SUPERSO\textsc{nic} Plasma Tunnel With a
Mercury Vapor Medium

by

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ABSTRACT

The possibility of a two-phase supersonic plasma tunnel is discussed. This system is to operate on a closed cycle and utilize the liquid-vapor phase of a suitable medium with ionization introduced into the supersonic stream by means of a high-frequency electrodeless discharge. Mercury is chosen as the most suitable medium because of the low power requirements for ionization and because it exhibits a vapor pressure variation suitable for supersonic velocities near ambient temperatures. The resulting system is essentially a low density wind tunnel with elimination of the extensive pumping or refrigeration facilities usually associated with such a device.

The proposed system is designed with the stagnation and condensation chambers being made from standard commercial pipe and utilizing a pyrex nozzle. The problems of actual construction of the apparatus are discussed in detail.

Results of initial operating tests are discussed and suggestions are made for future improvements of the plasma tunnel.
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SUPersonic Plasma Tunnel with a Mercury Vapor Medium

CHAPTER I

INTRODUCTION

Interest in recent years has developed in various aspects of ionized, high speed flow. Applications which require an understanding of this phenomena include satellite and missile re-entry problems, plasma confinement in fusion studies, boundary layer control in conducting fluids, and related magneto-aerodynamic studies.

This thesis consists of the design and construction of a piece of laboratory equipment which should be suitable for visual and experimental studies in some of the above mentioned fields. The apparatus is basically a supersonic wind tunnel, operating on a closed cycle, using mercury vapor as the gaseous medium. The plasma or ionized gas flow is achieved by means of a high-frequency electromagnetic discharge which is of the electrodeless type.

The present report is basically divided into four sections. Chapter II consists of a general outline of the study at hand and the theory involved. The theoretical considerations concern chiefly the physical principles
involved rather than the mathematical aspects. The next section deals with the design parameters of the plasma tunnel, such as materials, mechanical layout, and operating characteristics. Chapter IV is a discussion of the problems, methods, and special techniques encountered in the actual assembly of the apparatus. Chapter V outlines the starting procedure required in operation of the plasma tunnel, along with preliminary operation results.

Following the four chapters outlined above are the concluding remarks, Bibliography, and Appendix. The Bibliography is quite extensive and purposely includes some material not directly referred to in the text material. The reason for this is that the designed apparatus is to become a permanent laboratory fixture and the Bibliography will eliminate some of the hours of library reference investigation needed by those persons who choose to undertake future research utilizing the supersonic plasma tunnel. The entries in the Bibliography are numbered and will be referred to in the text material by that number as is common in engineering practice, thus eliminating footnotes.

All figures referred to in the text material are located in the Appendix in the closing pages of the thesis. Table I below gives the nomenclature and symbol notation which is followed throughout the report.
Table I

Nomenclature

| \( \mu \) = micron \((10^{-6} \text{ meters})\) | \( k \) = thermal conductivity |
| \( p \) = pressure | \( H \) = heat of vaporization |
| \( T \) = temperature | \( D \) = diameter |
| \( \varrho \) = density | \( Q \) = heat flow rate |
| \( w \) = mass flow rate | \( h \) = heat transfer coefficient |
| \( A \) = area | \( R_e \) = Reynolds number = \( D \nu \varrho / \mu_1 \) |
| \( L \) = length | \( P_r \) = Prandtl number = \( c_p \mu_1 / k \) |
| \( M \) = Mach number | \( V \) = velocity |
| \( c_v \) = specific heat at constant volume | I.D. = inner diameter |
| \( c_p \) = specific heat at constant pressure | O.D. = outer diameter |
| \( \gamma \) = ratio of specific heats | d.c. = direct current |
| \( R \) = gas constant | a.c. = alternating current |
| \( m \) = mass | h.f. = high-frequency |
| \( \mu_1 \) = viscosity | ( )_o = stagnation conditions |
| | ( )* = conditions for \( M = 1 \) |
A. Scope of Present Study. The study undertaken here is concerned with the design and construction of a supersonic plasma tunnel which operates on a closed cycle and which is suitable for advanced aerodynamic studies in ionized gas flow. The apparatus must fall within the economical limits of a university program and hence available equipment is to be utilized whenever possible.

In order to produce a flow of ionized gas it is necessary to utilize one of several possible methods. The means of ionization are classified according to various combinations of the following: (1) either electrodes or an induction coil can be used, (2) the coil or electrodes can be located internally or externally with respect to the gaseous medium, and (3) a choice exists between alternating and direct-current power sources.

The merits of the various methods will be discussed later and it will suffice here to say that electrodeless discharge, in which an external induction coil is used, has an unique advantage. Two distinct advantages gained here are: (1) no contamination of the fluid due to the anode and
cathode, and (2) the flow is not disturbed by projecting electrodes.

Past research in the ionization phenomena of gases shows that for a given set of conditions, the power required for a discharge is greatly affected by the gas pressure. The energy required being almost infinite at both zero pressure and infinite pressure, and a minimum at some pressure in between. Thus in order to take advantage of low voltage requirements, the operating facility will essentially be that of a low density wind tunnel. For most common gases this pressure is in the range of $10^{-2}$ to 100 mm Hg.

The few low density tunnels in existence require huge vacuum pumps or other systems of ejectors to remove the gas volume as it passes through the nozzle. These systems are extremely expensive. Eckert (reference 14) outlines a method which eliminates the need for a vast gas ejector system, this utilizes a two-phase system whereby a permanent gas is not necessary. The two-phase system makes use of the liquid-vapor cycle of suitable materials.

Chaun in reference 7 describes a two-phase system in which a gas is heated to about $800^\circ$ K at 100 mm Hg pressure in the stagnation chamber. The gas is then expanded through a supersonic nozzle into a receiving chamber where it is liquified and then pumped back into the stagnation chamber. This system, which is being built at the University of
Southern California, uses cryopumping with a helium refrigerator and can operate with any gases except neon, hydrogen, and helium, which cannot be cryopumped.

The economy of a system such as that described above depends greatly on the temperature level at which the liquid-vapor cycle occurs. Eckert's system, mentioned previously, eliminates the need for expensive pumps and refrigeration in that a testing medium is chosen that operates near the ambient level. In other words the medium to be used must condense at temperatures above room temperature or slightly below (for the pressures at which the system is to operate). The economy of this is evident, particularly if compared to Chaun's low density tunnel in which gases such as nitrogen must be cooled to about 800 K before liquification occurs.

There is one problem inherent with a two-phase system operating at ambient temperatures. The vapor or gas will condense in the nozzle due to expansion and cooling unless (1) the stagnation chamber temperature is extremely high (in the range of several thousand degrees Kelvin), or (2) heat addition is made at some portion of the nozzle. Since the purpose of this study is to produce an ionized plasma flow, the above problem is solved by energy addition through high-frequency discharge in the gas. For reasons to be discussed latter, this is best achieved by using an induction coil which is wound around the nozzle. A high-
frequency potential applied at the ends of the coil introduces an emf between the ends of the solenoid winding and also induces a magnetic field parallel to the axis of flow. The action of these fields excites the electrons of the atoms and adds heat to the gas through the accompanying decay and recombination processes.

The scope of the present study is to utilize the basic principles discussed above to produce a continuous flow of ionized gas in a supersonic plasma tunnel. The general features of the system to be built are shown schematically in Figure 1 of the Appendix. Flow is initiated merely by the pressure difference (of the liquid-vapor medium) which exists between the vapor pressure of the liquid at whatever temperature it is boiled and that pressure existing upon condensation at a lower temperature.

Assuming a suitable medium is available which is a liquid at room temperatures and at a few microns of Hg pressure, the entire system is initially pumped down to about 10 μHg. The basic operation cycle then is as follows: (1) the liquid in the bottom of the stagnation chamber is boiled by means of a heater (see Figure 1) and then superheated as it flows upward past the heating units; (2) next the flow is compressed as it flows through the glass nozzle to the throat where it expands into the test section; (3) here the high-frequency excitation ionizes the gas, adding heat which
counteracts the gas cooling and possible condensation; (4) the plasma then flows into a receiving chamber where it condenses on the walls which are cooled by the flow of a liquid coolant in the jacket surrounding the condensing chamber; (5) the cycle is then completed as the condensed vapor flows by gravity back to the boiling chamber.

The proposed system as shown in Figure 1 is basically that used by Eckert and described in reference 14, and to whom the idea for this project must be credited. In his tunnel, mercury was used as the testing medium and the entire system was blown out of pyrex and vycor glass. The plasma tunnel to be built here will differ in the respect that facilities for glass-blowing are not readily available.

B. Historical Sketch. This section is concerned with a brief discussion of the development of high-frequency electrodeless discharge and the events which led to its use as a method of energy addition in high speed gas flow. The purpose here is mainly to acquaint the reader (and those who wish to continue research in the field) with the literature available on the subject. A number of selected references have been included in the Bibliography, these in turn contain a multitude of additional references to which the interested reader can go.

Traditionally any electric current flowing in a gas is called a discharge. It is necessary to clearly differen-
tiate between electrode and electrodeless discharges using both direct and alternating currents. An electrode discharge is the excitation produced when two electrodes or plates (anode and cathode) are in contact with the gaseous medium with either an a.c. or d.c. potential across them. Electrode discharges with direct currents have been the basis for the great majority of experimental work during the past century. This type of excitation gives rise to the familiar spark, glow, corona, and arc discharges.

The interest here is only in the high-frequency electrodeless discharge which is classified into two types: (1) plate electrodes with the gas between them and enclosed in a container made of dielectric material, when a h.f. potential difference is applied to the electrodes the excitation is similar to the glow and corona discharge in the d.c. case; (2) high-frequency currents through a solenoid in which the wire windings are wrapped around a dielectric container with the gas inside, this results in the so-called "ring" discharge in which the currents induced by the magnetic field flow in a ring perpendicular to the axis of the solenoid.

The first work with electrodeless ring discharges was performed in England by Sir J. J. Thomson about the turn of the century. Reference 32 describes his early work using stationary gases (air, hydrogen, and helium) in cylindrical
and spherical containers. This paper published in 1927 was one of the first in the field and outlines the basic theory of the magnetic induction producing ring discharge. All theoretical work to date consists of modification and extensions of Thomson's theory. Thompson at this time asserted that the excitation and ionization of the gas was due to the circular potential in the gas which was induced by the magnetic field arising from the current in the solenoid. In other words, the discharge was electromagnetic in nature.

Townsend in 1928 claimed that Thomson was wrong, stipulating that the discharge is principally due to electrostatic effects which arise from the potential difference at the two ends of the solenoid (reference 35). MacKinnon in the following year asserted that both Thomson and Townsend were correct, the disagreement being due to the type of experiments they performed. At low excitation the dominant field is electrostatic and produces a visible glow discharge in the gas. As the alternating potential is increased the magnetic field increases proportionally but the electrostatic field does not. Hence if the excitation reaches a certain value the electromagnetic effects become dominant and result in ring discharge of high luminosity.

