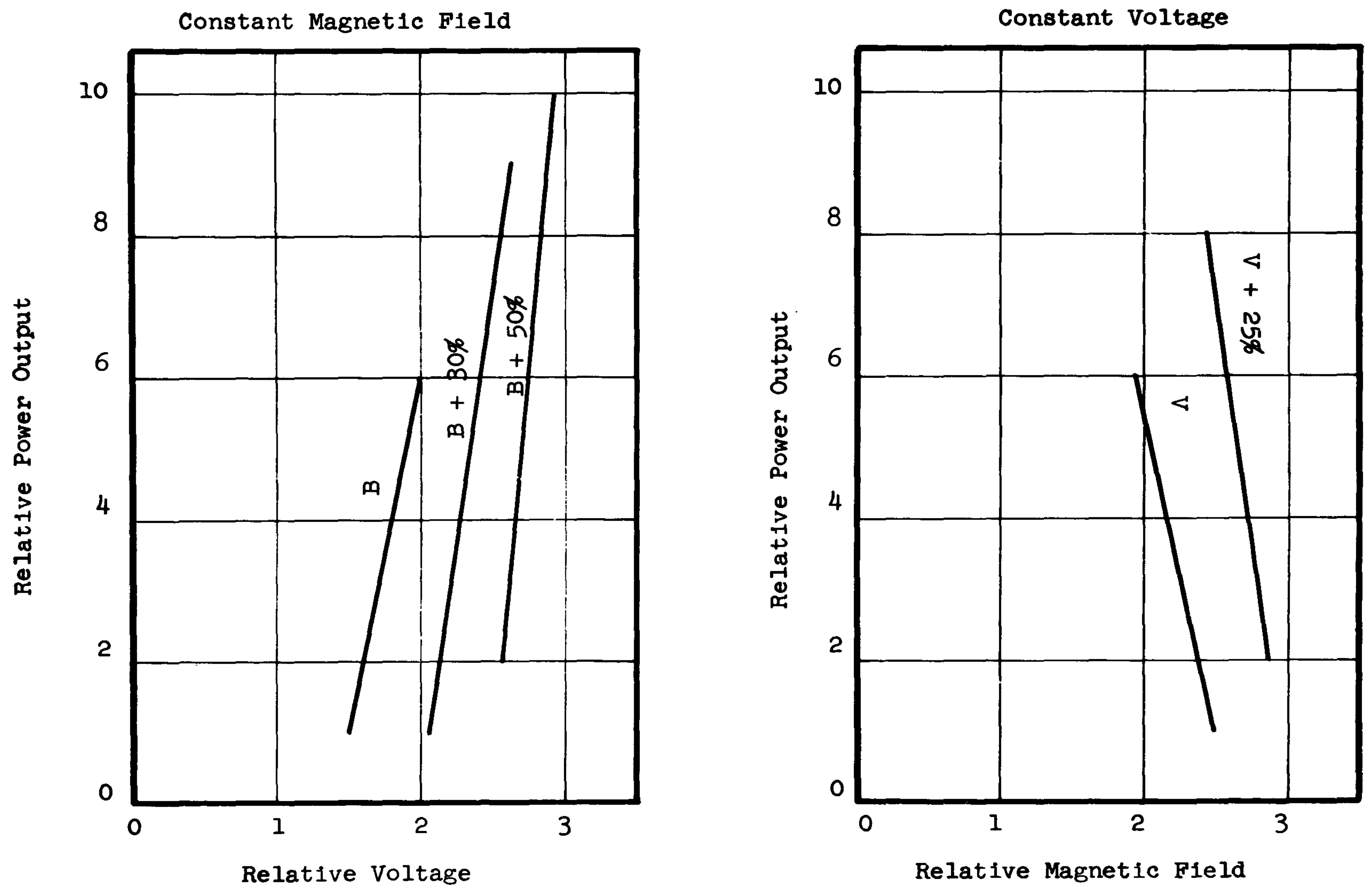
2. Magnetic Materials.

The first step was to search for more thermally stable magnetic materials. Most existing M-type devices employ Alnico V magnets. Our search revealed only one material with better thermal properties, and which was developed to the point of practical application. This material, platinum-cobalt, shows extremely good characteristics.



TYPICAL V-I CURVES FOR A MAGNETRON

Fig. 20



RELATIVE POWER CURVES FOR A MAGNETRON

Fig. 21

It is a 50-50 atomic percent platinum-cobalt alloy supplied by the Hamilton Watch Company. Its energy product is 9 X 10^6, a significant improvement over that of Alnico V which is 5.5 X 10^6. Its thermal coefficient field change is about .01% per degree C as compared with .02% per degree C for Alnico V. Its density is about twice that of Alnico V. The major drawback to this material, however, is its high cost. The cost of a magnet such as would be required for the 4J50 magnetron would cost approximately 50 times the present tube price.

Various ceramic-metal magnets were investigated. However, their magnetic variation with temperature is much higher than for the Alnico, and the value of B for its maximum energy product is too low for good magnetron magnet design. Therefore we have eliminated these types of magnets for this application.

The most promising magnetic material for this high temperature magnetron application is Alnico V-7. This material is similar to the Alnico V, but its magnetic dipoles are more completely oriented along the lines of maximum magnetic potential gradient. Its maximum energy product is 7 X 10^6, and its coefficient of magnetic-temperature variation is .016% per degree C. Unfortunately, this material cannot be fabricated, at the present state-of-the-art, in unusual shapes. Since dipole orientation depends on controlled cooling (under the influence of a magnetic field) the shape and size of these magnets is limited.

We have concluded that Alnico V is still the best material available for magnetron application. Very much improved thermal stability can be achieved by some sacrifice in size and weight. According to one manufacturer*, if the magnet is designed to operate at a value of H, which is 10% less than that corresponding to the maximum energy product, the thermal coefficient can be reduced by a factor of two or more. This would result in a .01% per °C, or better, thermal coefficient at a cost of 5% increase in the weight of the magnet.

3. Solution To The Problem of Magnetic Variation With Temperature.

As discussed in the previous section, this problem is reduced by designing the magnet to operate at higher B and lower H than those corresponding to the maximum energy product, and by very carefully orienting the magnetic dipoles when the magnet is formed. After having done this, if the magnetic variation with temperature is still higher than desired, one could design the magnet to provide the necessary gauss <u>at the elevated temperature.</u> This would mean that the magnetic field would be too high at room temperature. However, if the environment could be predicted as being within a certain relatively narrow temperature range, the effect on the magnet is predictable,

