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COLLEGE OF ENGINEERING
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Final Report

DEVELOPMENTS IN INDUCTIVE ENERGY STORAGE APPARATUS AND INITIAL HIGH CURRENT, HIGH DENSITY ARC EXPERIMENTATION AND ANALYSIS

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ABSTRACT

This report is a summary of the final tasks that were required to bring a six megajoule inductive energy power supply to an operational stage and the initial phases of an experimental program in the scientific study of electric arcs in extremely dense air. Currents up to 100,000 amperes have been attained. Nitrogen at a density up to 100 times normal atmospheric density has been used as the medium for the arc.

1.0 INTRODUCTION

Two research programs have been carried on concurrently at the University of Michigan. The first program has been concerned with the development of a six megajoule inductive energy storage power supply. Tests and development work on switchgear and on fuses that make the inductive energy power supply an operational system, as well as the development of electronic gear that was explicitly designed to operate in a region of high-energy transient phenomena are reported.

The second program has utilized the power supply to study high current arcs in extremely dense air. This report will discuss results that have been obtained with a special arc chamber. These results have significance relative to improvement in understanding of principles governing the behavior of the arc and its interaction with the environment. They also have direct observational significance in regard to gross aspects of behavior of high-density arcs in arc chambers.

As to one item of direct observational significance, the arc chamber used was designed to provide extended electrode surfaces over which the arc terminals would be free to move. It will be shown that the arc did move as it was expected to, and that the erossion from the copper walls of the arc chamber was, in fact, significantly reduced.

As to value toward improving knowledge of science underlying the behavior, the experimental results obtained are of sufficient volume so that their full significance can only be realized as a result of additional analysis. Underlying elements of the theory and suggestions as to extensions that appear necessary are presented in Appendix A of this report.

2.0 EQUIPMENT

Experimentation has involved the use of a six megajoule inductive energy power supply that was installed by the University of Michigan (Ref. 1). The power supply is capable of delivering 300,000 amperes from an energy coil of 120 microhenries into a high resistance load developing a peak voltage not to exceed 20,000 volts. The coil was designed and built at the University of Michigan. The switch gear has been developed as part of the over-all research and installation program. The coil is charged by an Allis-Chalmers unipolar generator with a nominal rating of 60,000 amperes.

Electronic instrumentation has been developed in association with this program to control the switch actuation and to control the oscilloscopic recording. A complete description of this instrumentation is presented later in the report.

Standard electronic equipment has been utilized to record the voltage across the arc chamber, the induced voltage measuring the time rate of change of flux in the center of the coil, the current prior to the opening of the switch, and the pressure developed within the chamber. The pressure has been measured with KISTLER MODEL 601 transducer. Suitable instruments were available commercially for the actual display and recording of data. A 24 channel Honeywell-Heiland Visicorder was used for the frequency range from 0 to 8000 cycles per second. Whenever frequency components in excess of 8000 cycles per second were considered significant, the signal was displayed on a dualbeam Tektronix 551 oscilloscope, and photographed on Polaroid film.

2.1 ARC CHAMBER

The arc investigations here reported have been made in an arc chamber that was designed to reduce contamination of the gas by utilizing extended electrode surfaces (Ref. 2). The chamber is composed of two copper canisters which are held in place by two larger steel canisters which are in turn held in place by an external bolting mechanism. The arc is initiated between the two halves of the chamber and allowed to move freely over the electrode surfaces. The basic concept in this design was that the self-magnetic field of the high current arc would be sufficient to drive the arc rapidly over all of the internal surfaces of the chamber providing that the aspect ratio, that is, the ratio of length to diameter were correct. The aspect ratio is approximately 2 1/2 to 1, the length of the chamber being approximately 9 1/2 inches and the diameter just under 4 inches. The volume of the chamber is approximately 110 cubic inches of 1830 cubic centimeters. The chamber is fitted with a pressure transducer and a gas inlet port so that the chamber may be pre-charged to any specific density. Experiments have been made with densities that vary from 10 to 100 times normal atmospheric density.

2.2 SYSTEM GROUNDING

The power system has been grounded at the generator in order to protect the most vulnerable item, and the most difficult to re-insulate. This decision was based on the fact that the field windings of the Allis Chalmers homopolar generator at the University are insulated for 2500 Volts, and it was considered desirable to maintain the windings and controls near ground.

If the system were to have been insulated from ground, then the voltages between various portions of the bus system and adjacent grounded structures would be determined by ratios of distributed capacitances. During the arc, a potential difference of many kilovolts would exist between the bus bars. Some large fraction of this voltage would appear between any two adjacent points as between the generator case and its field windings for example. Moreover, a one-point ground fault on the "hot" side of the coil would throw full coil voltage across from generator to its field coils and ground, with possible formation of a damaging secondary breakdown at or inside of the generator, if the controls and operator are to be maintained near ground potential.

A realistic criterion for an adequate ground is merely that its impedence be small compared to the reactance of the distributed capacitances between various parts of the main loop, and earth. Calculation and measurement both gave a total capacitance to earth of approximately 0.01 microfarads for the main current system. The natural resonant frequency of the energy storage coil is about 100 kilocycles per second and the distributed capacitance across the coil is also of the order of 0.01 microfarads. Hence, for frequencies above 100 kilocycles per second, the coil is effectively bypassed.

It follows that the distributed or lumped capacitances from any part of the main current loop to earth are less than the total of 0.01 microfarads. Therefore, their minimum reactances (for the highest frequency components at 100 kc) are greater than 100 ohms. If the highest voltage expected across the coil is 10 kilovolts, then a ground of 10 ohms or less at the generator should limit the generator-to-ground voltage to a maximum of 1 kilovolt, which is within its insulation design rating.

In the University of Michigan facility an armor plate safety wall, with foundation columns below ground level, was built between the arc chamber area and the instrument area. The resistance of this wall to remote earth was measured to be

4.5 ohms. During operation, the zero-volt bus bar was grounded, at the generator terminal, to the safety wall by means of a heavy flexible cable.

A nearby point on the same steel safety wall was chosen as the grounding point for the instrumentation system. As a result, the power system and the instrumentation system have nearly an ideal common single-point ground. Furthermore a good approximation to an iso-potential electrostatic and magnetic shield has been attained between the power system and the instrumentation equipment.

2.3 SURGE PROTECTION

Detailed attention was given to the possible effects of high voltage surges on bus bar and on instrumentation shields. During the course of any one test, the load impedance or voltage level may undergo very rapid changes. Most drastic are the changes associated with the voltage breakdown in a protective over-voltage spark gap, or the initial breakdown inside of the arc chamber when using an external fuse. In addition, it was anticipated that there would be sharp fluctuations of arc impedance during the main arc period as the result of magnetic forces on the arc, establishment of new anode and cathode spots, kinks, etc.

The arc may be considered a parametric voltage generator sending steep voltage fronts back along the bus bars toward the generator. These voltage fronts would be of little concern if they were confined to the high and low voltage bus bar pair, but for such rapid changes, the bus bar system no longer behaves as a lumped-parameter circuit. The main bus bar pair constitutes one transmission line. The bus bars taken together and the ground plane constitute another transmission line or parallel-plane waveguide. Energy is coupled from the first transmission line to the second as the wave propagates away from the arc source. As a result, portions of the so called low voltage or grounded bus system will not remain grounded during a fraction of a microsecond, which could cause breakdown of low voltage insulation in the generator or instrumentation.

In general, there are three types of surge protection, non-linear resistors, shunting capacitors, and discharge gaps, which may be installed at the source or at the point of possible failure. It was most effective here to provide surge protection across the weak point to be protected. "Thyrite" non-linear resistors were placed across the generator footing insulation, and to earth at the generator, effectively protecting both the footings and the field winding insulation. The units chosen have a resistance of the order of 100,000 ohms at the normal generator output voltage of 0 to 40 volts. The "knee" of their volt-ampere characteristic occurs at 150 volts, and with 1000 volts applied, their resistance falls to 10 ohms. The surge impedance of the transmission line system carrying the transient from the arc source is of the order of 100 ohms, so that a voltage divider action takes place limiting the voltage across the protected points to one or two thousand volts for a maximum initial transient amplitude of about 10,000 volts. The response time of the thyrite disks is short compared to 50 milli-microseconds. Recovery time is similarly short. In addition, the flat thyrite disks have a capacitance of a few thousand micro-micro farads, which acts with the surge impedance of the line to further attenuate the spike, while the thyrite material absorbs the energy of the transients, which prevents ringing in resonance with lead inductance. The thyrite bodies have a thermal capacity sufficient to absorb the energy in the spike transients without significant heating.

Instrumentation lead pairs were similarly protected at their point of entry through the safety wall. Smaller thyrite disks, having a knee at 50 volts, were connected from each signal lead to ground inside separate shield boxes for each pair. Non-inductive 10 ohm wire-wound resistors were placed in series with each lead on the primary-system side of the thyrites, so that over-voltage protection for the instruments extends from the sub-microsecond transient frequencies down to D.C.

2.4 INSTRUMENTATION INSTALLATION

Isolation of signal channels was complicated by the fact that the D.C. value of several variables is desired, and this required conductive connections to the power system. Since these D.C. signal channels did not have one point of connection to the power system, they could not be permitted to have a common connection at the recorder. If there were a common connection at the recorder, loop currents would flow which would introduce interaction between channels. Thus multiple channel data recording systems, such as the Honeywell Visicorder or Consolidated equipment, are provided with floating preamplifiers and galvanometers, so that conductive isolation can be maintained at the recording end of the system even if it cannot be at the transducer end. Inputs to the dual channel oscilloscope had to be selected with particular care, since one side of each input was common to the chassis of the scope.

In general, signal channels were kept isolated from source through to the recording element. Signals were carried on twisted pair, braid shielded cable with insulation outside the shields to isolate the shields from each other, except that triaxial cable was used in preference to twisted pair for the low level signal from the lower-boom I-R drop pickup. The cable was placed in steel raceway for further shielding and mechanical protection. The place where all instrument and control cables come through the steel plate safety wall and ground plane was chosen as the point for grounding the shields in order to insure that accidental high voltage on an instrument cable shield would be stopped at the safety wall. High voltage protection was provided on all signal leads at this same point of entry through the wall. Also, high frequency noise on the outside of the shields are grounded out at the wall.

The cable shields continue beyond the grounding point at the wall to the Visicorder pre-amplifier. The shields are not connected at the preamplifier end in order to avoid inductive pickup loops. However, the preamplifier and Visicorder cases are grounded back to the same instrument ground point at the wall by a separate ground lead. No portion of the shielding is grounded at more than one point.

Transient disturbances from the generator field relays, the main arc, and outside sources of interference can be distributed on the 117 volt A.C. supply lines. Their entry into the recording instruments is minimized by the use of four individual isolation transformers for the Visicorder-preamp combination, the oscilloscope, the Kistler preamplifier and the control circuit cabinet. These transformers have electrostatic shielding between primary and secondary. The shields, shells and cores of the transformer are grounded to the safety wall.

2.4.1 High Voltage Attenuator

The high voltage attenuator probe has been designed for use not only with the arc chamber, but also for surge tests. The high voltage end was designed and tested to 30 kilovolts. Attenuation ratios of roughly 100 to 1 or 1000 to 1 may be obtained. In order to obtain smooth, extended high frequency response in the attenuator, the capacitive reactance ratio is the same as the resistance ratio including effect of the capacitance of the connecting cable between the probe and the oscilloscope. To extend to frequencies for which the line is an appreciable fraction of a wavelength, the characteristic impedance of the line and the possibility of reflections were considered. In order to avoid reflections, the far end of the cable is terminated by a resistance equal to its characteristic impedance of 40 ohms. Rise time to 90% of full amplitude is about 30 millimicroseconds with negligible overshoot or ringing.

A refinement of the attenuator system was made to protect against common mode as well as line to line over-voltages and to permit varying lengths of cable to be used beyond the protection box depending upon instrument location and setup, since changing cable length would upset the capacitance balance. The protection circuit was designed in the form of an H-pad attenuator with the input and output impedances both matched to 40 ohms to prevent reflection. Back-to-back, 50 volt, 10 watt, silicon zener diodes were connected from each end of the center arm of the H pad to ground to perform the task of the Thyrites. The H pad was designed for an attenuation ratio of 5 to 1. This ratio could be adjusted somewhat by varying the center arm resistance without seriously upsetting the match at the input and output. Since it is normally desirable to ground the oscilloscope case, the protection circuit can be switched from a 40 ohm H-pad to a 40 ohm T-pad, offering the same protection and isolation features, while grounding the oscilloscope through to the grounded bus.

2.4.2 Current Measurement

The instantaneous current can be determined in three ways. The standard method of measuring current is to measure the voltage drop along a section of the bus bar with a known resistance. The second approach is to measure the magnitude of the magnetic field associated with the current. The third way is to measure the voltage induced by the time rate of change of the magnetic field and subsequently integrate the signal to determine the current. The resistive drop is the most accurate and reproducible measurement of the current prior to the opening of the transfer switch when there are no transients. The magnetic measurements will yield better results during the period of transients.

Potential connections for I-R drop were made along the central axis of the lower boom which leads to the transfer switch. The potential lead runs inside the metal of the boom. The resulting signal is carried to a protection box in triaxial cable to further reduce interference.

This connection was not intended to give the true current variation during the arc. With these large bus bars, the magnetic energy stored within the bus bar volume, due to flux linking the central current filaments, causes forward current to keep flowing along the center of the bus bar and a reverse skin current component to flow along the outer edges. The skin current has a time constant of about 0.1 seconds, so that it persists after the net current through the external circuit and the arc has gone to zero.

The second method of current measurement is the use of a field-sensitive probe such as a Hall Effect transducer or magnetoresistor. These probes usually require calibration in place against some direct measurement such as I-R drop. Ideal placement for such a probe would be in the center of the space between close spaced, parallel plane conductors or in the center of the coil. It is planned to add a Hall probe to the existing instrumentation.

The third method of obtaining the transient current, by integration of a magnetically induced voltage, was incorporated in the design of the main energy storage coil. Shielded leads were brought from a pickup loop which was mounted in the center of the storage coil through a simple attenuator and a protection box to one of the visicorder channels. A coupling coefficient was determined, and graphical integration of the trace has been used to calculate instantaneous current during the arc transient, and the power rate and total energy as functions of time. Desirable future additions to the equipment would be electronic analog multipliers and integrators. Then, current, power and cumulative energy could be plotted simultaneously with the voltage on the Visicorder record.

2.4.3 Pressure Gage Isolation

Chamber pressure instrumentation, consisting of a Kistler piezoelectric gage with 30,000 psi adapter and a Kistler charge preamplifier, was conductively connected to the ground bus side of the arc chamber so that it was necessary to bring the pressure signal leads through a protection box. The crystal pressure pickup cannot tolerate loads of less than several megohms so the charge preamplifier was located just ahead of the protection box instead of after it. The preamplifier was equipped with a small isolation transformer in the line supplying the preamplifier. The output impedance of the preamplifier is low enough so that the thyrites do not load it down.

3.0 SWITCHING AN INDUCTIVE SYSTEM

During the coil-charging operation current from the generator passes through a switch which, being in the closed position, causes the current to by-pass the arc chamber. This piece of switch gear is called the transfer switch. When the coil current reaches the value chosen for a particular test operation, the transfer switch is triggered to open with extreme rapidity, thus causing the current to be transferred from this switch to a parallel path through a fuse, which may be either inside or outside the arc chamber.

3.1 THEORETICAL CONSIDERATIONS FOR THE TRANSFER SWITCH

3.1.1 Dielectric Recovery vs. Voltage Recovery

When an inductive energy system is employed using a fuse, the fuse design is correlated with the current to be used so as to provide some specified time interval between cessation of current in the transfer switch and initiation of the arc. This time interval must be long enough to permit the transfer switch to recover dielectric strength adequate to withstanding without restrike the arc voltage which also appears across the open transfer switch. Thus there exists here, as in nearly all switching operations, a "race" between voltage recovery and dielectric recovery. In this electrical system dielectric recovery is given a "head start" to the extent of the dielectric recovery interval designed into the fuse. Furthermore, the extent of the voltage recovery is governed entirely by the arc voltage in the arc chamber. This has not been observed to exceed a few thousand volts in hot-shot practice generally. For the Michigan experimental apparatus it has not yet exceeded 2500 volts for internal fuse operation, and is not expected to exceed 4000 volts within the range of currents planned for the present arc chamber. With an external fuse this voltage has reached 8000 volts, and is not expected to be higher than this at larger currents.

3.1.2 Factors Governing Rate of Dielectric Strength Recovery

Ordinarily unionized air is an excellent dielectric. Clearly, if by the time the arc-chamber arc is initiated the region between transfer switch electrodes has become such ordinary air, it is a simple matter to provide the switch-blade spacing adequate to prevent restrike at voltages even very much higher than appear likely to occur. However, if substantial residual ionization exists between transfer switch electrodes when the arc-chamber arc initiates, there is likelihood of a restrike.

Thus there are certain quite obvious types of design attributes for a transfer switch and associated circuitry, to permit it to prevent restrike as follows:

a) The energy dissipated in the transfer switch arc should be small, in order to minimize the intensity and extent of the ionization in the switch at the beginning of the dielectric recovery period.

b) Electrode separation should be very rapid in order to provide as much distance and therefore, as weak an electric field as possible, when voltage reappears. This implies the use of very substantial electrode-moving forces, as the required acceleration is quite high.

c) The air containing the residual ionization should be removed as rapidly as possible from the nearest-distance inter-electrode spacing, as for example by an air blast.

Appendix B deals with means for minimizing the flash intensity, as called for by (a) above. Items (b) and (c) deal with engineering design and experience matters discussed below in relation to means used to achieve successful transfer switch performance.

3.1.3 Mechanism of Dielectric Failure in an Ionized Gas

If restrike occurs because of the presence of residual ionization between the electrodes, the detail physical electronics of the restrike process might be either quite similar to or quite different from the mechanisms of "voltage breakdown" in un-ionized air. This question has not been extensively studied, and such information as is available deals primarily with empirical evidence rather than with theory.

The before-extinction passage of a large current between transfer switch electrodes occurs, of course, by means of a very intense atmospheric-pressure plasma, which is highly conducting, and carries the current at quite low voltages. This plasma decays rapidly as soon as current flow through it stops on completion of the current transfer. By the end of the recovery period allowed by the system, and in spite of air blast, there may exist between the switch electrodes, or perhaps along a non-direct and blast-blown path from one to the other, air that is still extremely hot. While the switch arc was being sustained by current flow, because it plays at atmospheric pressure, presumably both the extremely hot current-carrying plasma and its immediate environmental air, somewhat less hot, were each internally in thermal equilibrium. It is reasonable to suppose that in the very rapid cooling of the process each local region is as before internally in thermal equilibrium.

Experience with magneto-hydrodynamic apparatus has shown there are three quite different conductivity regimes of hot gas behavior. Above some such temperature as 2500°K, air has reasonably good volume electrical conductivity; below perhaps 2000°K its conductivity is of a rather low-grade "semiconductor" nature, yet easily measureable, and perhaps comparable with that in combustion flames; at still lower temperature, air becomes an excellent insulator. Certainly by the time the air in the switch has cooled to this third regime, danger of restrike has passed.

Re-establishment of a transfer switch arc due to appearance across the switch of a few thousand volts before its air is a good insulator, would probably occur through the partially cooled "semiconducting" air by mechanisms very different from those applying when there is high volume conductivity. It seems likely that for the semiconducting regime "voltage breakdown" occurs by mechanisms very similar to those applying for cold air, but with differing magnitudes because of the presence of the ions. Probably in the semiconducting regime the negatively charged particles are primarily negative ions rather than electrons. Thus one would expect, for semi-conducting-air restrike, a localization of initial electron emission following roughly the pattern that occurs in cold air, which tends to favor arc initiation to points where electrode radius of curvature is small. Electronic emission current density is presumably high at arc initiation and remains so.

If voltage appears across the switch while the air still has good volume conductivity, due to presence of substantial electron density, the body of the gas will presumably remain essentially an equipotential at anode potential, thus providing a substantial voltage drop adjacent to the cathode. This should initiate a low-density electron emission over the cathode surface, comparable to that in a glow discharge, and with increasing voltage the current density should compel transitions to the arc type of cathode.

These somewhat speculative comments are made here primarily to call attention to the probability of a very real difference between mechanisms of restrike while the gas is still extremely hot and those after it has cooled into the region of relatively poor conductivity.

3.1.4 Recovery Voltage

In the experiments here reported the "voltage recovery" takes place in a totally different manner from that occurring in large power utility circuit-breakers. This voltage recovery discussion is, therefore, unique to the present type of system.

