MASTER

FINAL REPORT — DESIGN PHASE

1 X 70 CM COOLED WIP ELECTRON GUN

1 DECEMBER 1978 — 2 FEBRUARY 1979

AEROSPACE GROUPS

HUGHES

HUGHES AIRCRAFT COMPANY
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I. INTRODUCTION

Presented here are design criteria for a full scale (70 cm) WIP electron gun system capable of long run operation with the following specifications:

Foil area	~70 x 1 cm
Electron beam current density	~1 A/cm ²
Pulse length	<200 ns FWHM
Pulse tail	<25 f pulse energy
Average current	~2 ma/cm ²
Voltage	-150 kV
Voltage droop	<10%
Rep rate	~10 KHz
Power	~20 kW
Run time	~30 min.
Jitter	~10 μ s

1.0 ELECTRICAL ANALYSIS

1.1 HIGH VOLTAGE REGION

At breakdown of gases below Paschen minimum there are generally four regions of ultimate stress:

- a. Electrode surface (vacuum breakdown)
- b. Insulator inner wall (internal flashover)
- c. Insulator outside wall (external flashover)
- d. Gas path (Paschen breakdown)

By varying the different parameters (e.g., electrode-insulator geometry, gas pressure, etc.), the individual regions can be stressed to a greater or lesser extent.

From previous experience upper limits of stress have been defined for the various conditions:

a. Vacuum breakdown	80 kV/cm — steel electrodes
b. External flashover	25 kV/cm — Epoxy feedthrough
c. Internal flashover	-- — vendor specification
d. Paschen breakdown	20 kV/cm — Pd = 1 in Helium

Figure 1.1-1 illustrated the Paschen curves for air, H₂ and Helium. As shown the WIP gun operates far to the left of breakdown in Helium and can also operate in air.

The electrode configuration is basically a coaxial cylinder design with a maximum electric field strength defined by

$$E = \frac{V}{2.3 r} \frac{1}{\log \frac{r+a}{r}}$$

Figure 1.1-1. Paschen curves for air – Helium.

where

V = applied voltage

f = inner cylinder radius

a = separation

For V = 150 kV, r = 4 cm, and a = 4 cm we obtain a stress of E = 54.1 kV/cm. The molybdenum cold cathode faces a molybdenum ribbon grid with approximately 80 percent transmission. This produces a field intensification at the grid equal to

$$\delta = \left[1 - e^{-\pi(1-T)} \right]^{-1} = 2.14$$

where T is the grid transmission. At the inner cylinder the field is

$$E = \frac{V}{r \ln r_2/r_1} = 43 \text{ kV/cm}$$

Thus the grid is stressed to 98 kV/cm. This is conservative for pure molybdenum surfaces. Figure 1.1-2 illustrates an experimental breakdown curve for a 25 cm gun.

Figure 1.1-2. Experimental paschen curve for 25 cm WIP E-gun.

1.2 HIGH VOLTAGE FEEDTHROUGH

The high voltage feedthrough design is critical for successful operation. In particular, the nature of the insulator surface properties are important in its voltage holdoff properties. Hughes' experience indicates improved performance is achieved by tapering the insulator at the cathode end and employing high conductivity materials such as epoxy instead of alumina ceramics. It is believed that the surface charge distribution on the insulator greatly effects its surface breakdown properties. A high surface conductivity allows for rapid charge redistribution and hence results in greater voltage holdoff capability. Tests conducted on identical electrode structures indicate that epoxy insulators can sustain a voltage rise rate of 10 kV/sec without flashover up to 200 kV while an alumina insulator generally requires tens of minutes to reach the same potential without breakdown.

The epoxy is a hard, shock resistant material with a resistivity of 10^{10} ohm-cm. It can operate at temperatures up to 100°C without significant outgassing. Its arc tracking susceptibility is extremely high. This was determined by subjecting an epoxy feedthrough to 100 kV high current pulses in air with a Marx bank. After several hundred arcs directed along the epoxy surface, the insulator was installed in the gun and operated at 160 kV without breakdown problems.

The insulator assembly for the 70 cm gun has four cooling passages cast into the epoxy material. These openings feed "Coolanol" to the molybdenum cathode. The cooling fluid has the following electrical characteristics:

Dielectric Strength	35 kV/inch
Resistivity	10^{12} ohm-cm
Dielectric Constant	2.51

A closed cycle pumping system will circulate and filter the cooling fluid to the required specifications.

The high voltage feedthrough is illustrated in Figure 1.2-1. The plasma wire feedthrough is shown in Figure 1.2-2; it is designed for a surface stress of less than 25 volts/mil.