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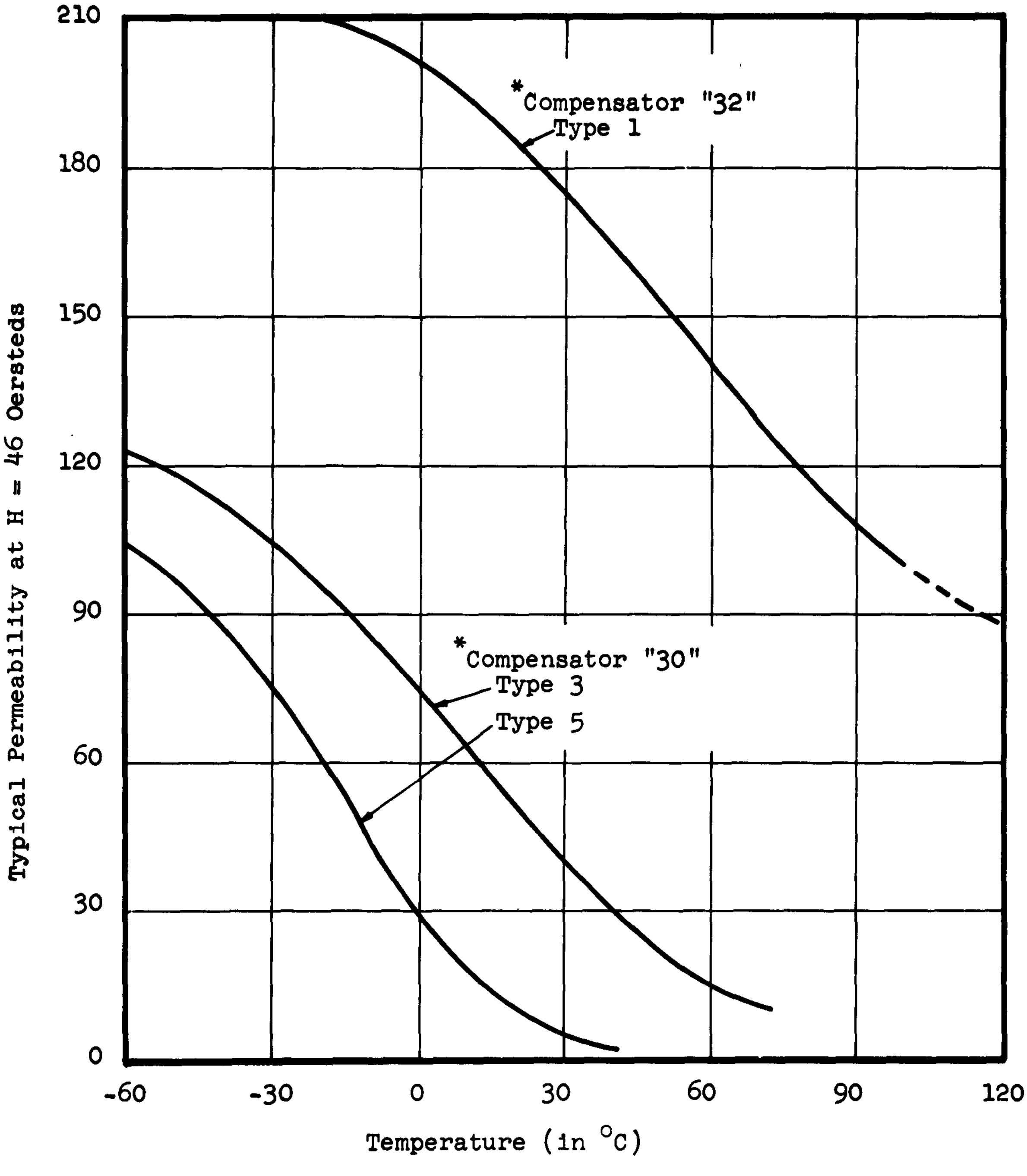
*Indiana Steel Products Company, Valparaiso, Indiana.

and it could be designed to provide the proper gauss over that range. The only complication in this approach would be that check-out and test of the tube would have to be accomplished in the same temperature range. Also, if the tube were operated at any other temperature, the operating voltage would have to be adjusted.

If more uniform operating conditions are desired over a wide range of temperature variations than can be achieved by the previously-described methods, one must use temperature compensating magnetic shunts. The common material for this purpose is iron-nickel alloy, marketed by the Carpenter Steel Company of Reading, Pennsylvania. Litton has utilized two types of compensating alloys; 29% nickel-71% iron, and 32% nickel-68% iron. The higher percentage nickel alloy gives compensation up to higher temperatures, but has a less linear characteristic. The 32% nickel alloy does not compensate above 130°C. Curves of permeability as a function of temperature of both the 29% and 32% alloys are shown in Fig. 22. For higher percentage nickel alloys, permeability variation occurs at a higher temperature, but becomes much less linear. It is to be noticed that the curves tend to flatten at the lower temperatures for a higher nickel content alloy. To overcome these problems, the designer would use the combination of the high percentage nickel alloy along with parallel shunts made of



CHARACTERISTICS OF THERMALLY COMPENSATING
MAGNETIC SHUNT MATERIALS
(*From Carpenter Steel Co. Catalogue)

Fig. 22

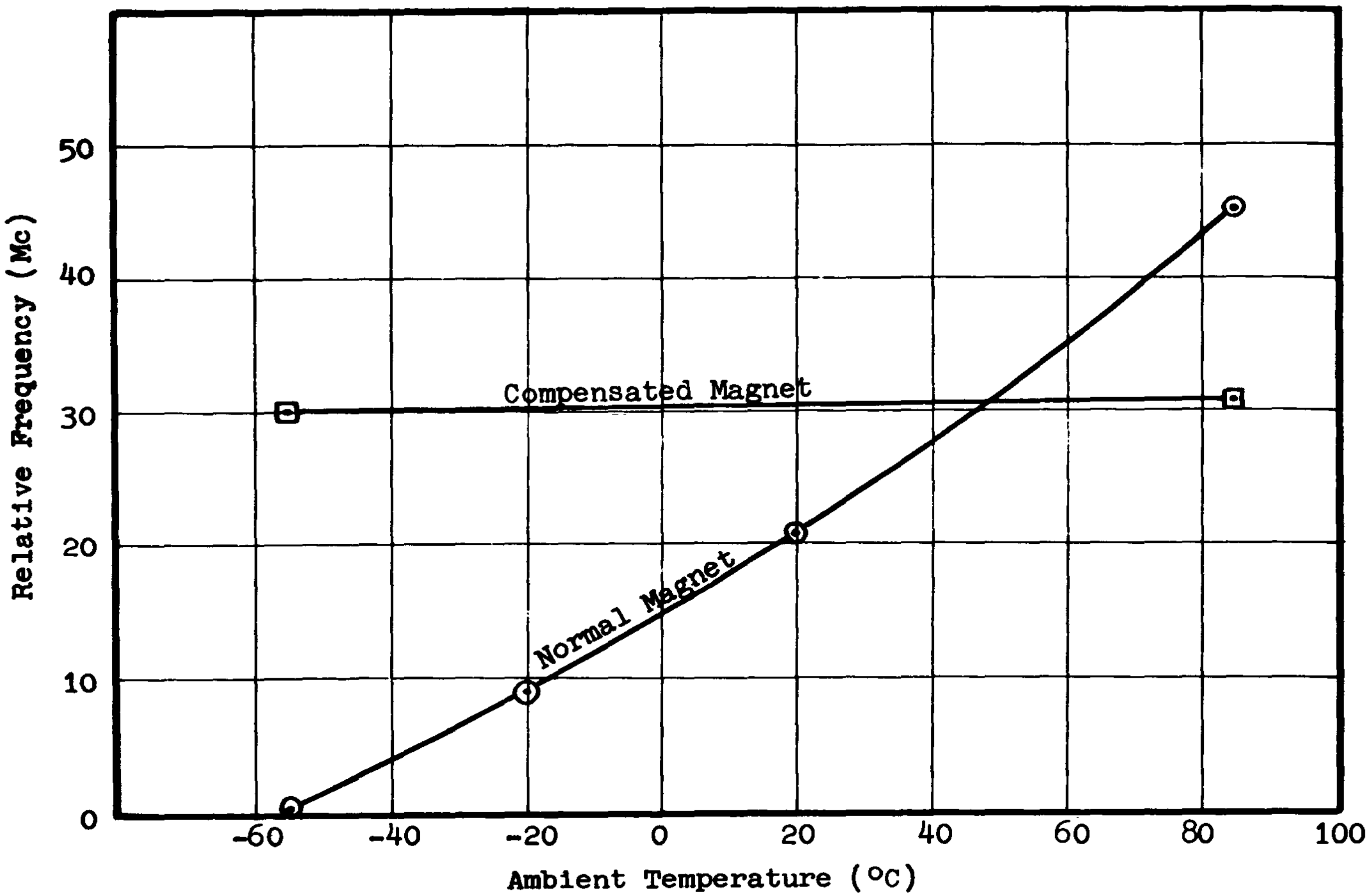
the lower percentage nickel material. Thus, a relatively linear compensation could be approached over the entire temperature range.