In the present apparatus the following itemized behavior aspects appear:

a) <u>A minimum voltage recovery interval is determined by the experimenter</u>. Until the fuse melts, the voltage across the arc chamber, fuse, and switch, all in parallel, remains at a low value, from perhaps 10 volts to 30 volts, depending on the resistance of the fuse and of any isolating series resistor used. The experimenter governs this "minimum recovery time" when choosing the fuse section.

b) <u>Rapid voltage rise to a limited value when the fuse melts</u>. Experiments here and elsewhere have shown that, once the internal fuse melts, initiating the arc, the voltage across the arc-chamber arc initially rises very rapidly, in a matter of one or two milliseconds, (sometimes in less than a millisecond) to a value ranging from 600 volts to perhaps 2000 volts. After that the further rise is gradual, reaching a maximum of from 1500 to 2500 volts in experiments here reported. At higher currents it may reach 4000 volts after a rapid initial rise to perhaps 2500 volts. This arc voltage pattern is in fact the "voltage" recovery" pattern for the transfer switch. Thus in the present apparatus the transfer switch voltage recovery is governed by the arc chamber arc, not by the external circuit. In a power system circuit-breaker, voltage recovery is governed by the external circuit.

c) <u>No evidence exists for very high transient voltage "spikes"</u>. In earlier phases of the present work various semi-analytical considerations led to the presumption that there might appear, at the moment of arc-chamber arc initiation, extremely short-duration transient voltages that might reach 10,000 to 20,000 volts, and last a few microseconds or less. The transfer switch design work was carried on under the presumption that it might be necessary for the transfer switch to withstand such "voltage spikes" as a very brief but hazardous portion of the voltage recovery. In the experiments conducted so far there has appeared no evidence of extreme voltage spikes, nor are there firm reports of their appearance on comparable apparatus. Apparently this problem has not in fact appeared.

d) Malfunctions may cause early voltage recovery. If the fuse separates early for mechanical or magnetic-force reasons, the arc-chamber arc initiates early, with a resulting rapid voltage recovery well before the end of the design "minimum recovery interval."

Overall, the voltage recovery pattern is straightforward, easily described and unique to this type of apparatus.

3.1.5 Contrasts with Duty Required of Commercial Circuit Breakers

The requirements that must be met by the transfer switch are in complete contrast to those that must be met by a circuit breaker in an electric power utility system. Certain pertinent details of this contrast will be itemized here.

a) Relationship of energy dissipation in the transfer switch arc to loop inductance. In either system, the energy dissipated by the arc in the switch is the energy stored in the inductance of the circuit loop that is interrupted when the switch opens. In a hot-shot system this loop consists of the transfer switch, the fuse in the arc chamber, and the electrical connections between them. In an electric utility system it consists chiefly of the portion of the transmission or distribution circuit between the circuit-breaker and the short-circuit fault on the line that causes short-circuit current to flow. The stored energy is proportional to the inductance of the loop and to the square of the current flowing in it at initiation of the circuit-opening process.

b) Duality; low switch arc energy provided by low hot-shot transfer switch loop inductance, but by high power system circuit-breaker loop inductance. There appears here a duality type of electrical system contrast. A constant-current generator is the dual of a constant-potential generator. An electric power utility system is of the constant-voltage type. The designer and operator determine the voltage on the basis of system needs, and variable local parameters of the circuit govern the current. In such a constant-voltage system, a large loop inductance results in a small current and therefore in small energy dissipation at a circuit-breaker opening. In contrast to this, in relation to transfer switch operation, an inductive energy system is of the constant-current type. The designer and operator specify the current on the basis of experimental needs, and local circuit parameters determine the voltage. In this constant-current system, a small loop inductance is necessary if the energy dissipation in the transfer switch arc is to be kept small. Another aspect of the duality contrast appears in observing that a power utility circuit breaker opens into an open circuit, whereas the transfer switch opens into a short circuit, which imposes a far less severe duty requirement.

c) Low transfer switch inductance a requirement in a hot-shot system, but of no value in a utility system. In either type of system the switch is part of the local loop whose stored energy is dissipated in the switch. As the hot-shot system requires a small total loop inductance, it is important to keep to a low value the contribution the transfer switch makes to the loop inductance. In a utility system a large loop inductance is desirable, therefore existence of substantial inductance in a circuit breaker has no harmful effect on system performance, and may in fact be beneficial. As a consequence, commercially available circuit breakers exhibit substantial inductance values.

d) Hot-shot loop circuit inductance is at the designer's disposal. The circuit loop involved in the current transfer exists locally in the neighborhood of the arc chamber and the transfer switch. This makes it possible for the designer to choose materials and configuration for this circuit loop that will make its inductance extremely small, without prejudicing in any way the operation of the system as a whole. In contrast to this, in an electric utility system this loop inductance is a property of the transmission system in which the circuit-breaker is to be used.

e) Hot-shot switch always opens with current flowing, whereas power system circuit breakers must be able to operate at no load. The obvious consequence is that the hot-shot switch can use magnetic forces to assist in opening.

3.1.6 Decision to Build Rather than Buy a Transfer Switch.

It is clear from the preceding section's content that if a commercial circuit breaker were to be used to serve the transfer switch function, the circuit breaker's own inductance would establish the lower limit to the local loop inductance. This would thereby establish a least value of energy dissipation in the circuit breaker's own arc. Thus the circuit breaker would have to be rugged to enable it to dissipate in its own arc the energy that appeared there only because the circuit breaker, being physically large, had necessarily substantial inductance. A smaller switch could be less rugged because it was smaller. Clearly an alternative to the large high-inductance commercial circuit breakers would be to use a transfer switch having an inductance small enough to permit reducing to a low value the energy dissipated in the transfer switch flash. Because no commercial demand exists for low-inductance, high-current switches, no such switches are commercially available, and it was decided to design the transfer switch as part of the installation of the power supply.

3.2 DEVELOPMENT OF THE TRANSFER SWITCH

Experience from previous work with inductive energy storage coils played an important role in the design of the fast acting current transfer switch and a fuse system. The largest current previously used was 5,000 amperes which was so much less than the 100,000 to 300,000 amperes that would be used in the new system, that a new design in switching apparatus had to be developed.

The transfer switch had to meet three basic requirements with a fourth design consideration:

1) The switch had to be small to be fast acting and yet physically large enough to give sufficiently low contact resistance so that it would not melt during the charging cycle;

2) It had to be able to develop enough voltage to transfer the current into a parallel load;

3) It had to develop substantial dielectric strength within some reasonable time after the current transferred into the fuse element in order to withstand the voltage that appears across the switch when the fuse melts.

4) It should be designed so that in case of a failure, with subsequent large energy dissipation in the switch, the necessary repairs require only a short time, without excessive cost for materials.

Several types of fast acting transfer switches were considered, with a minimum mass in order to attain the fastest possible action. The maximum surface area for minimum mass in the switch contact arm is attained in a knife switch which utilizes the surface area on both sides of the blade to reduce the contact resistance and current density. The Detroit Edison Company donated, from their warehouse supply, 24 knife switches to this program. Each switch was rated at 2400 amperes and had been used on 600 volt d.c. street railway service in the City of Detroit. The transfer switch is a modification of one of these switches.

Each of the requirements was tested prior to the final design of the switch. The test instrument was the transformer coil in the Plasma Engineering Laboratory that was built in 1957 (Ref. 3). This coil has a primary winding of 117 turns with a two turn secondary, which yields after losses a current ratio of approximately 50:1. The primary is operated at up to 5000 amperes and 40,000 volts during the transient. The secondary rating is 250,000 amperes at 800 volts.

The effect of joule heating in the switch is easily calculated and, in the absence of the coil, calculations would have been considered satisfactory for design. However, it was a relatively simple task to connect 480 volts a.c. directly across the primary of the coil, giving a primary current of 500 amperes and a secondary current of 25,000 amperes. The knife switch, which normally has three blades, each three inches wide, was fitted with a single blade two inches wide. The blade carried 25,000 amperes rms for 2.2 seconds, which corresponded to a normal charging cycle at 150,000 amperes peak current. There were no adverse or unexpected events.

The voltage developed by the transfer switch was measured on the secondary of the coil at 100,000 amperes. The switch opened into a load of 160 micro-ohms and 0.5 microhenry in approximately one millisecond developing about 60 volts. This switch opened due to the self-magnetic force field alone in nine milliseconds, there being no externally applied opening force.

A switch was specially constructed at this time to perform the next two series of tests. It utilized the fingers from one of the heavy switches, but used a smaller blade since the current was relatively low. The switch was actuated by an air cylinder which was preloaded and trip-released by a toggle brace. An air blast was directed towards the fingers to clear the arc away from the copper contacts and increase the voltage on the air column. The switch was tested first at very low current levels and subsequently at 5000 amperes.

The transfer switch was intentionally allowed to arc over in order to determine the magnitude of the voltage in the switch, and to gain insight into the amount of damage that could be expected if the switch should arc over when used with the large coil of the inductive storage power supply. The voltage trace shown in Figure 1B was recorded during the switch arc. For comparison Figure 1A shows normal switch operation with the current transferring rapidly into the coil. It was determined that the switch would develop nearly 1000 volts and since most of the voltage appeared across the arc column, relatively little damage was done to the switch fingers and blade.

A series of switch tests were made toward determining the rate of recovery of the dielectric strength once the current goes to zero in the switch. The dielectric voltage recovery tests were made on the primary of the coil at currents of 5000 amps. The current was transferred from the switch to a fuse which after a short time would break and apply the high voltage across the switch. The fuses which were used could develop at least 50,000 volts, but a voltage of 15,000 to 20,000 volts was considered adequate. A ball gap was used to limit the voltage and dissipate the energy after breakdown. The size of the fuse controlled the time lag between zero current and high voltage on the switch. An upper limit of one millisecond, and probably less, was established as the time for the switch to recover sufficient dielectric strength to withstand 15,000 volts after transferring 4500 amperes. Several attempts were made to shorten the hold time of the fuse, but smaller fuses have a higher resistance and the switching would not be complete by the time the fuse blew up, resulting in continuing arc in the switch blades without a current-zero. Knowledge obtained during this series of tests led to confidence that a knife switch with a suitable actuating mechanism could reasonably be expected to operate at high currents on the large coil power supply.

The switch which was subsequently installed on the six megajoule coil is shown in Figure 2. This system uses an air cylinder both to accelerate and decelerate the switch blade. The air cylinder is pressurized prior to use, and the force is released by a squib-actuated toggle. An air blast is directed at the point of last contact between blades and fingers. The air for the blast is stored in a 30 gallon air tank at 100 psi. The blast is started 0.2 seconds prior to the switch actuation. The air flow is limited by the seven orifices in the air-blast "nozzle". These orifices have a total cross sectional area of one-half square inch. The pressure in the tank drops to 60 psi in one second during this flow.

The initial tests on the switch were made at 30,000 amperes. The current was transferred into a load of 250 micro-ohms on the first test. The load was increased on subsequent tests to 1500 micro-ohms. The maximum voltage was attained when the switch transferred 38,000 amperes into 1500 micro-ohms with a voltage of 56 volts. The inductance associated with the switching losses was only 0.1 to 0.2 microhenries, and was associated almost entirely with the switch blade. The inductive voltages were, therefore, quite small.

This switch was used successfully at currents up to 100,000 amperes in the arc chamber test operations which followed. Data shows that the switch has regularly cleared in 1.0 to 1.5 milliseconds.

One aspect of this switch mechanism is not considered to be completely satisfactory. When the switch opens, it swings to the farthest point, being stopped by the air pressure in the cylinder. It then swings back and recloses about 0.08 seconds after it originally opened and begins to recharge the coil. It then reopens, but this time much more slowly. The second opening probably causes about half of the total damage to the switch blades, but otherwise is not important. Inasmuch as only one set of blades has been used for the entire program it has not been worth the effort to modify the switch. Eventually, the switch will be modified so that it will not reclose. It might be noted at this point that consideration previously had been given to designing a switch system which would utilize a reclosing operation in the main switch to limit the duration of the arc. However, the electrical characteristics of the two operations were not sufficiently similar to make it practical.

The transfer switch operated effectively until the middle of September. An electrical connection in the air control solenoid circuit was broken inadvertently prior to a test at 100,000 amperes on 20 September. Since the switch opened and cleared before the fuse broke, it appeared to be a normal experiment. Subsequent examination of the data infers that switching action in this operation without an air blast was very slow, requiring four milliseconds to clear, and the fuse held only for one additional millisecond.

On the next test on 21 September, also at 100,000 amperes and without the air blast, the switch did not clear before the fuse broke. The entire 600,000 joules stored in the coil went into the switch arc. While this was not planned, it did nevertheless produce much valuable information. The voltage across the switch is shown in Figure 3. The current was building up in the fuse for about six milliseconds prior to the fuse break. When the current transferred back into the switch, the switch voltage went up to approximately 350 volts within two milliseconds, and then gradually rose to over 1000 volts which was slightly above the voltage measured at 5000 amperes with an air blast on the switch on the transformer coil. The reason for this is probably associated with the magnetic fields from the high current. The switch is designed so that the magnetic fields assist in the switching, (Ref. 4) and on the previous test, it was shown that the magnetic field is marginally capable of generating enough voltage to complete the switching operation in five milliseconds.

The actual damage to the switch was quite minor. The switch fingers and some of the insulation around the switch was damaged. Figure 4 shows the switch shortly after the arc. The air cylinder, is connected electrically to the switch blade and could have carried the full arc current, but either did not carry the full current or else the current density was quite low, because it suffered virtually no damage. The cathode of the arc was anchored to the switch fingers. The two outside fingers, as shown in Figure 5A. show quite graphically the pinch effect associated with large currents. The blades as shown in Figure 5B were not seriously eroded because the arc stayed on the heavy copper end block for the most part.

3.3 USE OF FUSES

As applied to arc chamber technology, there are two designations of fuses, the internal fuse which is inside the arc chamber and part of the chamber design, and the external fuse which is outside the chamber and may be designed independently of the chamber. Both perform the same task of allowing the switch to clear. The internal fuse breaks, and simultaneously initiates the arc and causes the high voltage to appear in the same physical location. The external fuse outside of the chamber causes the high voltage but does not directly initiate the arc within the chamber. Gas breakdown within the chamber can be most conveniently accomplished in dense gas by using a small wire inside the chamber which will explode. The initiating wire would normally have only about 2% of the cross sectional area of the internal fuse.

The techniques of using an external fuse to switch a coil were initially developed some time ago in order to switch the current from the primary to the secondary of the transformer coil. Fuses were used for currents in the range of 1000 to 5000 amperes and were required to develop up to 50,000 volts. The fuse was an oil filled cylindrical tube of glass reinforced plastic, 15 inches long with a bore not exceeding one fourth inch. The fuse was a piece of copper wire varying in size from #22 to #18 depending on the current, and about 18" long. The oil fuse developed the high voltage in a very short time. A fuse that held for two milliseconds would develop 40,000 volts in 0.2 to 0.3 milliseconds, developing up to 50,000 psi.

3.3.1 External Fuse Design

During the initial design of the 6 megajoule energy-storage power supply it was assumed that an external fuse could be used to accomplish the switching, although the exact form of the fuse was to be designed later. Data that was then available (Ref. 5) together with a short investigation of fuse characteristics indicated the following statements were empirically true for fast-acting fuses:

1) The length of time the fuse will hold is inversely proportional to the square of the current density;

2) The length of time required to be destroyed (the blow-up time) is proportional to time the fuse carried the current;

3) The shape of the voltage trace from the time the voltage starts to rise is approximately exponential;

4) The energy that is dissipated in the fuse is proportional to the maximum voltage, to the initial current, and to the time that the fuse takes to blow.

The first statement leads to, and is supported through exploding wire technology, (Ref. 6) by the expression:

$$\int J^2 \, dt = \text{Constant} \qquad (1)$$

Since the current is generally constant in time in this work:

$$J^2 t_h = 3.5 \times 10^{10} \text{ sec-amp}^2/\text{in}^4$$

where

J = current density,

t_h = time the fuse holds.

This expression has been found to be correct within 10% throughout all of the fuse work that has been done.

The second statement can be expressed as

$$t_h = K_1 t_b \qquad (2)$$

where

t_b = time from the start of the voltage rise to the maximum voltage.

This statement is a first approximation, with the value of K_1 varying from 10 for a two to four millisecond fuse to 15 for a much slower 20 or 30 millisecond fuse. The number will only be valid, however, as long as the blow-up time is short so that confinement of the arc can be maintained.

The third statement expressed mathematically is

$$v = V \exp (t/\tau) \qquad (3a)$$

where

v = the time-varying voltage across the fuse
V and τ are constants of the operation

The constant V may be interpreted as the voltage drop across the fuse wire at melting temperature, but the time constant has no simple physical meaning. The voltage rises to many times the initial voltage, typically from 50 to 300 times the voltage at the beginning of the fuse action, and this infers that an interval of from four to six times τ is required to develop the voltage. There exists then one approximate empirical relationship that

$$t_b \cong K_2 \tau \qquad (3b)$$

where K_2 has been found experimentally to have a value of about five for the type of fuse that was employed in the present project.

Finally, the last statement may be written as

$$W_f = K_3 V_{max} I_o t_b \qquad (4a)$$

$$= K_4 V_{max} I_o t_h \qquad (4b)$$

where I_o is the initial current in the fuse, and W_f is the total energy dissipated in the fuse, since there is the ten to one ratio between the t_h and t_b by Equation (2). The value of the constant K_4 is approximately .05 for the fast acting oil filled fuses that have been used, but is subject to a variation of $\pm$ 20%.

These equations can be used first to gain an insight into the feasibility of using fuses in the operational system, and, second, can be used quantitatively in the design of a fuse. The first conclusion to be drawn, specifically from Equation (4), is that for any system the fuse time must be held to a minimum and, therefore, the transfer switch must open, clear, and develop dielectric strength in the shortest possible time. The transfer switch on the transformer coil was designed to operate in two milliseconds at 5000 amperes and the transfer switch on the large coil used in the present project has operated in four milliseconds at 100,000 amperes in order to comply with this criterion.

It should be noted, from Equation (4) that the energy dissipated in the fuse is proportional to the current. The stored energy in the coil is proportional to the square of the current. The fraction of the energy lost varies inversely as the current. Thus if an external fuse can be constructed which will use one third of the energy at 50,000 amperes, it should be possible to build a fuse which uses only one sixth of the energy at 100,000 amperes, and only one eighteenth, or approximately 5% of the energy at 300,000 amperes, providing that the time and voltage conditions remain unchanged.

The necessity for fast acting switches is further indicated by Equation (2) which shows that it is not possible to build fuses which will hold for a long time but blow in a short time, unless something can be done which will significantly alter the shape and correspondingly the rate of voltage rise of the fuse for the voltage as given in Equation (3). More will be said about the possibility of attaining this later.

If Equation (4) indicates a fast switch requirement, and inertial effects place a limit on the switch motion, then a compromise must be made. As examples, for 5000 amperes, 40,000 volts, a hold time of 2 ms, the calculated energy lost in the fuse is 20 kilojoules. The transformer coil stores 200 kilojoules at 5000 amperes so that the fuse energy is 10%. A fuse which holds for 5 ms would consume perhaps 25% of the energy. It would then be feasible to consider using a 5 ms fuse, but a shorter fuse time would be highly desirable.

On the 6 megajoule coil a test was made to prove the feasibility of an external fuse as a switching device. The actual conditions in the test were 8,000 volts, 42,000 amperes, and 4 ms hold time which calculated out to 67,000 joules into the fuse out of an original stored energy of 105,000. An integration of the voltage trace indicated 25,000 amperes and 40,000 joules remained in the circuit and was dissipated in the arc chamber after the fuse went out, which was excellent correlation.

From these two examples it becomes apparent that for the various inductive systems that are presently in existence, the switching must be accomplished in a very few milliseconds in order to use an external fuse. An external fuse system should not even be considered for use in conjunction with a switch that requires in excess of 10 milliseconds to clear.

3.3.2 Fuse Fillers

Oil filled fuses have been used extensively to accomplish the switching of the transformer type of coil. However, it was deemed worthwhile to investigate alternate fuse fillers. Fuses have been used for some time and have reached a high degree of reliability. The most common fillers in standard fuses are quartz, or sand of fairly fine size, and boric acid. Boric acid is normally considered to be an effective filler because the arc inside of the fuse will break down the crystal structure and release the water of hydration. Quartz, or sand, apparently merely acts as an extended surface heat sink which causes a relatively high voltage. Extensive work on both types of fuse fillers has been reported (Ref. 5)

A series of tests were made with the transformer coil using a fuse that was filled with boric acid. Both wire and copper tape were used as fuse elements. The rate of voltage rise in these tests was considerably less than the rate of rise using an oil filled fuse.

Another series of tests was made to investigate the possibility of using a sand or quartz filled fuse. A commercial 2500 volt, 100 amp fuse was procurred and was tested for this application. The fuse contained about 30 very fine estimated No. 26 silver wires in parallel. The wires were embedded in a fine clean white sand. The fuse was tested at 3500 amperes and was found to hold for approximately 20 milliseconds with the rate of rise being quite rapid, about 3 milliseconds, to a voltage of about 10 kv. The fuse was dismantled and rebuilt using 10 No. 26 copper wires, which gives a total cross section that is slightly larger than one No. 18 wire. The fuse was then inserted in the circuit and tested. The voltage rise on this test was quite similar to that obtained with the original fuse wires. At this point it appeared that it might be feasible to utilize a sand filled fuse instead of the oil filled fuse even though the rate of voltage rise was not quite as fast.