Thomson worked with low pressures when the gas was easier to ionize and hence could easily obtain the bright ring discharge. On the other hand, Townsend's research was
at higher pressures and his available power was not sufficient
to excite the medium beyond the electrostatic glow stage.
In reference 25, MacKinnon describes his experiments with
mercury and iodine which verify the theory set forth above.
Later work, by Knipp (reference 22) and a separate group
headed by Smith (reference 20) experimentally verify
MacKinnon's results.

In 1932 Kunz extended Thomson's original theoretical
equations in a paper (reference 23) which included the elec­
trostatic field effects. References 33 and 36 give a summary
of the work done by both Thomson and Townsend in the years
following their first papers mentioned previously. By this
time both are in agreement with the idea that initial low
excitation discharge is due to the electrostatic field while
the intense ring discharge is due to electromagnetic induc­
tion.

In the years that followed, very little new theoret­
ical or experimental work was done. Most of the research in
gaseous discharge was concerned (and still is) with electrodes
rather than electrodeless excitation. As recent as 1948,
Margenau in a theoretical paper (reference 19) states:
"Unfortunately, theory enjoys in this field at present the
large amount of liberty that always goes with a scarcity of
experimental data. The subject is new and measurement is
difficult." The validity of Margenau's statement can be
emphasized by the fact that his theory (and everyone else's) is in poor agreement generally with experimental results. In fact, the experimental data often is not in good agreement between the various workers doing the same type of work.

For our purposes, the topic of h.f. electrodeless discharge came into life again about seven years ago. In 1954, Early and Smith reported experiments with d.c. arc discharge in an attempt to heat a supersonic air stream (reference 13). They had little success, but at this time emphasized the fact that electrodeless excitation looked promising and warranted investigation. Chaun in reference 7 gives a good discussion of the various aspects which must be considered in the application of h.f. discharge to high speed flow. In 1958, Eckert of the Convair Scientific Research Laboratory gave results of successful application of ring discharge to supersonic mercury vapor flow (see reference 14).

Two recent theoretical papers are available, reference 15 includes the effects of the variable conductivity of a stationary gas and reference 28 considers the effect of a moving gas volume in an infinite cylinder. Both papers are further extensions of Sir J. J. Thomson's original theory and are a step forward in an attempt to make a better representation of the actual physical case. The only problem is that the resulting equations are complicated and can
only be solved after reduction through simplifying assumptions.

Reference 3 describes recent success in the ionization of argon and mercury vapor in supersonic flow. Various universities throughout the country are initiating investigation into the field, but very little information is available at the present. The entire subject is extremely lacking in experimental results and only research can fill the missing gap needed between mathematical theory and a good physical understanding of the problems.

C. General Theory. The discussion in this section is limited to the physical and mathematical aspects of the problem at hand, with the exclusion of the discharge process itself which is treated in section D of the present chapter.

Referring to Figure 1 on page I of the Appendix and the discussion associated with it on page 7, it is seen that the success of the plasma tunnel depends upon the availability of a proper testing medium. Since it is desired to operate at low pressures where ionization is readily accomplished with a minimum of power, the medium chosen should be a liquid at room temperature and a few microns Hg of pressure. This will permit condensation of the high temperature plasma on the walls of the exhaust chamber when cooled by ordinary tap water, thereby eliminating intricate
refrigeration equipment. In summary, the testing medium should exhibit the following properties for optimum operation:

(a) It must have a gas-liquid cycle working in a temperature range near or above ambient conditions.

(b) A minimum of power should be necessary for a large degree of ionization, hence the medium must have a low ionization potential.

(c) The variation of vapor pressure with temperatures from the ambient level to a few hundred degrees higher should be such as to produce pressure differences suitable for supersonic flow.

(d) The medium should be chemically inert so as not to corrode the containing vessel.

(e) For quantitative studies, the vapor should preferably be pure and monatomic. This insures that the thermodynamic and discharge parameters are as simple as possible, thus making experimental results more readily available for physical interpretation.

(f) Another qualification is that the ions resulting from excitation and ionization should not be stable because the heating of the gas stream is the result of the recombination
processes following breakdown. In addition, stable ions would hinder liquification at the end of the cycle.

Atmospheric air is eliminated on the basis of the above criteria, even though it is the gas of most practical interest. The usual monatomic gases do not meet the desired vapor pressure variations desired, and most of them do not liquify except at extremely low temperatures.

Eckert in reference 16 discusses various materials which can be used in a plasma tunnel in the light of the above mentioned criteria. His investigation shows that the alkali metal, cesium, is the best medium due to its low melting and boiling points (28.7°C and 690°C at atmospheric pressure) and its low ionization potential of 3.9 volts. Unfortunately, cesium is extremely reactive and must be rejected for a university system. Eckert's final conclusion is that the metal, mercury, comes the closest to satisfying the requirements.

Mercury melts at a temperature of -38.9°C and hence is a liquid at room temperature. The boiling point is 357°C at atmospheric pressure and it has an ionization potential of 10.4 volts. Although mercury vapor is reasonably non-corrosive, it is toxic in nature and tends to form amalgams with many materials. The toxicity should not present a problem in a confined system under a vacuum. However it is
necessary to utilize materials in the construction of the plasma tunnel which do not readily combine with mercury.

On the basis of the above discussion, mercury was chosen as the testing medium for the plasma tunnel. From reference 2, the vapor pressure of mercury at room temperature is about $1.3 \times 10^{-3}$ mm Hg. Going to the schematic of Figure 1, assume that the system is initially pumped down to about $10^{-2}$ mm Hg at which time the mercury is in a liquid state. For purposes of illustration, assume that the liquid is boiled in the heating chamber at a temperature of $200^\circ$C. The vapor pressure at this temperature is 17.3 mm Hg and is approximately the stagnation pressure. Now as the vapor rises past the superheating elements, the temperature can be raised to a point which will allow expansion a short distance beyond the nozzle throat before the cooling is sufficient to produce condensation. At this point the high-frequency excitation can be applied to excite and partially ionize the gas with subsequent heating of the main stream through recombination and related processes.

The heated plasma will then flow into the receiving chamber and condense on the walls which are cooled by tap water. Although the water temperature is approximately that of room temperature, the temperature inside the chamber will be slightly higher at the walls. For the purpose of discus-
sion, assume that this temperature is approximately twice room temperature at 40°C giving a vapor pressure of $6.3 \times 10^{-3}$ mm Hg. Comparing this with the assumed stagnation pressure above, the available pressure ratio across the nozzle is of the order of 2700. This is more than ample for supersonic flow.

After liquification the mercury then flows back to the boiling chamber by gravity. A bend in the return line (see Figure 1) is kept full of mercury and prevents the original vapor from flowing in a reverse cycle.

To evaluate the performance of the nozzle, it will be necessary to know some of the aerodynamic properties of mercury vapor. Assuming a perfect gas and isentropic flow (this may be valid only prior to ionization) the familiar equations of pressure, temperature, and density ratios as referred to stagnation conditions can be used. These are tabulated below as a function of the Mach number (see Shapiro, reference 29).

1. $\frac{T_0}{T} = 1 + \frac{\gamma - 1}{2} M^2$  
2. $\frac{p_0}{p} = \left(1 + \frac{\gamma - 1}{2} M^2\right)^{\gamma/(\gamma-1)}$  
3. $\frac{\rho_0}{\rho} = \left(1 + \frac{\gamma - 1}{2} M^2\right)^{1/(\gamma-1)}$
The flow rate (\(w\)) per unit area is given by

\[
\frac{w}{A} = \sqrt{\frac{\gamma}{R}} \frac{p_0}{\sqrt{T_0}} \frac{M}{(1 + \frac{\gamma - 1}{2} M^2)^{\frac{\gamma + 1}{2(\gamma - 1)}}}
\]

(4)

For a monatomic gas such as mercury vapor, the ratio of the specific heats (\(\gamma\)) is equal to 5/3 (1.667). Using this value along with the geometry of a particular nozzle, the properties above can be calculated. The familiar working chart for isentropic flow has been constructed from tabulated values in reference 21 and is shown in Figure 2 in the Appendix for easy reference.

For choked flow with \(\gamma = 5/3\), the Mach number at the throat is unity and equations 1 through 4 become,

\[
\frac{T_0}{T^*} = 1.335
\]

(1a)

\[
\frac{p_0}{p^*} = 2.05
\]

(2a)

\[
\frac{\phi_0}{\phi^*} = 1.54
\]

(3a)

\[
\frac{w/A^*}{(w/A)_{\text{max.}} = 1.51 p_0 \sqrt{T_0}} \text{ gm cm}^{-2} \text{sec}^{-1}
\]

\[
= 2.145 \times 10^{-2} p_0 \sqrt{T_0} \text{ lb in}^{-2} \text{sec}^{-1}
\]

(4a)

where \(p_0\) is in mm Hg pressure and \(T_0\) in °K for equation 4a. The pressure ratio \(p_0/p^*\) above is only slightly higher than for air, and the previously estimated value of 2700 for the ratio of stagnation to exhaust pressure is far in excess to
that needed for supersonic flow. Of course if the total pressure ratio was as low as 2.05, the resulting flow would be greatly over-expanded with the possibility of a shock existing somewhere in the nozzle.

A few remarks are in order in regards to the position of the induction coil used for excitation and heating of the gas stream. First of all to be effective it must be separated from the vapor by a dielectric material, with no metal components in contact with the wire windings. Preferably this position is around the glass nozzle, either fore or aft of the throat. In supersonic flow there is an extreme temperature differential across the throat causing large thermal stresses at this point. To place the coil in a position to heat the flow entering the throat would increase the thermal stresses at the position where they are most critical. Hence it is preferable to place the windings in the region following the throat position. With this arrangement the stagnation temperature must be sufficiently high so as to pass the flow up to the position of the induction coil before condensation occurs.

The question now arises as to just what effect heat addition to a supersonic expanding flow has on the flow itself. Heat addition to either subsonic or supersonic flow effectively raises the stagnation temperature. Familiarity with subsonic flows would at first glance give the
impression that the Mach number and velocity would accordingly be increased in the supersonic case, but this is not so. From Chapter 8 of Shapiro (reference 29) the following relation is obtained. This equation relates the dependence of heat addition and area change on the Mach number.

\[
\frac{dM^2}{M^2} = \frac{2(1 + \frac{\gamma - 1}{2} M^2)}{M^2 - 1} \frac{dA}{A} - \frac{(1 + \gamma M^2)(1 + \frac{\gamma - 1}{2} M^2)}{M^2 - 1} \frac{dT_o}{T_o}
\]  

(5)

From equation 5 it is seen that for supersonic flow, increasing the area causes an increase in \( M \), while heat addition or increase of \( T_o \) causes a decrease in \( M \). In regards to the other properties, heat addition to supersonic expanding flow always causes an increase in the flow temperature and pressure while decreasing the Mach number, stagnation pressure and gas velocity. Hence on the basis of the above considerations it is seen that for the present case where heat is to be added to the plasma flow, the ratio of exit to throat areas must be made larger than for the case of no heat addition if comparable Mach numbers are to be obtained.