Figure 1.2-1. High voltage feedthrough.

Figure 1.2-2 Plasma wire feedthrough.

1.3 ELECTRON OPTICS

Ion Extraction

Ions can be extracted from a plasma through an aperture in the containing system, by application of a negative potential to an external electrode. In the concentric-cylinder-wire discharge the plasma is essentially at anode potential thus ions can be extracted by perforating the cathode. Beam current is then controlled by varying the plasma parameters. It is important to note that the current which can be drawn from the plasma depends on the conditions in the plasma and its boundary: The flow pattern of the extracted current is determined by space change forces in the beam. When one talks about space-change limited beam current, it is the flow pattern which is "limited" not the total current.

The plasma sheath is defined as the transition zone between the plasma where $(n_i - n_e)/n_e \ll 1$ and time beam where n_e is zero. It is a region with an electric field which reflects electrons and accelerates ions. The ion current density J_s across the sheath is given by

$$J_s = n_i v_i e$$

where

$$v_i = \left(\frac{k T_e}{M_i} \right)^{1/2}$$

To a first approximation, this current is the saturation current which can be drawn from a plasma. The relationship between current density and electron temperature indicates the one may increase the saturation emission current by increasing the electron temperature.

The current flow from the plasma must be self-consistent, i.e., the ion velocity must be derived from the acceleration potential which in turn is related to the ion density. The basic equations are rate of change of momentum

$$\frac{d(Mv)}{dt} = eE$$

conservation current

$$\text{DIV } j = \text{DIV}(nv) = 0$$

and Poissons equation

$$\text{DIV } E = 4\pi ne$$

This has been solved for the cylindrical case by Langmuir and Compton (1949). For ions, we have

$$\frac{i}{L} = \frac{3.43 \times 10^{-7} V^{3/2}}{M^{1/2} (r \beta^2)} \text{ amp/cm}$$

where

$$\beta = 1 + 0.9769(r_o/r)^{2/3} \text{ SIN} \left[1.0854 \log_{10}(r/11.93 r_o) \right]$$

here

V = Voltage VOLTS

M = Ion Mass (He = 4)

r = cathode radius cm

r_o = plasma sheath radius cm

i = current amperes

By plugging in the experimental parameters: $V = 400$ volts, $i = 1.0$ AMP, $L = 35$ cm, $r = 3$ cm we can obtain the plasma sheath radius r_o . We thus have $r_o = 2.9$ cm, hence the sheath thickness is 0.1 cm.

Transverse components of the accelerating field exist in the extraction grid causing the ion beam to diverge. An approximate treatment of this can be obtained by using the thin lens formula for apertures in electrodes due to Davisson and Colbrick (1931).

For a circular aperture the equivalent focal length is $4V/E$, V being the voltage and E the longitudinal electric field at the anode: For a slit the focal length is one half this value. To a first approximation the electric field space charge in the beam is $4/3 (V/d)$. Thus, the divergence of a circular aperture of radius a is $a/3d$ and $2a/3d$ for a slit aperture. For $a = 1$ mm and $d = 1.7$ cm is equal to 20 milliradians. This value however is not a constant since the beam divergence depends on the voltage and extracted current. It is impractical to exploit beam propagation properties with a variable current device. The beam actually goes through a focus. When a control grid separates two regions in which the electric field E is different, a lens action takes place. The focal length of for a slit aperture is equal to

$$\frac{1}{f} = \frac{E_1 - E_2}{2 V_g}$$

V_g is the voltage of the grid center, E_1 is the field prior to the grid and E_2 is the field the ion encounters after passage through the lens. In the present context E_1 can be neglected in comparison to E_2 , thus we have

$$\frac{1}{f} = \frac{1}{2d} \left(\frac{V}{V_g} - 1 \right) \quad ; \quad V_g \sim V_G + \frac{3}{4}(E_1 + E_2) a$$

where d is the length of the acceleration region, V is the high voltage potential and V_g is the grid potential. For $d = 4$ cm, $V_g = 6000$ V, and $V = 150$ kV the focal length f is less than a few millimeters. The beam thus diverges from this point. It is interesting to calculate how much the beam has spread when it strikes the cold cathode.