Litton has applied the single material shunting technique to several tubes. A notable example is the L-3444, M-type, backward-wave oscillator. In this type of tube, the frequency varies rapidly with magnetic field at constant voltage so that the magnet must be temperature compensated in order to avoid thermal frequency drift. Fig. 23 illustrates the improvement obtainable by this means. The previous design was uncompensated. The only change in the new design was the addition of compensating shunts.

It should be noted that we have not carried out this temperature compensation and stabilization in the L-3458 because the scope of this program did not include the extensive design work required to modify magnets or to advance the state-of-the-art in shunting techniques to the point of application of this wide temperature range.

B. Oxidation.

Corrosive oxidation is an ever present problem in high temperature operation involving metalic parts. In the case of electron tubes, these problems cannot be met by straight-forward use of "Stainless" materials. In those regions where rf currents are flowing, the electrical resistance of these materials makes their use impractical. Sealing of glass or ceramic directly to these materials is impossible due to differential thermal expansion. Thus, while stainless can be used for the



THERMAL FREQUENCY DRIFT CURVES FOR L-3444

Fig. 23

exterior shell in the vacuum assembly, a simpler solution is to cover the exterior of parts fabricated from normal materials with an oxidation resistant coating.

1. Problems Caused By Corrosive Oxidation.

There are two basic problems caused by corrosive oxidation. First, is the "brute force" disintegration of the metals. Second, is the electrical and thermal insulating properties of the oxides formed.

In our testing of the L-3458 magnetrons without protective coating, after 11 1/2 hours of operation at 350°C the copper cooling fins were about half disintegrated. This causes a proportionate reduction in heat conduction path. The exposed Kovar also corroded quite badly. Since the Kovar is extremely thin, this provides a threat to the vacuum envelope, particularly in the ceramic-to-metal seal area.

Before disintegration becomes a problem, the insulating properties of the oxides begin to inhibit performance. The thermal insulation on the cooling fins reduces radiant heat dissipation. The oxides formed on the electrical connection also provide areas of increased electrical resistance. In addition, any oxides formed in the rf connections will alter the rf output match.

2. Oxidation Prevention.

The ideal solution to this problem would be to coat all exposed parts with some inert metal such as gold. Within the <u>operating</u> temperature range, this would solve

the problem. However, at bake-out temperatures, the gold migrates into the copper parts and the copper is reexposed. It would seem that the logical solution to this would be to gold plate the tube after bake-out. This has been done in some cases but, in general, it appears to be a dangerous practice. Nascent hydrogen is often left under the plating, and this could permeate the vacuum envelope. Also, at elevated temperatures, the gold diffuses into the copper. Therefore the direct approach is not advisable.

One method of stopping gold diffusion at elevated temperatures is to nickel plate first. The nickel reduces the diffusion rate of gold into copper by approximately 100. However, nickel, itself, is sufficiently corrosion resistant that the gold plating was found to be unnecessary except on the electrical connections. This was the final processing employed on the L-3458.

Plating of magnets was not found to be very satisfactory because the porosity of the magnetic materials made uniformity of plating unpredicatable. In order to prevent corrosion by the elevated operating temperature, the magnets on the L-3458 were plasma sprayed with alumina. This solved the magnet corrosion problem very nicely. However, for other external parts of the tube, where plating was feasible, the plasma spray was not used since the plating also functions as a gas permeation barrier (see Section III,A,2).

Another area that bears further investigation with reference to oxidation prevention is that of high temperature epoxy coatings. Those that were found under this program were highly susceptable to abrasion and, therefore, not considered satisfactory. However, this field is rapidly making progress. Should such an epoxy or other plastic coating be developed, it would supply a much simpler and cheaper solution to this problem.

VII. DISCUSSION OF THE TUBE LIFE DATA.

The testing in this project was essentially a two step operation. Any modifications incorporated into the tubes were first tested at room temperature in order to compare the effect of this change with existing data. Then the modification was tested at elevated temperature. In this way, the high temperature effects were distinguished from the effects caused by the modifications.

The following is a chronological discussion of the major areas of tube testing which was carried out during this program.

4J50 No. 30087.

This was the first modified 4J50. At this point no new tube type designation had been adopted. The modification was a change from the normal mush cathode to a dispenser or "Phillips" cathode. It was life tested at room temperature for 222.6 rf hours. During this time, the cathode temperature was monitored through the output window. It averaged 900°C. Testing was terminated when a high voltage arc occurred across the glass cathode insulator bushing. A crack resulted and the tube went soft. Up to that point, no significant deterioration was noted in tube operation.

There was no noticable erosion at the cathode surface. Since evaporation and migration of barium is low at this operating temperature, long cathode life was predicted on the basis of this test.

4J50 Nos. 30264 and 30266.

These tubes were modified to test a new window design. The former had a .025 inch ceramic window; the later had a .037 inch window. Neither tube reached test due to the problems with the window design.

4J50 X No. 8.

This was another dispenser cathode tube. It also incorporated a new ceramic cathode insulator bushing. It operated for a total of 555.4 rf hours. Failure was due to an open heater lead.

Here again, the problem could well be attributed to assembly of a non-standard tube. It did not reflect a basic difficulty. This tube operated well throughout its life. This test confirmed the new ceramic bushing design and added to the data in favor of the Phillips cathode.

4J50 Nos. 30240 and 30246.

It was felt that the hardest test for a cathode would be to cycle the tube allowing sufficient off time in each cycle to cool the cathode to room temperature. In this way, the maximum thermal stress would be placed on the cathode (and to a lesser extent the rest of the tube). On this basis, these two tubes were built, incorporating the Phillips cathode.

They were run on a rigorous schedule. The latter tube operated 211 hours before it demonstrated an emission problem. This was found to be due to gas in the tube. The former

operated 480 hours when the test was arbitrarily terminated. No. 30240 yielded excellent operation and indicated no cathode problems.

<u>4J50 Nos. 30252 and 30261.</u>

These tubes were the first tubes to be tested at elevated temperature. Both were standard 4J50's. Their performance had deteriorated markedly by the time ambient temperature reached 200°C. Both tubes failed by the time 350°C was reached. The tests were terminated because of gas within the tube. Spectrographic analysis showed a predominance of hydrogen, indicating permeation, probably through nickle bearing (Kovar) parts.

<u>L-3458 Nos. 15 and 16.</u>

These were the first tubes carrying the new designation for the high temperature magnetrons. They were essentially 4J50's with ceramic replacing the glass parts, and the Kovar parts being copper-clad. Nos. 15 and 16 utilized a new output window design. This again was a thin window. They operated 11 1/2 and 8 hours respectively when, due to arcing, leaks occurred in the windows. In spite of the window problem, it was evident from these tests that the copper cladding of the Kovar parts significantly reduced permeation since no indications of gas within the tube appeared until the leaks occurred in the windows.

L-3089, Nos. 2004 Through 2007; And L-3083, Nos 2943 And 2944.

These tubes were chosen for use in cathode evaluation because they were inexpensive and easily modified for this purpose. These tests involved the evaluation of molybdenum as a base material for mush cathodes as compared to a nickel base. Barium molybdate was formed at the matrix-base interface on the molybdenum base cathodes. This caused cracking or "mud-flatting" of the matrix. For this reason, molybdenum was abandoned as a base for cathodes.

L-3458, No. 18.

This tube was destroyed during the first minutes of high temperature exhaust due to an oven failure.

L-3089, No. 2009.

This was the first tube to be exhausted on the double envelope exhaust station. The room temperature life test was arbitrarily terminated at 1200 rf hours. This method of exhaust proved to be satisfactory.

L-3458, No. 56.

This tube incorporated all of the modifications developed under this program. The latest rf window configuration was utilized; it being a half-wavelength thickness. The high temperature test included three temperature cycles from room temperature

to 340°C during the 42 hours life of the tube. Failure occurred due to faulty Kovar window frame material (determined by microscopic examination), and had little bearing on the high temperature performance.