D. High-Frequency Induced Discharge. Consider an initially neutral gas, application of a sufficiently strong electric field of any type will produce a discharge in the gas which is the result of the rapid increase in the number of free electrons. These charge carriers are released from
molecules or atoms by the energetic bombardment of the free electrons that are accelerated by the electric field. Statistically there are always a few free electrons even in a neutral gas which can be accelerated. When the electrons attain sufficient kinetic energy they will produce additional electrons upon colliding with neutral atoms, which in turn will be accelerated until their kinetic energy is sufficient to cause ionization upon collision. Under the proper conditions this cascading mechanism creates large numbers of free electrons, making the gas highly conductive and resulting in "breakdown" or "discharge" in the gas.

The cascade action continues until all available electrons have been released, or until equilibrium is reached. This is when the number of electrons produced is equal to the number recaptured plus those lost by diffusion away from the action of the applied field. At high densities (small mean free path) the free electrons will make many inelastic collisions as they are accelerated by the field, losing part of their energy with each collision. Resulting in a low probability that the electron will achieve sufficient kinetic energy to produce ionization. Thus at high densities a higher field strength is required to produce ionization than at low densities. The magnitude of this applied field being such as to give the electron sufficient kinetic energy to
cause ionization within a distance of one mean free path.

For the other extreme, at very low densities, diffusion will be a dominating factor and a high field is again necessary in order to ionize the gas before the electrons are lost. Thus for each particular gas species there is a minimum breakdown field strength as a function of pressure or density.

The above remarks are general in nature and apply to both a.c. and d.c. discharge. Referring momentarily to d.c. electrode discharge, there are four principle classifications: spark, glow, arc, and corona discharge. Spark discharge usually occurs above a certain pressure for any constant potential between the electrodes and involves a sudden breaking down of the resistance of the gas. As the pressure is lowered, the spark will change to a glow discharge. Arc discharges are of the same nature as spark and glow discharges, being characterized by currents of much greater intensity than in the other two cases. The arc discharge is initiated either by a spark or glow discharge, or by a contact between two electrodes that are separated. Corona discharge is a term generally accredited to the faintly luminous, transitory discharge which sometimes occurs in the range between the spark and glow discharge.

Electrode excitation for the case of a d.c. potential is a function of the gas species, temperature, field
strength, density or pressure, and geometry of the contain­
ing vessel. Further information on the discussion above can be found in references 6, 10, 24, and 34.

Discharge using a.c. potentials can be produced in either of two ways: (1) by use of metal electrode plates with a potential across them, and (2) with a coil winding which results in an electric potential between the ends of the winding and an induced magnetic field parallel to the axis of the windings. The a.c. discharge, in addition to being a function of the parameters listed above, is also dependent upon the frequency of the applied emf. At low frequencies the potential required for excitation is approx­imately the same as in the d.c. case. As the frequency is increased up to the radio frequency range \(10^4\) to \(2 \times 10^9\) cycles/sec) or higher, the power required for ionization is greatly reduced. This, along with the advantage of not having electrodes in contact with the gas to cause contam­ination and to disturb the flow, eliminates d.c. discharge in favor of electrodeless r.f. excitation.

An additional factor for electrodeless excitation is that when electrodes are inserted in a moving gas, perpen­dicular to the flow direction, the motion of the gas causes blowout and it is extremely difficult to maintain a discharge (see reference 13). Also the arc or glow discharge when ap­plied in this manner does not uniformly ionize the gas. To
effectively use electrodes in a flowing gas, it would be best to have them aligned parallel to the streamlines of flow, but this would greatly disturb the velocity profile. This same argument also makes the electrodeless plate discharge undesirable, in that to lessen the effect of blowout it would be desirable to have the plates aligned normal to the flow direction. This is impossible for the case of a supersonic nozzle due to the presence of the stagnation and condensing chambers.

On the basis of the preceding discussion, the induction coil with a high-frequency potential is by far the most favorable means by which to induce discharge. Besides the criteria of requiring less power for discharge, it also enables discharge in any desired region of the nozzle since the windings can be wrapped around any position chosen. As mentioned previously, the discharge mechanism here is that at moderately low pressures and at a low applied potential a dull glow appears in the gas which produces initial electrons. As the potential is increased the magnetic field induced by the current in the windings predominates and produces a bright discharge which is shaped in the form of a ring.

An attempt to explain the peculiarities of the so-called "ring" discharge might be made by consideration of
"ambipolar" diffusion. In an ionized plasma, the electron temperature is generally much higher than that of the atoms and ions (see Chapter 3 of reference 1). Consequently they also have a higher mobility and tend to diffuse away from the center of the plasma in greater quantities than the positive ions. This unequal diffusion causes a negative space charge to occur near the walls containing the plasma which in turn results in an attraction of the positive ions towards the wall. The net result is a radial diffusion of particles of both signs towards the walls where they recombine on the wall giving it the deionization energy. This is undesirable since it heats the walls rather than raising the temperature of the gas stream. This radial diffusion is called ambipolar diffusion.

It is proposed here that ambipolar diffusion is possibly the origin of the ring discharge. The magnetic field induced by the solenoid is parallel to the axis of the nozzle or cylinder containing the gas. As the electrons and ions move radially from the center of the plasma due to the process discussed above, a force arises on the particles. This force is equal in magnitude to the product of the charge times the vector cross product of the particle velocity and the magnetic field intensity \( \mathbf{F} = q(\mathbf{v} \times \mathbf{B}) \). According to the right-hand rule, this force is perpendicular to the plane
of $\vec{v}$ and $\vec{E}$, and hence is tangent to a circle about the plasma axis. This gives rise to a circular current or the ring discharge.

Very little theoretical work has been done with high-frequency (h.f.) induced discharges, and what has been done does not agree very well with experimental results. This is partly due to the difficulties of precise measurements and the difficulties of including the many variables in a solvable equation. To even begin a mathematical analysis of the problem at hand would be a thesis in itself and no attempt as such will be made here. It must be pointed out that an accurate representation of the discharge in a plasma tunnel would include the following variables: gas species, temperature, density or pressure, velocity of flow, geometry of the containing vessel, electric and magnetic field strength, applied frequency, and other properties associated with the particular gas.

Little if any information on induced discharges can be found in textbooks, reference 17 deals with a.c. ionization but has only a short section on electrodeless discharge using a solenoid. Outside of the above text, it is necessary to go to individual papers for information. Thomson in reference 32 notes that electrodeless induced discharges are inhibited by insertion, within the coil windings, of a glass rod coated with aluminum, silver, platinum, sulphur, cadmium,
or graphite. On the other hand, coatings of fused salts such as potash, soda, sodium chloride, or lithium chloride tended to aid discharge. These experiments were with air, hydrogen and helium in a still medium, but could possibly be of usefulness in the present mercury plasma tunnel if difficulty in starting the discharge is encountered.

In induced discharges, excitation and illumination of the gas is produced at energies considerably below the ionization potential. This is because of the existence of metastable and resonance states. These states are the result of an electron absorbing a definite quantum of energy from h.f. radiation or from a collision. As the electron decays from these states, a definite amount of radiation or light quanta is given off. The probability of the electron falling from a metastable state and emitting radiation is small, but it is high for a resonance level. Ionization is the extreme case of excitation in which the electron actually escapes from the atom.

It is expected that mercury vapor in a state of excitation or low ionization will be illuminated such that visual studies can be made on the flow around a probe. At high frequencies and field strengths, the brightly illuminated ring discharge makes visual studies difficult.

E. Properties of Mercury Vapor. Mercury is a metal and is usually considered as a monatomic element. Some
properties of interest are tabulated in Table II for atmospheric conditions. In the vapor state, the relations \( c_p = \frac{5R}{2} \) and \( c_v = \frac{3R}{2} \) are obeyed which is characteristic for monatomic gases. The National Bureau of Standards (reference 2) tabulates \( c_p \) for temperatures ranging from \(-39^\circ C\) to \(500^\circ C\) and the amount of deviation from \(\frac{5R}{2}\) is small for this range. The above reference also gives experimental values for the following in the same temperature range: heat of vaporization, compressibility factor, specific heat of liquid mercury, absolute entropy, and the vapor pressure. Some of these tabulated properties are shown by Eckert in reference 16 in graphical form. The variation of vapor pressure versus temperature is illustrated in Figure 3 for future reference.

While mercury vapor is generally considered to be a monatomic gas, bands in the absorption and emission spectra of low temperature mercury vapor proves the existence of molecules \( (Hg_2) \) and is discussed in reference 38. The energy of dissociation determined from various experiments results in conflicting values, ranging from 0.03 to 0.45 volts. At any rate, the dissociation energy is so low that for practical considerations it can be neglected. The first ionization potential for Hg atoms is 10.4 volts, while the second ionization potential is 18.8 volts. For the present purpose, those above the first potential are not of interest.
The effective discharge voltages in mercury are actually those of the excitation potentials below that of the first ionization potential. Quoting the values of reference 10, metastable states (those states with a small probability of emitting radiation upon decay) exist at 4.64 and 5.44 volts, while there is a resonance state (high probability for the emission of radiation) at 4.87 volts. At these potentials, rays in the ultra-violet region of the spectrum are emitted. Reference 20 gives results of experiments in which they conclude that excitation and discharge

Table II

Properties of Mercury Vapor

<table>
<thead>
<tr>
<th>Atomic number = 80</th>
</tr>
</thead>
<tbody>
<tr>
<td>Atomic weight = 200.61</td>
</tr>
<tr>
<td>$R = \text{specific gas constant} = 9.9 \times 10^{-3} \text{ cal gm}^{-1} \text{ K}^{-1}$</td>
</tr>
<tr>
<td>$c_v = 1.485 \times 10^{-2} \text{ cal gm}^{-1} \text{ K}^{-1}$</td>
</tr>
<tr>
<td>$c_p = 2.475 \times 10^{-2} \text{ cal gm}^{-1} \text{ K}^{-1}$</td>
</tr>
<tr>
<td>$\gamma = \text{ratio of specific heats} = 1.667$</td>
</tr>
<tr>
<td>$H = \text{latent heat of vaporization} = 72 \text{ cal gm}^{-1}$</td>
</tr>
<tr>
<td>Boiling Point = $357^\circ \text{C}$ (at 760 mm Hg)</td>
</tr>
<tr>
<td>Melting Point = $-38.9^\circ \text{C}$ (at 760 mm Hg)</td>
</tr>
<tr>
<td>First ionization potential = 10.4 volts</td>
</tr>
</tbody>
</table>
occur in Hg vapor at an effective potential of about 5 volts with a dull, bluish-white glow resulting. As the applied emf is increased to the point where the vapor attains its ionization potential, the intensity increases until a bright ring discharge occurs.