We assume that each aperture of half width a deflects a bundle of initially parallel ions to a point focus. The maximum deflection Φ experienced by an ion which grazes the aperture and is given by

$$\Phi = \tan^{-1} \left(\frac{a}{f} \right)$$

The motion of this ion beyond the focus in a uniform field can be shown to be

$$x = 2 \left(\frac{Vg}{V - Vg} \right) d \sin \Phi \cos \Phi \left[\sqrt{1 + \frac{4(z - f)}{d} \left(\frac{V - Vg}{Vg} \right) \frac{1}{\cos^2 \Phi} - 1} \right]$$

where V is the potential at a distance z from the grid. For $z = d$ and $V \gg Vg$, this can be simplified to

$$x_{\max} = 2 \left(\frac{V}{V - Vg} \right)^{1/2} d \tan \Phi$$

where $x_{\max}$ is the maximum position of the beam edge perpendicular the beam axis. By substitution this leads to

$$x_{\max} = \frac{a}{2} \left(\frac{V}{Vg} - 1 \right)^{1/2}$$

By plugging in a 0.25 cm, $V = 150$ kV, $Vg = 6000$ V, we see that $x_{\max}$ is approximately 0.75 cm. The dispersion is more severe at low current levels or low grid voltages. The overlapping beams thus produced by each aperture give rise to a linear current density without the shadow of the grid structure. Figure 1.3-1 illustrates the axial beam spread.

The transverse beam spread may be evaluated as follows: suppose an ion enters the high voltage region of uniform field E_y with an initial velocity

$$v_o = \sqrt{\frac{2eV_o}{m}}$$

Figure 1.3-1. Axial beam spread.

having been accelerated through the cathode fall potential V_o . If the entrance angle with respect to the grid is θ , the equations of motion are

$$v_x = v_o \cos \theta$$

$$v_y = v_o \sin \theta + \frac{eEy}{m} t$$

By integrating with respect to time and eliminating t , one obtains

$$y = \tan \theta x + \frac{Ey}{4 V_o \cos^2 \theta} x^2$$

at $y = 5$ cm the ion strikes the cold cathode, therefore its maximum position from the edge of the grid is

$$x = \left[+ \tan \theta + \sqrt{\tan^2 \theta + \frac{Ey y}{V_o \cos^2 \theta}} \right] \frac{2 V_o \cos^2 \theta}{Ey}$$

$$x = \frac{2 V_o \cos^2 \theta}{Ey} \left[\tan \theta + \sqrt{\tan^2 \theta + \frac{Ey y}{V_o \cos^2 \theta}} \right]$$

For $V_o = 6000$ volts, $Ey = 60$ kV/cm, V and $\theta = 60^\circ$ max, $x = 0.57$ cm. Thus the ion beam arriving at the cold cathode is 0.54 cm larger than the extraction grid size. The majority of secondary electron emitted leave the surface with an energy of less than 10 eV. They leave the surface with a cosine distribution. Thus at $\theta = 45^\circ$, the electron strike the ground plane 0.06 cm farther than the vertical position of the ion impingement. Hence the electron beam at the extraction grid is approximately 0.66 cm larger than the ion beam. Figure 1.3-2 illustrates the transverse beam spread.

Both axial and transverse spreading may be minimized by contouring the cold cathode to shift the cosine secondary peak towards the center of the electrode. This, however, will increase electron emission about the perimeter of the beam. In addition, the transverse spreading may be minimized by providing a somewhat radial field in the transverse dimension.

Figure 1.3-2. Transverse beam spread.

1.4 LOW/HIGH VOLTAGE CIRCUITRY

Presented below are design criteria for the pulse circuitry associated with WIP guns. This includes considerations related to both the high voltage supply and plasma driver.

First, with regard to the high voltage section, the following parameters are assumed:

Cable length	= 100 ft.
Cable capacitance	= 20 pf/ft.
Pulse FWHM	= 200 ns, triangular
Peak current	= 100 A
Voltage	= 150 kV

The first consideration is whether or not to treat the charging cable as a transmission line. In order to qualify as a transmission line, the pulse switching time must be small compared to the cable one way transit time, otherwise the cable behaves like a simple lumped capacitance. In the present case, the pulse rise time is 100 ns, approximately equal to the one way transit time. Therefore, treatment of the cable as a capacitor is justified. In this case, the cable capacitance, designated C_1 , is in parallel with an externally added capacitance, C_2 . Both are charged initially to 150 kV and discharged through a characteristic impedance given by the ratio of gun voltage to total current, or 1.5 k Ω in this case. The stipulation here is that the cable voltage be prevented from dropped 10% below the charge voltage to prevent eventual cable breakdown. The relevant calculation is the discharge time of C_1 and C_2 in parallel through the 1.5 k Ω impedance. This is most easily accomplished through calculation of the charge transfer, since the current is a function of time. The initial stored charge, Q_o , is

$$Q_o = (C_1 + C_2) V_o$$

where V_o is the initial voltage.