L-3458, Nos. 37 Through 42.

These tubes were operationally checked and delivered to the Navy.

The most significant result of this program is that tube life at elevated temperature was extended from a matter of minutes to more than forty hours. It is felt that if a statistically significant number of tubes were tested, the extension in life would be in the hundreds of hours.

VIII. DISCUSSION OF TUBE REPAIR.

A. General Discussion.

The primary factor in considering a tube repair program is the cost per kilowatt hour. If this cost can be reduced by repairing used tubes instead of using new tubes, then this course of action is desirable. As a part of this contract, effort was concentrated on the study of the technical problems of tube repair. A natural result of this study was a better insight into the economic situation.

In this section of the report, we will review our findings in both the technical and economic areas. We will refer primarily to magnetron repair since this contract concerned magnetrons and also, their history is more extensive. However, we will generalize our conclusions as much as possible in order to broaden the scope of coverage.

The first step in any good repair program is visual inspection and retest. This is of such importance that it can, in some cases, be economic within itself, even if no repair is attempted on rejected tubes. This is so because, historically, about 50% of the returned tubes are found to be good and can be returned to the field with little or no rework. The next step is to determine the extent of damage. In any given program, a set of predetermined standards will be specified which will dictate whether or not a specific failure will be repaired.

A given program will also further specify whether rejected tubes will be dismantled for salvage, or scrapped. Again, historically, the number of repairable tubes will be about 25% of the total number returned from the field. This would result in 75% of these tubes being returned to service. However, these figures can be greatly modified by a given set of circumstances.

The second area of consideration concerns the end use of the tube in question. As a manufacturer, the main concern is designing a tube for repairability. However, the design should be closely coordinated with the repair program. As a designer of electron tubes attempting to design repairable tubes, the manufacturer finds himself in one of three situations:

(a) The simple situation where he is designing a tube for a system which was, in itself, tailored to the use of repairable tube. This would be an ideal situation in that all the various factors attendant to repairability could be optimized along with the program for tube repair.

(b) A second situation would be one in which the manufacturer is designing a tube for a new system which was not tailored to a repairable tube. This is the usual situation. In this case, changes in the system might be proposed which would facilitate the use of certain optimum parameters in a repairable tube. If

the proposal is attractive enough in terms of ultimate cost per kilowatt hour, then there would be no reason why such changes could not be seriously considered. However, judging from past experience, the manufacturer would still have to be competitive on initial cost per tube. This fact would limit the projected reduction in cost per unit of power.

Another restriction would be placed on a manufacturer of repairable tubes. Assuming that the manufacturer was successful in obtaining both a basic tube contract and the repair contract, and the number of systems and tubes was sufficient to warrant multiple source procurement, the tube manufacturer would probably be required to repair other, so-called unrepairable, tubes as well as his own. The problem here is that his repair program could not be standardized to the same degree as in a program involving only repairable tubes.

(c) A third situation would be similar to the second with the added restriction that, instead of a new system, the designer would be building a replacement tube for equipment already in the field. In this case, the designer would be severely restricted by the electrical and physical parameters of the existing system. The problems of competitive initial unit cost and multiple source tube repair would also apply here.

All of these problems would make it appear as though any tube repair program was doomed to failure. However, a tube repair program can indeed be a profitable venture if the foregoing considerations are properly considered in light of the competitive market and the customer's advantages. The major advantage accruing to the customer from a repair program would be a reduction in tube cost over the lifetime of the system, i.e., the previously mentioned reduction in cost per unit of power. An important side effect of a repair program is that tube failure analysis often pinpoints weaknesses in the over-all system. Also, the tube manufacturer has a more intimate knowledge of the causes of tube failure. In this way he can readily improve the design and construction techniques thereby extending tube reliability.

Other important advantages that accrue from tube repair programs are the conservation of materials such as copper bodies and permanent magnets, and the reduction of machine shop time since many machined parts do not wear out.

A seeming disadvantage of repair programs to the tube manufacturers is the lowered requirement of total number of tubes to be manufactured for a given equipment life. However, this can be more than offset by the greater profit margin that generally accompanies tube repair activity, because of the greater risk involved. Thus, the profitability of a repair program can be greater than that of manufacture

of more tubes, even though the level of manufacturing activity is lower.

The factors involved in tube repair, including criteria for determining when such repair programs are economically favorable, are discussed in Subsection "B", the prevalent causes of tube failure are discussed in Subsection "C", the design of magnetrons (and other types of microwave tubes) for repairability is discussed in Subsection "D".

B. Determining Factors in Tube Repair Feasibility.

In the case of a tube repair program, the primary considerations are tied closely with the various technical facits. These will be discussed along with the individual technical problems. The secondary considerations include such things as logistics, supply and demand, unit cost ratio, etc.

The implication of this might be that the primary considerations are the determinants in establishing feasibility of a repair program. This is not necessarily the case. There are certain fixed costs that will be incurred in any repair program. In the extreme, if these costs add up to more than the cost of building a new tube, then a repair program can not be feasible. Or, even more extreme, a tube may be either irrepairable or irretrievable by the nature of its design or application.

1. Economic Factors.

Probably the most important second order consideration is logistics. A detailed discussion of this subject is beyond the scope of this report. However, it must be recognized as a salient factor in the economics of tube repair. Logistics in this case would involve dispersion of tubes, i.e. systems, in the field, locations of field usage with respect to repair stations, feasibility of setting up special repair stations, all related to numbers of tubes involved and unit cost of tubes. Another very important consideration would be the feasibility of setting up visual inspection and retest operations at the field location. This might reduce the number of tubes in transshipment by a factor of two.

Another consideration involves multiple source tube supply. In this case, the initial repair program would have to include the, so-called, non-repairable tubes. The relative repairability of all tubes would then have to be studied and some designs may be scrapped in order to "streamline" the repair program.

Unit cost ratio refers to the cost of a new tube with respect to the cost of a repaired tube delivered to the field. This figure would be weighted by the projected extension in tube life due to repair and an increased

profit margin to cover the increased risk in the repair program. The resulting figure would reflect a change in cost per unit of power. The fiscal feasibility of the repair program would rest on this number.

2. Technical Factors.

Most primary considerations concern individual internal part design. These will be treated under the heading of "Design of Tubes for Optimum Repairability." The following are more general "process" considerations.

One of the most important technical factors which determines the feasibility of the tube repair is the ease with which the damaged part can be replaced. Re-weldable flanges play a very important role in this regard, since they reduce the required number of brazes, and the concommitant danger of opening vacuum leaks.

Another important consideration is the ease of cleaning the inside of the tube, either chemically or by vacuum firing. Usually, it is most desirable to be able to mechanically clean the inner parts of the tube. If this is not possible, chemical cleaning may be used, but it is important that there be no pockets within the tube which are relatively inaccessible, and which prevent a thorough rinsing.

Also, a prime factor in the technical feasibility of tube repair is whether or not major parts of the tube are destroyed by tube operation. For example, if the tuner and cavity of a magnetron are destroyed by wear during tube life, and if the tuner and cavity represent the major portion of the value of the tube, then the tube is not a candidate for repair. However, if the tube is designed so that the worn parts can be easily replaced (or dismantled) then the feasibility may be high.

3. Repair Versus Cost Range.

The following generalized statements can be made on the basis of Litton's considerable experience and research in tube repair.

Beginning at the low end of the scale, a $50 tube has generally been found to be non-repairable. However, field retest might become feasible for a large volume of such tubes. The next step up the scale, historically, is a $300 magnetron. This has been found suitable for retest, minimum external repair, and recharge of magnets. A $1000 tube has permitted cathode and window replacement. Most component parts have been considered replacable in a properly designed $5000 tube. At the top of the scale, in a $15,000 tube, all parts are considered economically replacable or repairable.