Further experimentation with the sand filled fuses indicated that the peak voltages that were attainable for this type of fuse were considerably less than the voltage attainable with an oil filled fuse for the same length. Typically, a sand filled fuse about 2 feet long would develop from 8-10 kilovolts. A similar oil filled fuse could be expected to generate nearly 100 kilovolts. The length of the fuse would need to be quite long, and the number of parallel wires seemed to be excessive. In order to obtain the required voltage it was finally concluded that it would be most advisable to utilize the oil filled fuse for initial tests. However, if the blast flame from the oil filled fuse should pose a particular hazard it will be possible in the future to perform tests directly on the large coil and determine the feasibility of using a sand filled fuse.

3.3.3 Staged Fuses

The oil filled fuse has a large capacity to recover its dielectric strength as soon as the arc is extinguished within the fuse. According to Equation (4) above the energy dissipated in the fuse is directly proportional to the maximum voltage and the time the fuse has to hold. If either factor can be significantly reduced, the energy dissipated in that fuse will be correspondingly reduced. The feasibility of using a system of fuses and ball gaps was investigated. The transfer switch was used to transfer the current into a fairly large fuse, that is, one that had a long hold time. When this fuse blew it generated a voltage only sufficient to break down a low voltage ball gap. The current then transferred into a fuse with a much shorter hold time. The first fuse would recover its dielectric strength while the second fuse carried the current. When the second fuse blew it could cause the required high voltage. As the hold time was only that necessary for the oil filled fuse to recover its dielectric strength, not the time required for the transfer switch to recover its dielectric strength, the total energy dissipated in the two fuses could be made less than the energy required to accomplish the switching using one fuse. The voltage trace from an experiment utilizing two fuses is shown in Figure 6A. The first fuse was a boric acid filled fuse and the second was an oil filled fuse. The voltage generated by the first fuse is probably not in excess of 2 kilovolts and it recovered its dielectric strength in about 4 1/2 milliseconds to sufficient extent to withstand a voltage of approximately 12 kilovolts.

A similar experiment was performed utilizing two oil filled fuses at 5000 amperes with the second fuse holding only for approximately 0.8 millisecond. Even though the first fuse only generated approximately 2 kv, it withstood an estimated 15 kilovolts 0.8 of a millisecond later. Thus, it was shown that there is an alternative method to accomplish the switching at high currents and high voltages that does not consume as much energy as indicated in the equations above if a system of fuses is used instead of one single fuse.

4.0 DESIGN OF CONTROL CIRCUITS

Two characteristics of the experimental system made necessary the development and fabrication of some specialized control circuitry. The first of these characteristics was the need to time, to the millisecond, various switching operations, and to synchronize the start of oscilloscope sweeps so that pre-selected portions of the transient might be spread out for detailed examination. The second system characteristic that required specialized circuit consideration was the presence of stray electric and magnetic fields of high intensity and broad frequency spectrum. These stray fields were generated primarily by switching transients and arc fluctuations in the main current loop, but transients were also produced by auxiliary equipment such as generator field control relays, etc. Under these conditions, special care had to be taken to prevent false triggering of the switching controls, or the introduction of artifacts into the recorder traces.

Early in the design of the main current system, it was decided to actuate the transfer switch and the tail chopping switch pneumatically. Air pressure was to be built up in advance and then the motion was to be initiated by pulling a toggle pin or rupturing a diaphragm. Electrically fired deflagrating squibs were chosen as the means of pulling the pin or rupturing the diaphragm. One part of the control circuitry had to be able to provide accurate sequence timing and reliable firing of squibs.

The electronic timing units were designed to provide repeatable time delays of 1 to 50 milliseconds with a resolution of plus or minus 0.5 milliseconds. For maximum flexibility, the electronic portion of the control system was built up of three basic types of units. They are a trigger generator, a pulse delay unit, and a squib or cap firing unit. Mounting space was provided in a single relay-rack cabinet for three trigger generators, two pulse delay units, two squib firing units, and a power supply. Interconnections between units are made with shielded patch cords, so that the arrangement and sequencing of functions can be changed easily.

4.1 THE TRIGGER GENERATOR

The schematic diagram for the Trigger Generator is shown in Figure 7. Input connectors are provided for either coaxial cable or shielded pair cable. Several different input types and modes may be selected by switches.

The first input type to be considered is a contact closure, such as that of a contact in the cam timer. With the "Pulse-Contact" switch in "contact" position, the signal input to V-1 grid is disconnected, and the low side of the input jack J-2 is grounded. The high side of the inputs is connected through C-2 to the grid of V-2. As long as the external contacts remain open, the input side of C-2 is charged to minus 100 volts through R1. When the external contacts close, the input side of C-2 is brought to ground potential, producing a positive-going 100 volt step at the grid of V-2. V-2 conducts, triggering the output blocking oscillator V-3. The "Triggering Threshold" control P-2 should be fully clockwise to hold V-2 cut-off.

If the trigger generator is to respond to a pulse, such as that generated by a pickup coil, SW-1 should be switched to "Pulse". If one side of the pulse source may be grounded, the "single-ended" position of SW-2 should be selected. Then, response to only positive going, or only negative going pulses may be selected by SW-3. By the combined use of the "Sensitivity" control P-1 and the "Triggering Threshold" control P-2, any given point on the input waveform may be selected as the level or instant of output trigger generation. In this way the generator can be prevented from responding to noise of smaller amplitude than the desired triggering pulse. The input signal may be a pulse, a step, or a ramp as long as its amplitude is greater than 10 volts and its rise time less than 10 milliseconds. In this single-ended mode, V-1 acts as a phase inverting stage for negative-going signals.

If both sides of the pulse source must be above ground or if it is desired to respond to the difference between the outputs of two sources having common grounded sides, the "Differential" mode of SW-2 may be selected. The "Triggering Threshold" control P-2 must be set fully counter-clockwise so that V-1 and V-2 will be in balanced conduction. This configuration is known as a differential amplifier. It tends to reject any common-mode voltage (i.e. a voltage which appears with equal-magnitude and the same sign at the grids of both V1 and V2).

The output stage of the trigger generator is a blocking oscillator. V-3 is normally cut off by a negative bias. When the current through V-2 increases sharply, enough voltage is produced in the secondary winding of T-1 to bring the grid of V3 above cutoff. V-3 conducts, adding to the primary current of T-1. A regenerative process is begun which produces an output voltage step of 150 volts in the tertiary, or output, winding of T-1. The rise time of this step is 0.3 microseconds. The output voltage holds for 7 to 10 microseconds, and then a reverse regeneration takes place which drops the voltage to zero. The silicon diode D-1 clips off the negative overshoot. The pulse transformer and circuit values were chosen to give a pulse of high voltage, low impedance, 1000 ohms, fast rise and fall, and a duration which is very short compared to any millisecond timing interval, but long enough to insure positive triggering of almost any following device.

Since the tertiary winding of T-1 is floating, the output may be grounded to give either pulse polarity or may be used as a differential "floating output". The choice is made in the external connections. The floating output helps prevent signal or ground loops, particularly in connecting to the Tektronix oscilloscope. A separate 20 volt, 100 ohm output is available, having one side grounded.

It was not considered necessary to incorporate single pulse lock-out provisions in the trigger circuit since they are provided in the oscilloscope and in the squib firing units.

Type 5963 tubes were selected in preference to other miniature, medium-mu, dual triodes because they are designed for computer service. The two halves are closely balanced, and the cathodes are made to resist the loss of cathode emission capability even if the tube operates in a normally cut off condition for long periods.

Particular care was taken in layout and construction of the units to avoid not only the entry of noise signals, but also crosstalk between channels. Chassis which carry more than one unit have shield baffles between sections. Tubes are shielded. All chassis have bottom pans. The backs of panel controls which are above the main chassis are enclosed in shield boxes. All units have an R-C decoupling filter in the B-plus line. All shields of incoming or outgoing lines are terminated at the front panel so that no interference is radiated into the insides of the cabinet or chassis. The power line entry receptacle, power switch, fuses, and pilot light are all inside a shield box which connects directly with the line isolation transformer. Component boards are used. Internal wiring is a combination of point to point and laced harness. Stage grounds are returned to a common bus at the front panel.

4.2 THE PULSE DELAY UNIT

The pulse delay unit schematic is shown in Figure 8. The input requires a negative pulse from the trigger generator. Diode V-1 blocks positive signals, and decouples the time-delay section from the input during the timing interval. The time delay section is a voltage-controlled, cathode-coupled, monostable multivibrator. V-3 is the normally-conducting tube. A negative pulse from the input is coupled through V-1 and C-2, 3, 4, or 5 to the grid of V-3 driving it in the negative direction. The common cathode voltage of V-2 and V-3 falls with the grid of V-3 until it is caught at the level of V-2 grid voltage established by the setting of the delay vernier control P-1. At this point V-2 conducts, causing the plate voltage of V-2 to fall still further and to remain low even after the negative input trigger has disappeared. The grid of V-3 is held cut off until the charge on the timing capacitor C-2, 3 4, or 5 has leaked off through R-7.

As the grid voltage of V-3 comes back up toward the cathode voltage established by the setting of P-1, V-3 begins to conduct and V-2 begins to cut off. A very rapid regeneration takes place, restoring the stable-state conditions which prevailed before the input pulse. The delay range is determined by the time constant of R-7 together with either C-2, 3, 4, or 5. The range steps are by factors of 2 or 2.5 to 1. Within each range, the delay can be varied 5 to 1 by adjustment of P-1. Hence, there is considerable overlap of the high end of one range and the low end of the next. Delay time varies almost linearly with rotation of the "Delay Vernier" control P-1.

Capacitor C-6 and resistor R-8 form a differentiating network which converts the square wave at the plate of V-2 to a negative-going pulse at the beginning of the delay interval and a positive-going pulse at the end. Tube V-4 does not respond to the negative going pulse since it is already cut off. However, the positive-going pulse at the end of the delay drives V-4 into conduction. V-4 acts as a shunt trigger for the blocking oscillator V-5. The rest of the action is the same as that of the trigger generator output stage previously described.

4.3 THE SQUIB OR CAP FIRING UNIT

Design of the squib firing unit proceeded backward from the characteristics of the squibs or blasting caps with which it might be used. Either squibs or caps consist of an igniter wire, a primer train and a charge of explosive. The explosive in a squib is of a slow burning or deflagrating type. That in a cap is of a fast burning or detonating type.

The time from application of voltage to explosion of the charge varies inversely with the current through the igniter. For current less than 0.3 amperes a squib will not explode. For currents in excess of 5 amperes a minimum of 2 milliseconds is required to fire a squib. No matter how fast the igniter is blown, there is still a 2 millisecond chemical ignition lag. The igniter wire resistance was between 1 and 2 ohms.

The squib firing circuit was designed to put 20 amperes through 1 to 2 ohms for 2 milliseconds. An electrolytic capacitor is charged from the 200 volt power supply and discharged through a pair of thyratrons since relays available with closing times of 1 millisecond have contacts that are rather light. A pair of small 2050 thyratrons, operated at their maximum short-pulse current rating can carry 20 amperes for a few milliseconds with the starting time for the thyratrons being a microsecond or less.

The relays and warning lights shown in the schematic are intended to prevent the accidental connection of a new squib to a live circuit, or premature firing by electrostatic discharge, etc. As soon as the squib igniter wire is destroyed, the capacitor voltage, which is still high, appears across RY-1 coil. This initiates a sequence which drops the arming circuit back to the "safe" condition. The capacitor is disconnected from B+ and discharged to ground. The squib lead is disconnected from the thyratrons and grounded. A squib continuity check is also provided for testing the squib prior to firing.

Input to the squib firing unit may be a positive going pulse from a trigger generator or a delay unit. It may also be a contact closure, when the input selector switch is set at "contact".

5.0 ARC CHAMBER RESULTS

The experimental program to determine the behavior of the arc in the split chamber was carried on concurrently with the proving in of the test equipment so that experimentation started at a relatively low energy level. The first experiment was made at 30,000 amperes in the coil which stored approximately 54,000 joules of energy. The chamber was pressurized to 400 psi, approximately 25 atmospheres, with pure nitrogen. The arc chamber was fitted with an internal fuse that was cut from copper plate 1/8 inch thick and had a cross sectional area of .024 sq. in. The fuse was designed to hold for a period of approximately 20 milleseconds. The fuse was bolted into the two halves of the arc chamber with bolts which were sealed to prevent leakage of the gas.

Following the discharge the chamber was disassembled and the upper half of the chamber, the anode, was photographed immediately upon disassembly. The amount of contamination within the chamber was very slight, consisting mostly of a very fine brown dust believed to be primarily condensed copper vapor as shown in Figure 10A. The fuse was almost totally destroyed. Figure 10B indicates that the fuse was only slightly damaged by the magnetic forces and that most of the fuse destruction was caused by heating effects.

After cleaning the chamber halves it was found that except in the immediate vicinity of the fuse attachment there was very little damage to the arc chamber. There were surface marks that were very shallow and easily removed with emery paper. On the anode in particular there were a number of very fine marks not over a tenth of a millimeter in width. These ran in the general pattern to indicate that the arc moved away from the insulation, perhaps with multiple anode spots.

A second test was made at approximately the same current level and the effects on the arc chamber are shown in Figure 11A. The arc apparently blew directly across the chamber from the initial starting point and attached to the opposite wall. It moved, again leaving the extremely fine marks that are shown in the Figure. The contamination was relatively small. The fuse had been modified slightly for this second experiment. The cross sectional area at the break points was the same, but it had been designed with three notches. The purpose in the notches was to insure that only short sections of the fuse would be melted. Long sections would be thrown out of the arc region by the magnetic forces without adding significantly to the contamination in the gas.

For the next experiment, the current was increased to 45,000 amperes, where the fuse showed a tendency to break rather than melt. From this point on two sections of fuse were invariably found within the chamber indicating that the three notches all broke at approximately the same time. Oscilliscopic traces obtained at a later date indicated that three notches would break within approximately one half of a millisecond of each other. The arc, however, appears to have anchored to the cathode rather than moving directly up the cathode wall. The damage caused by this is shown in Figure 11B.

The experimental program continued on up to the level of 100,000 amperes without any significant delays. At this level a series of tests were conducted at various pressures in order to determine the effect of gas density upon the erosion and on the voltage and current characteristics. At currents in the range of 50,000 to 100,000 amperes the very fine arc tracks that were seen initially were no longer observed. Those tracks that were left in the copper indicated that the surface underwent considerable melting. All arc tracks were relatively difficult to remove, requiring the use of a small hand grinder in order to remove the tracks sufficiently to proceed to the next test. The overall erosion, however, was not particularly severe. In Figures 12A and 12B the cathode and anode are shown after an experiment at 100,000 amperes with initial gas density of 100 times normal atmospheric density. The cathode spot appears to anchor in one location and then move, probably discontinuously to another position, never moving very far up the side wall of the arc chamber. The anode appears to move in a more continuous fashion. The initial pattern, that is the pattern nearest the insulator or end of the copper liner, shows a broad pattern of surface erosion. The anode spots, however, appear to have moved up almost to the end wall. Tracks have been found on the end wall of the anode.

Even at the higher current levels the cathode spot still anchors to the fuse termination, as shown in Figures 13A and 13B. The effects of the cathode anchoring are greatly affected by the pressure or density of the gas. In Figure 13A the photograph was taken after a test at 100,000 amperes with an initial pressure of 1500 psi. Figure 13B was taken after an identical discharge except that the initial pressure had been reduced to 150 psi. There is a difference by a factor of ten in pressure between the two experiments, but all other variables were held constant. Note in the upper picture that the spot is most severly eroded close to the nut that holds the remains of the fuse in place but that the region of severe erosion probably is not more than one and one-half inches in diameter. Part of the apparently eroded area, that near the outer edges, is due to spraying of the melted metal rather than actual erosion. In Figure 4B the nut was almost completely melted away and the damaged area is perhaps twice the diameter or four times the area.

The density of the gas within the arc chamber plays an important part in the determination of the voltage characteristic of the arc during the discharge, in addition to affecting the amount and character of erosion on the surface of the copper liner. In Figure 14 there are three sets of traces that were recorded on the Tektronix 551 dual beam oscilloscope. The first set of traces was recorded during a test where the initial pressure was 150 psi. The next set of traces was recorded on a test where the initial pressure was 400 psi, and the third set of traces was recorded during a test where the initial pressure was 1500 psi. The voltage fluctuations are much more severe in the lower density discharge. The rate of rise to the first voltage peak is also much slower in the low density discharge. In the very high density case, the voltage appears to be almost a square function. This contradicts the original concept involved in the design in the arc chamber, where it was assumed that the small column associated with the high density discharge would behave much more radically than the larger diameter and more diffuse column of a low density discharge.

The average voltage during each of these discharges has been calculated for the duration between the time when the voltage reaches it's first maximum and the time when the voltage begins to trail off, that is, gradually drop towards zero. A plot of these average voltages versus the corresponding initial pressure are given on Figure 15. The graph indicates that the initial density in the gas has a small effect on the average voltage during the discharge.

Preliminary reduction of data indicates that the average arc voltage increases as the current level during the discharge is increased. Figure 16 shows the average voltage during the central portion of the discharge plotted as a function of initial current in the discharge for three different initial pressures. The slopes of the curves for 150 and 400 psi initial pressure are positive, indicating that the voltage is increased when the current is increased. This was partially anticipated in the design of the chamber when it assumed that the magnetic forces would tend to drive the arc through the air with sufficient velocity to increase the arc voltage. The results obtained here tend to verify this assumption.

There is a point on this graph that indicates quite vividly that the behavior of this arc is not completely clear at this time. A test was made at 80,000 amperes with an initial pressure of 1500 psi. The arc voltage was over 3,000 volts with a peak near 3500 volts. The time of discharge was correspondingly reduced to approximately 4 milliseconds. The arc chamber indicated that the anode had reached the end wall of the chamber, but the erosion was not severe. The arc must have been moving very rapidly across the walls in order to account for the very high voltage. It is this type of anomalous behavior that will best be explained through the use of high speed photography. With time resolved photography, it will be possible to correlate the position of the arc and the location of the arc spots with the corresponding voltage at a particular instant. It should be possible to determine whether the fluctuations that are observed in the voltage traces are primarily a function of the arc spot or whether they are a function of the arc length and arc velocity.

5.1 THE POSITIVE VOLT-AMPERE CHARACTERISTIC

In practically all of the information that has appeared in the literature on electric arcs, the concept of negative volt-ampere characteristic has always remained dominant. The characteristic has been explained qualitatively as the result of higher conductivity and larger arc diameter as the current is increased, thus requiring a smaller voltage to sustain the arc. Quantitatively, this is born out by the Suits and Portisky analysis of the arc, a modification of which appears in Appendix A. However, with the very high current arcs that have been observed at extremely high pressures, there have been occasions when the voltage has decreased at the same time that the current was decreasing, which requires a positive volt ampere characteristic. This has been observed here to occur randomly in the main portion of the discharge, and invariably at the end of the discharge.

The voltage in the main portion of the discharge has been subject to rather large random variations. A definite tendency of the voltage to rise or fall with time has not been established at this time. It appeared with some of the earlier data that the voltage would tend to rise as the current decayed. Later it was observed that there was no direct relationship between the current and the voltage, with the voltage remaining approximately constant with an exception of the effects of the sharp dips in the voltage that are recorded towards the end of the discharge, which could be ascribed to a voltage restrike. The voltage traces in Figure 14 are quite typical. However, traces have been recorded that indicate both positive and negative slopes on the volt ampere characteristic, without there being any direct correlation to the current or pressure during the discharge.

The original concept of the negative volt-ampere characteristic needs to be modified for transients. The normal characteristic is associated with the steady-state arc and based on time averages of voltage and current. In the experimental situation in question here, the voltages and currents are both measured instantaneously and on an extremely short time scale. It would appear that the individual instantaneous effects such as arc position, magnetic forces, etc., are the predominating factors that control the instantaneous arc voltage.

During the last ten percent of the discharge, as the arc is dying, the voltage tends to drop quite gradually and uniformly. During this period, there is very definitely a positive volt-ampere characteristic but this is most probably a function of the preceding heating that occurred in the gas and not a normal arc characteristic. The arc column has been heated to an extremely high temperature throughout a reasonably large volume, a volume large enough to conduct 100,000 amperes. During the last ten thousand amperes of the discharge, the required cross sectional area of high conductivity gas is greatly reduced. The energy that is associated with the original column cannot apparently be dissipated throughout the volume rapidly enough to require a high voltage across the arc column during this period. This then is not a region of true positive volt-ampere characteristic but a region in which an overheated gas with resulting excessive conductivity cannot decay rapidly enough to approximate a normal arc condition.