The chemical activity of mercury varies with respect to different elements. Mercury combines easily with the following to form alloys or amalgams: potassium, sodium, gold, silver, tin, zinc, lead, bismuth, and copper. It is relatively stable with respect to: iron, platinum, aluminum, chromium, manganese, nickel, cobalt, and glass (see reference 18). The General Electric Company from their experiments with mercury vapor power cycles report that over long periods at high temperatures in steel pipe lines, the deposit of mercury and iron oxides presents a problem. This can be eliminated by dissolving in the mercury 0.001 to 0.0001 percent by weight of titanium and 0.002 percent by weight of magnesium (reference 30). The above problems are not expected to be serious for short period operation in the present system.

In regards to the toxic nature of mercury, reference 12 reports that the human body can safely absorb up to 0.001 gram of mercury a day over extended periods. Amounts greater than this will result in "mercury poisoning." Dangerous contamination ranges from 0.1 to 20 milligrams per cubic meter.
of air, depending upon the person. Symptoms of mercury poisoning occur and develop in the following stages.

1. Stomatitis (inflammation of the mouth).
2. Salivation and foul breath.
3. Tremors.
4. Erethism (peculiar timidity).
5. Degeneration in various digestive and circulatory organs.

Along with stages 4 and 5 there is receding of the gums and loosening of the teeth. This is followed by the skin becoming yellowish in color with dotted hemorrhages forming. Recovery from the poisoning is possible if the person is removed from all possible contact with mercury while he is still in the tremor stage, otherwise it is likely to be fatal.

Detection of minute amounts of mercury vapor (one part per million in air) can be made with selenium-sulphide coated paper. This chemical is extremely reactive to mercury vapor and forms a black compound when the above mentioned amount is present.
CHAPTER III

PLASMA TUNNEL DESIGN CRITERIA

A. General Layout. Originally the entire system was designed according to the basic schematic of Figure 1 using an all glass system. The stagnation chamber and condensing chamber were to be made of commercial pyrex pipe, tees, and elbows. This was desirable because mercury vapor is inert with respect to glass, but the design was rejected because of the numerous joints necessary. The requirement of a vacuum in the micron range makes it desirable to have as few connections as possible and getting a good o-ring seal at high temperatures in the stagnation region would be extremely difficult. Having the components blown from pyrex as in Eckert's system was not practical because of economical considerations.

The remaining alternative was to use a metal system, preferably stainless steel because of its stability with mercury. This was also ruled out because of economic reasons and the final decision was to use steel components. The drawing shown in Figure 4 (page V of the Appendix) gives the critical dimensions of the final design layout.
The stagnation and condensing chamber components are made from standard, seamless, welding pipe fittings. The stagnation chamber consists of a tee, two reducers, and a section of pipe. The receiving chamber consists of a tee and three reducers. The water jacket is made of copper and a steel base plate to be used for mounting. The return line is 1/4 inch O.D. stainless steel tubing. The steel fittings and flanges are all arc welded as shown in Figure 4. Critical dimensions are shown in the diagram and an extensive explanation of the various sections is given in Table III of the Appendix. A thick coating of asbestos was applied to the outside of the stagnation chamber, but is not shown in the drawing.

Analysis and discussion of some of the particular components in the design is covered in sections to follow.

B. Nozzle Design and Operating Characteristics. As was mentioned in part C of Chapter II, larger area ratios are required (when heat is added to a supersonic expanding flow) in order to obtain Mach numbers of the same magnitude as in the case of no heat addition. The ratio of exhaust area to throat area \( (A_e/A^*) \) was chosen to be 100. By extending the chart in Figure 2 or using the tables of reference 21, this area ratio corresponds to a final value of \( M = 11.7 \) if the gas expands fully without condensing. The throat diameter was taken as 0.2 inches so that excessive
amounts of mercury would not be necessary for choked flow. For $A_e/A^* = 100$, the resulting exit diameter is 2 inches.

The nozzle would preferably be made from vycor because of its high thermal shock resistance, but pyrex was chosen as being adequate and less expensive. A bi-conical nozzle was selected for its ease of construction. The details of the nozzle design are illustrated in Figure 5. The connections to the two chambers of the plasma tunnel consist of slip-over flanges with asbestos inserts to protect the glass as shown in Figure 4. O-rings of 1/8 inch thickness are used for a vacuum seal.

To determine the maximum flow rate that the throat will pass for choked conditions, reference is made to equation 4a.

$$\frac{w}{A^*} = 2.145 \times 10^{-2} p_o \sqrt{\frac{T_o}{T_o}} \text{ lb in}^{-2} \text{ sec}^{-1} \quad (4a)$$

$A^* = 0.0314 \text{ in}^2$ for a throat diameter of 0.2 inches, hence

$$w_{\text{max.}} = 6.75 \times 10^{-4} p_o \sqrt{\frac{T_o}{T_o}} \text{ lb sec}^{-1}.$$  

In order to see the order of magnitude of the expected flow rate, assume $T_o = 450^\circ \text{C}$ and $p_o = 75 \text{ mm Hg}$. This gives $w_{\text{max.}} = 1.88 \times 10^{-3} \text{ lb/sec} = 0.853 \text{ gm/sec}$. From Figure 3 it is seen that $p_o = 75 \text{ mm Hg}$ corresponds to a mercury boiling temperature of approximately $250^\circ \text{C}$ in the stagnation chamber.
For the value of \( p_0 \) used above, the resulting exit pressure is 0.0547 \( \mu \) Hg if the vapor could fully expand without condensing. The flow would then be greatly over-expanded because as mentioned previously the pressure in the exit chamber is expected to be of the order of 10 \( \mu \) Hg. This results in the formation of a normal shock somewhere downstream of the throat in order that a pressure increase can occur to match the exit and back pressures. In the actual physical case with h.f. induced excitation and heat addition, the resulting flow pattern is unknown but the existence of a normal shock in the nozzle is still possible.

The stagnation conditions in the plasma tunnel must be such that the mercury vapor does not condense before the h.f. discharge can become effective. Figure 6 is a plot of \( T_0 \) vs \( p_0 \) for the condition that the vapor temperature at the throat is such that condensation will first start to occur. This graph will be useful as a guide to the stagnation conditions necessary for the flow to reach the throat and induction coils before starting to condense. Assuming that the stagnation pressure is approximately equal to the vapor pressure of the liquid mercury in the boiling chamber, Figure 3 can be used to determine the necessary boiling temperature. The region of valid operation is to the right of the curve. Due to relaxation in that the vapor will not condense immediately upon reaching its condensation temperature, the
actual physical curve might be shifted somewhat to the left of that shown. This would enlarge the valid operating region illustrated.

Note should be made to the thermal stresses which exist at the nozzle throat. Calculations were made as to the possible stresses arising in this region by assuming that the throat region could be treated as a small cylinder. Using the stress formula's for a cylinder and the data for pyrex glass from reference 26, along with assuming that the wall temperature was 85% of the gas temperature at the throat (for $T_o = 450^\circ C$) the calculated stress was more than twice that of the recommended value for pyrex (1000 psi).

In spite of the above results, it was decided to use pyrex rather than vycor for the following reasons. (1) No account was made in the calculations for the wire windings; by making the windings closely spaced and tight around the throat, the resulting heating of the outside wall of the nozzle would greatly reduce if not eliminate the large temperature gradient. (2) Past experience shows that pyrex can be used safely at temperatures higher than that recommended by the manufacturer. (3) The value of $T_o = 450^\circ C$ (giving $T^* = 269^\circ C$) is a maximum value and actual operation is not expected to be at this high of a temperature. (4) Vycor is considerably more expensive than pyrex and is harder to fabricate.
It is interesting to note that on assuming the pressure in the condensing chamber is about 10\(\mu\)Hg and no heat addition is made during expansion, then the stagnation temperature required for complete expansion without condensation is \(T_o = 15,330^\circ C\).

**B. Heating Requirements.** To determine the wattage required for boiling and superheating the mercury, some assumptions must be made and these will be on the basis of maximum temperature conditions. For ease in calculations, it is assumed that the stagnation chamber consists of two cylinders, the 6 inch diameter superheating portion and the 4 inch diameter lower section where boiling occurs (see Figure 4). Denote by the subscripts 1 and 2 the conditions in the lower and upper chambers respectively.

In addition to the above model, it is assumed that the temperatures in region 1 and 2 are constant in their respective chambers and that the mass flow rate of vapor out through the nozzle is equal to that of the liquid mercury entering the boiling region. Using the above, the rate of heat input required in region 1 \((Q_{in_1})\) and in region 2 \((Q_{in_2})\) can be written,

\[
Q_{in_1} = c_p w (T_1 - T_r) + wH + Q_a + Q_b
\]
\[
Q_{in_2} = c_p w (T_2 - T_1) + Q_c + Q_d
\]
where the small difference between the specific heat of the liquid and vapor at constant pressure has been neglected.

Figure 7 illustrates the approximated stagnation chamber model with its dimensions and the various heat fluxes shown. The term $c_p w(T_1 - T_r)$ represents the heat required to raise the temperature of the condensed vapor entering the chamber at temperature $T_r$, with a mass flow rate of $w$, to the temperature $T_1$ at which it enters the upper chamber.

$H$ is the latent heat of vaporization. Similarly, $c_p w(T_2 - T_1)$ is the energy required to raise the gas to the temperature $T_2$ at which it enters the nozzle. The $Q$'s represent the rates of heat lost through the various sections as shown in Figure 7.

Combining the previous two equations gives

$$Q_{in} = Q_{in1} + Q_{in2} = c_p w(T_2 - T_r) + wH + Q_a + Q_b + Q_c + Q_d \quad (6)$$

The rate of heat flow through a composite cylinder wall is given by the relation

$$Q_{cyl.} = \frac{2\pi L(T_i - T_o)}{\frac{1}{k_s} \ln \left( \frac{D_o}{D_i} \right)_s + \frac{1}{k_a} \ln \left( \frac{D_o}{D_i} \right)_a} \quad (7)$$

and that of a composite plane wall by:

$$Q_{wall} = \frac{A(T_i - T_o)}{\frac{x_s}{k_s} + \frac{x_a}{k_a}} \quad (8)$$
In equations 7 and 8, \( L \) is the cylinder length, \( k \) the thermal conductivity, \( D \) is the diameter, \( A \) is area, and \( x \) is wall thickness. The "s" and "a" subscripts refer to the steel and asbestos, while "o" and "i" represent outside and inside conditions respectively. \( Q_o \) and \( Q_c \) can be evaluated through equation 7 and equation 8 is used to determine \( Q_a \) and \( Q_d \).

In the analysis for the heat loss it will be assumed that the inside steel wall temperature is equal to the gas temperature and that the outside temperature of the asbestos is that of room temperature. This is equivalent to neglecting the convection terms and the resulting temperature drop at the two surfaces. In effect this introduces a factor of safety into the calculations since the actual energy loss rate will be less than that given by equations 7 and 8.