C. Causes of Tube Failure.

Before entering into a discussion of how to design for repair, it will be helpful to briefly review the typical causes of tube failure. These failure causes may be divided into two categories: Equipment failures that cause apparent or real failures of the tube, and actual tube failures:

1. Equipment Failures That Cause Tube Failures.

A very common occurrence is a system failure of some sort where the cause is unknown. Normally, the magnetron is suspected and is replaced as a first step in the equipment repair. Many times the tube is not at fault but, rather than removing the new tube and replacing the original tube, the original tube is rejected. A simple matter of testing the tube to determine whether or not it meets the start-of-life specifications will quickly restore this tube to operation.

A second common equipment failure that results in tube rejection is loss of cooling. The most common cooling failure is caused by the deposit of material in the cooling jacket of the tube when external coolant is introduced into the system.

A third common equipment failure that results in tube rejection is faulty contact fingers at the rf output connection or the dc input as evidenced by burning of these areas on the tube.

There are probably many other types of equipment failure which result in tube failures, but they are less distinguishable by evidence on, or in, the tube.

A somewhat different, but very common cause, of tube failure is accidental breakage of insulators or other tube components during handling or installation. In early phases of equipment use, this is the most common type of failure. This type of failure can be reduced by proper training of operating personnel.

2. Common Causes of Tube Failure Due To Failure Within The Tube.

A very typical entry on a tube failure report form is "arcing". This is often the result of poor vacuum in the tube, and can result from any one of these causes:

(1) evolution of absorbed gases within the tube by local heating or thermal working of the tube parts.

(2) permeation of gas (mostly hydrogen) into the tube envelope through heated thin metal membranes.

(3) development of vacuum leaks.

The latter cause of tube failure must be eliminated by tube development or pilot production programs. The first two items were major subjects of the study under this contract.

Another typical cause of failure in magnetrons is moding. The basic electrical design of magnetrons determines whether or not they will mode easily. If other electromagnetic modes on the anode lie close in operating voltage to the desired π-mode, then a small mechanical change in the tube will cause a good tube to mode. However, a well-designed tube should not mode even with a relatively large deterioration. One of the chief causes of magnetron moding is deterioration of the cathode. Cracks and irregularities which occur in the cathode surface can cause uneven rf and dc electric fields, which can further cause so much deterioration of π-mode operation that the oscillation is thrown into another mode. Also, poisoning of the primary and secondary emission can cause moding. Moding is also caused by tuner misalignment and destructive arcs.

Another cause for failure is end-hat emission. This difficulty is generally caused by emitting material migrating onto an end-hat which is made of material which does not sufficiently suppress electron emission.

For tunable magnetrons, typical causes of failure are wear of the internal sliding parts of the tuner, bellows fatigue, and misalignment of the tuner. The latter may be caused by damage within the tube, or by damage to the external tuning assembly.

Window failures are mostly caused by mechanical breakage, loss of cooling, or waveguide arcs.

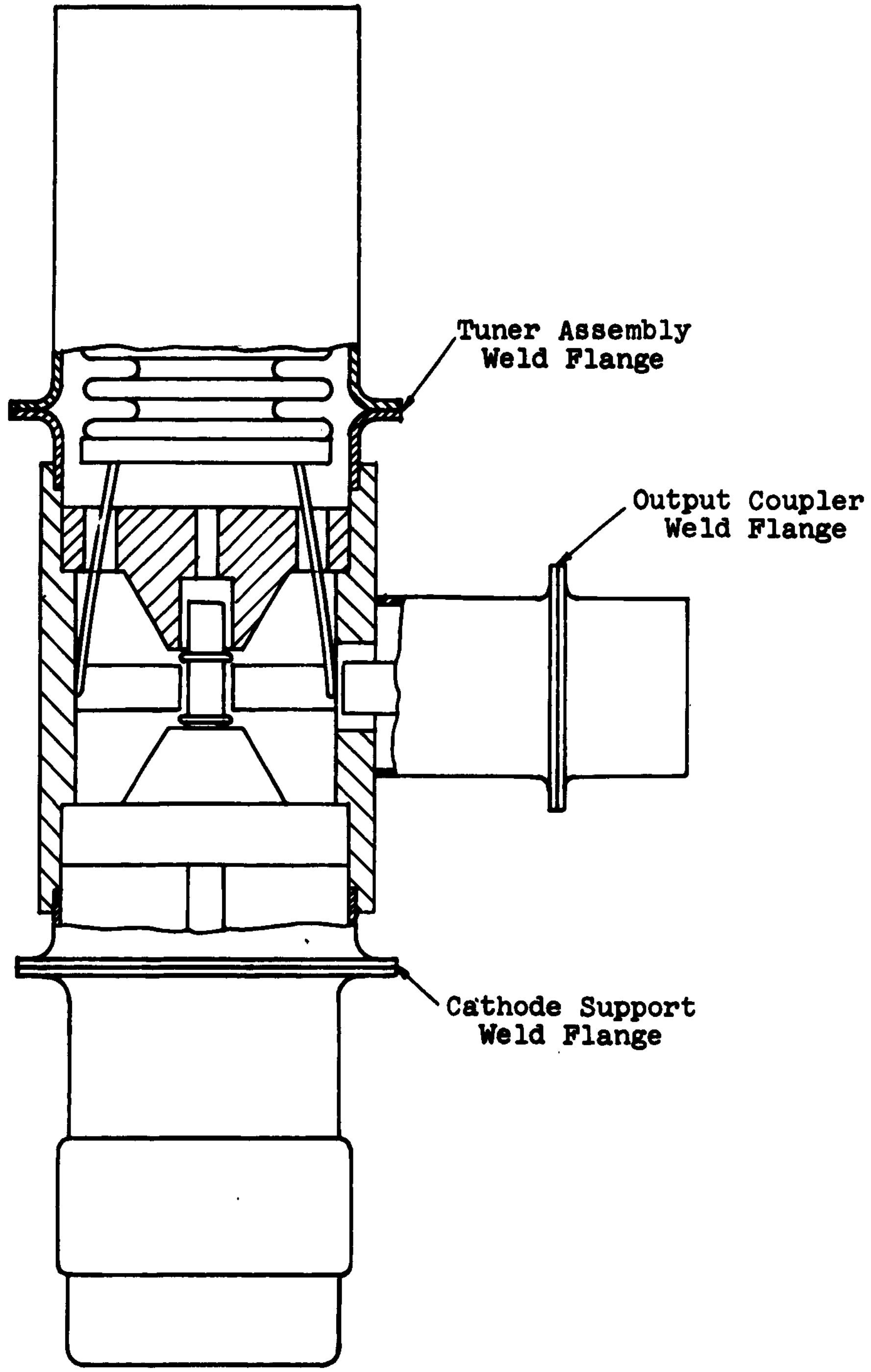
Typical magnet degaussing failures are caused by proximity or contact of magnetic materials with the magnet on the tube.

D. Design of Tubes For Optimum Repairability.

One of the most important aspects of the design which makes for ease of repairability is the ease of replacement of any component part of the tube. Fig. 24 shows a typical magnetron in diagrammic form with its component parts after disassembly of the permanent magnet, cutput coupler, and external tuning assembly. These three assemblies are usually external to the vacuum assembly on most magnetron designs and therefore do not normally need any special design consideration for repairability.