5.2 INSULATOR EFFECTS

It has been found experimentally that the average arc voltage is dependent upon the minimum arc length. The arc chamber is designed so that it is possible to put an isolating sleeve inside the chamber that determines the minimum length. The original design used an insulator that was one-half inch long. After initial tests at 30,000 amperes indicated that the cathode spots did not move, a special insulator was made that was one and three quarters inches long. It was found that average arc voltage was significantly increased by using this longer sleeve. A third sleeve was made which was as long as possible in view of the location of the fuse bolts. This sleeve was approximately two and a half inches long. The average arc voltage was again increased. In these three cases, the pressure and the initial current were held constant. The results are plotted in Figure 17.

The reason for this behavior has not been determined quantitatively, though qualitatively the longer arc length should require a higher voltage. However, if the arc were to be blowing out rapidly then the arc length ought to be several times the minimum arc distance and the arc voltage should be two to three times the absolute minimum voltage. Judging by the location of the arc spots up the side wall, the arc is very definitely longer than the minimum path and it doesn't appear that there is any significant re-strike effect or other short-circuiting effect that would tend to lower the overall voltage.

The arc chamber was designed to be operated under conditions where the magnetic forces surrounding the arc column would be sufficiently large to drive the arc throughout the chamber so that the arc would not be able to stabilize in any one position. The sleeve length effect could be due to the fact that the current level is not high enough to create the field to drive the arc as was originally envisioned. However, evidence has been obtained that indicates that at 1500 psi initial pressure the arc column diameter is small enough so that the arc will be driven by its self-magnetic field, as was noted above concerning the point on Fig. 2 where the arc voltage was approximately 50% higher than the voltage recorded on a similar test under identical conditions when the arc blew to the end wall of the arc chamber.

It is anticipated that part of the future program will be devoted to determining the effect of the insulating sleeve inside of the chamber and its effect at varying currents and density. At currents in excess of 100,000 amperes, there should be sufficient magnetic force to prevent the arc from locating near the ends of the sleeve. If this is the case, then a curve such as is shown in Figure 17 will be, instead of a sloping increasing line, a practically level line at some higher level.

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Appendix A

Principles Governing Arc Configuration

1. Restricted-Diameter vs. Volume Conduction Plasmas. It has become quite evident, both from indirect experimental evidence obtained under this contract, and from scaling considerations relating to arcs reported in prior literature, that the arcs so far studied under this contract consist of extremely hot plasma cores that are small in diameter relative to arc chamber dimensions, and shift rapidly laterally through an ambient gas that has a temperature low enough to be essentially non-conducting electrically. This is in sharp contrast to the process of electrical current passage in magnetohydrodynamic electric power generators; there the entire volume of gas within the active portion of the apparatus is at a high enough temperature to give it an electrical volume conductivity adequate to permit passage of the total current without appearance of a restricted-diameter plasma. Experiments have shown that for air or nitrogen a volume conductivity adequate for such large-volume conduction requires a temperature approaching $3000^{o}K$ if "seeded" with alkali earth metal vapor, requiring considerably higher temperatures without such seeding.

Certainly there could exist in arc chambers generally resembling ours, for any gas, a temperature throughout the chamber high enough so that electrical conduction would occur throughout the volume, rather than by means of a restricted-diameter plasma threading through a non-conducting ambient gas. Under such volume-conduction conditions the arc voltage and current density would both be relatively low, even for a very high current; the watts input per thousand amperes would be correspondingly low, and the gas temperature high enough to cause very rapid thermal destruction of the chamber lining. Because of its low-voltage property, the onset of volume conduction should be easily recognized by electrical measurements, once the validity of this set of concepts is recognized.

Although, as just indicated, when volume conduction has been established, the rate of energy introduction becomes low, there will already have been introduced a large amount of energy per gas particle, because of the contributions of ionization and excitation to the internal energy. Re-extraction of this energy as directed kinetic energy, would then have to be preceded by recombination into neutral gas particles.

The rapidity of approach to the volume-conduction condition obviously depends on the rapidity of heating of the arc chamber gas, and therefore, for given current, on the arc voltage during the restricted-diameter cool-ambient arc condition. Clearly the higher the arc voltage during this interval, the more rapidly energy enters the gas, so the more quickly conversion to volume conductivity occurs.

Because of the sharp contrasts in form and effects on environment between conduction by restricted-diameter plasma cores and that by volume conductivity over a large section, it is important to understand clearly the principles that govern the configuration of and heat transfer from the restricted-plasma arc, which appears for objectives presently dealt with to be of primary importance.

2. <u>Relationship of the Experimental Program to Principles Governing Arc Behavior</u>. The work under this contract has resulted in obtaining a considerable amount of data interrelating the initial gas density, the initial arc current, to some extent the minimum arc length, and the transient

variations in arc current, arc voltage, and chamber pressure. This permits determination, based on experiment, of the actual instantaneous rates of heat transfer to the gas, and the total heat transferred to it, for the ranges of controllable parameters used. Use of enthalpy tables then permits, to the extent of dependability of such tables, an estimate of the time variance and ultimate value of the <u>average</u> gas temperature. Such data and its interpretation do not tell what the temperature is in the restricted-diameter plasma, and can give not much better than an order-of-magnitude estimate of the per unit length rate of heat transfer from the plasma to the cool ambient gas outside it because the arc length, particularly its time variance, are only roughly known because of the wandering of the arc terminations and its path between them.

The experimental work initiated under the present contract is being extended for a few weeks, using University resources in a direction that should provide direct optical determination of the arc configuration and its fast changes, possibly also of the conducting-plasma temperature, during the restricted-diameter condition. This will permit determination of the heat transfer rate per unit arc length as a time-varying function of arc current, gas density, and rate of lateral movement of the plasma through the ambient. Such data will permit a closer approach than is yet possible to establishing useful check points relating governing theory to experimental results.

When such check points are established, it should be possible to use them to determine both the nature and the magnitude of the physical parameters, such as heat conductivity, collision cross sections, viscosity, and others perhaps not yet appreciated, as they actually occur in the real environment.

This should then permit extrapolation of the theory toward usage in a wide variety of quite contrasting environments and operating parameters.

3. Heat Transfer Mechanisms. As discussed in the Status Report dated 16 July, 1962, under this contract, the rate of lateral energy transport from the restricted plasma is one of the four factors that in combination govern the size of the restricted cross section and the voltage gradient along the arc. It is therefore important to understand the mechanisms of lateral energy transport, being in the present case almost entirely heat energy transport. It is relatively easy to determine by simply calculations, with adequate experimental confirmation, that only very little of this heat energy is removed by radiation.

Existing literature dealing with restricted-plasma arcs treats only relatively stationary arcs. For there the convective and conduction heat transfer can be dealt with as though the arc were a solid object at the temperature and having the geometry of the actual arc.

However, for arcs here studied, there is adequate experimental and theoretical evidence that the arc conducting path moves with great rapidity, transversely to its length axis, under the influence of its own magnetic field. The motion appears to be so rapid as to forbid believing that this movement comprises actual lateral transfer of the gas particles of the plasma core. Rather, it appears almost certain that the attribute of gas conductivity moves laterally, this being an attribute of gas-particle assemblies that comes and goes as the current path moves laterally. This suggests two qualitative comments as to the nature of the behavior, as follows:

a) Heat transfer mechanism. In this case, heat transfer out of the arc is believed to occur very simply by the conduction attribute moving sideways, leaving behind the column of extremely hot gas through which current formerly passed. The heat of this left-behind column is transferred to the rest of the ambient gas by the usual processes, presumably in this case primarily conduction and convection, although significant live-spectrum radiation cannot a priori be ruled out for all interesting cases.

b) Mechanism causing lateral movement of conductivity. The out-layer electrons of a restricted-core plasma lie in a "crossed-field" environment, the two fields being the arc's longitudinal electric field and the transverse magnetic field associated with the arc's existence in its particular circuit configuration. The electrons are therefore given a component of motion at right angles to both fields; this describes as to force direction the familiar arc "blow-out" tendency. The electrons respond to this lateral force much more readily than do the heavier positive ions, and therefore penetrate into the cool adjacent ambient gas. The high temperature of the electrons causes rapid excitation and ionization of this new region, thereby establishing a new conduction path. The lateral movement of the electrons, leaving older ions to some extent behind, establishes in effect, a live doublet of charge which tends to drag the ions after the electrons, thus in fact causing some mass transfer of the gas that lay in the original conducting path. However, presently available evidence indicates that the primary behavior in the fast-moving, high-density arcs here dealt with is a lateral movement of the conducting property rather than of the gas itself. Thus, it appears probable that existing arc theories must be modified, before application to arcs here studied, by postulating a method of heat transfer out of the arc column consistent with rapid lateral movement of conductivity rather than of the gas itself.

The detail study of this mechanism, and its incorporation suitably into the theory governing arc configuration and gradient, is beyond the scope of the effort presently reported on, but is a very important part of future planned research programs. Such research effort can, it is believed, adequately crystallize the relation between theory and the realities of fast-moving, high-density restricted-plasma arcs.

4. The Set of Four Governing Relationships Which Combine to Determine Arc Gradient and Configuration. It has been reasonably well established in arc literature that a restricted-plasma arc's selection of its operating voltage gradient and diameter occurs in response to a set of four governing relationships in combination. These are:

a) The Saha-equation relation expressing electron density in terms of temperature, for a condition of quantum-mechanical thermal equilibrium.

b) A plasma conductivity equation;

c) A lateral energy-transport conservation relationship;

d) A least-power requirement. This is related to the fact that, for given current, the required gradient becomes very high for very small diameters, because of the extreme temperature then involved, and also very high for a large diameter because of the large heat-transferring surface area, with a minimum gradient of an intermediate diameter. The arc selects this inimum-gradient, that is, minimum-power diameter as its preferred operating state, and exhibits this configuration with its corresponding gradient.

The next several sections will deal with particular aspects of these four governing relationships.

5. Thermal Equilibrium; the Saha Equation. A generally accepted conclusion is that reasonably complete thermal equilibrium exists in arcs operating at gas densities higher than perhaps one-fifth to one-third of that existing at atmospheric pressure and 273°K temperature. Under such conditions the Saha equation, involving in an exponent the ratio of ionizing potential to the kinetic temperature in electron volts, governs the electron density within the plasma. The Saha equation may be stated as follows, in mks units:

$$N_n^{\,2} = 2.4 \times 10^{21}\, N_g\, T_p^{\,3/2} \exp\left(\frac{-V_I}{V_{T_p}}\right) \qquad \text{(A-1)}$$

In this and later expressions:

N_n is the electron number density in the plasma, in electrons per cubic meter;

N_g, V_I, are respectively the number density and ionizing potential of the neutral gas in the plasma; T_p, V_{T_p} symbolize the temperature in degrees and the kinetic temperature within the plasma, respectively in degrees Kelvin and in electron volts, thus of course

$$T_p = 11600\, V_{T_p} \qquad \text{(A-2)}$$

In a mixture of gases, Eq. A-1 describes the contribution to the electron number density due to each gas taken separately, the appropriate values of N_g and V_I being employed for the two determinations. The total electron number density is then the sum of the two values of N_n so found, and the total ion concentration is equal to the total electron concentration, and each gas contributes to the positive ion density in an amount equal to the electron density found from its applicable form of the Saha equation.

In general, it is found, because of the exponential dependence on ionizing potential, that the electron concentration is controlled chiefly by the gas having the lowest ionizing potential.

It is desirable in many applications to modify eq. A-1 to give an expression in terms of partial pressure in atmospheres rather than of gas concentration; note that

$$N_g = 2.7 \times 10^{25} P_g \frac{273}{T_p} \qquad \text{(A-3)}$$

where

P_g = the partial pressure, in atmospheres, of the gas in the arc whose concentration is N_g particles per cubic meter;

2.7×10^{25} = the Loschmidt number, being the number of particles per cubic meter of any gas at a pressure of one atmosphere (760 mmm of Mercury) and a temperature of 273^{o} Kelvin.

Elimination of N_g in favor of P_g in Eq. A-1 gives as another form of the Saha equation

$$N_n^{\,2} = 1.77 \times 10^{49} P_g T_p^{\,1/2} \exp\left(\frac{-V_I}{V_{T_p}}\right) \qquad \text{(A-4)}$$

6. <u>The Arc Conductivity Equation</u>. The second important arc relation, expressible directly, from the meanings of symbols used, as follows:

$$I = q_e \frac{\pi D^2}{4} N_n G_n E. \qquad \text{(A-5)}$$

The new symbols here are:

I = the current in amperes carried by the arc;

D = the diameter of the plasma of the arc that carries the current, in meter units;

q_e = the absolute value of the charge carried by an electron, being 1.6×10^{-19} coulomb per electron;

G_n = the mobility for longitudinal motion of the electrons in the arc plasma, in meters per second per volt per meter.

E = the longitudinal electric field strength in volts per meter.

In his book FUNDAMENTALS OF ENGINEERING ELECTRONICS, Second Edition, John Wiley and Sons, 1952, Dow gives in Eq. 15-296 on page 479 the following expression for electron mobility:

$$G_n = 5.93 \times 10^5 \times 0.38 \frac{\ell_n}{\sqrt{V_{T_p}}} . \qquad \text{(A-7)}$$

In this expression

ℓ_n = the electron mean free path in meter units.

5.93×10^5 = the numerical value of $\sqrt{q_e/m_e}$, where,

m_e = the mass of an electron, being 9×10^{-31} kilogram.

It is desirable here to express the mean free path as a function of temperature and pressure, thus adapting Eq. (15-20b) in Dow to the present pressure units,

$$\ell_n = \ell_{no} \frac{1}{P_g} \frac{T_p}{273} \qquad \text{(A-8)}$$

where

ℓ_{no} is the electron mean free path as it would be in the ambient gas or gas mixture if it were to exist at 273°K and a pressure of one atmosphere, in meter units, and here P_g is the total (i.e., not partial) gas pressure in the arc in atmospheres.

On using these relations and Eq. A-2 in Eq. A-6, the mobility becomes

$$G_n \cong 0.9 \times 10^5 \frac{\sqrt{T_p}}{P_g} \ell_{no} \quad . \tag{A-9}$$

Use of this and of the numerical value of q_e in Eq. A-5 leads to the following expression as a useful form of the arc conductivity equation:

$$I \cong 1.12 \times 10^{-14} \quad \frac{D^2 N_n \sqrt{T_p} \;\; E \;\; \ell_{no}}{P_g} \tag{A-10}$$

Note again that P_g is in atmospheres, and that I is the current in amperes.

7. Lateral Heat Transfer from a Stationary Arc... an Illustration Study. As the arcs of present interest are by no means stationary, and lateral heat transfer from them very greatly affected by their extremely rapid lateral motions, it must be presumed that one may not use in the present study the heat transfer principles that have been found applicable to a stationary arc. Yet a review of how knowledge of lateral heat transfer from a stationary arc is used, in establishing the nature of the arc gradient and arc configuration, can substantially aid in perceiving how a true knowledge of heat transfer from a fast-moving arc would be used in an analytical study. Such a review also shows some of the reasons why

stationary arcs typically exhibit a decline of arc voltage with increasing current. For these reasons this section contains a brief statement of principles governing lateral heat flow from a stationary arc, and the next section shows how they are used in establishing the lateral energy-balance equation.

As stated above, it was established some years ago that for stationary arcs the lateral heat transfer can be dealt with as though the arc were a solid object. Thus there are employed the heat transfer relation for a cylindrical object under forced convection. A good starting-point reference is APPLICATION OF HEAT TRANSFER DATA TO ARC CHARACTERISTICS, by C.G. Suits and H. Portisky, Phys. Rev. 55, 1184, June 15, 1939. Also very useful in this connection are the books KINETIC THEORY OF GASES, by E.H. Kenaard, McGraw-Hill Book Co., 1918, HEAT TRANSMISSION, by W.H. McAdams, Second Edition, McGraw-Hill Book Co., 1942, and the current edition of the HANDBOOK OF CHEMISTRY AND PHYSICS, published by the Chemical Rubber Publishing Co. The appropriate heat transfer relation is stated by Suits and Poritsky, also in somewhat different format on pages 220-222 of McAdams' book. The presently useful approximate form is:

$$\frac{hD}{k_h} \cong 0.47 \left(\frac{C_p \quad \mu h}{k_h}\right)^q \left(\frac{DF}{\mu h}\right)^s \qquad \text{(A-11)}$$

In this expression the exponents are of particular importance. The symbolism is as follows:

- D is the diameter of the arc plasma, in meter units;
- h is the heat transfer away from the arc surface, conveniently expressible in kilogram calories per second per unit area per degree Kelvin of

temperature difference between the arc plasma surface and the ambient temperature.

k_h, μ_h are respectively the heat conductivity and viscosity coefficients for the gas in the region surrounding the arc; this is the region in which the temperature drop from the arc temperature T_p to the ambient temperature takes place. As both of these quantities are functions of local temperature, some average value must be used. It is customary to use for k_h and μ_h the values corresponding the arithmetic mean between the arc and ambient temperatures. In many arc studies the ambient temperature is ignorably small relative to the arc temperature, so that it is often reasonable to use for k_h and μ_h the values for half the arc temperature. This is, of course, only an approximate treatment. Units for k_h are kilogram calories per square meter per second per degree Kelvin per meter; for μ_h units are kilograms per second per meter. F is the mass flow of gas kilograms per second per unit area, this being the flow past the object simulating the arc, due to forced convection of the environmental gas. For some stationary arc studies the forced convection is parallel to the arc length, in others it is at right angles to the arc length. However, the concept of F, and of Eq. A-11 is that there exists a mass flow of ambient gas of such a nature as to cause cooling by convection flow, and this applies for either kind of flow. The important reasoning based on this equation does not involve so much the numerical value of F as it does the exponent, s, applied to F. However, this exponent may be different for the two contrasting kinds of forced convection, normal-to-the-cylinder and parallel-to-the cylinder (here "cylinder" referes to the arc as being a lone cylindrical object cooled by forced convection).

C_p is the specific heat at constant pressure, in kilogram calories per kilogram per degree Kelvin. In the case of environmental gases being heated and processed, as is true for some industrial arcs, the endorthermic reactions have the same general effect as an increase in the specific heat.

The dimensionless ratios in Eq. A-11 are named as follows:

$\frac{DF}{\mu h}$ is the Reynolds number; (A-12a)

$\frac{hD}{k_h}$ is the Nusselt number; (A-12b)

$\frac{C_p \mu_h}{k_h}$ is the Prandtl number. Generally speaking, under simple heating and cooling conditions the Prandtl number for any gas is reasonably constant at all temperatures at a value not far from 0.75. However, if endothermic reactions occur in the environmental gas it is reasonable to expect that the Prandtl number may be a function of temperature.

As to the exponents in Eq. A-11,

s = the exponent applied to the Reynolds number; this is given in pages 220 to 222 of McAdams as having a value of 0.52, whereas Suits and Poritsky give on page 1189 of their paper the value 0.6. The precise value is not important, but it is significant that it should in general lie between 0.5 and 0.6 for most reasonably simple conditions.

q = the exponent applied to the Prandtl number in Eq. A-11. McAdams recommends on page 222 the value 0.3 for this exponent for forced convection from a solid cylindrical body to any gas, the flow of gas being normal to the cylinder. For conditions in which the Prandtl number is a constant independent of temperature, the value of this q exponent is not important, as the whole Prandtl number factor becomes an empirical factor whose numerical value is not essential to use of the gross reasoning. However, when endothemic reactions take place in the environmental gas, Prandtl's number or its equivalent will vary with temperature, and the exponent q then has significance.

In general, the viscosity varies with temperature. For reasons mentioned above in defining k_h and μ_h, it is customary in many arc studies to use values of these quantities corresponding to half the arc plasma temperature, this being nearly enough the arithmetic mean between arc plasma temperature and that of the cool ambient gas remote from the plasma. Under these assumptions, it is found that, nearly enough,

$$\mu_h \cong A_\mu T_p^{\,n} \qquad \text{(A-13a)}$$

$$k_h \cong A_k T_p^{\,m}$$

In these equations:

A_μ , A_k are empirical constants, different for different gases;

n, m, are empirical exponents, different for different gases. For most stationary-arc studies, the range of temperatures involved in any calculations is moderate enough - perhaps from 4000°K to 8000°K - so that these A_μ, A_k, n, and m quantities can in fact be treated as constants. Over a much larger range of temperatures such treatment might be only partially valid.

Values of these exponents for different gases may be extracted from information in the tables on pages 410 and 411 of McAdams. Also, the discussions of the dependence of viscosity on temperature given on and near page 151 of Kennard are helpful.