Figure 7 shows the diameters of the commercial pipe and plate thicknesses used in the design. It is assumed that the chamber is to be covered with a one inch thickness of asbestos cement except for the top plate which has a one-half inch thickness of asbestos. The following values are assumed for the calculations: \( T_i = T_r = 20^\circ C, \ T_1 = 250^\circ C, \ T_2 = 450^\circ C, \ w = w_{\text{max}} = 1.88 \times 10^{-3} \text{ lb/sec} \) (see page 34).

Values for \( c_p \) and \( H \) are those listed in Table II on page 30. From reference 11 the thermal conductivity of steel is \( k_s = 35 \text{ Btu hr}^{-1} \text{ft}^{-1} \text{F}^{-1} \). The value for asbestos was taken
The results of the calculations give the following:
\[ c_p w (T_2 - T_r) = 37.9 \text{ watts}, \quad wH = 257 \text{ watts}, \quad Q_a = 25.4 \text{ watts}, \]
\[ Q_b = 518 \text{ watts}, \quad Q_c = 1270 \text{ watts}, \quad \text{and} \quad Q_d = 212 \text{ watts}. \]
Equation 6 then gives for the required energy input: \( Q_{in} = 2320.3 \text{ watts}. \) The energy requirements calculated should be quite adequate since extreme operation temperatures were assumed and convection was ignored in determining the losses.

On the basis of the calculations, a 550 watt flat ring heater was ordered to heat the mercury at the bottom of the stagnation chamber (see Figure 4). Three stainless steel immersion rod heaters were also purchased: two rod heaters, 12 inches long, with 500 watts each at 115 volts; and one heater, 24 inches long, with 1000 watts at 115 volts. The cartridge heating units were all 1/2 inch in diameter with 3/8 inch threaded bushings. The total power available from the above is 2550 watts.

C. Condensation Criteria. Calculation of the rate of water flow and the length or area of the surrounding cooling jacket needed to condense the vapor is complicated by the fact that the actual gas velocity, temperature and pressure resulting from ionization in the nozzle are unknown. In addition, the geometry of the receiving chamber eliminates application of standard heat transfer relations unless a simplified model is assumed. Another problem is the calcula-
tion of the film heat transfer coefficient on the inside wall. It is unknown whether the vapor is condensing in droplets as it expands into the chamber, or if it condenses by contact with the cooled surface.

To simplify the analysis, it will be assumed that the condensing chamber shown as a welding tee in Figure 4 can be replaced by an equivalent cylinder. The problem is then reduced to the case of a counterflow heat exchanger with mercury vapor in the inner cylinder and water in the annular space between the inner and outer cylinders. It is further assumed that the temperature of the water is constant along the length of the water jacket. This is equivalent to saying that it is an infinite reservoir and can absorb the heat required to cool the mercury vapor without raising its own temperature.

The mass flow rate of tap water available in the laboratory was determined by experiment to be about 60 lb/min (1 lb/sec). The previously calculated flow rate of mercury vapor through the nozzle was \( \dot{m}_{\text{max}} = 1.88 \times 10^{-3} \text{ lb/sec} \), which puts the two quantities in a ratio of about 530/1. This should be sufficient to keep the water temperature from changing more than a few degrees before it leaves the cooling jacket, and thus can be taken as constant.

A schematic of the assumed model is shown in Figure 8 on page X of the Appendix. The method of design will be to
choose the chamber and jacket diameters, then assume a reason­ably high entrance gas temperature $(T_h)$ and a low chamber pressure $(p)$. Taking extreme values of $T_h$ and $p$ above re­sults in the maximum amount of heat necessary to be absorbed by the water to condense the vapor, and introduces a factor of safety into the results. With the value of $p$ chosen, the temperature at which condensation starts to occur $(T_c)$ can be determined from Figure 3. Using this data the heat trans­fer coefficients can be calculated and the appropriate equa­tions solved for $L$, the length of the water jacket required.

An additional problem arises in that the convection coefficient $(h)$ between the mercury vapor and cylinder wall is small when the flow is considered turbulent with no con­densation, while when condensation occurs the coefficient is much larger due to the presence of a film of liquid mercury forming on the walls. For this reason the problem is considered in two parts. In region 1 the vapor is cooled from temperature $T_h$ down to the point where condensation first occurs at $T_c$, hence $Q_1 = w_c p (T_h - T_c)$. In region 2 it will be assumed that the temperature remains constant at $T_c$ while condensation occurs and the water absorbs energy of the amount $Q_2 = wH$. $H$ is the heat of condensation and $w$ is the mass flow rate of the vapor.

The respective lengths of the water jacket to absorb energies $Q_1$ and $Q_2$ are denoted by $L_1$ and $L_2$ respectively,
where $L = L_1 + L_2$ is the total length required. This is shown in Figure 9 along with the approximated temperature distribution.

For the analysis the diameters shown in Figure 8 are used and values of $T_h = 300^\circ C$ and $p = 10 \mu Hg$ pressure are assumed. For this pressure, condensation occurs at a temperature of approximately $T_c = 50^\circ C$. The tap water available has a temperature of $T_3 = 26^\circ C$ as measured and the flow rate at full pressure is about 60 lb/min. The appropriate heat transfer equation in region 1 for counterflow with two cylinders, one within the other, is

$$Q_1 = \frac{\pi L_1 \Delta T_{m_1}}{\frac{1}{D_a h_1} + \frac{\ln(D_b/D_a)}{2 k_s} + \frac{1}{D_b h_3}} \tag{9}$$

where $\Delta T_{m_1}$ is the logarithmic mean temperature difference and is expressed as follows (see reference 11).

$$\Delta T_{m_1} = \frac{(T_h - T_3) - (T_c - T_3)}{\ln \frac{T_h - T_3}{T_c - T_3}} = \frac{T_h - T_c}{\ln \frac{T_h - T_3}{T_c - T_3}} \tag{10}$$

In region 2, condensation is assumed to be the dominant factor and the temperatures are thus constant. The heat transfer equation is that for steady state through a cylinder as shown below in equation 11. $k_s$ is the conductivity of steel.
\[
Q_2 = \frac{\kappa L_2 (T_c - T_3)}{\frac{1}{D_a h_2} + \frac{\ln (D_b/D_a)}{2 k_s} + \frac{1}{D_b h_3}}
\]  

(11)

\(D_a\) and \(D_b\) are the diameters of the inner and outer surfaces of the condensing chamber as shown in Figure 8.

It should be noticed that the denominator of equations 9 and 11 differ only in the heat transfer coefficients at the inner surface, \(h_1\) and \(h_2\). \(h_1\) is the convection coefficient for a turbulent gas and can be determined from Nusselt's relation below (reference 5).

\[
h_1 = 0.023(R_e)^{0.8}(P_r)^{0.4}
\]

Equation 12 is an empirical relationship for turbulent flow in a pipe and is applicable for the heating or cooling of any fluid that is less viscous than water. This viscosity limitation allows use of the relationship for mercury vapor but not for liquid mercury. Equation 12 applies directly to tubes with a length to diameter ratio of 40 or greater. For a short tube the film conductance is increased and the right hand side of the equation should be multiplied by a "shortness factor." For length to diameter ratios between 1 and 10 (which should include the extreme limits for the condensation chamber), the factor varies from 1.76 to 1.34 as tabulated in reference 5. This factor
will result in an increase of the value of $h_1$ calculated by equation 12 and will decrease the cooling length ($L_1$) required as can be seen from equation 9. In the analysis to follow, the shortness factor will be omitted which introduces a factor of safety into the calculations.

The film coefficient for the condensation of the vapor ($h_2$) can be approximated by another expression due to Nusselt (see reference 5). This relation shown below is

$$\text{h}_2 = 0.725 \left( \frac{k^3 \varrho^3 g H}{D_a \mu_2 \Delta T} \right)^{1/4} \quad \text{(13)}$$

derived from theoretical considerations and is applicable to the condensation of any pure saturated vapor. The derivation assumes that the vapor condenses as a continuous film and generally gives results that are slightly low in comparison to experimental data. A conservative or low value for $h_2$ will again insert a factor of safety in the calculation of the required length ($L_2$) because it will make the calculated value larger than necessary.

Strictly applied, equation 13 represents the conductance of a continuous film on the outside of a horizontal tube due to the condensation of a pure saturated vapor. As an approximation, it will be applied here to the inside of a large tube. Very little experimental results are available for the film coefficient of a condensing vapor. For steam
the experimental values vary from 2000 to 14,000 Btu \( \text{hr}^{-1} \text{ft}^{-2} \text{F}^{-1} \) and it is expected that the value for mercury should fall near the upper limit because of its high conductivity (\( k_2 \)) and density.

Going back to equation 12 and its evaluation, the Prandtl number is given by \( P_r = \frac{c_p \mu_1}{k_1} \). For a monatomic gas, Maxwell derived the following from kinetic theory,

\[
k = 2.45 \mu_1 c_v
\]

which can be written

\[
P_r = \frac{c_p \mu_1}{k} = \frac{c_p}{2.45 \frac{\mu_1}{c_v}} = \frac{c_p}{2.45} = \frac{\gamma}{2.45} = \frac{1.67}{2.45} = 0.68
\]

Substitution of \( P_r = 0.68 \) into equation 12 gives

\[
h_1 = 0.20 \left( \frac{k_1}{D_a} \right) (R_e)^{0.8}
\]  \hspace{1cm} (12a)

where \( R_e = \frac{(D_a V_1 \rho_1)}{k} \). Assuming stagnation conditions of \( T_0 = 450^\circ \text{C} \) and \( p_0 = 75 \text{ mm Hg} \), equation 4a gives \( (w/A)_{\text{max}} = 4.2 \text{ gm cm}^{-2} \text{sec}^{-1} \) for the mass flow rate of mercury vapor per unit area and equals \( \rho_1 V_1 \). \( D_a \) from Figure 8 is 4 inches. For the \( T_0 \) and \( p_0 \) chosen, reference 16 gives \( \mu_1 = 5.25 \times 10^{-4} \text{ gm cm}^{-1} \text{sec}^{-1} \) and \( k_1 = 0.03715 \mu_1 \text{ cal cm}^{-1} \text{sec}^{-1} \text{K}^{-1} \). This results in \( R_e = 81,000 \) and \( h_1 = 2.39 \text{ Btu hr}^{-1} \text{ft}^{-2} \text{F}^{-1} \).

To evaluate \( h_3 \), the convection coefficient of water, the Reynolds number can be written
where \( w_3 = 93 \Delta V_3 = 60 \text{ lb/min} \) as mentioned previously. \( D_e \) is the equivalent diameter for an annular space, equal to four times the area (A) divided by the wetted perimeter. Using the properties of water at 26\(^\circ\)C gives \( R_e = 2140 \) and \( P_r = 7.1 \). A Reynold's number of 2140 falls into the lower portion of the transition zone from laminar to turbulent flow. Reference 11 gives experimental data for water pipe flow in the transition zone. The data is represented in a plot of \( R_e \) vs \( (hD/k)(P_r)^{1/3} \) and it is pointed out that the results hold for noncircular ducts if the equivalent diameter \( (D_e) \) is used in place of the tube diameter. For the \( R_e \) calculated above, the tabulated data gives:

\[
(h_3D_e/k_3)(P_r)^{1/3} \approx 6
\]

Using the Prandtl number calculated above, along with the data from Figure 8 for \( D_e \), gives \( h_3 = 31.8 \text{ Btu hr}^{-1} \text{ft}^{-2} \text{F}^{-1} \). This value of \( h_3 \) is for a length to diameter ratio of 50 and should be multiplied by a shortness factor as in the case of the calculation for \( h_1 \). As a conservative measure, this factor will be omitted.