The important magnetron items which must be replaceable are as follows in order of their importance from the standpoint of failure history:

		Typical Value Of Tube
(1)	Cathode	25%
(2)	Window	2%
(3)	Internal Tuning Assembly	25%
(4)	Body-Anode Subassembly	13%
(5)	External Parts	10%
(6)	Exhaust And Test	25%



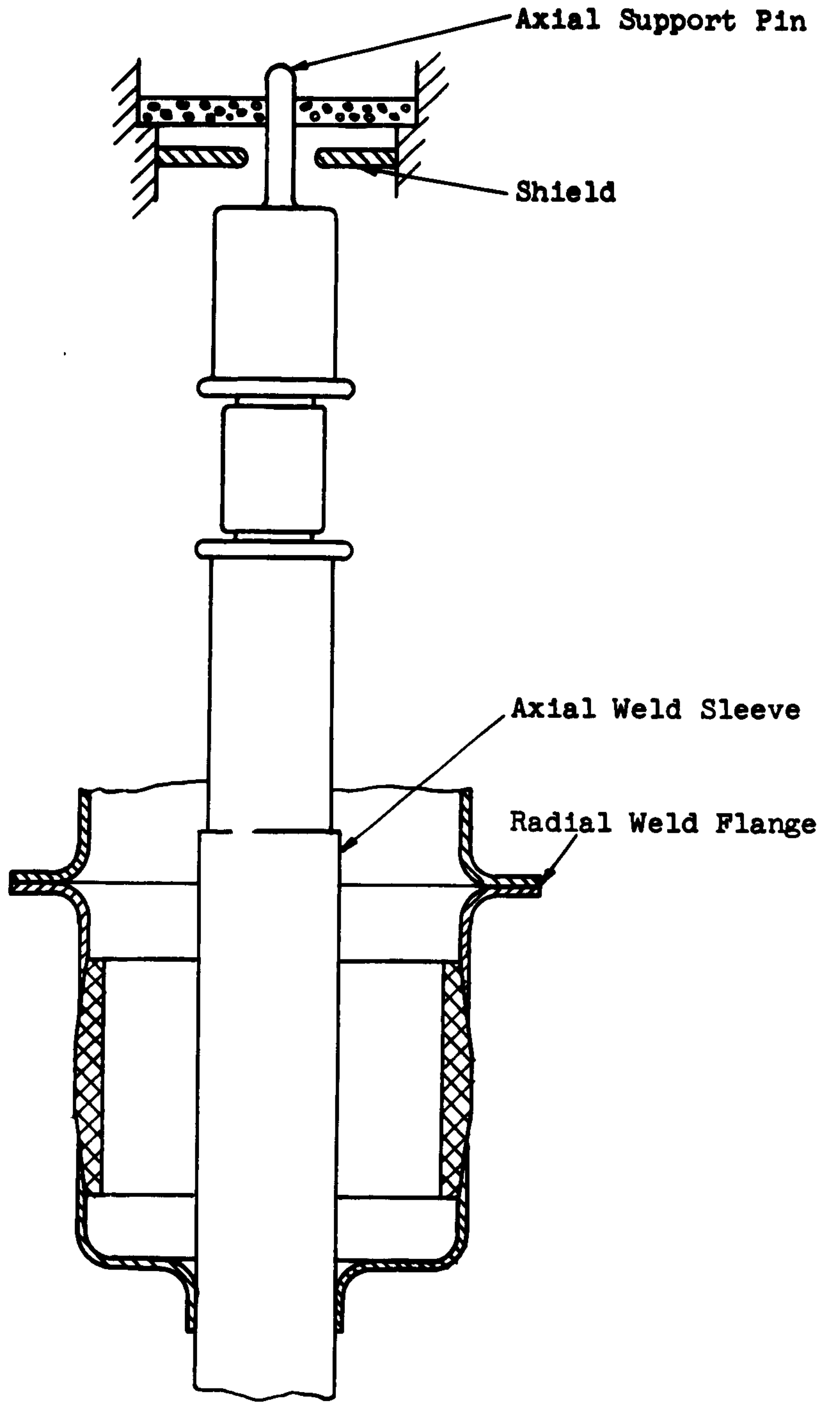
INDUCTIVE PIN TUNED MAGNETRON

Fig. 24

1. Cathode.

Most modern magnetrons use an axially supported cathode. For purposes of vibration and shock resistance, as well as for purposes of making cathode replacement easier, it is adviseable to design the tube in such a way as to provide an axially sliding or axially flexible support on the end of the cathode opposite the main cathode support.

Fig. 25 shows such an axially flexible support. It is important to add the evaporation shield to prevent dc current leakage across the lower surface of the insulator. If the main support insulator which holds the cathode support is made of glass, it is a simple matter to align the cathode and the tube in a glass lathe and seal it in. However, for tubes which are of most interest for this study, the insulator should be made of a ceramic bushing which is brazed to the metal sleeves on each end. It is desirable in the final seal-in of the cathode that the axial and radial position of the cathode support be adjustable so that precise axial and radial alignment of the cathode be achieved. One way of accomplishing this flexability is shown in Fig. 25, where the radial freedom of motion is achieved by the radial flange A, and the axial freedom of motion is achieved by the cylindrical joint, B.



SKETCH OF CATHODE SUPPORT

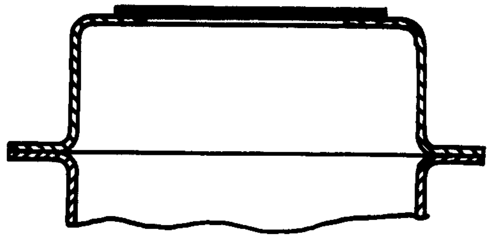
Fig. 25

After the alignment of the tube body and cathode has been accomplished in an adjustable lathe, the radial joint A is first rf brazed with moderate axial pressure being applied on the flange at B forcing the flanges together at A. Then the cylindrical joint at B is rf brazed. To take the cathode out, the cathode side of the radial flange at A is "faced" off. If it is desired to salvage the cathode, the outer cylindrical sleeve at B is machined to the inner sleeve diameter.

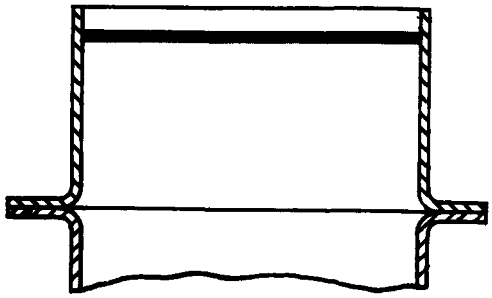
2. Window Repair.

Windows are generally of the waveguide type at higher frequencies, coaxial line type at lower frequencies.

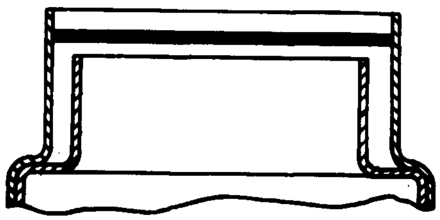
Waveguide windows at the higher frequencies are generally made in circular form. Fig. 26 shows various forms of repairable circular waveguide windows. Fig. 26a shows an iris type circular window in circular waveguide which is joined to the tube envelope by a radial welding flange. Fig. 26b shows a more symmetrical version of the same type of circular waveguide window. Fig. 26c shows a choke-type window assembly in rectangular guide. In this case, the welding flange is folded into cylindrical shape at the joint to achieve the more precise centering of the window required by the choke.