One may extract from these various sources, with some help from information in the HANDBOOK OF CHEMISTRY AND PHYSICS, the estimates that for air $n \cong 0.76$, $A_\mu \cong 2.3 \times 10^{-7}$, and for nitrogen $n \cong 0.69$ and $A_\mu \cong 0.99 \times 10^{-7}$. These are helpful in suggesting expected orders of magnitude.

The information available as to the dependence of m on temperature is somewhat more sketchy than for n, except when the constancy of the Prandtl number for variations in temperature can be used to evaluate at least the exponent m. In general, in the present study is appears reasonable to determine m in the absence of endothermic reactions by the use of the constancy of Prandtl's number, then to assume that the major effect of any endothermic reaction is on the effective specific heat rather than on the heat conductivity. In this case the exponent m would have about the same value as the exponent n, and A_k can be found if A_μ is known, providing

Prandtl's number is given its value at or near 0.75.

The basic form of the power balance equation is

$$EI = \left(\begin{array}{l} \text{the power in watts per meter of} \\ \\ \text{length of arc} \end{array} \right. \tag{A-14}$$

The initial form of the power balance equation is obtained by equating the Eq. A-14 electrical power introduced to the heat power laterally removed. In order to permit use of h in kilogram-calorie units, there is introduced the numerical factor to convert to joule units. The resulting equation is, in watts per meter of arc length:

$$E\,I \cong 4.19 \times 10^{6}\ \pi\ Dh\ \ (T_p - T_a)\,. \tag{A-15a}$$

Use of Eq. A-11 to eliminate Dh gives:

$$E\,I \cong 4.19 \times 10^{6} \pi (T_p - T_a) \times 0.47\ k_h \left(\frac{C_p \mu_h}{k_h}\right)^q \left(\frac{DF}{\mu_h}\right)^s \tag{A-15b}$$

Numerical multiplication gives

$$E\,I \cong 6.2 \times 10^{6}\ \pi (T_p - T_a)\ k_h \left(\frac{C_p \mu_h}{k_h}\right)^q \left(\frac{DF}{\mu_h}\right)^s \tag{A-15c}$$

In these expressions

T_a = ambient temperature, remote from the arc plasma. In earlier steps, certain of the coefficients have been dealt with on the basis that $T_p >> T_a$. In all previous cases where this has been used, its effect on the analytical process was relatively minor. At this point the same assumption will be made, that

$$T_p - T_a \cong T_p\ . \tag{A-16}$$

However, in the present use, failure of validity of this relation could affect the analysis very substantially, and this can happen in the arc chamber as heating of the ambient gas makes it become no longer "cool". Thus this assumption may have to be altered in extending the logic to treatment of the arc in an arc chamber. However, Eq. A-16 will be assumed valid in this present illustrative study.

In some industrial arcs there occur continuing endothermic reactions in the gas close to but outside the arc plasma. For example, in the Huls 8000-volt 1000 ampere arc process used in Germany during World War II to convert methane to acetylene as a first step to producing synthetic rubber, considerable endothermic conversions from CH_4 to C_2H_2 took place. In order to be able to account for such reactions, or any other temperature-dependent changes in enthalpy that might occur in this near environment, it is convenient to presume some variation of specific heat with temperature. Within the temperature range of interest in any particular problem, the following convenient type of equation will usually describe such dependence reasonably well,

$$C_p \cong A_c \; T_p^{\,r}. \qquad \text{(A-17)}$$

In this expression

A_c is an empirical proportionality constant;

r is an empirical exponent.

Of course in simple cases $r = 1$; the presence of marked enthalpy changes will in general make r substantially different from zero, and may be expected to increase A_c.

It is now possible to restate the Eq. A-15 power balance equation in a way that involves temperature dependence in a reasonably complete way, as follows:

$$E\,I \cong 6.2 \times 10^6 \; T_p \; (A_k T_p^{\,m}) \left[A_c^q \; T_p^{\,rq} \left(\frac{A_m}{A_k}\right)^q \; T_p^{\,(n-m)q} \right] \frac{D^s F^s}{A_m^s T_p^{\,ns}} \qquad \text{(A-18a)}$$

This simplifies to

$$E\ I \cong 6.2 \times 10^{6}\ (A_k^{1-q} A_c^{q} A_m^{q-s})\ D^{s} F^{s} T_p^{\ 1+m+q(r+m-n)-ns}. \qquad \text{(A-18b)}$$

Thus a convenient form of the power balance equation is

$$E\ I \cong BF^{s} D^{s} T_p^{\ p}\ , \qquad \text{(A-19a)}$$

with the two new quantities being

$$p = 1+m+q(r+m-n)-ns; \qquad \text{(A-19b)}$$

$$B \cong 6.2 \times 10^{6}\ A_k^{1-q}\ A_c^{q}\ \ A_m^{q-s} \qquad \text{(A-19c)}$$

Of course this is in fact an empirical quantity that is invariant over some limited but interesting range of values of arc plasma temperature T_p. To establish an order of magnitude for p, one may use as approximate magnitudes $m \cong n \cong 0.8$, $q \cong 0.3$, $s \cong 0.5$. Conceivably r might be zero, or fractional as between 0.5 and 1.0, or it might be substantial, as between 1 and 2 or 3. Thus as to order of magnitude,

p might be about 1.4 or 0.3r. (A-20)

8. Temperature-Governing Relation, from Combination of Several Principles. The three major relations described above, namely: the Saha equation, the conductivity equation, and the power balance equation, will be restated here for convenient reference;

The Saha relation, Eq. A-14:

$$N_n^{\ 2} = 1.77 \times 10^{14}\ P_g \sqrt{T_p}\ \exp\left(\frac{-V_I}{V_T}\right). \qquad \text{(A-21a)}$$

The conductivity relation, Eq. A-10,

$$I = 1.12 \times 10^{-14}\ \frac{D^2 N_n \sqrt{T_p}\quad E\ \ \ell_{no}}{P_g}. \qquad \text{(A-21b)}$$

The power balance relation, Eq. A-19:

$$E\ I = BF^{s}\ D^{s} T_p^{\ p}. \qquad \text{(A-21c)}$$

For the arc-chamber arcs of present interest, comments as follows indicate to what extent arc-chamber arcs fit the model used:

The current I is determined by the external circuit, under the control of the operator; the range of presently interesting values is from 50,000 amperes to 1,000,000 amperes.

The pressure P_g in the Saha equation, units here being in atmospheres, is the arc chamber pressure, presumed in this analytical model to be uniform throughout the chamber. Its initial value is at the disposal of the operator, the interesting range of initial values being from about 10 to 300 atmospheres. As the average temperature T_a within the arc chamber increases, P_g increases proportionally, the gas density (proportional to P_g/T_a) remaining constant except as nitrogen or oxygen dissociation takes place.

The forced convection mass flow rate F is not determinable for the arc chamber problem; some approach to its value can perhaps be obtained by optical observations of the lateral arc movement. However, it seems probable that this movement is so fast as to discredit the concept used in the present model that lateral heat flow can be dealt with as though the arc were a solid object. Thus Eq. A-21b must presumably be modified substantially, perhaps even taking a very different form, in dealing with arc-chamber arcs of present interest.

One important objective for continuing study beyond the present contract is to give theoretical-study attention as to what equation should replace eq. A-21b.

The ionizing potential to use in dealing with the arc in nitrogen, which even at temperatures of several thousand degrees is molecular nitrogen, so that V_I would in such a case be that of the nitrogen molecule.

B is, from Eq. A-19c, dependent on various properties of the environmental gas, and is affected by any enthalpy increases taking place, as these increase A_c. Since the exponent q is fractional (of the order of 0.3) these affect B roughly as the cube root of this magnitude.

ℓ_{no} is a property of the gas content of the arc plasma, rather than of the ambient gas. However, under the presumptions that nitrogen does not dissociate in the arc plasma, ℓ_{no} for diatomic nitrogen gas can be used; page 581 of Dow gives this as $\ell_{no} \cong 0.4$ in millimeter units, being thus $\ell_{no} = 4 \times 10^{-4}$ in meter units.

From this point on the discussion will deal with conditions in which the forced convection flow rate is determinable, as is true, for example, in the Huls arc.

9. <u>Use of the Least-Power Requirement to Establish Arc Voltage Gradient, Diameter, and Temperature.</u> To illustrate how the theory of essentially stationary arcs is used to complete the analytical determination of arc parameters, this section will show the application of Eqs. A-21 for this purpose.

For the stationary arc model, all of the quantities in Eqs. A-21 are controllable by the designer or operator of the apparatus except D, N_n, T_p, and E. Thus the theory must permit use of the <u>three</u> Eq. A-21 relations to complete the solution. Obviously, three equations cannot determine four variants. There must be a fourth condition. The fourth condition here employed will be a least-power condition. That is, the value of T_p will be that causing passage of the required current I at a minimum power input, that is, a minimum value of E.

Pinch effect may also contribute toward providing a fourth condition; pinch effect will not be considered in this sections' illustrative discussion.

The analytical procedure in reducing the Eq. A-21 set of equations is to eliminate D and N_n by algebraic manipulations, to give:

$$E^{1+(2/s)} = H_1 F^2 \; I^{1-(2/s)} T_p^u \; \exp\left(+ \frac{b}{2T_p} \right) . \qquad \text{(A-22)}$$

In this equation

$$H_1 = \frac{2.44 \times 10^{-11} \; B^{2/3}}{\sqrt{P_g} \quad \ell_{no}} \; ; \qquad \text{(an empirical constant)} \qquad \text{(A-23a)}$$

$$b = 11600 \quad V_I \quad ; \qquad \text{(A-23b)}$$

$$u = \frac{2p}{s} - \frac{3}{4} \quad .$$

If p is given the value 1.4, as in Eq. A-21, with r assumed to be zero, and s given the value 0.5, u becomes about 5. If p is 1.4 and s is given the value 0.3, u becomes about 8.6 Of course if r has a value greater than zero, u is somewhat increased.

The minimum-E temperature, for given values of current I, flow rate F, and gas properties as described by H_1, is obtained by taking the derivative of Eq. A-22 with respect to T_p and equating the result to zero, then solving for T_p. Such treatment leads to the relation

$$T_p = \frac{b}{2u} \qquad \text{(A-24)}$$

With the arc temperature established in accordance with this relation, the gradient E, arc diameter D, and electron concentration N_n, are also determined.

It turns out that the various factors contributing to the value of the exponent u are not well enough known for the actual arc environment to lead to a result closely in accord with experimental observations as far as the temperature is concerned. As an example, in analyzing the 8,000 kilowatt continuous-duty Huls arc theoretically, two extreme values that might conceivably apply for the exponent s are 0.3 and 0.6, leading to values of 8.6 and 3.9 respectively, if p = 1.4. The Huls arc plays at P_g = 1.2 atmospheres in a mixture of acetylene and atomic hydrogen; of these two acetylene has the lower ionizing potential, 11.5 volts, so that acetylene governs and b = 11.5 x 11600 = 134,000. The temperature is then given by Eq. A-24 as follows:

$$T_p = 134{,}000/2 \times 8.6 = 7700^{o}K, \text{ if } s = 0.3, p = 1.4 \quad \text{(A-25a)}$$

$$T_p = 134{,}00/2 \times 3.9 = 17100^{o}K, \text{ if } s = 0.6, p = 1.4 \quad \text{(A-25b)}$$

Comparison with arcs in which the arc plasma temperature has been measured indicates that at this pressure and for an ionizing potential of 11.5 electron volts the temperature is probably about 6000^{o}K.

However, in spite of the obvious inability to use the theory to predict temperature, the type of analytical treatment leading to Eq. A-22 has been on the whole a valuable contribution to the understanding of the physics of high-density arcs. It does predict correctly the general order of magnitude of the temperature. The major contribution is, however, the conclusion that the arc temperature depends <u>only</u> on the ionizing potential and the various exponents q, s, n, m, and r in the dimensionaless-ratio scaling equations, and not on the proportionality constants in those equations.

The other important quantities, E, D, and N_n are exponentially dependent on the temperature, because of the form of the Saha equation. Therefore no engineering utility attaches to a knowledge of T_p which is in error by more than a few hundred degrees, and certainly the knowledge of the various exponents is not firmly enough based to permit determining the temperature this closely by using Eq. A-24. Therefore in making comparative studies of arcs in relation to the theory, reliance is commonly placed on values of T_p obtained by comparison of a given arc with others for which the temperatures have been measured. Many of the careful measurements of arc temperatures have been made on arcs operating subject to free convection. However, a review of the general logic of the analysis shows that forced convection arcs should have temperatures slightly less than those for free convection arcs, therefore having substantially lower electron concentrations and somewhat higher gradients, for the same current. The temperature cannot drop very much below the free convection measured values without reducing the electron concentration to so low a value that the required current could not be carried with any reasonable gradient.

10. Arc Volt-Ampere Properties, and the Effect of Gas Flow. Eq. A-22 is easily adapted to indicate the nature of the volt-ampere characteristic to be expected for the arc. Thus for a given gas flow rate F and arc temperature T_p, Eq. A-22 may be restated as follows, in terms of a new empirical constant H_2:

$$E^{1+\frac{2}{s}} = H_2 \, I^{1-\frac{2}{s}} \tag{A-26a}$$

This arranges into the form given by Suits and Poritsky for forced convection, expressing mathematically the familiar fact that the arc has a negatively sloping volt-ampere characteristic:

$$V = EL = \frac{H_3}{I^{\left(\frac{2-s}{2+s}\right)}} \tag{A-26b}$$

Here

L = the arc length, in general determined by the apparatus.

V = the arc voltage.

If, for example, s = 0.3, or s = 0.6,

$$V = \frac{H_4}{I^{0.74}} \quad \left[\text{if } s = 0.3\right] \tag{A-27a}$$

$$V = \frac{H_4}{I^{0.54}} \quad \left[\text{if } s = 0.6\right] \tag{A-27b}$$

In these various equations

H_1, H_2, H_3, H_4, are empirical or semi-empirical constants. These volt-ampere relations are generally in accord with observations at to type of characteristic curve, but the precise exponents cannot be stated for forced convection conditions, because of the scarcity of experimental information.

It is because of this type of volt-ampere relation that it is generally necessary to employ rectifier apparatus that compels the current to have a specified value, rather than employing a constant voltage source of power.

The Eq. A-22 form indicates quite correctly that an increase of gas flow rate F causes an increase in arc gradient.

11. <u>The Experimentally-Determined Temperature Approach</u>.

It has been pointed out above that in principle the mathematical expression of the minimum-gradient condition can be employed to give a set of equations that can be solved formally for the arc temperature, gradient, and electron concentration. However, the solution requires the use of various properties of the gas that are imperfectly known, also of similarly imperfectly known properties of the arc environment, such as the temperature dependence of heat conductivity and of viscosity in the forced-convection condition. It therefore turns out to be not practical to study temperature conditions by analytical means.

However, Suits and his associates, and others since them, have <u>measured</u> arc temperatures of typical gases experimentally, by various methods. The measurements indicated, as theory predicts, a marked dependence of the arc temperature on tne ionizing potential of the ion-contributing gas. Thus it may be said that for an arc to survive at all in a given gas at a given pressure the temperature within the arc must lie within a relatively restricted range of values. This range is governed primarily by the ionizing potential and the pressure. If more than one kind of gas is present in the arc, the situation will, in general, be governed by the gas having the least ionizing potential.

However, most temperature measurements have been made on arcs at or near atmospheric pressure, although some have probably been made on mercury-vapor arcs operating at relatively high pressures.

12. <u>Analysis in Case Enthalpy and Chemistry Changes Dominates the Behavior</u>. In the Huls arc, operating under strong parallel-flow forced convection, the primary lateral heat transfer is in fact into the endothermic reaction converting methane to acetylene. In this case, nearly enough:

$$E\,I\,L = \left(\begin{array}{l}\text{the total power used to raise} \\ \text{the enthalpy of and convert the} \\ \text{gas from methane to acetylene} \\ \text{and hydrogen}\end{array}\right) \qquad \text{(A-28)}$$

If now the length L is known, and the power needed for conversion of a measure gas flow is calculated, the gradient is at once established, and as so determined checks reasonably well with the experiment. If then the temperature T_p can be estimated by comparison with similar arcs, the electron concentration N_n is determinable from the Saha equation. The values of N_n and T_p can then be used in the conductivity equation to give arc diameter. This procedure does in fact provide a quite satisfactory check with observed data.

One significance of these comments is the indication they give of the possible utility in analysis of substitution of another, entirely different kind of heat balance relation for Eq. A-21c, when the solid-object heat transfer relationship is of very doubtful validity. It appears probable that such will be found the case in further studies of arcs of present interest. If, in fact, the arc by moving laterally continuously "processes" into an ionized state new gas, the behavior begins to bear considerable resemblance to the Huls arc, in that changes in potential energy rather than in kinetic energy may dominate the physics of the power balance relationship.

Appendix B

Energy required to be dissipated in the transfer switch flask

The following relation governs, the energy--that is, time-integrated volt-amperes---represented in the arc in the transfer switch during transfer of current flow from the transfer switch to the arc chamber, for a situation in which the inductive voltage very substantially exceeds the resistive voltage in the transfer switch circuit loop:

$$\left\{\begin{matrix}\text{Energy in transfer}\\ \text{switch arc}\end{matrix}\right\} = \left\{\begin{matrix}\text{energy stored}\\ \text{in coil inductance}\end{matrix}\right\} \times \left\{\frac{\text{inductance of transfer switch loop}}{\text{inductance of coil}}\right\} \qquad \text{(B-1)}$$

As an illustration, consider a 120 microhenry coil carrying 315,000 amperes, representing 6 megajoules of energy storage, with an achievable inductance of 0.4 microhenry in the circuit involving the transfer switch, the arc chamber, and the connection between them. In this case:

$$\left\{\begin{matrix}\text{Energy in transfer}\\ \text{switch arc}\end{matrix}\right\} = 6{,}000{,}000 \times \frac{0.4 \times 10^{-6}}{120 \times 10^{-6}} = 20{,}000 \text{ joules} \qquad \text{(B-2)}$$

This represents a very easily contained flash.

As symbolism to use in the proof of this relationship, let

W_{tr} = integrated joules in the transfer switch arc;

W_o = joules stored in the coil just prior to transfer;

L_o = inductance of the energy storage coil, henrys;

L_1 = inductance of the local circuit loop, henrys;

R_1 = resistance in ohms of the local circuit loop, including resistance of the arc-initiating fuse, and of any isolating resistance that may be used;

I_o = current in the energy storage coil, assume invariant during the brief current transfer period, because of the large value of L_o, in amperes;

i_a = instantaneous current in the local circuit, amperes;

i_{tr} = instantaneous current in the transfer switch arc, amperes;

v_{tr} = voltage across the transfer switch arc.

It is the voltage across the transfer switch arc that causes build-up of the current in the arc chamber and associated transfer switch circuit loop. Therefore

$$v_{tr} = L_1 \frac{di_a}{dt} + R_1 i_a \qquad \text{(B-3)}$$

Assume here, subject to later check, that the resistive voltage is negligible relative to the inductive voltage, in this equation. Thus, nearly enough

$$v_{tr} = L_1 \frac{di_a}{dt} \qquad \text{(B-4)}$$

Clearly, from the circuit form,

$$i_{tr} + i_a = I_o \qquad \text{(B-5)}$$

with I_o time-invariant. Also, instantaneous power in the transfer switch arc is $i_{tr}\, v_{tr}$, expressible, nearly enough, as

$$i_{tr}\, v_{tr} = (I_o - i_a)\, L_1 \frac{di_a}{dt} \qquad \text{(B-6)}$$

Also, of course

$$W_{tr} = \int_0^{t=\infty} i_{tr} v_{tr}\, dt \qquad \text{(B-7)}$$

Because the current transfer to the arc chamber circuit, and zero of time is taken to be at initiation of the transfer switch arc as its contacts separate, so that

$$\text{when } t = 0, \qquad i_a = 0, \qquad \text{(B-8a)}$$

$$\text{when } t = \infty, \qquad i_a = I_o. \qquad \text{(B-8b)}$$

After introduction of Eq. (B-6) into Eq. (B-7), straightforward mathematical processing shows the second term in the integrations to be just half the first, the result being finally:

$$W_{tr} = \frac{1}{2} L_1 I_o^2 \qquad \text{(B-9)}$$

Of course, as in any storage coil,

$$W_o = \frac{1}{2} L_o I_o^2 \qquad \text{(B-10)}$$

Therefore, nearly enough, if the resistive voltage in the local circuit loop is small,

$$W_{tr} = W_o \frac{L_1}{L_2} \qquad \text{(B-11)}$$

as stated in words in Eq. (B-1). The voltage v_{tr} of the transfer switch arc is usually of the order of 100 volts, rising as contacts separate and current transfer proceeds. If R_1 is 100 microhms, use of 300,000 amperes for I_o would cause $I_o R_1$ to reach its greatest value of 30 volts at arc extinction. Thus, throughout the transfer the inductive voltage would in fact dominate the behavior, as assumed in arriving at Eq. (B-11). Use of a larger R_1 would cause W_{tr} to increase somewhat above its Eq. (B-11) value. However, if the actual transfer of the current takes place in a time interval short compared with the time constant L_1/R_1 of the local circuit loop, W_{tr} will have approximately its Eq. (B-11) value. If $L_1 = 0.4$ microhenry, and $R_1 = 100$ microhoms, the loop time constant is 4 milliseconds. If the transfer switch arc voltage, v_{tr}, were to remain constant at 100 volts, the current transfer would be completed in less than a millisecond, which is substantially less than L_1/R_1. Actually, of course, v_{tr} rises as the contacts separate, causing current transfer to be accelerated as it approaches completion.