To determine \( h_2 \) given by equation 13, the tabulated properties of liquid mercury at \( T_c = 50^\circ\)C = 122\(^\circ\)F were used.
as listed in reference 11. \( H = 72 \text{ cal/gm} \) and \( D_a = 4 \) inches from Figure 8, \( g \) is the gravitational constant. \( \Delta T \) was taken as \( T_c - T_3 \) as a conservative value. The resulting calculation gives \( h_3 = 13,700 \text{ Btu hr}^{-1}\text{ft}^{-2}\text{F}^{-1} \) which is very large, as was expected, and compares to the upper limit of experimental values for steam listed on page 46.

Now using: (1) \( Q_1 = w_{\text{max}} c_p (T_h - T_c) \) and \( Q_2 = w_{\text{max}} H \); (2) equations 9, 10, and 11 along with \( k_s = 35 \text{ Btu hr}^{-1}\text{ft}^{-1}\text{F}^{-1} \); (3) the values of \( h_1 \), \( h_2 \), and \( h_3 \) calculated above; and (4) the information given in Figures 8 and 9—the lengths of cooling jacket required in regions 1 and 2 are found to be \( L_1 = 1.91 \) inches and \( L_2 = 6.62 \) inches, or \( L = L_1 + L_2 = 8.53 \) inches. On the basis of this calculation, the horizontal portion of the cooling jacket in Figure 4 was made 8-1/2 inches long. In addition, an effective vertical section of about 6 inches was included to insure complete condensation in the chamber.

As a quick check on the assumption that the water temperature remains constant, the heat absorbed from the vapor \( (Q_1 + Q_2) \) must be equal to \( w_3 c_p \Delta T \) of the water, or

\[
Q_1 + Q_2 = w_{\text{max}} c_p (T_h - T_c) + w_{\text{max}} H = (w_3 c_p \Delta T)_{H_2O}
\]

Using \( w_3 = 60 \text{ lb/min} \) and the other values as given in the previous discussion above, it is found that \( (\Delta T)_{H_2O} = 0.27^\circ\text{F} \) as a temperature increase. There are no heat losses out of
the cooling jacket because the room temperature (20°C) is less than the water temperature. The tendency would be to cool the water if anything, neglecting frictional effects within the cooling jacket.

E. Oscillator and Power Supply. The energy requirements necessary to heat the mercury vapor and prevent its condensation in the nozzle are unknown and represent a problem which borders near impossibility in solution. The difficulties are due to the many variables discussed in Chapter II and the fact that the percentage of energy which goes into the gas as a result of induced discharge is unknown. This topic is one of the reasons for building the plasma tunnel, that is, to provide facilities to study the addition of heat to supersonic flows.

For initial operation, use is to be made of existing equipment in the universities' Aeronautical Laboratory. This consists of a Colpitts oscillator and a 1500 volt power supply. The power supply was originally built by the Arizona Research Laboratory and has an output of 1500 volts d.c. at 300 milliamps. The total power available is thus 450 watts.

The Colpitts oscillator uses a #813 special purpose tube in conjunction with a filament transformer. The transformer operates on a 60 cycle primary with 10 volts center tap and at 5 amps. The oscillator operates at a frequency
of approximately 5 megacycles ($5 \times 10^6$ cycles/sec.) with the induction coil presently in use.

As a note on the difficulty of making reliable calculations, Roes (reference 27) who designed Eckert's power supply mentions that power in the range of 10,000 watts was calculated as the amount necessary to insure ionization of mercury flow. Eckert in reference 14 shows photographs of ionized mercury in the highly excited ring discharge state, which required only 1000 watts of h.f. power. He mentions that glow discharge is obtained at voltages reduced below the figure above. The frequency of oscillation in his experiments was 29 megacycles. The only other experiment on the ionization of supersonic flow of Hg vapor reports use of a 1500 watt power supply with a 40 megacycle oscillator (reference 3).

With the available power supply and oscillator and on the basis of the above discussion, it is expected that the glow discharge state can at least be attained using the present equipment.
CHAPTER IV

CONSTRUCTION OF APPARATUS

A. Assembly of Stagnation and Discharge Chamber.

The pipe fittings used for construction of the two chambers were first sandblasted inside and out in order to remove all paint and grease. The various sections were arc welded according to the diagram in Figure 4. Each section was welded one piece at a time and then leak checked before the next part was welded to it. Difficulties were encountered in the welding and in some cases a single joint had to be welded three or four times before a satisfactory seal was achieved. All joints required at least two passes of the welding bead.

The following procedure was used in the welding. First two beads were placed on a joint and then, using rubber gaskets, flat plates were clamped over any open ends. One plate was fitted with a selastic fitting to which a vacuum pump could be attached with a Pirani vacuum gage in the hose line between welded section and pump. Then the section was pumped down until the exact location of the leaks could be detected. After this the clamped plates and gaskets were removed and the welding bead was ground down with an air grinder. Then the section was rewelded at the
necessary spots.

The above process was repeated until no leaks were detected after the section was pumped down to about 10 \( \mu \text{Hg} \) pressure. The pump available for use had a ultimate pressure of about 1 \( \mu \text{Hg} \) so that a vacuum of 10 \( \mu \text{Hg} \) was considered sufficient, otherwise it would have been necessary to wait several days between each test before the maximum vacuum was attained.

The method of detecting the leaks was to squirt alcohol on the welded joints after the section was pumped down. Since alcohol molecules are larger than those of air, they cause an appreciable deflection of the gage needle if any leaks are present. In many cases the leaks could not be detected in the above manner because the gage would only show appreciable deflection at low air densities. This occurred when the leak was so large that a vacuum of at least 2 mm Hg could not be attained. When this occurred the process was to clamp the open ends as before and internally pressurize the section in question with an air hose to about 20 psi. Then using soap water the leaks could be detected and rewelded.

In order to seal the various sections by clamping plates over the ends, so that they could be evacuated, it was necessary to use vacuum grease on the rubber gaskets to prevent leakage. A large quantity of cheap vacuum grease
was purchased particularly for this purpose.

Flanges with the required o-ring grooves were made to specifications in the Mechanical Engineering Shop. Also the 1/4 inch plate welded to the base of the stagnation chamber (see Figure 4) was ground smooth on the bottom so that a maximum amount of heat could be transferred from the heater to the liquid mercury. The top side of this plate was purposely left rough so that the heat transfer area available between the plate and mercury would be large.

In all there were 12 welded joints and the welding and leak testing process covered a period of about two months before completion. Three braces were then welded onto the stagnation chamber so that it could be mounted on a table. Water with soap detergent was also boiled in the chamber for a period of about 5 hours to ensure a clean surface.

Upon completion of the arc welding, a copper jacket was cut from 6 inch diameter copper tubing, 1/4 inch thick. This was cut up into sections so that it was in the shape of a pipe tee and could be fitted around the condensing chamber. The pieces were then brazed together with a 1/4 inch steel base plate to be used for mounting on a table (see Figure 4). The leaks in the cooling jacket were sealed with ordinary soft solder, and the jacket was painted with aluminum paint.
One other problem arose and this was that a seal could not be made between the heating rods and the threaded holes in the upper flange of the stagnation chamber. To weld these joints as was done for the thermometer wells would possibly result in damage to the ceramic cap on the heating elements. It was finally decided to solder them with 95-5 lead-tin solder which melts at about 450°F. With this arrangement it is necessary to keep the area in the immediate vicinity of the threaded connections from getting too hot and melting the solder.

The final task was coating the stagnation chamber with asbestos. Asbestos cement was used and applied in a paste form by putting one layer on at a time. For initial testing it was decided not to cover the top of the stagnation chamber because of the discussion above. As a compensating factor, a 2 inch thick layer of asbestos was placed around the lower 4 inch diameter boiling section. The design thickness of 1 inch was used for the rest of the chamber.

The completed stagnation and discharge chambers are shown mounted with the nozzle in the photographs of Figures 10 and 11.

B. Vacuum Seals and Techniques. Completion of the two main chambers left only the assembly of the return line and the four o-ring connections. A standard rubber o-ring
was used for the flange at the rear of the condensing chamber, since temperature is not a problem there. The proper size for the groove is: 1/8 inch thickness, O.D. = 4-1/2 inches, and I.D. = 4-1/4 inches. For the flange on the stagnation chamber, initial leak testing was done with a 6-1/4 inch I.D., 6-1/2 inch O.D. rubber o-ring. After the chamber was satisfactorily sealed, a special o-ring was installed for high temperature operation. This is a stainless steel, self-energized teflon coated o-ring with nominal tube size = 1/8 inch, and with a 6-1/2 inch nominal ring diameter. If need is ever found to remove the flange under discussion in order to check the heating units or for other reasons, care must be taken so that the stainless steel o-ring is not disturbed. This o-ring is extremely expensive to replace and can be re-used if it is not removed from its original sealing position.

Although no problems in particular were encountered in regards to o-ring seals with the two flanges mentioned above, just the opposite was true for the nozzle to stagnation and receiving chamber connections. Most of the trouble arose from the fact that the purchased nozzle did not conform to the design specifications of the original drawing shown in Figure 5. This drawing shows an entrance I.D. of 3 inches and an exit I.D. of 2 inches. The purchased nozzle had an entrance I.D. of 2-13/16 inches (O.D. ~ 3-1/8 inches)
and an exit I.D. of 1-7/8 inches (O.D. = 2-3/16 inches). Also the distance from the large end to throat was 3-7/8 inches and from throat to small end measured 8-1/4 inches instead of the dimensions shown. The over-all length was thus greater by 1/8 inch.

The changes mentioned were not particularly serious and the two flanges of Figure 4 were altered accordingly to meet the specifications above. The real problem was that the nozzle as received did not have a true center-line, that is if one end is maintained flush with the vertical, then a straight-edge placed against the other end stands at a slight angle to the vertical. In addition to the above, the O.D. of the entrance end varies about + 1/16 of an inch from the average O.D. of 3-1/8 inches listed above, if measurements are taken at different angles around the end in question. This made it difficult to fit an o-ring properly, particularly metal o-rings which are not flexible.