(a)



(b)



(c)

WELDABLE WINDOW FLANGES

Fig. 26

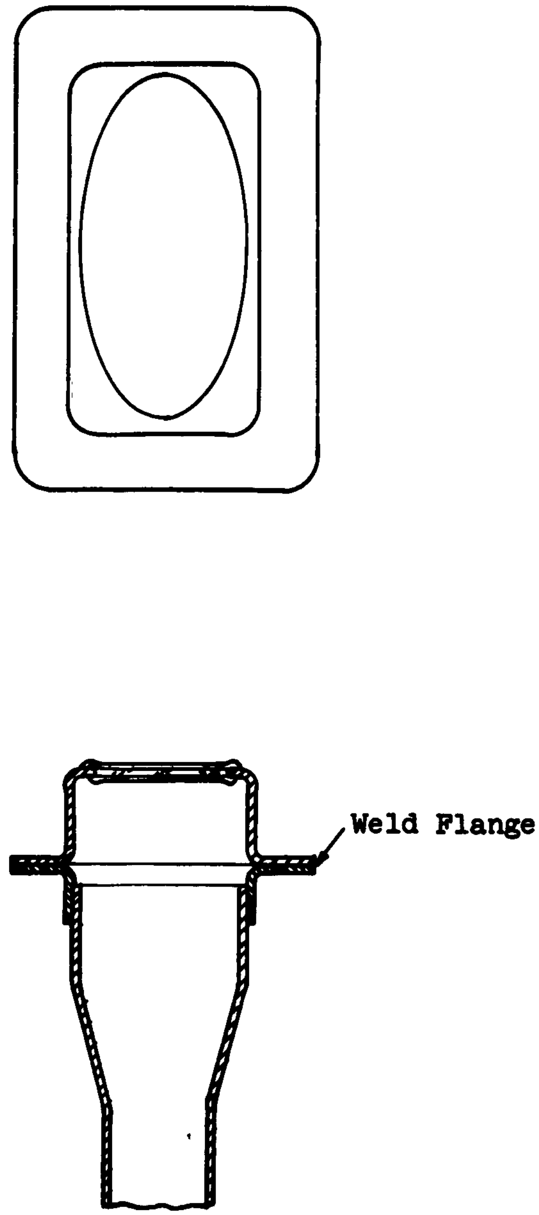
For lower frequency waveguide windows the circular designs of Fig. 26 a and b may be used, or one can make rectangular windows with radial weld flanges as shown in Fig. 27.

Fig. 28 shows a coaxial type window with an outer conductor joint similar to those shown for the circular waveguide windows. The inner conductor must have axial freedom of motion to avoid stress being developed across the window. One way of solving this problem is to use a sliding capacitive joint, as shown in Fig. 29, to join the inner conductor to the inner portion of the tube.

In either method shown above, it is important that axial flexibility be achieved between the inner conductor connected to the inner part of the tube to prevent mechanical stress from being built up across the window. Both types of inner conductor connections can be used with either the disc or conical type coaxial windows. All of the windows described can be easily replaced by cutting open a flange and replacing the window sub-assembly.

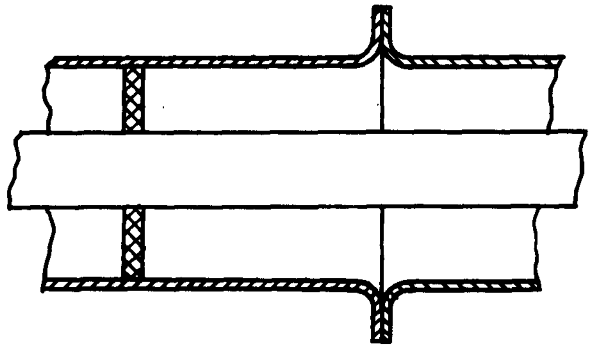
3. Tuner Replacement.

Magnetron tuners can generally be classified into four categories as follows:

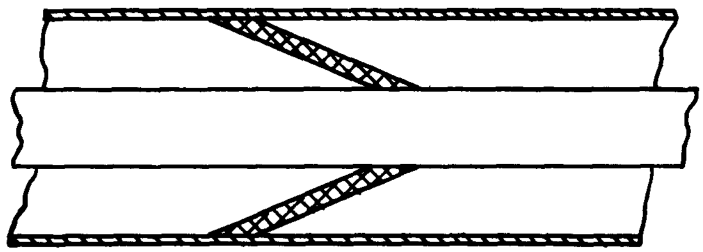


RECTANGULAR WELDABLE FLANGE
OUTPUT WINDOW

Fig. 27



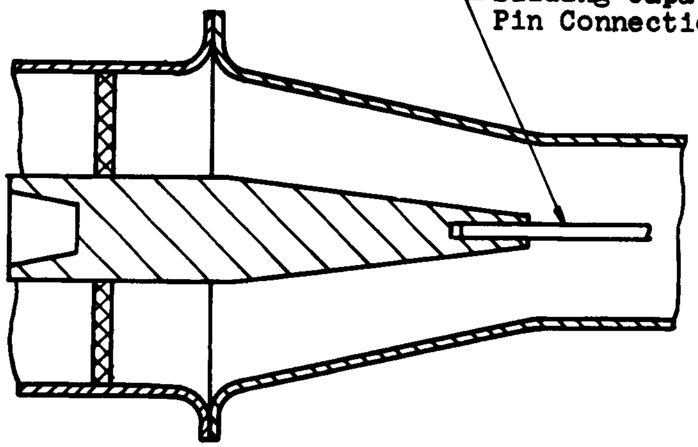
(a) Disc



(b) Conical

TWO POSSIBLE WELDABLE FLANGE COAXIAL WINDOWS

Fig. 28



CAPACITIVE PIN CONNECTION ON
WELDABLE COAXIAL WINDOW

Fig. 29

(a) Inductive pin tuners,

(b) L-C ring tuners,

(c) Non-contacting cavity tuners,

(d) Sliding short circuit tuners.

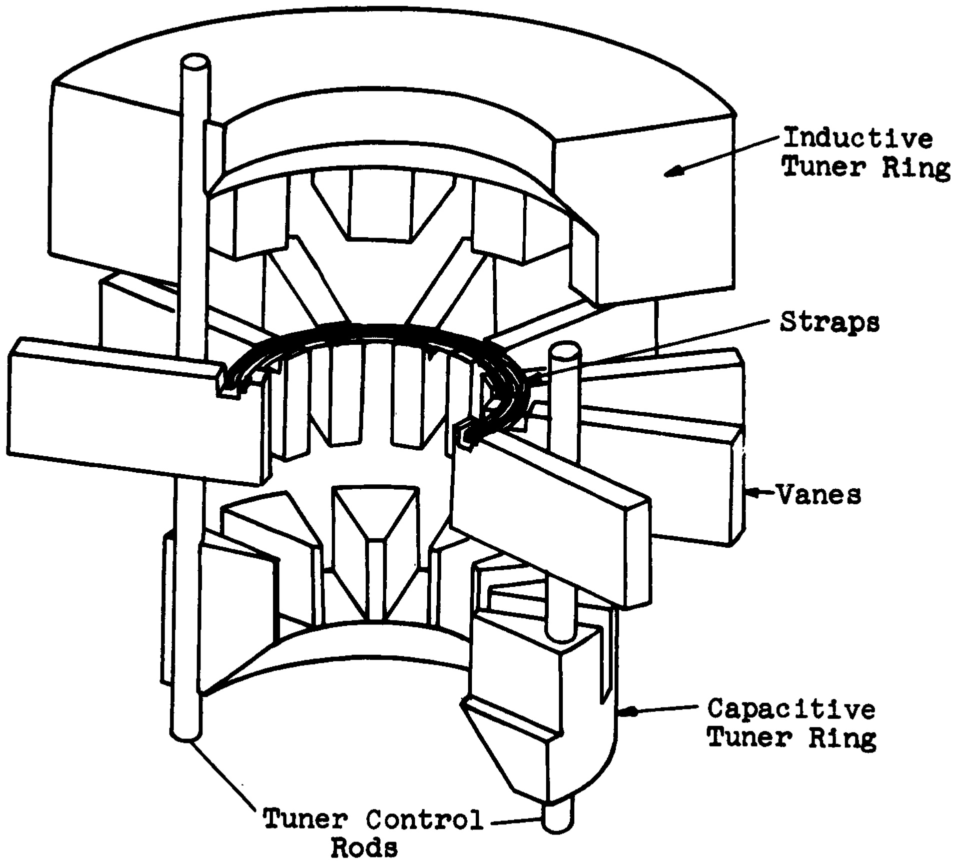
In the case of the inductive pin tuner, the movement may be either radial inward with an individual bellows for each pin, or it may be axial movement with a multiplicity of pins, mounted on a single crown, moving together. For the latter type of tuner, the pins may either move with clearance between cavity and pin, or the pins may slide on the backs of the cavities. In either of these cases, the tuning pins usually extend axially inward from the external tuner (through holes in the pole piece) into the backs of the cavities.