Note that the energy dissipated in the transfer switch is to a large degree independent of how long it takes to transfer the current. If the voltage across the transfer switch arc is high, the power in the arc is correspondingly high, but the arc lasts only a short time. If the arc voltage is lower, the power in the arc is less, but it lasts longer.

Appendix C

Notes on Fuses

In this report in the discussion on fuse technology two equations were given. Both can be shown to be part of a coherent set of concepts. The first equation stated that the voltage across the fuse, as it blew up, grows approximately exponentially, or

$$v = V \exp(t/\tau) \qquad \text{(C-1)}$$

It was also stated that the energy lost in the fuse was the product of three terms which may be stated as

$$W_f = K_4 V_m I_o t_h \qquad \text{(C-2)}$$

The energy dissipated in the fuse is dissipated in two steps. Some energy is dissipated in the form of joule heating, raising the fuse element to the melting point temperature. Additional energy is dissipated in the arc formation and melting of material as the fuse blows up. Expressed mathematically this can be stated as

$$W_f = W_{fm} + W_{fe} \qquad \text{(C-3)}$$

where W_{fm} is the energy required to bring the fuse to the melting temperature and W_{fe} is the energy dissipated in the fuse during the blow-up time. Fortunately the first term in this equation, the energy lost in melting, can be calculated quite accurately utilizing the elementary equation

$$W_{fm} = cm \Delta T \qquad \text{(C-4)}$$

where C is specific heat, m is mass of copper heated, ΔT is temperature rise. The temperature rise is the change in temperature from room temperature to the melting point of copper which is approximately 1060°C. The specific heat is tabulated in HANDBOOK OF PHYSICS AND CHEMISTRY and while not constant over this entire range, does not vary by more than about 5% having a mean value of .095 in cgs units. The mass in the fuse length can either be determined by calculation from the product of density and volume or by direct measurement. For analytical purposes the mass may be written in the following form

$$m = d \ell A \qquad \text{(C-5)}$$

where d is fuse density, ℓ and A are fuse length and sectional area.

It was noted earlier that the square of the current density times the time that a fuse takes to reach the melting point is approximately a constant which may be written as

$$J^2 t_h = 3.5 \times 10^{10} \ \frac{\text{amp}^2\text{-sec}}{\text{in}^4}$$

Keeping in mind that the current density is simply the ratio of current to area this may be rearranged to the form

$$A = K_5 I_o \sqrt{t_h} \qquad \text{(C-7)}$$

where K_5 is a new constant.

The length of the fuse wire will normally be held to a minimum so that the transfer switch may be operated with as little back voltage as possible. For any given voltage requirement there is a minimum fuse length that must be used in order to obtain the required voltage to transfer the current into the load when the fuse blows up. This may be stated as an equation using still another constant K_6:

$$\ell = K_6 V_m \qquad \text{(C-8)}$$

On substituting the equation (C-8) and (C-7) together with (C-5) back into (C-4), one comes to the conclusion that the total energy dissipated in melting can be expressed as

$$W_{fm} = C \Delta T (K_6 V_m)(K_5 I_o)\sqrt{t_h} = K_7 V_m I_o \sqrt{t_h} \qquad \text{(C-9)}$$

where K_7 has an obvious meaning.

The form is interesting in that this says that the energy that goes into melting is a function of the required fuse voltage, the current that it has to switch, and the square root of the time that the fuse has to hold. It will be shown below that only a small error will be involved in assuming that the melting energy will vary linearly with the time as equation (C-2) indicated.

The energy that goes into the fuse destruction can be written as the time integral of the product of voltage and current in the fuse which can be written as

$$W_{fe} = \int_{o}^{t_b} Vi \, dt \qquad \text{(C-10)}$$

or

$$W_{fe} = VI_o \int_{o}^{t_b} \exp(t/\tau) \, dt \qquad \text{(C-11)}$$

This requires an elementary integration which takes the form of

$$W_{fe} = I_o \left[V \exp(t_b/\tau) \right] \tau \qquad \text{(C-12)}$$

By using the definition of the maximum voltage this is given the form

$$W_{fe} = I_o V_m \tau \qquad \text{(C-13)}$$

It was noted under the discussion of fuses that the time to blow up is approximately 5 time constants yielding a final form for the energy in the explosion of

$$W_{fe} = \frac{I_o V_m t_b}{5} = \frac{I_o V_m t_h}{50} \qquad \text{(C-14)}$$

The combination of these two sources of energy dissipation takes the final form of

$$W_f = \frac{I_o V_m t_h}{50} \left[1 + \frac{K_8}{\sqrt{t_h}} \right] \qquad \text{(C-15)}$$

The value of $K_8/\sqrt{t_h}$ for a 2 ms, 40,000 volt 5000 ampere fuse calculates out to 1.6. The value would be decreased for a lower voltage or longer time, but the variation in the total fuse energy is not great with the final result that equation (C-2) yields adequate accuracy if K_4 is given a value of .05.

APPENDIX D

ORIGINAL DATA

Reproductions of original data are included in this report in Figures 18-26 as part of this Appendix. The data are included primarily for use by other investigators in the field of high current, high density electric arcs, so that a direct comparison of experimental results is possible. In addition, the data may be useful to others in correlating, extending, or proving an existing hypothesis through the utilization of data that has been taken in an arc chamber with different geometric factors.

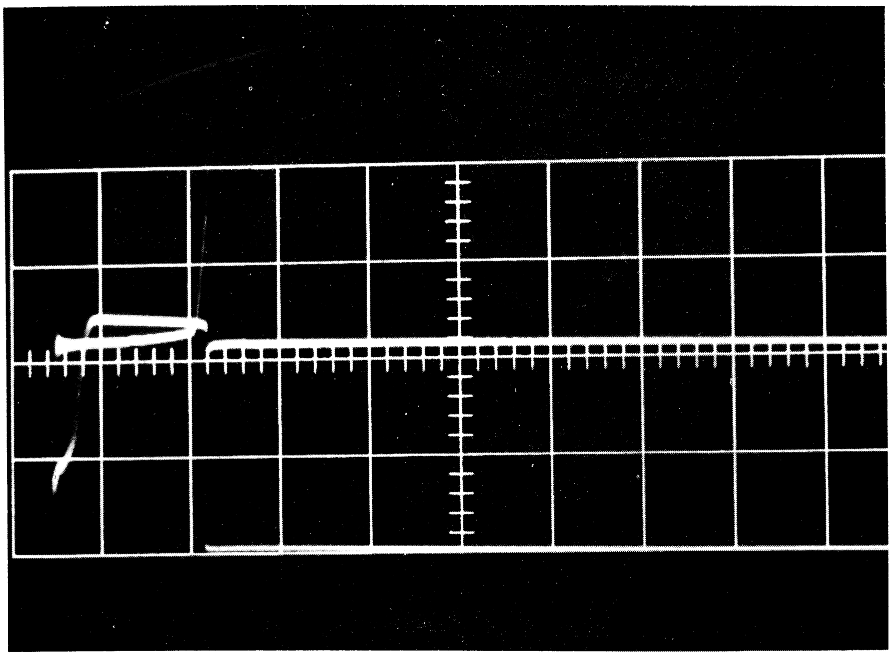


Figure 1A. Fuse current (lower trace, 2000 amperes per division) and fuse voltage (upper trace, 10 KV per division). A ball gap was used as the load. The time scale is 2 ms per division.

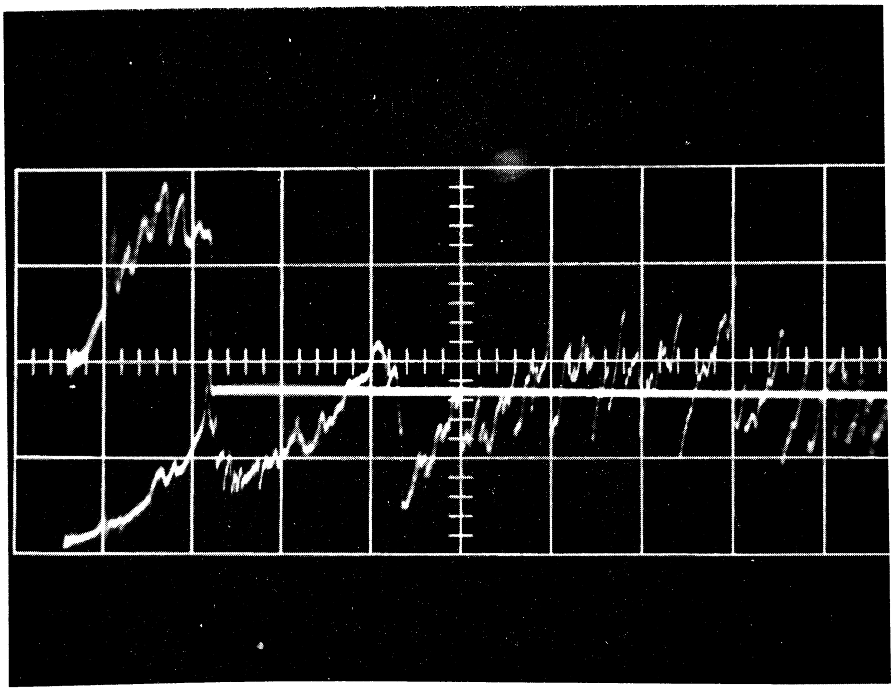


Figure 1B. Experimental switch arc-over. The fuse current signal was used to trigger the oscilloscope. The lower trace (450 volts per division) records the voltage developed by the fuse (2 ms per division).

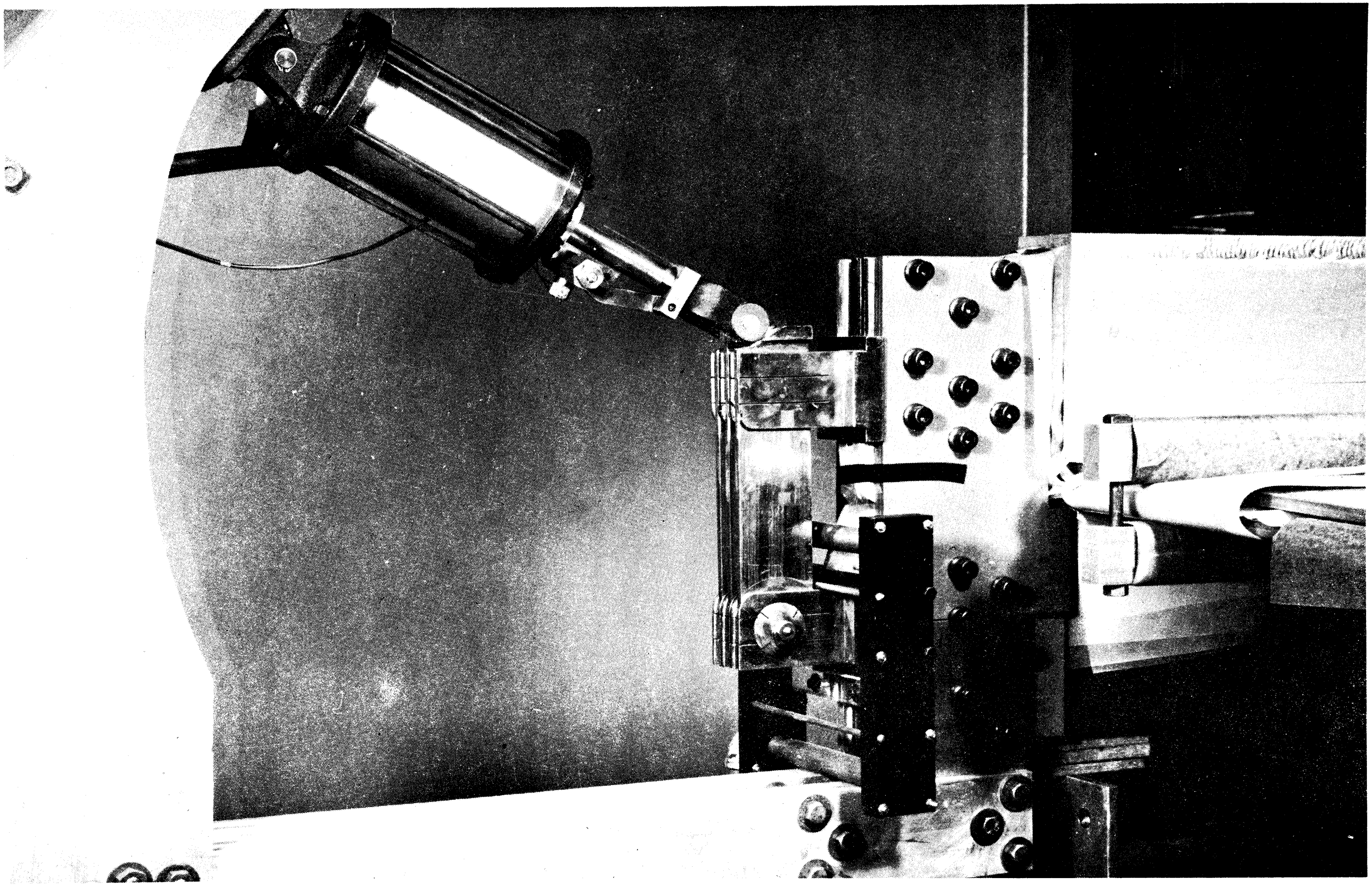


Figure 2. The transfer switch. The air cylinder is pre-loaded and released by the toggle. An air blast is used to move the arc away from the contacts.

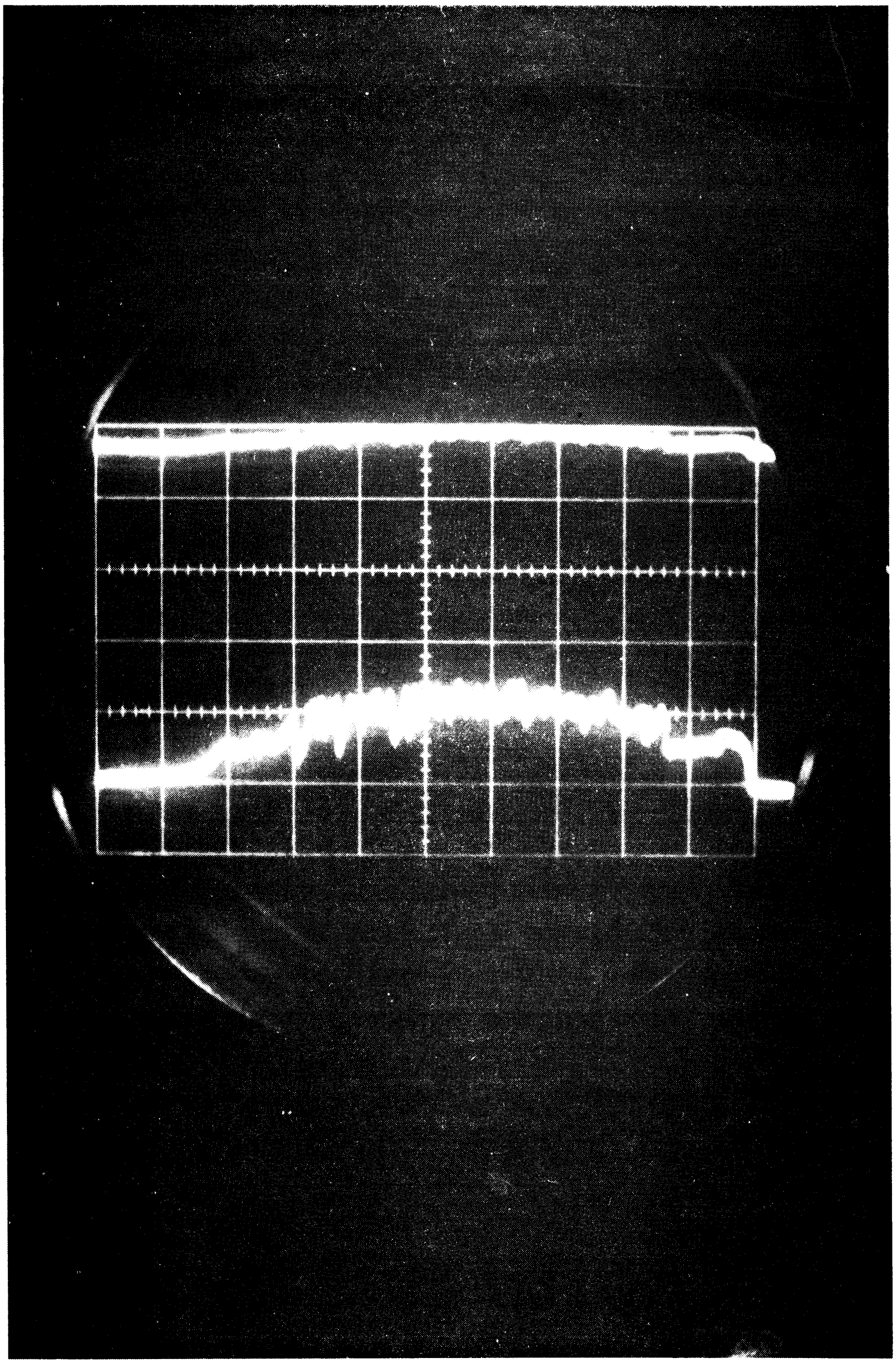
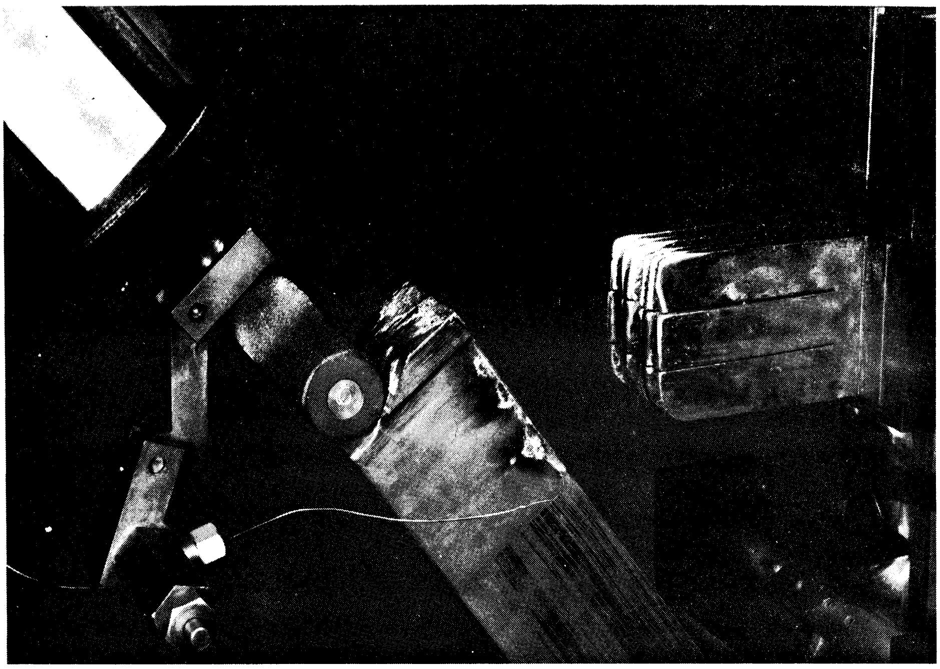


Figure 3. The voltage across the switch arc at 2 ms per division and 700 volts per division.