The method used to align the two chambers and nozzle is as follows. First C-clamps were used to secure the stagnation chamber to the table after a hole had been cut for the lower section, see Figure 10. Then the three braces were adjusted until the exit flange of the stagnation chamber was vertical and a plane parallel to its face was also parallel to the table edge. Holes were then drilled and the stagnation chamber was secured to the table. Following
this, the nozzle was secured to the chamber using a rubber o-ring and the special flange shown in Figure 4. This flange had been designed to fit the nozzle contour with the dimensions as given in Table III of the Appendix.

After the previous operation was completed, the angle between the plane of the exhaust end of the nozzle and the vertical was determined. From this information a circular wood block, 9 inches in diameter was cut (with a center hole for the return line) to the proper angle and size that would make the entrance flange of the condensation chamber at an angle so as to meet flush with the pyrex nozzle. This can be seen in Figures 10 and 11.

Four holes were drilled through the mounting block to match the steel mounting flange and slots were made in the table top to allow sliding of the condensing chamber forward and back. After connection of the nozzle is completed, the condensing chamber can be bolted to the table through these slots.

One point of caution, the alignment achieved is only good for one particular position of the nozzle. If the nozzle is rotated $45^\circ$, for example, the ends will not be flush with the two chambers due to the lop-sidedness of the nozzle construction.

Initial alignment was made with rubber o-rings, but in actual operation a temperature resistant material will be
necessary. The condensing chamber connection will probably be cool enough (because of the water jacket near-by and exposure to the atmosphere) to allow the use of a rubber o-ring. The connection to the stagnation chamber should use a metal o-ring of some type since rubber has excessive creeping and out-gasses at temperatures over 100 or 200°F.

Several weeks were spent using a special aluminum solder and soft aluminum wire, in an attempt to make metal o-rings that would give a proper seal. Due to the flange arrangement it is necessary that the sealing o-ring be soft enough to compress without putting excessive pressure on the glass and breaking it. The attempts to use aluminum o-rings were unsuccessful because any minute indentation or scratch made on the aluminum as it was formed to the proper shape and soldered, resulted in leakage. An attempt was made to place a completed o-ring between two flat steel plates and compress it in a vise in order to remove all surface effects. This was also unsuccessful because it then became impossible to compress the o-ring further without applying excessive pressure on the end of the glass nozzle. A small chip was made on the outer edge of the nozzle during one such attempt and the project was abandoned.

It was finally decided to use rubber o-rings for initial operation of the system and assembly was completed
in this manner. The return line using flared 1/4 inch stainless steel tubing and fittings was assembled with no leakage problems occurring.

C. Electrical Layout. The general arrangement of the oscillator and control panel can be seen in Figure 11. The Colpitts oscillator is bolted to the table just behind the nozzle. A large number of induction windings were used (16 turns) because the resulting field is proportional to the product of the current and the number of current loops. Since the amperage is small (0.3 amps maximum) in the available power supply as compared to that used by others in experiments utilizing induction, the large number of windings should partly compensate for this.

Three voltage regulators are used, one for the flat plate heater, one for the three immersion rod heaters, and the other for the power supply. All three are wired for a maximum output of 135 to 140 volts so that any of the components can be over-driven if so desired. The maximum rated amperage of each regulator is set at 7.5 amperes. Care must be taken with the superheating rods because if all three are hooked in parallel in order to have their maximum output of 2000 watts, then at full voltage they will draw about 17 amps which would burn out the voltage regulator presently in use. In order to have maximum power it is
necessary to connect two of them directly to a 115 volt line and regulate the third for a variation of heat input.

For preliminary testing the three rod heaters were wired with the 1000 watt heater and one of the 500 watt heaters in series. The other 500 watt heater was placed in parallel to the two in series. This arrangement draws about 7-1/4 amps at full voltage with a resulting power available of about 835 watts, which is within the capability of the voltage-regulator.

The control chassis is shown in Figure 12. There are four separate panels. Each contains its own switch, fuse, and test light. The top panel holds the voltage regulators for the heaters. The one on the right is hooked up to the flat ring heater at the bottom of the stagnation chamber, while the other is wired to the three immersion units. The second panel from the top houses the voltage regulator for the 1500 volt power supply directly below it. The bottom panel has the transformer (and switch) required for the tube in the Colpitts oscillator. Operation details will be discussed in Chapter V.

D. Special Equipment. The available vacuum pump used with the system is a Welch Duo-Seal pump with vented exhaust. The ultimate pressure is in the range of 1 μHg or slightly less. The line between the plasma tunnel and vacuum pump contains a glass cold trap which is immersed in an ice
bath to prevent any possible mercury vapor from reaching the pump and being exhausted into the atmosphere. A fitting to hold a Pirani vacuum gage is located between the cold trap and pump as can be seen in Figures 10 and 11.

It is desirable to have a valve between the pressure gage and pump, and a diaphragm-type valve was inserted in the line initially, but for some reason failed to operate. After a certain vacuum was reached, the valve would automatically close the line and prevent evacuation of the system. The reason for this was probably that it used some type of diaphragm which would collapse after the pressure reached a certain minimum. Another valve of the same type also failed to function properly, so the line was connected without a valve since none of better quality and the proper size were readily available.

Five pounds of triple-distilled mercury was obtained to use in the system. This amount is enough to fill the bottom of the stagnation chamber to about 0.8 of an inch depth and permit continuous operation for about 44 minutes even if there was no return line (assuming a maximum flow rate of $1.88 \times 10^{-3}$ lb/sec as has been done in the previous chapter).
CHAPTER V

OPERATION OF PLASMA TUNNEL

A. Startup Procedure. A brief discussion of the operating procedure necessary will be covered here. First of all the system must be evacuated before the two-cycle liquid-vapor mercury process can operate as desired. The final pressure should be as low as possible but never below 2 μ Hg. The reason for this is that mercury at room temperature will boil at 1.2 μ Hg pressure. At 26°C (the cooling water temperature) the critical pressure is 2 μ Hg and would prevent condensation in the receiving chamber if that pressure was maintained there. An optimum pump-down pressure would be about 10 μ Hg before operation begins. For the pump in use this usually requires over-night pumping.

Once the system is evacuated, the switches for the heating units can be turned on and the voltage regulators turned up to any desired percentage of maximum voltage. It must be kept in mind that the regulators are wired for overload capacity and a reading of 100% on the dials corresponds to a 135 volt input rather than 115 volts.

To operate the oscillator, the transformer to the power tube filament in the oscillator must always be switched
on first to protect the tube and prevent it from burning out. After this the switches for the power supply and its voltage regulator can be turned on, and the connecting cord between the two checked for proper attachment (see Figure 12). The voltage of the power supply regulator should never be turned up until the power supply relay kicks in, this can be determined by an audible click and occurs about one or two minutes after the switch is thrown. After this the system is ready for operation and the degree of ionization in the nozzle can be varied according to the percent of input voltage on the power supply regulator.

B. Initial Operation Results. The power supply and oscillator was first tested with no mercury in the system. Figure 13 shows the resulting induced discharge. The discharge would first occur when about 170 volts potential was read on the meter of the power supply. The glow initially is confined to the region of the induction coil and gradually spreads throughout the nozzle as the input is increased. Operation pressure was about 25 μHg and the discharge was purple in color, indicating a nitrogen glow. The induced glow would extinguish when the voltage was reduced below 100 volts, which indicates that less power is required to maintain discharge than to initiate it. This can be expected since once a supply of free electrons is created, the discharge passes more easily.
Two tests were run using mercury, the first was initiated with about 25 \( \mu \)Hg pressure in the system. Rubber o-rings were used in the nozzle connections with an aluminum probe projecting into the test section. Cutting oil was used in the thermometer wells. As the mercury began to boil, discharge was initiated with approximately the same voltage requirements mentioned above for air. The glow was a dull blue in color, indicating mercury discharge. The intensity of the discharge was brightest in the region of the coils, being bright blue-white in appearance at this point. Superheating and boiling temperatures as read from the two thermometers gave approximately the same readings during this test.

As the temperature in the stagnation chamber was gradually increased to about 100\(^\circ\)C, rings or disks began to appear throughout the expansion section of the nozzle. The discharge in the region of the coils were bright bluish-white in color, followed by a dark space just after the windings of about one inch in length. Following this there were alternate dark spaces and dull blue disks of uniform width (about 1/8 inch) throughout the remainder of the expansion portion of the nozzle. This phenomena is similar to the striated discharge or Faraday disks observed under certain conditions in d.c. glow discharge in a cylinder of stationary gas.
As the temperature was increased, the thickness of the disks decreased slightly and they shifted downstream a short distance. By setting the probe into vibration, the striations or disks could be made to oscillate rapidly along the nozzle axis with approximately the same frequency as the vibrating probe.

The test above was terminated when the thermometer in the upper portion of the stagnation chamber read 160°C and that in the boiling section was 150°C. This corresponds approximately to $T_o = 160°C$ and $p_o = 2.8 \text{ mm Hg}$. The Pirani gage at this point gave a pressure of about 300 $\mu$Hg (0.3 mm Hg) in the line between the cold trap and vacuum pump. The pressure in the exhaust chamber was undoubtedly somewhat higher. From this information and the results observed above, it can be concluded that if there was any flow at all it was in the low Mach number range of subsonic flow. Even at the final temperature, the voltage required to initiate discharge was still about 170 volts.

One note of discouragement occurred during the test and this was that the oscillator circuit draws the rated current (0.3 amps) of the power supply when its output was less than one-half of the rated output. The critical potential was about 600 volts, and since it cannot be overdriven without blowing a fuse, the available power is about 180 watts rather than the 450 expected. This could possibly
hinder operation at higher temperatures and supersonic speeds when a larger amount of power is expected necessary to maintain discharge. The parameters above can be altered by using a different coil. This would change the effective inductive capacity of the circuit and the amount of current being drawn.

The second test was initiated with the system at 30 \( \mu \) Hg pressure. Other factors were the same as in the previous test except an attempt was made to keep the temperature of the vapor entering the nozzle greater than the temperature of the boiling mercury. Using this approach, the peculiar striations which occurred in the previous test were not observed. As the vapor temperature increased above 180°C, condensation of the mercury vapor occurred in the entrance section of the nozzle. This was gradual at first and then built up until the throat was filled with mercury at a vapor temperature of about 220°C and boiling temperature of 160°C, giving \( T_o \approx 220°C \) and \( p_o \approx 4 \) mm Hg from Figure 3.

The condensate on the entrance section of the nozzle was brownish in color. This was attributed to either of two causes: (1) the rubber o-rings were becoming soft and contact with mercury resulted in a brown compound due to chemical reaction, (2) mercury oxides were being formed with the rust on the walls in the stagnation chamber. Previous
to assemble the stagnation chamber had been cleaned of impurities by boiling soap and water in it. This also removed a rust resistant film which had been applied earlier and resulted in a certain amount of rusting within the chamber.

At the temperatures mentioned above, the Pirani gage gave a reading of about 400 μHg. Also at 600 volts potential and the maximum current rating of the power supply, a bright discharge was occurring in the region of the induction coils during the condensation process. This discharge only extended a few inches beyond the coils and did not reach the probe. There was also a tendency for the discharge to periodically backflash into the stagnation chamber. The voltage regulators for both sets of heaters at this point were at 80% of their maximum dial reading and the system had been in operation for about one and one-half hours.