The discharge criterion is that the charge dissipated through current be a fraction, f , of the initial charge, or

$$I_p T = f(C_1 + C_2) V_o$$

where I_p is the peak current, T is the pulse FWHM and integration is over a triangular wave form. For the parameters listed above,

$$C_2 = 1.3 \times 10^{-9} - C_1$$

where C_1 and C_2 are in farads. For the cable data listed previously, $C_1 = 2 \times 10^{-9} f$, therefore, no external capacitance is required in this case. If C_2 equal to C_1 is added externally, then the voltage drop would be 3%.

Calculation of circuit behavior in the plasma section is greatly complicated by the time varying plasma load impedance. Typical voltage and current wave forms are shown idealized in Figure 1.4-1 for a capacitive discharge driving the plasma trigger wire. Discharge capacitance was about 4000 pf. For short pulse applications, it is desirable to operate the plasma over-volted since this increases plasma density and decreases both current turn on lag and jitter. However, as indicated in Figure 1.4-1, the plasma

Figure 1.4-1. Wire discharge voltage current characteristics.

impedance drops quickly as the current rises to its peak value, indicating the initial stages of current avalanche. The avalanche process is prevented from running to completion by the falling drive voltage which is a result of depleted storage capacitor charge. At the point of current turn over, the plasma can be thought of as running in a stable, positive resistance mode with a well defined impedance. For short pulse applications, therefore, high voltage and low capacitance values are suggested such as in charged cable arrays. Furthermore, the wave forms suggest that a two-component circuit is preferable to a single capacitive discharge. In this case, the applied voltage is composed of a short, high voltage spike followed by a lower voltage, longer pulse. Application of a square voltage wave form is not desirable unless it is preceded by a high voltage spike. Therefore, a simple PFN is not suitable. A more favorable circuit is the pulse charged "peaking" circuit described below.

The peaking circuit shown in Figure 1.4-2 is desirable because it can drive a variable impedance load and provides an initial high voltage transient followed by a lower voltage pulse. The voltage at the trigger wire can be expressed as

$$v = \frac{V_o}{1 + C_2/C_1} (1 - \cos \omega t)$$

$$\omega = \frac{1}{\sqrt{L_1 C}} \quad , \quad C = \frac{C_1 C_2}{C_1 + C_2}$$

Figure 1.4-2. Plasma driver circuit.

For the case $C_2 \ll C_1$, the initial voltage goes to $2v_o$ at the trigger wire in a quarter period. The length of the initial transient is determined by L_1 and C_2 . The slower secondary pulse is determined by L_1 , L_2 and C_1 . It is important to keep L_2 as small as possible.

For the conditions considered above, it was found that a transmission line analysis was not appropriate. However, pulses appreciably shorter than those indicated above may be of interest. In this case a transmission line calculation may be useful. Such an analysis is included below.

The situation of interest is the discharge of a charged line of impedance R_o into a resistive load R . It is further assumed that the other end of the line, attached to a power supply, is bridged by an impedance much larger than that of the line. The situation is pictured in Figure 1.4-3. The analysis follows the voltage v as a function of time.

Figure 1.4-3. Transmission line model.

The line is initially charged to a voltage V_o , and the switch is closed at $t = 0$. For $t \geq 0$, a negative discharge wave is launched to the left of magnitude V_1 ,

$$V_1 = \frac{R_o}{R_o + R_o} V_o$$

The voltage at the switch, V_2 , is

$$V_2 = \left(1 - \frac{R_o}{R + R_o}\right) V_o$$

The voltage V_2 remains at this value until the pulse V_1 travels down the line, reflects off the open end and returns. Since, it was assumed that at the left end the impedance was large compared to that of the line, the reflection coefficient is +1 and the pulse V_1 returns to the right without attenuation and with the same polarity. The period over which the voltage V_2 remains stationary is then the two way line transit time, T .

$$T = 2LR_o C$$

where L is the line length in meter, and C is the line capacitance in farad/meter.

When the pulse V_1 , encounters the switched section once more, it subtracts from the voltage V_2 and suffers reflection with a reflection coefficient, r ,

$$r = \frac{R - R_o}{R + R_o}$$

and propagates to the left with amplitude V_3

$$V_3 = rV_1$$

Since $R > R_o$, polarity is preserved. The voltage at the switch then drops to a new value, V_4 , for the period T .

$$V_4 = V_2 - (1 + r) V_1$$

In a similar manner, the left travelling pulse returns to the switch and reflects with voltage V_5

$$V_5 = rV_3$$

The switch voltage then drops to the value V_6 for the third T period.

$$\begin{aligned} V_6 &= V_4 - V_3 - V_5 \\ &= V_2 - (r + 1)^2 V_1 \end{aligned}$$

The final time history over three T periods is shown in Figure 1.4-4.