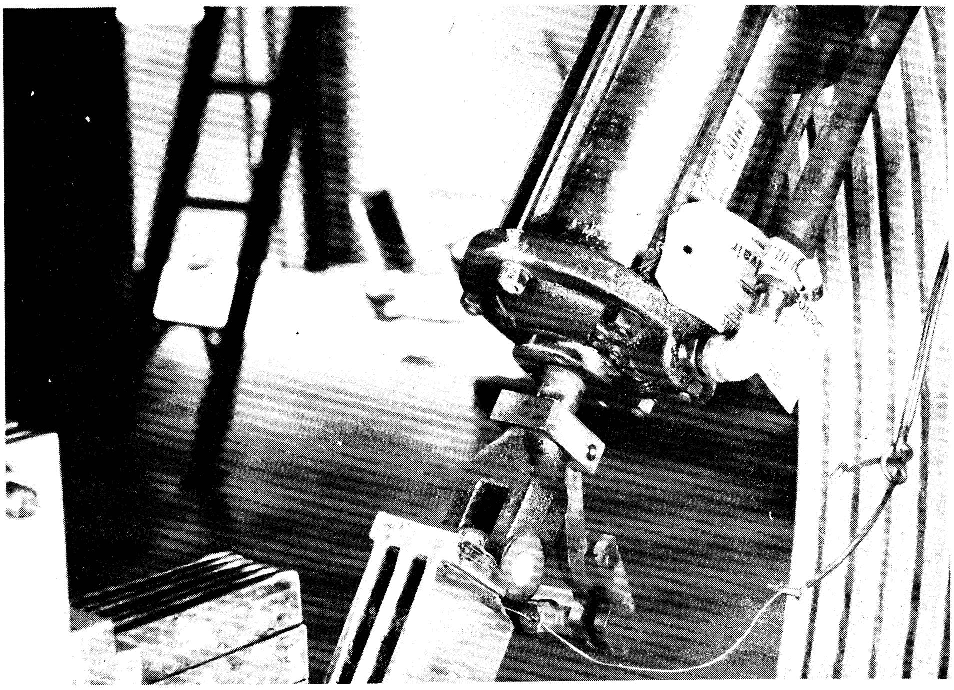


Figure 4. Two photographs of the switch after the arc-over. The air cylinder was not damaged.

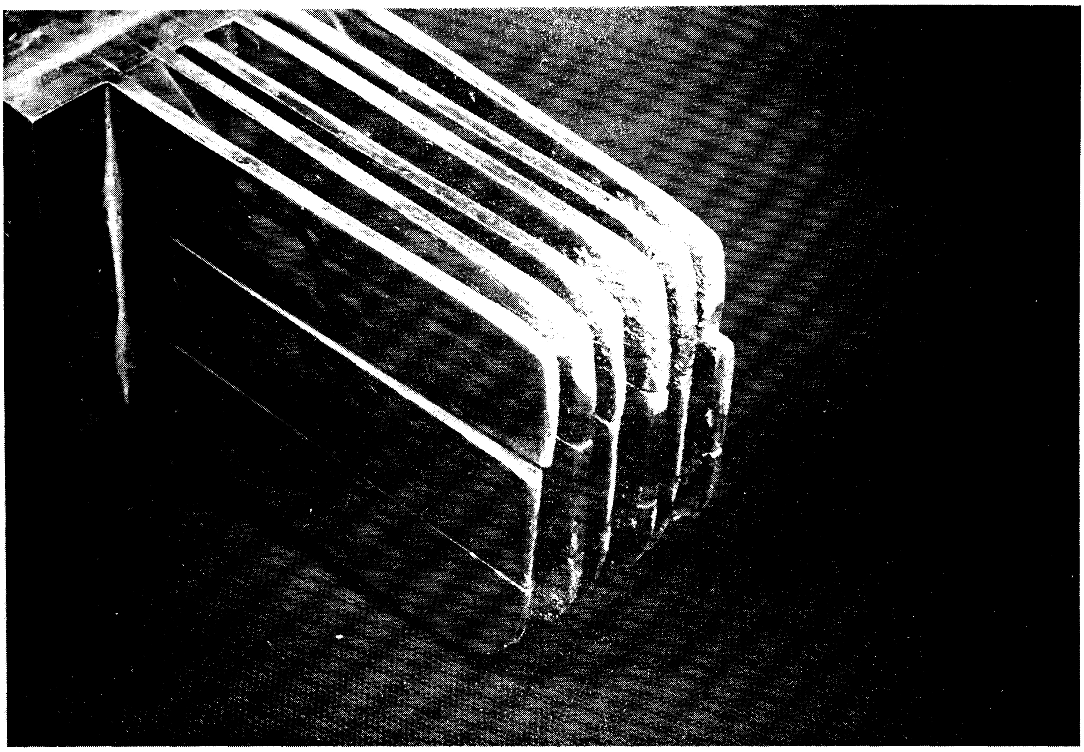


Figure 5A. The fingers after the 100,000 ampere arc-over.

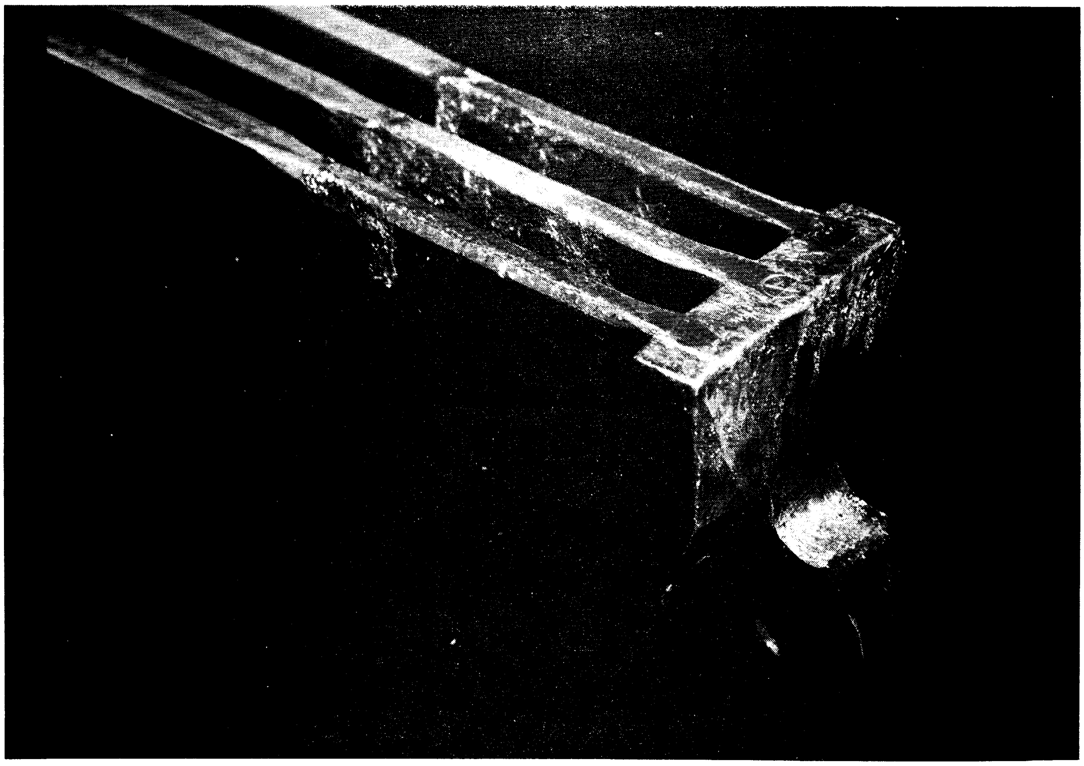


Figure 5B. The switch blades after arc-over. The erosion at the contact edges is the result of 20 to 30 operations.

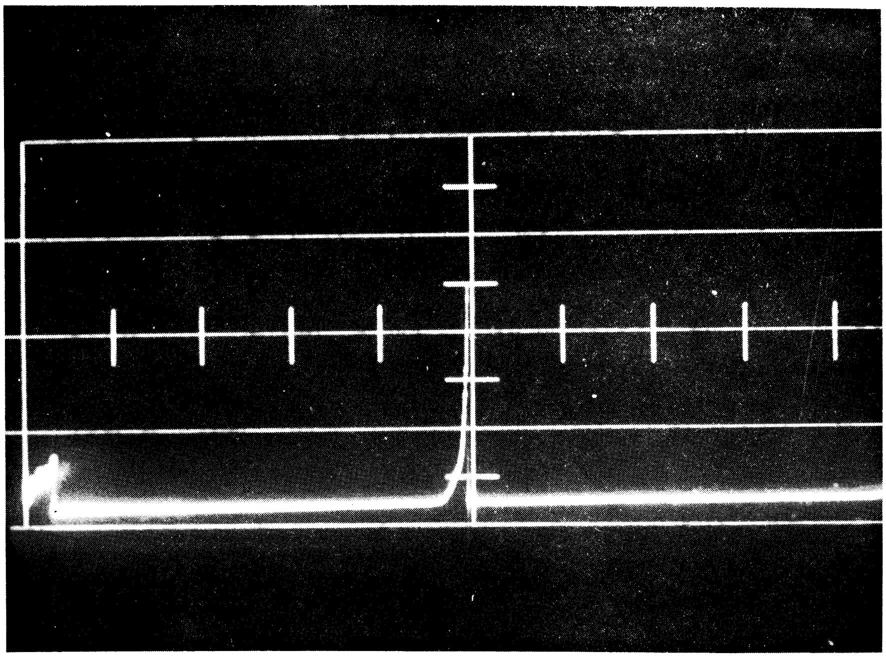


Figure 6A. Staged fuses at 3000 amperes. The first fuse filled with boric acid withstood the voltage from the second fuse which was oil filled. The sensitivities are 500 volts per division and 1 ms per division.

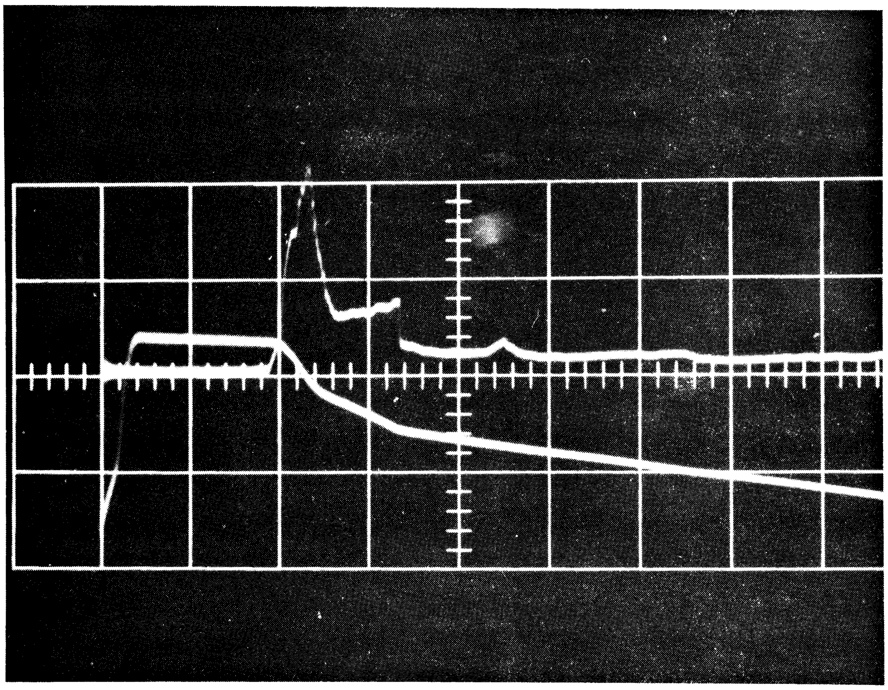
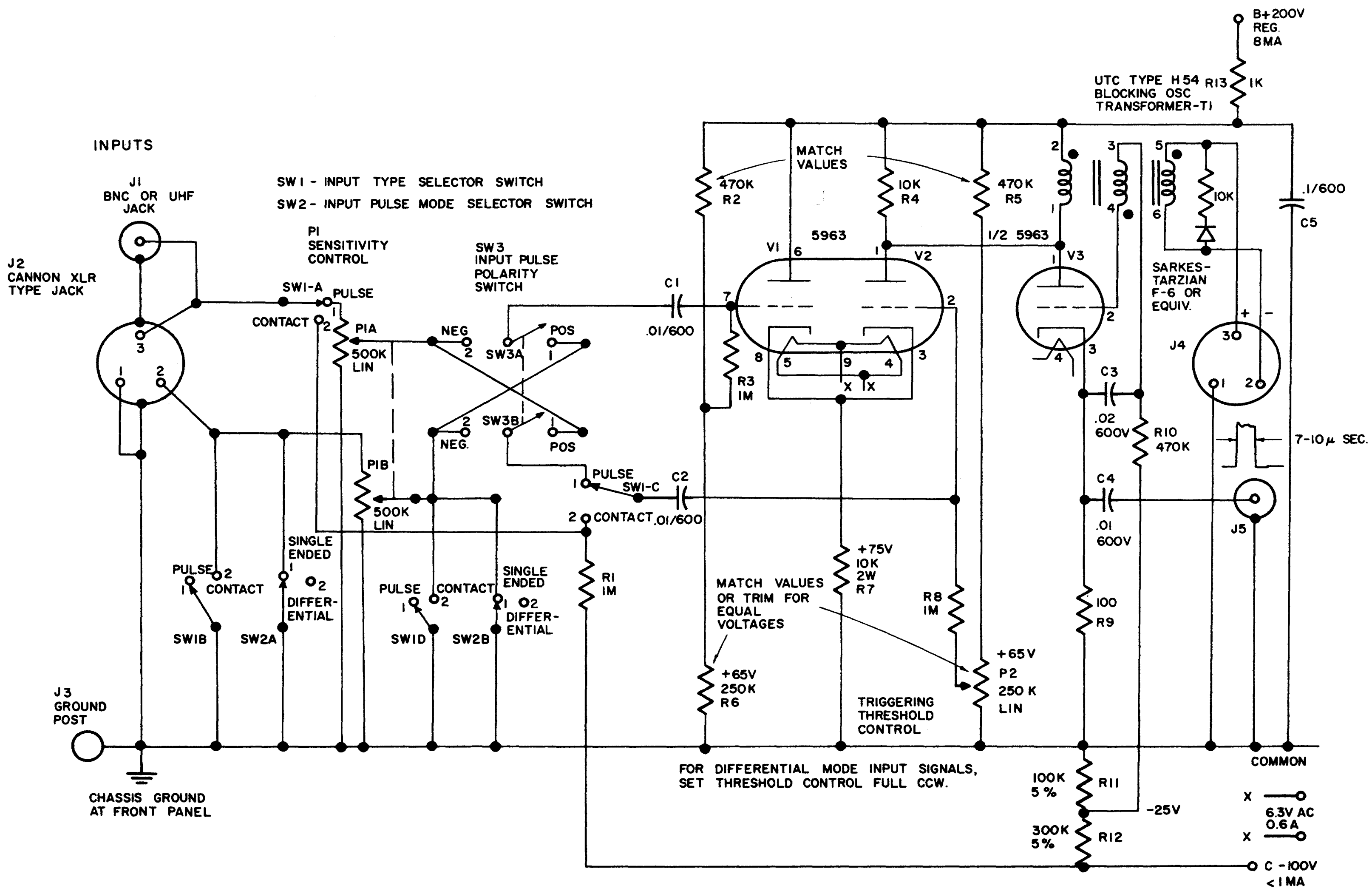
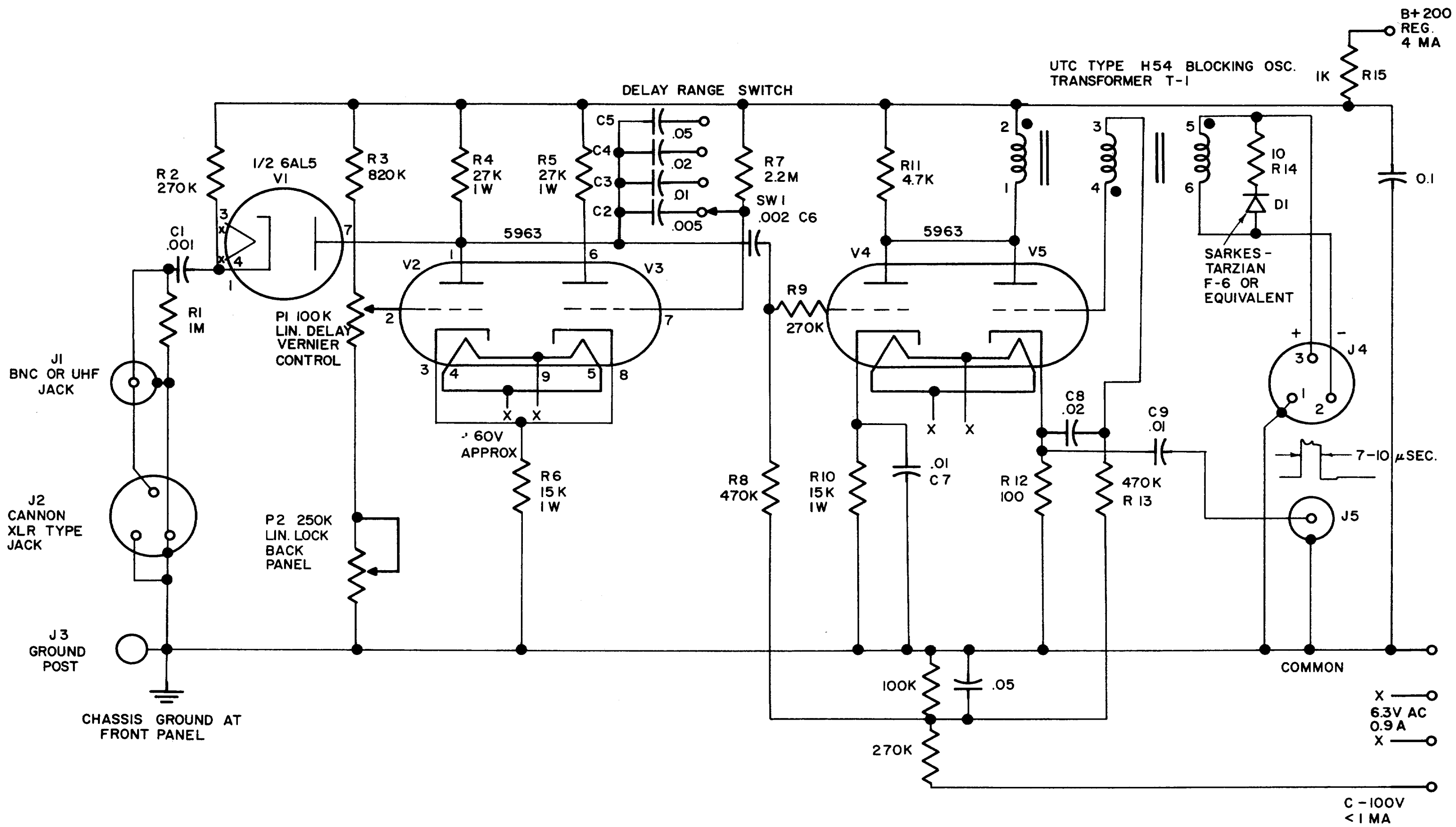


Figure 6B. A short fuse, 6 inches long, failure. The maximum useful voltage is 1.5 division or 15,000 volts. The current sensitivity (lower trace) was 2000 amperes per division and time, 2 ms per division.



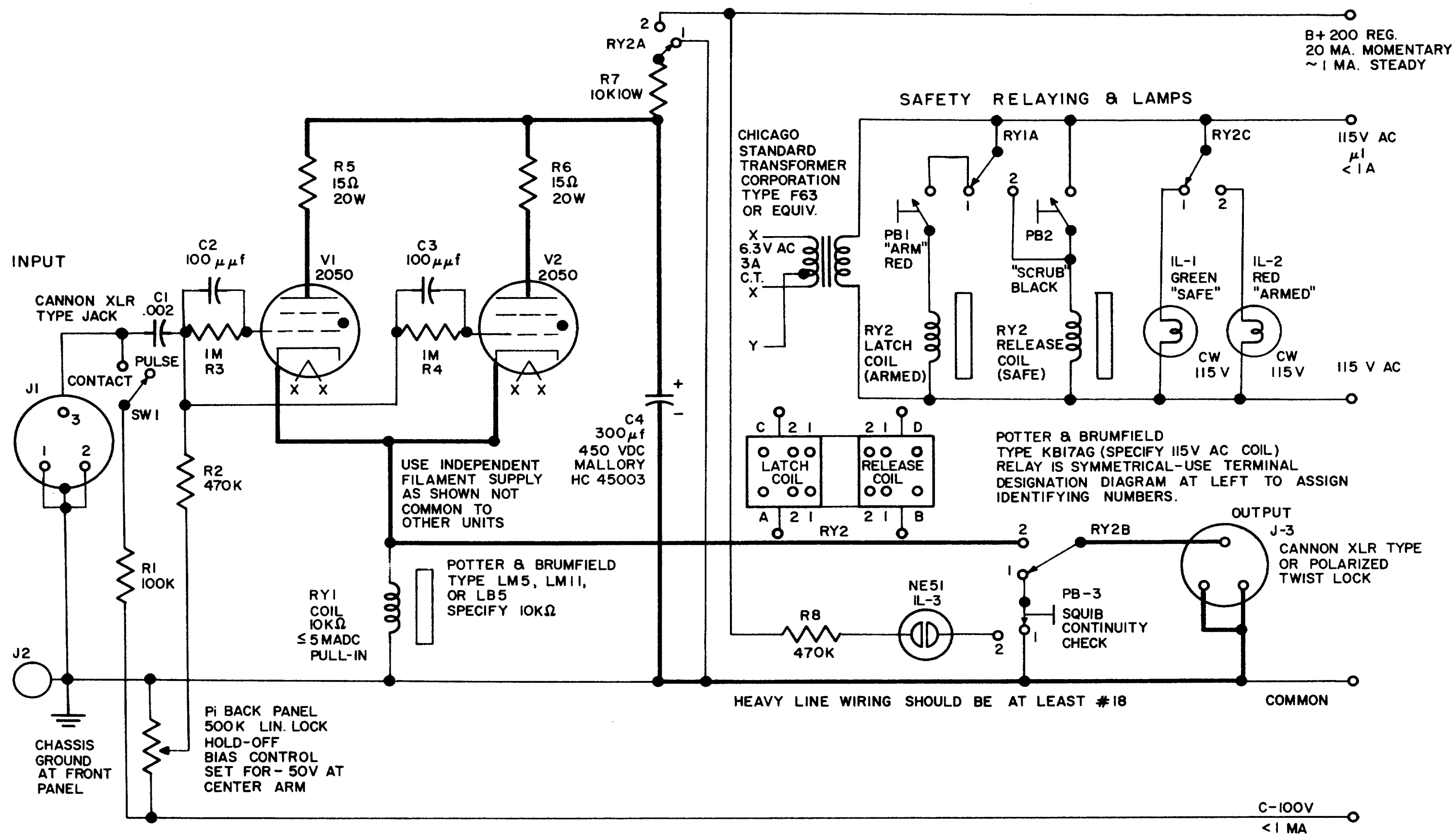
Notes: All resistors 1/2W 10%, except as marked. K = Kilo, M = Mega. Input signal required: >10V Ramp, Step, or Pulse with Rise Time of less than 10 milliseconds.
Output signals: +, -, or floating 150V @ 10 μ sec. and +20V @ 10 μ sec. Impedance levels: Input: 300KΩ ; Outputs: 1000Ω and 100Ω . Rise time ≤ 0.1 μ sec.