As the temperature increased to \( T_o = 260^\circ C \) (500°F) and \( T_{boil} = 183^\circ C \) (360°F) with \( p_o \approx 9 \) mm Hg, the condensed mercury began to clear off of the sides of the nozzle entrance. The liquid Hg in the throat region was forced into the expansion portion of the nozzle and then flowed into the receiving chamber. At this time, when the power supply was turned off, difficulty was met in restarting the discharge. The voltage regulator would be turned to zero input and slowly increased to the maximum (600 volts at 0.3 amperes),
and this cycle was repeated until discharge was finally initiated during one of the attempts. Full power was required to start the induced discharge at this stage, but could be reduced to about 50% of this value before the discharge was extinguished.

The Pirani gage in the vacuum line gave a pressure reading of nearly 500 \( \mu \text{Hg} \) at the temperatures last mentioned. At this time the temperature of the steel reducer leading from the stagnation chamber to the pyrex nozzle was determined by means of a thermocouple which had been soldered at the position of the flange weld before the asbestos was applied. This gave a temperature of 250\(^\circ\)F which is about twice that at which rubber o-rings can be used without excessive creepage and out-gassing. For this reason the testing was halted.

From the results of the above observations, it is likely that the stagnation pressure was sufficient to produce supersonic flow. This was not actually achieved because the stagnation temperature was insufficient to prevent condensation of the main portion of the gas stream before it reached the throat where the discharge could take over.

Referring to Figure 6, which represents the critical conditions which must be met to prevent condensation prior to the throat, it is seen that all of the stagnation pressure and temperature combinations used in the two tests fall
considerably to the left of the curve. In order to obtain supersonic speeds it will probably be necessary to at least meet the conditions of the critical line. Eckert in reference 14 notes that his system had approximately $T_o = 450^\circ C$ and $p_o = 50$ mm Hg, which falls within the operating region of Figure 6 of the present study.
CHAPTER VI

CONCLUSION

In brief summary of the present thesis, the general theory and physical phenomena related to induced electromagnetic discharge of supersonic mercury vapor flow was discussed in the opening sections. This was accompanied by extensive reference to related studies of others listed in the Bibliography. Factors concerning the design of a closed-cycle mercury vapor plasma tunnel was discussed and calculations were made in regards to nozzle characteristics, heating requirements, and the critical parameters concerning condensation of the vapor exhausting from the nozzle. An extensive discussion is included in regard to the actual construction and vacuum sealing problems encountered.

Two preliminary test runs were made, neither of which were successful in obtaining supersonic flow. The reason for the above being that adequate glass to metal seals could be made only with rubber o-rings which in turn limited the stagnation temperatures such that the flow would condense before reaching the nozzle throat.
In order for the induced discharge to be effective in ionizing the flow and eliminating condensation in the nozzle, it is expected that the stagnation conditions will have to meet the criteria shown in Figure 6 along with the proper pressure ratio between stagnation and exhaust chambers. The pressure ratio of \( \frac{p_o}{p^*} = 2.05 \) as calculated from isentropic considerations will have to be exceeded in actual operation because the addition of heat to an expanding supersonic stream results in an effective reduction of the stagnation pressure and an increase of the stream pressure.

In order that future tests be successful, it will be necessary to use o-rings of material other than rubber. Teflon o-rings will not stand up in the temperature range desired, thus a soft metal of some kind must be used. Attempts were unsuccessful in getting a seal with soft aluminum wire which had been soldered to form an o-ring. Suggestions for a proper seal would be to obtain commercial soft aluminum or copper o-rings. These could also be made in the laboratory from sheet stock. If copper is used it must be annealed and then quenched to obtain the desired softness.

Future experiments with the present apparatus will probably require a better nozzle and if purchased, it should have wide flared ends with an o-ring groove. This would permit use of stainless steel o-rings and eliminate the
problems presently encountered. It may also be necessary to use a larger power supply, since the initial testing gave results which suggest that the present equipment may not be able to initiate a sufficient percentage of discharge during supersonic operation.

Once proper seals are achieved, the plasma tunnel will provide a source for a variety of interesting experiments associated with magneto-aerodynamics. Among these are studies of boundary layer profiles in conducting mediums such as experienced by re-entry vehicles. This can be simulated by visually observing the changes in shock waves and flow patterns as a current is passed through a small coil winding imbedded within a suitable probe. Other possible experiments include an analysis of the percentage of the applied power which goes directly into heating of the plasma. This could possibly be accomplished by correlating the light intensity of the discharge to the temperature of the gas and then comparing it with the input energy. The plasma tunnel can also be used as a demonstration unit for visual observation of shock wave phenomena without the requirement of a Schlieren system or other optical instruments.


## APPENDIX

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<td>11.</td>
<td>Photograph of Complete Facility</td>
<td>XI</td>
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<td>12.</td>
<td>Photograph of Control Panel</td>
<td>XII</td>
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<td>13.</td>
<td>Photograph of Induced Discharge in Air</td>
<td>XIII</td>
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<td>14.</td>
<td>Details for Figure 4</td>
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### Table

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<td>III. Details for Figure 4</td>
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Figure 1
Schematic of Plasma Tunnel
Figure 2

Working Chart for $\gamma = 1.67$
Figure 3

Vapor Pressure of Mercury

III

Temperature (°C)
Table III
Details for Figure 4

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tr>
<td>a</td>
<td>1000 watt heating element, 24&quot; long with 3/8&quot; pipe threads, located 1&quot; from chamber centerline.</td>
</tr>
<tr>
<td>b</td>
<td>Two 500 watt heating elements, each 12&quot; long, located on a 2&quot; radius from chamber center at a 45° angle from nozzle.</td>
</tr>
<tr>
<td>c</td>
<td>Flange with 9&quot; O.D., 8 holes tapped at an 4&quot; radius for 1/4&quot; bolts, also tapped for heating rod bushings. Not shown is a 5&quot; thermometer well located in plane to that of Figure 4 through the 24&quot; heating rod and 2-1/2&quot; from it.</td>
</tr>
<tr>
<td>d</td>
<td>Flange welded to top of stagnation chamber with o-ring groove cut having I.D. = 6.199&quot;, O.D. = 6.518&quot;, and depth = .098&quot;.</td>
</tr>
<tr>
<td>e</td>
<td>Superheating and boiling portions of stagnation chamber made from welded pipe fittings with dimensions and welds as shown.</td>
</tr>
<tr>
<td>f</td>
<td>Flange with 5-1/2&quot; O.D. and 3&quot; I.D., 4 holes tapped for 1/4-20 NC threads, o-ring groove has 3-1/4&quot; O.D. and is 1/8&quot; deep.</td>
</tr>
<tr>
<td>g</td>
<td>Flange with 5-1/2&quot; O.D. and 1/4&quot; thickness, I.D.'s are 3.153&quot; and 3-1/2&quot; with 1/2&quot; thick base to fit nozzle contour.</td>
</tr>
<tr>
<td>h</td>
<td>Asbestos rings between flanges and nozzle.</td>
</tr>
<tr>
<td>i</td>
<td>Pyrex nozzle, see Figure 5.</td>
</tr>
<tr>
<td>j</td>
<td>Flange to attach nozzle, 4-1/2&quot; O.D. with 1/4&quot; outside thickness and 1/2&quot; base thickness. I.D.'s are 2.456&quot; and 2-9/16&quot;.</td>
</tr>
<tr>
<td>k</td>
<td>4-1/2&quot; O.D. flange welded as shown with 2&quot; I.D. and 4 holes tapped for 1/4-20 NC threads. o-ring groove is 1/8&quot; deep with 2-1/2&quot; O.D.</td>
</tr>
<tr>
<td>l</td>
<td>Condensation chamber constructed from welded pipe fittings with dimensions as shown.</td>
</tr>
<tr>
<td>m</td>
<td>Copper water jacket cut from 6&quot; copper pipe and brazed to fit inner chamber.</td>
</tr>
<tr>
<td>n</td>
<td>Steel thermometer well extending 1-1/4&quot; into chamber and surrounded by 1&quot; diam. ring to reduce cooling effects of water.</td>
</tr>
<tr>
<td>o</td>
<td>Selastic fitting with o-ring to hold probe in position.</td>
</tr>
<tr>
<td>p</td>
<td>1/4&quot; aluminum probe extending into nozzle.</td>
</tr>
<tr>
<td>q</td>
<td>6-1/4&quot; O.D. flange with 4 holes drilled at 2-7&quot; radius for bolting. 1/4&quot; pipe threads at center for selastic fitting.</td>
</tr>
<tr>
<td>r</td>
<td>Flange with 6-1/4&quot; O.D. and welded to chamber. o-ring groove has O.D. = 4.62&quot;, I.D. = 4.25&quot;, and depth of 0.123&quot;.</td>
</tr>
<tr>
<td>s</td>
<td>Circular steel plate, 9&quot; O.D. and welded as shown. Four holes drilled to 1/4&quot; for mounting condensing chamber.</td>
</tr>
<tr>
<td>t</td>
<td>Brazed joint between 1&quot; by 1/2&quot; pipe reducer and stainless steel male tubing connector.</td>
</tr>
<tr>
<td>u</td>
<td>Stainless steel union tee connecting &quot;flared ends&quot; of the 1/4&quot; O.D. tubing between condensing chamber and return line.</td>
</tr>
<tr>
<td>v</td>
<td>Stainless steel tubing leading to cold trap, vacuum gage, and vacuum pump—in order as listed.</td>
</tr>
<tr>
<td>w</td>
<td>1/4&quot;, O.D. return line with bend to trap liquid mercury.</td>
</tr>
<tr>
<td>x</td>
<td>550 watt ring heater with terminals for wire connections.</td>
</tr>
<tr>
<td>y</td>
<td>Thermometer well at angle and within 1/4&quot; of base plate.</td>
</tr>
<tr>
<td>z</td>
<td>Stainless steel male connector for flared tubing, threaded into wall of stagnation chamber.</td>
</tr>
</tbody>
</table>
NOTE: Symbol “→” Denotes Welds
See Table III for Details
SCALE: 1” = 1/5”

Figure 4
Dimensioned Drawing of Plasma Tunnel
NOTE: Nozzle is of Circular Cross-Section

MATERIAL: Pyrex

SCALE: 1" = 1/2"

Figure 5

Details of Supersonic Nozzle
Figure 6

$T_o$ vs $p_o$ for Condensation at the throat

VII
Figure 7
Schematic of Assumed Stagnation Chamber Model
for Energy Calculations
Figure 8
Schematic of Assumed Condensation Model for Heat Transfer Calculations

Figure 9
Assumed Temperature Profile for Figure 8
Figure 10

Photograph of Mercury Tunnel
Figure 11
Photograph of Complete Facility
Figure 12

Photograph of Control Panel
Figure 13
Photograph of Induced Discharge in Air