For the case of interest here

$$R_o = 50\Omega$$

$$R = 1.5 \text{ k}\Omega$$

$$r = 0.9355$$

Therefore,

$$V_2 = 0.9677 V_o$$

$$V_4 = 0.9053 V_o$$

$$V_6 = 0.8469 V_o$$

$$T = 180 \text{ nsec}$$

Therefore, over the time of the pulse, the voltage has dropped roughly 5% assuming the extreme case of a step function switch.

Figure 1.4-4. High voltage characteristics.

2.0 THERMAL ANALYSIS

2.1 HIGH VOLTAGE EMITTER

Thermal analysis of the high voltage emitter (alternately known as the cathode) primarily involves determination of steady state surface temperature and performing the iteration required to optimize performance of the mechanical elements of the emitter and the cooling system characteristics. The mechanical elements include structural deformation, high temperature material effects on the vacuum system and ease of fabrication. Cooling system characteristics include flow rate, pressure drop, heat transfer capacity and system complexity.

The following structural and thermal criteria were considered in the cathode design.

- a. Heat Load: 3 kW total
- b. Operation Duty Cycle: Continuous
- c. Material: Molybdenum
- d. Emitter Surface Area: 675 sq. cm (9.2 cm x 73.34 cm)
- e. Maximum allowable internal stress: 10,000 PSI
- f. Allowable support electrode deflection: 0.004 inches (based on electrode manufacturers spec.)

The following coolant system criteria were considered in the cathode design:

- a. Dielectric strength of coolant: 30 kV (0.1 inch gap)
- b. Volume Resistivity: 1×10^6 ohm-cm
- c. System Pressure: 100 PSIG max (based on reasonable pump power)

- d. System Flow Rate: 10 GPM max (based on reasonable pump size)
- e. Material Compatibility: molybdenum, epoxy, SS, copper

For continuous operation, a single pass, multiple passage, flat plate heat exchanger design was considered adequate for this application. Twelve passages (0.10 inch square cross section) milled lengthwise in the back surface of the emitter was established as the basic heat exchanger configuration.

Five candidate coolants were evaluated with respect to flow rate, pressure drop and emitter surface temperature. The five coolants were: deionized water, 3M Company Fluorinert #FC 75, Monsanto Coolanol #25 R, Monsanto Coolanol #20, and WEMCO #55822.

Water, while being the most efficient coolant, was eliminated from contention due to the difficulty in maintaining proper levels of deionization. The WEMCO product was eliminated due to excessive viscosity which produced high system operating pressures. The remaining candidates, while all satisfactory for the application stated were then appraised with respect to cost, and system pressure drop. Optimization of these factors resulted in the choice of Coolanol #20 for the high voltage emitter cooling media.

Thermal and structural analysis indicate the following system operating characteristics based on the use of Coolanol #20 heat transfer fluid and 3 kW energy dissipation.

- a. Coolant Flow Rate: 4 GPM
- b. Coolant Inlet Temperature: 80°F
- c. Coolant Outlet Temperature: 93.4°F
- d. Maximum emitter surface temperature: 119°F
- e. Flow System Pressure Drop: 43 PSIG
- f. Emitter Bending Displacement: 0.002 inch
- g. Emitter Longitudinal Expansion: 0.004 inch
- h. Emitter Lateral Expansion: 0.0005 inch

The coolant passages slotted in the backside of the molybdenum emitter plate will be enclosed by electroformed nickel 0.060 inches thick. A cross section of the emitter illustrating the coolant passages is shown in Figure 2.1-1.

Figure 2.1-1. Cross section-high voltage emitter.

Interface stress levels at a 200 PSIG will not exceed 3600 PSI in the area of the outlet header.

Thermal expansion of the emitter during operation will produce a deflection in the high voltage stand-off insulator of less than 0.002 inches. The shield enclosing the back side of the emitter will be fabricated from 304 stainless steel. Sufficient clearance between the molybdenum emitter and shield will be provided to prevent interference due to differential thermal expansion, approximately 0.006 inches longitudinally and 0.001 inch laterally.