For this type of tuner it is possible to pre-braze the tuner pole piece to the body before the tuner is inserted. Whether or not this is done is very important so that the holes in the pole piece are well aligned with the cavities in the anode block. Fig. 24 shows a typical inductive pin tuner assembly. The inner tuning assembly, consisting of the tuning cup, bellows, push rod, crown, and tuning fingers may be made as a sub-assembly. The tuning sub-assembly sleeve may be assembled as an integral part of the tube body, or it may be attached to a

short sleeve at B after the tube body assembly has been completed. The internal aligning assembly may be inserted, the tuner jigged from the opposite end of the tube, and the joint at A may then be rf brazed or welded. Because of the previous alignment required of the tuner cup, it may be desirable to rf braze the joint at A, in which case it is necessary to machine off the metal at A to remove and replace the tuner.

The inductive pin tuner which is located on the outer diameter of the body and which moves radially inward in the cavity is a much simpler problem since there is only one pin per bellows and there is no pole piece to contend with. The replacement of this type of inductive pin tuner is basically similar to that discussed in the previous paragraph.

Another common type of tuner is the L-C or L-ring, C-ring type of tuner as shown diagrammatically in Fig. 30. In this tuner a set of inductive slugs is mounted on a ring or set of guide rods on one end (axial) of the vane structure, and a set of capacitive slugs which are integral with the inductive slugs is mounted on the other end. As the combination L and C tuner is moved axially through the cavity, both L and C type tuning combine to give wide tuning ranges. Either L or C tuners may be located on



L-C RING TUNED MAGNETRON
Fig. 30

the tuner end of the anode. This tuner may be replaced exactly like the inductive pin tuner with two exceptions. The tuner pole pieces must be removable, since the holes in the pole pieces for tuner motion can not normally be made large enough to accommodate the inductive and capacitive slugs. It is also usually necessary to be able to remove the cathode pole piece, in order to facilitate assembly of the internal tuner parts. Also, while it is possible to construct the L and C tuner assembly so that it is removable from the vane structure, some tuner designs do not permit this. In this case, one of the tuner rings must be assembled from the cathode end of the tube.

IX. CONCLUSIONS.

The factors important in improving the life and reliability of high-power, pulsed magnetrons - particularly under conditions of high ambient temperature and altitude - have been thoroughly examined and studied. It has been found that the most serious problems of high temperature operation arise as a result of gas permeation through the walls of the tube envelope and the evolution of gas from deep within the metal parts of the anode and cathode.

The solution to these problems must be approached from two directions: The source of gas entering the tube envelope must be cut off as completely as possible, and the residual gas that does get through must be gettered or ion pumped as quickly as possible. The first of these approaches is the more important since the larger the inflow of gas, the shorter will be the tube life as a result of eventual saturation of the getter or ion pump. Therefore our study has put the primary emphasis on stopping gas permeation through the envelope and on removing all adsorbed gases from the inner parts of the tube.

The main gas permeation problem has been found to be due to hydrogen diffusion through the nickel-bearing alloys which are employed near output windows, etc. Copper cladding and nickel plating of these surfaces was found to have a profound effect in reducing the hydrogen permeation. In addition, all glass seals were replaced by ceramic, and a high temperature bakeout technique (850°C)

with vacuum on the outside of the tube was developed. This latter technique not only removes gases from deep within the metal parts on the inside of the tube envelope(reducing gas evolution), but also causes the inner tube parts to act as a very good getter. The importance of these procedures has been proven by experiments; whereas ordinary tubes would not operate for more than a few minutes at an ambient temperature of 350°C, tubes built with these procedures (e.g.; the L-3458) operated for more than 40 hours without the use of an ion pump.

The elimination of all silver-bearing brazing alloys, and the exclusive use of gold-copper alloys for brazing, is very necessary for high temperature operation because of the high vapor pressure of silver at elevated temperatures. The exclusive use of gold-copper brazing alloys has been standard procedure for all Litton tubes for years.

The use of titanium inside the tube envelope for gettering purposes has been studied experimentally, and while it getters the gas, the sputtering of the metal and its compounds onto other parts of the tube may be the cause of undesirable effects which we have noticed, particularly the occurrence of secondary emission and multipactor. Therefore, we do not recommend the use of titanium or similar gettering metals inside the tube envelope. However, the use of titanium in an ion pump which is connected to the tube envelope as an appendage would be satisfactory, particularly if there were no straight paths from the ion pump to the tube through which

titanium atoms or ions could travel. Such a pump could be designed to operate with the L-3458. It could use the fringing field from the tube's permanent magnet, as well as the tube's pulse voltage, for operation and thus avoid the necessity of a separate voltage supply and magnet for the ion pump. Such an ion pump, however, could not be designed for the L-3458 within the scope of this program.

Another improvement in pulse magnetron reliability has been made through the improvement of the nickel mush cathode. Instead of molding the mush material in continuous layers onto the outer diameter of a molybdenum cathode head, we have found that molding it into narrow grooves in the molybdenum head greatly reduces problems of cracking and "mud-flatting" after many cycles of heating and cooling. Furthermore, we found it important to use a centering insulator and alignment pin on the unsupported end of the cathode to avoid misalignment under high temperature conditions. This type of cathode was used in the magnetron samples delivered under this program.

As a result of our studies, we feel that the barrium aluminate impregnated tungsten matrix cathode would be the best type for high-power, pulsed magnetrons. One design was tried unsuccessfully for the L-3458, and therefore was not incorporated in the tubes which were shipped under this program. However, a new design of this type of cathode has been made and should be satisfactory for new versions of the tube.

The treatment of the external surfaces of the tube, cooling fins, and magnet to prevent oxidation and corrosion by high ambient temperatures has been accomplished by nickel plating and alumina plasma spray techniques.

It is concluded that high ambient temperature operation of magnetrons is feasable. To prove this, a pulse magnetron, the 4J50, was redesigned in accordance with the previous discussion, and was given a type number, the L-3458. Even without an attached ion pump, a sample tube of this type operated in an elevated ambient temperature (350°C) for more than 40 hours, and failure of this tube was due to a window leak which had nothing to do with high temperature operation or to the high power output. Some gasiness of the tube was observed, indicating that some improvement is desired. Six L-3458 magnetrons were built and delivered under this contract to demonstrate the progress achieved under the program. A discussion of recommendations for further study follows this section.

Extensive studies have been made of the economic and technical feasibility of tube return and repair programs. The chief factor is the cost of repair relative to initial cost of the tube and the transportation costs. The cost of repair is greatly reduced if the initial design of the tube has been made with emphasis on repairability. If the tube has been designed for maximum repairability, it will generally be feasible to repair the tubes if there is sufficient volume of use, unless the cost of transportation is excessive.

Such repair programs not only save money for the user, but also provide valuable information for future corrective design to the repairer if he is also a producer of the tube.

X. RECOMMENDATIONS FOR FURTHER STUDY.

A great deal of progress has been made under this program toward improving the reliability of pulse magnetrons and increasing their capability to operate under high ambient temperature conditions. However, more work on this problem would be highly desirable at this point to capitalize on the progress made to date. The use of copper clad tungsten vanes, improved barium aluminate cathode design, additional reduction in gas permeation, and the use of an ion pump appendage are items which at this point could be accomplished on the L-3458 with a minimum of effort, and which would greatly increase tube life under high ambient temperature operation. The power capability of the L-3458 would also be greatly increased, especially at more normal operating ambient temperatures.

The extension of these techniques to tunable, pulsed magnetrons and to tunable CW magnetrons would be very desirable. The basic problems to be solved in this regard would be the development of long life bellows which would stand up under high temperature without fatigue or gas permeation, and the development of tuner bearing surfaces which would have long life under the high temperature conditions.

The extension of the design-for-repair concept to CW magnetrons, and to high-power, traveling-wave tubes and klystrons would also be desirable.

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