2.2 EXTRACTION GRID

Thermal analysis of the extraction grid involves determination of the grid centerline temperatures for various cooling techniques and for a variety of materials. Of primary importance is the choice of materials which would not exhibit excess outgassing at elevated temperatures and not deform under the thermal and structural loads imposed by continuous operation.

The following structural and thermal criteria were considered in the grid design.

- a. Heat Load: 39.66 w/cm^2 (energy deposited at upper grid plane)
- b. Operational Duty Cycle: continuous
- c. Coolant: Water
- d. Grid Configuration: $8.53 \times 70.48 \text{ cm}$
- e. Rib Arrangement:* 130 ribs at 0.50 cm centers
(Rib cross section $0.081 \times 0.95 \text{ cm}$)

Preliminary investigation immediately indicated a need for an active cooling system to permit CW operation. For example, the temperature of a molybdenum rib will raise approximately 156° per minute without an adequate cooling system.

The types of cooling systems considered included single and double pass heat exchanger cooled ribs, short gap (contact) cooled headers and single and double channel cooled headers. While all methods offer varying degree of rib cooling, the most reliable method without excessive fabrication complexity and resultant cost is the single channel cooled header. Individual

*Note: Center filament electrode shield cross section $1.27 \times 0.95 \text{ cm}$

ribs are cooled by conduction of heat through the half-span of the rib through a braze junction and into the water cooled headers.

Material tradeoffs were performed leading to a choice of molybdenum ribs brazed to OFHC copper headers with a 70% silver - 30% copper braze alloy. The headers are gun drilled the entire length to provide a leak free coolant passage. A stainless steel expansion loop interconnects the two headers to provide a single pass flow system. Figure 2.2-1 illustrates the structural configuration of the extraction grid. Slotted shields on both ends are to prevent buckling of adjacent ribs.

Analysis of the grid cooling system based on 3.4 kW total energy dissipation on the top surface of the ribs results in the following operating characteristics.

- a. Water flow rate: 4 GPM
- b. Water inlet temperature: 70°F
- c. Water outlet temperature: 75.8°F
- d. Maximum rib temperature: 562°F (on centerline of rib)
- e. Grid longitudinal thermal expansion: 0.005 inches
- f. Grid lateral thermal expansion: 0.003 inches

It is of interest to note that radiation contributed less than 1% of the total cooling required.

While OFHC copper and aluminum were also thermally acceptable candidates for the rib material, each were rejected for other considerations. Without an elaborate braze fixture a brazed OFHC copper structure cannot retain sufficient dimensional stability required for high energy extraction grid applications. Even a single rib, twisted during brazing and placed in operation would cause failure of the entire grid structure. The twist would cause additional shadowing resulting in additional energy absorption and a proportionally larger thermal expansion of the twisted rib relative to the other ribs. Progressively more twisting and buckling would ensue until the adjoining ribs would be affected resulting in additional rib failures. The use of an aluminum grid structure was rejected to avoid use of relatively high vapor pressure materials in large surface area, high temperature applications.

The grid fasteners are positioned to permit longitudinal and lateral thermal expansion of the grid and to prevent compression loading of the ribs due to E-Gun chamber deflection caused by vacuum loads.

Figure 2.2-1. Extraction grid.

2.3 FOIL BACKUP

Thermal analysis of the foil backup plate is similar to the analysis performed on the extraction grid. Determination of the foil support grid centerline temperatures utilize the same techniques with exception of the foil support grid geometry and the higher power density.

The following structural and thermal criteria were considered for the foil backup and foil support grid design.

- a. Heat Load: 285 w/cm^2 (energy deposited at upper foil grid plane)
- b. Operational Duty Cycle: continuous
- c. Coolant: Water
- d. Grid Configuration: $1.27 \times 70.1 \text{ cm}$
- e. Rib Arrangement: 220 RIBS at 0.3175 cm centers
(RIB cross section $0.063 \times 0.475 \text{ cm}$)

As with the extraction grid analysis, an active cooling system was found necessary to permit CW operation. At this point serious consideration was given to determine an acceptable configuration of the cooling passages and the choice of materials for the foil grid/support structure complicated the routing of coolant passages.

To avoid excessive costs of manufacturing a detachable foil grid structure and the possibility of compromising E-Gun vacuum integrity by additional seals, a one piece design was proposed. A brazed assembly consisting of a foil support grid and a support structure was also considered but was found to be unnecessary as a result of additional thermal analysis.

Candidate materials for the one-piece design were OFHC copper, aluminum and molybdenum. Of these, type 6061-T6 aluminum, because of its high thermal conductivity, dimensional stability and ease of fabrication, was chosen. Proximity of the coolant channel to the foil support rib centerline maintained sufficiently low surface temperatures so that significant outgassing will not occur. A cross section of the foil backup plate is shown in Figure 2.3-1.

Figure 2.3-1. Foil backup and cooled blank-off plate.

Thermal and structural analysis indicate the following system operating characteristics based on the use of water coolant and 3.9 kW total energy dissipation on the top surface of the support ribs.

- a. Water Flow Rate: 4 GPM, each side
- b. Water inlet temperature: 70°F
- c. Water outlet temperature: 83.66°F
- d. Max rib temperature: 252.5°F (on rib centerline)
- e. Rib longitudinal expansion: 0.00078 inches
- f. Allowable compressive stress: 31,500 PSI
- g. Allowable rib buckling stress: 55,150 PSI
- h. Calculated compressive loading stress: 15,590 PSI

Of particular importance was the analysis of the rib compressive stress which if excessive would have caused rib failure due to column buckling. The results of this investigation as summarized above, indicate that compressive stresses caused by thermal expansion of the ribs is less than one-third the stress required to cause buckling. It is of interest to note that the calculations indicate a similarly designed two-centimeter foil backup plate operating at the same total power, would have buckled.

2.4 COOLED BLANK-OFF PLATE

Thermal analysis of the blank-off plate is a straightforward thermal investigation of a longitudinally channeled thin wall heat exchanger. Unfortunately the temperature effects on the structural elements of this unit must also be considered. While the blank-off plate may in itself be capable of absorbing the full output electron-beam energy, the resultant thermal distortions must detrimentally affect the delicate structure of the foil support. Since the electron beam flux will cause an upward bow of the blank-off plate, sufficient vertical clearance must be provided to avoid contact between these elements. It is to be assumed that the foil will be replaced by the cooled blank-off plate for other than short pulse operations.

The following structural and thermal criteria were considered in evaluating the cooled blank-off plate design:

- a. Heat Load: 18 kW
- b. Operational Duty Cycle: 1/2 hour
- c. Life: Limited for acceptance test only
- d. Coolant: Water

A design consisting of three coolant slots longitudinally milled in a plate was analyzed for this application. The cross sectional dimensions of each slot is 0.125 by 0.200 inches spaced 0.200 inches on center. Coolant was assumed to flow in a single pass arrangement at sufficient velocity to ensure turbulent mixing.

Thermal and structural analysis indicates the following system operating characteristics based on 18 kW total energy dissipation.

- a. Water Flow Rate: 3 GPM (total for three slots only)
- b. Water Inlet Temperature: 70°F
- c. Water Outlet Temperature: 106.4°F
- d. Max Blank-off Plate Surface Temp: 165.3°F
- e. Water Pressure Drop: 20 PSIG
- f. Max. Lateral Deflection: 0.00057 inches
- g. Max. Longitudinal Deflection (between bolts): 0.0003 inches

A cross section of the cooled blank-off plate is shown in Figure 2.3-1.

Two additional coolant slots have been incorporated in the blank-off plate flanking the three coolant slots provided for E-Beam energy absorption. Since the function of the cooled blank-off plate is to provide a method of monitoring the electron beam power, i.e.: measuring coolant temperature rise, these (extra) coolant slots perform the function of impeding heat loss by conduction. For this same reason, the structure of the blank-off plate consists of a OFHC copper insert (containing coolant slots) brazed into a stainless steel frame. The lower thermal conductivity of the stainless steel also reduces external heat loss. A more accurate indication of total power output can thus be obtained.

3.0 VACUUM DESIGN

One of the most important variables in the operation of the WIP electron gun is operating pressure. Typically, Helium is bled into its system with a constant pumping speed and adjusted to an operating pressure of one to five microns. During a run, the plasma and electron beam interact with the vacuum chamber causing its pressure to rise. At a critical point (35 μ Hg) Paschen breakdown in the high voltage region terminates the run.

To maintain a constant pressure during operation either or both the pump speed and the Helium leak rate may be varied. The rate at which the pressure rises usually cannot be determined accurately due to the many variables involved, however an estimate can be made by assuming certain ideal conditions.

The pressure region of interest is from one to 30 millitorr of Helium pressure. Here mean free path varies from 13.6 cm to 0.4 cm. Usually this region is referred to as a medium vacuum and two types of flow are applicable: molecular and Knudsen. In molecular flow the particles can move freely without mutual hindrance. The criterion is: the mean free path of a particle is greater than the diameter of the conducting tube. Knudsen flow is defined as the transition from viscous to molecular flow and it dominates at pressures where the mean free path is equal to the tube diameter. Thus in our case, pressures less than five millitorr represent molecular flow while those above this represent Knudsen flow.

The effective pump speed at the gun depends on the total conductance of the system. Valves, cold traps, tubing, etc., introduce flow resistance which results in an effective pump speed S_{eff} smaller than the pump volume flow rate. The relation between the pump speed S and S_{eff} is

$$\frac{1}{S_{\text{eff}}} = \frac{1}{S} + \frac{1}{L}$$

where L is the total conductance of the system. In addition

$$\frac{1}{L} = \frac{1}{L_1} + \frac{1}{L_2} + \dots + \frac{1}{L_n}$$

where $L_1, L_2, \dots, L_n$ and the individual values of conductance for various components. In general the conductance of a vacuum component depends greatly on the type of flow. Numerical values of conductance depend not only on pressure but also on the shape of the cross-section of the connecting piece and also whether the tube is straight or bent.

For straight tubes of diameter d

$$L = 180 \frac{d^4}{\ell} \bar{p} + 12.1 \frac{d^3}{\ell} \cdot \frac{1 + 256 \frac{d}{\ell} \cdot \bar{p}}{1 + 316 \frac{d}{\ell} \cdot \bar{p}}$$

where $\bar{p} = p_1 + p_2/2$ and ℓ = tube length - cm.

For an aperture of surface area $F(\text{cm}^2)$ and laminar flow

$$L = 76.6 \cdot \delta^{0.712} \frac{F}{1 - \delta}$$

where δ is the pressure $p_2/p_1 < 1$. For molecular flow

$$L = 11.6 F$$

The previous values must be modified for Helium. For molecular flow multiply the above by 2.64; for laminar multiply by 0.92.

The following calculations assume that a 600 liter/sec roots pump is connected to the electron gun through 4 feet of pipe 8 cm in diameter. Each pump has a vacuum port size of 65 cm².

The ultimate pressure p_e and the effective pump speed are related by

$$S_{\text{eff}} = \frac{Q}{P_e}$$

where Q is the total gas evolution in the container. Since our operation pressure ranges from 1 to 25 millitorr, we can calculate the gas evolution rates at these values. The conductance ranges from 126 to 410 liters/sec at 1 and 25 millitorr respectively. This changes the effective pump speed from 88 to 173 liters/sec. The gas evolution rate thus changes from 8.85×10^{-2} to 4.34 torr liter/sec. This corresponds to 2.85×10^{18} and 1.4×10^{20} particles per second. Assuming that no outgassing occurs, this is the range of the Helium leak rate necessary to establish a 1 to 25 millitorr operating pressure.

At an operating pressure of 10 millitorr S_{eff} equals 134 liters/sec and Q is 1.34 torr-liter/sec. When the beam comes on (sputter yield is 5) at 100 mA/cm² of ion current over 3000 cm² we introduce 1.9×10^{20} particles per second into the vacuum system. This corresponds to 1.86 torr-ℓ/sec and causes a pressure rise of 4×10^{-11} torr in 2 μsec.

Electron desorption at the oxide layer can release 10^{18} particles per 100 cm² oxide layer, which corresponds to a 3 millitorr rise in pressure. This change in pressure is especially troublesome since the particles released are impurities. Fortunately this only occurs when the system has been opened to atmosphere. The other process which releases particles from the cathode is outgassing. Outgassing requires an ion to strike a gas atom trapped in the cathode, thereby releasing it. Since most of the trapped gas atoms are Helium the amount of atoms released per second is equal to the number of incident ions per second. Thus outgassing releases $6 \cdot 10^{16}$ particles per

second into the vacuum system. This corresponds to $4 \cdot 10^{-4}$ torr-ℓ/sec and thus has little effect on the pressure. Ion beam heating of the gas in the acceleration gap may also raise the pressure, especially for a sealed-off system. The mean free path for ions is 4 cm at 10 millitorr of Helium pressure which corresponds to 25% of the ions hitting a neutral He atom once. Theoretically this results in a 2.33 millitorr He per second rise in pressure. At one millitorr this effect is negligible.

The two possible mechanisms for increasing the WIP Gun pressure have been shown to be electron desorption and ion beam heating. The electron desorption introduces impurities into the WIP gun which must be taken out of the gun during processing. The ion beam heating requires an in-flow of cold neutral Helium atoms while the hot neutral atoms are removed. Electron desorption occurs shortly after the run is started and decreases exponentially after a few milliseconds. These factors are important for CW or high repetition rate systems.

E-gun installation and support equipment