Fig. 7. Trigger generator.



Notes: All resistors 1/2W 10%, except as noted. K = Kilo, M = Mega. All capacitors 600 WVDC ceramic, mica, or high quality paper. C2 thru C5 10% tolerance or better. For initial calibration set range switch to C2, set P1 at CCW end (min), and then adjust P2 for 1 millisecond delay, using calibrated scope. Input signal required: Negative - going pulse > 50V and < 100 μ sec. Output signals: +, -, or floating 150V@10 μ sec. and +20 V@10 μ sec. Impedance levels: Input 20K Ω; Outputs: 1000 Ω and 100 Ω. Rise time $\leq$ 0.1 μ sec. to adapt circuit for delays shorter than 1 Msec. add capacitors smaller than .005 on range switch SW 1. For delay longer than 50 Msec. increase R-7. This unit is intended to follow a trigger generator.

Fig. 8. Pulse delay unit.



Notes: All resistors 1/2 W 10%, except as noted. K = Kilo, M = Mega. Capacitors, except C-4, 600 WVDC, ceramic or mica. Input signal required: Positive-going pulse > 50V with rise time < 1 M sec. or contact closure (with SW 1 in "contact" position). Unit is intended to operate from contacts, trigger generator, or pulse delay unit output. Output: 200V into open circuit; 20A into short circuit with time constant of discharge 3 M sec. Typical Squib Igniter is blown by this circuit in 100 μ sec. Typical chemical lag in squib is 2 M sec., after electrical ignition

Fig. 9. Squib or cap firing unit.

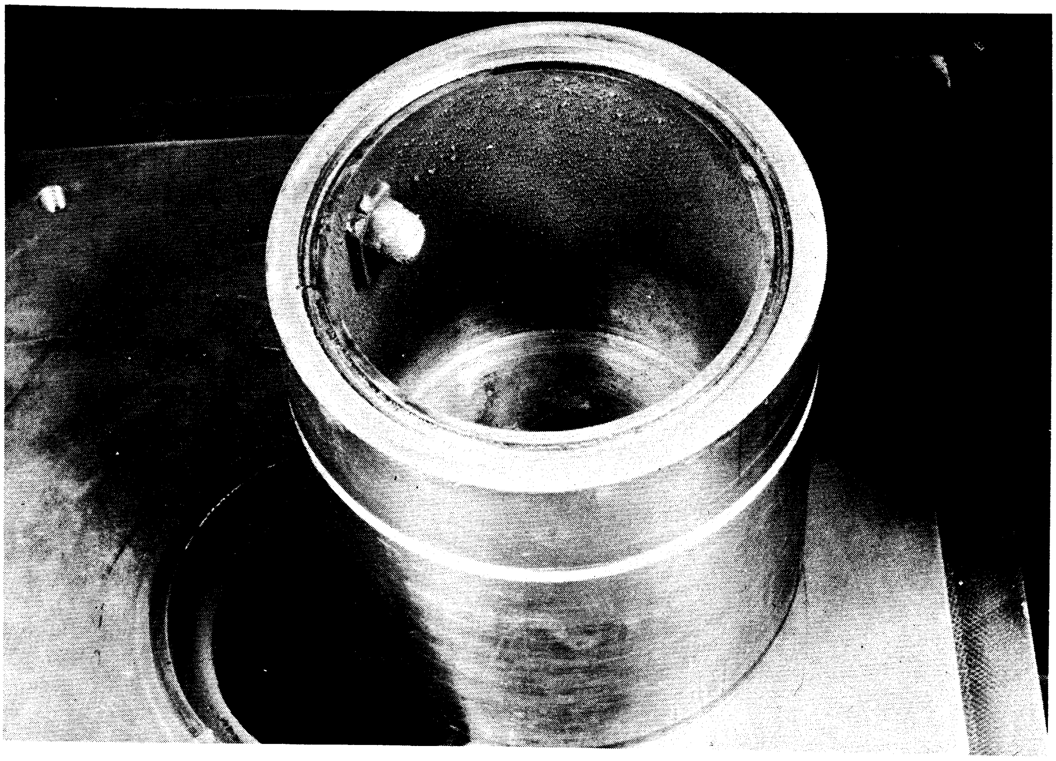


Figure 10A. The anode, the upper half of the chamber, after the first experiment at 30,000 amperes and 54,000 joules.

Figure 10B. The cathode after cleaning with soap and water.

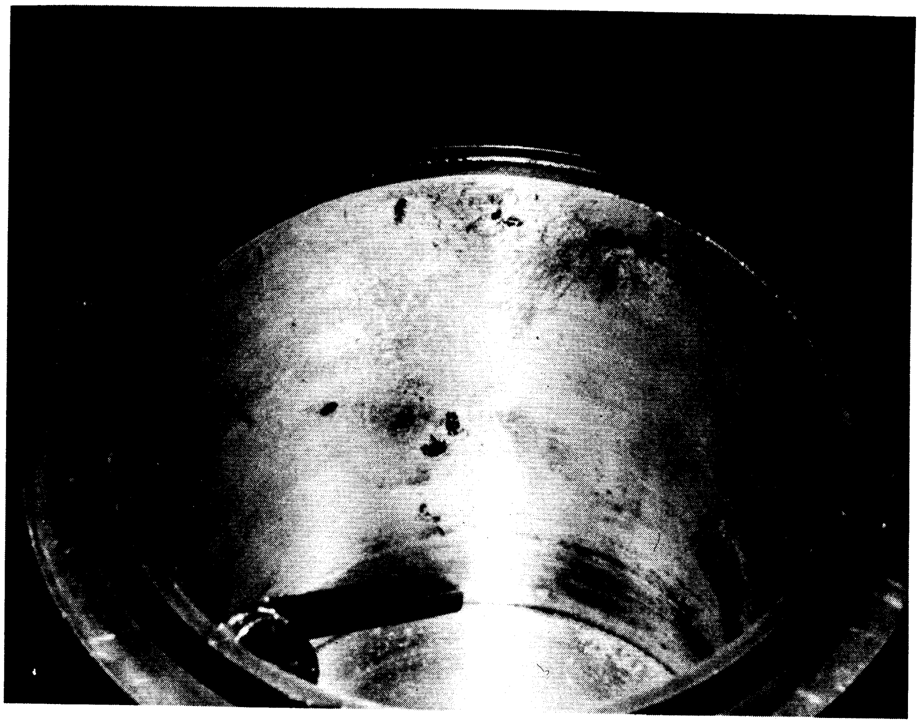


Figure 11A. Fine traces on the anode after the second test at 31,000 amperes.

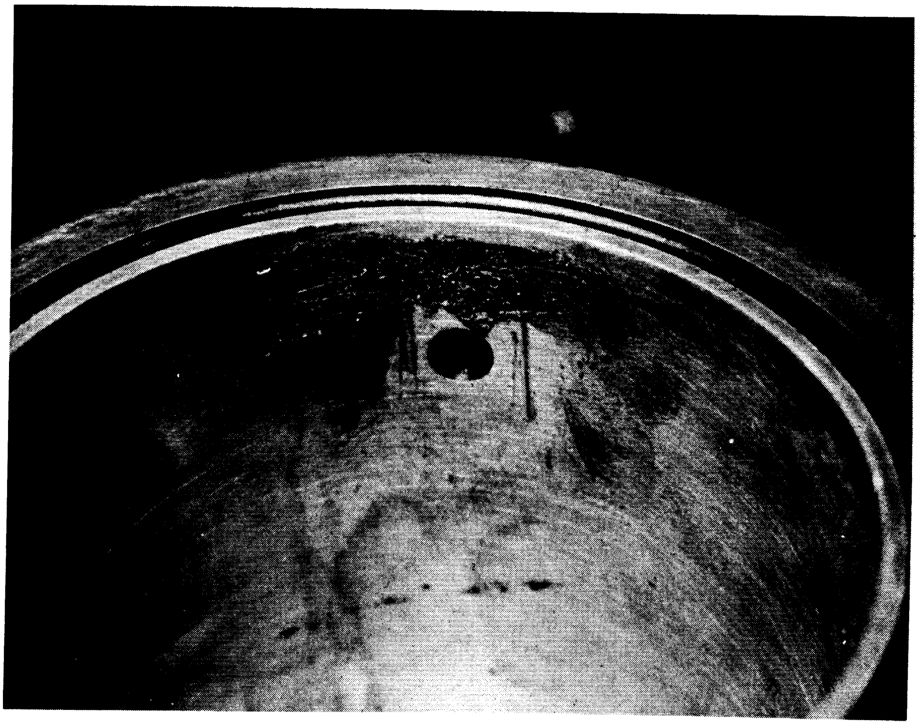


Figure 11B. The cathode after 45,000-ampere experiment.

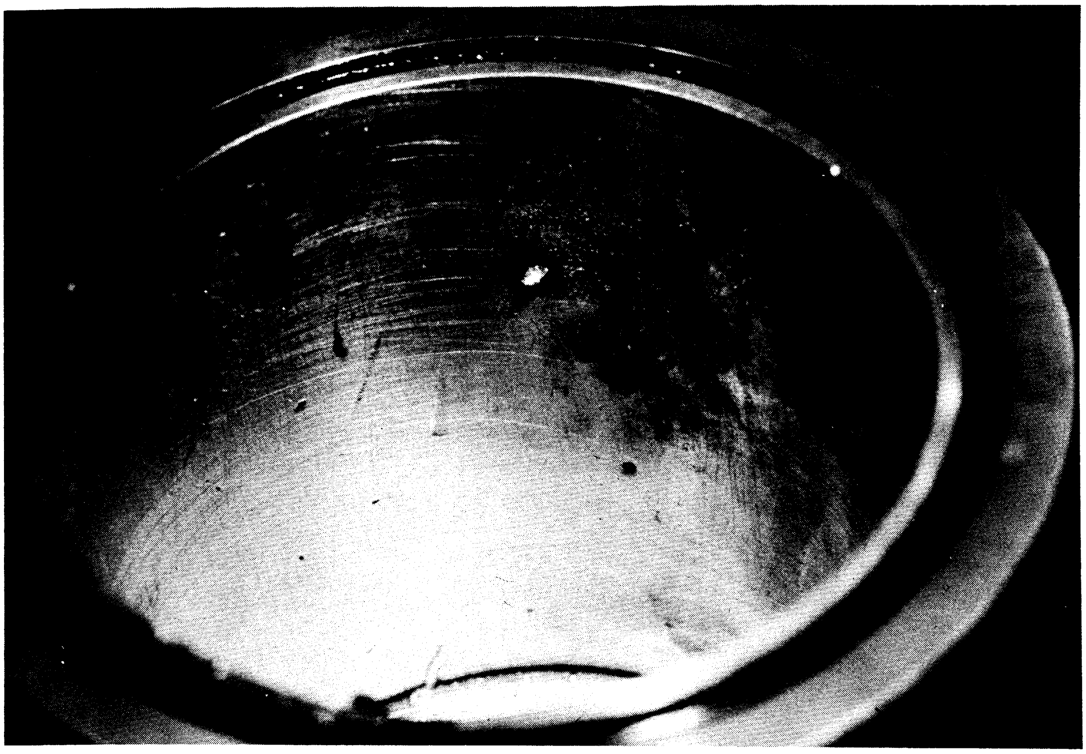


Figure 12A. The cathode marks at I_0 = 100,000 amperes, P_0 = 1500 psi. The anode for this test is shown below.

Figure 12B. The anode marks.

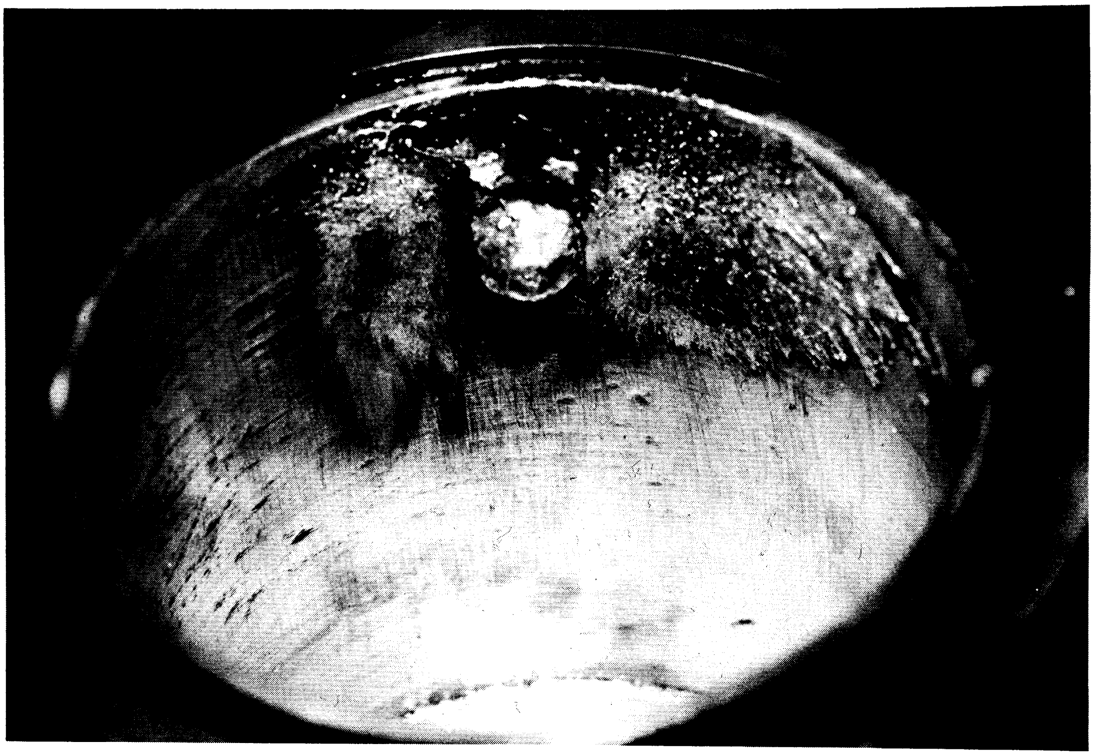


Figure 13A. The cathode spot at the fuse terminal at I_o = 100,000 amperes, P_o = 1500 psi.

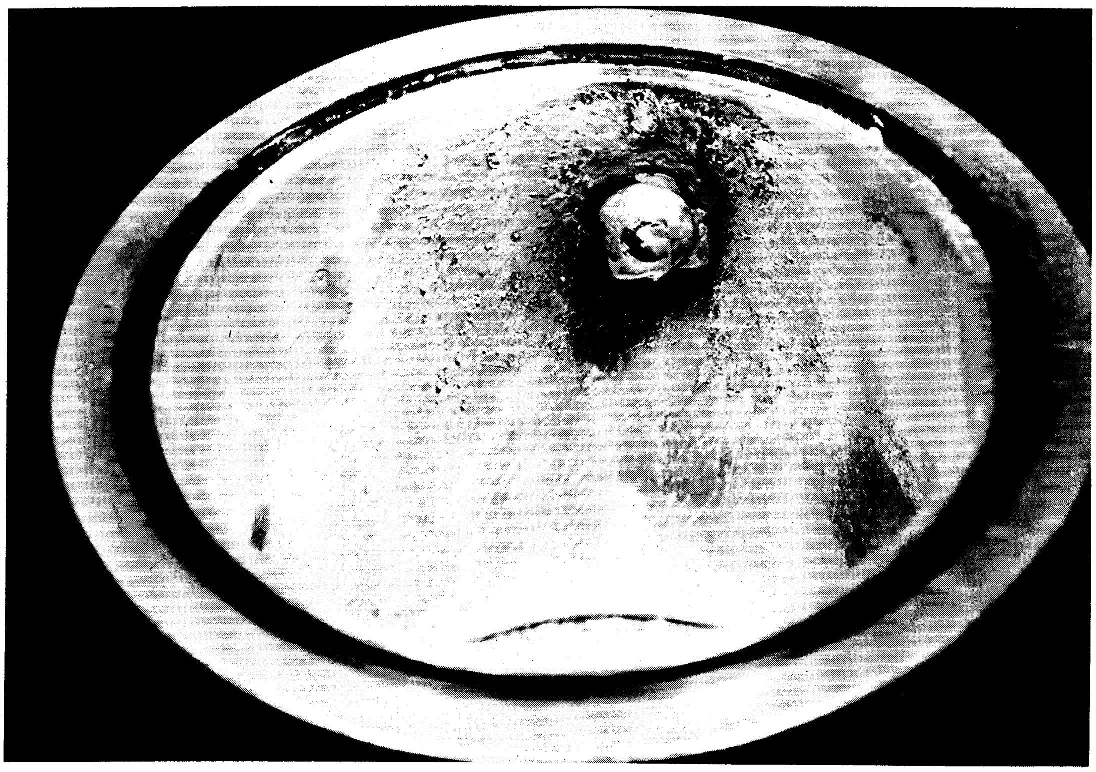
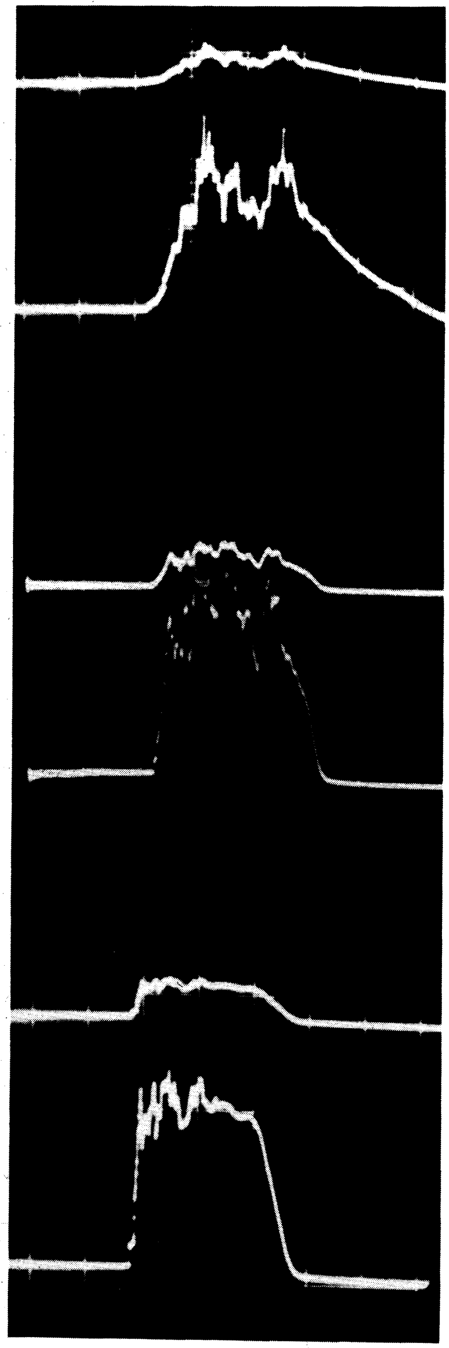


Figure 13B. The cathode spot for I_o = 100,000 amperes, P_o = 150 psi.



P_o = 150 psi

P_o = 400 psi

P_o = 1500 psi

Figure 14. The voltage across the arc chamber for three initial densities at I_o = 100,000 amperes. Each voltage is recorded with two traces, the upper at 3500 volts per division and the lower at 700 volts per division.

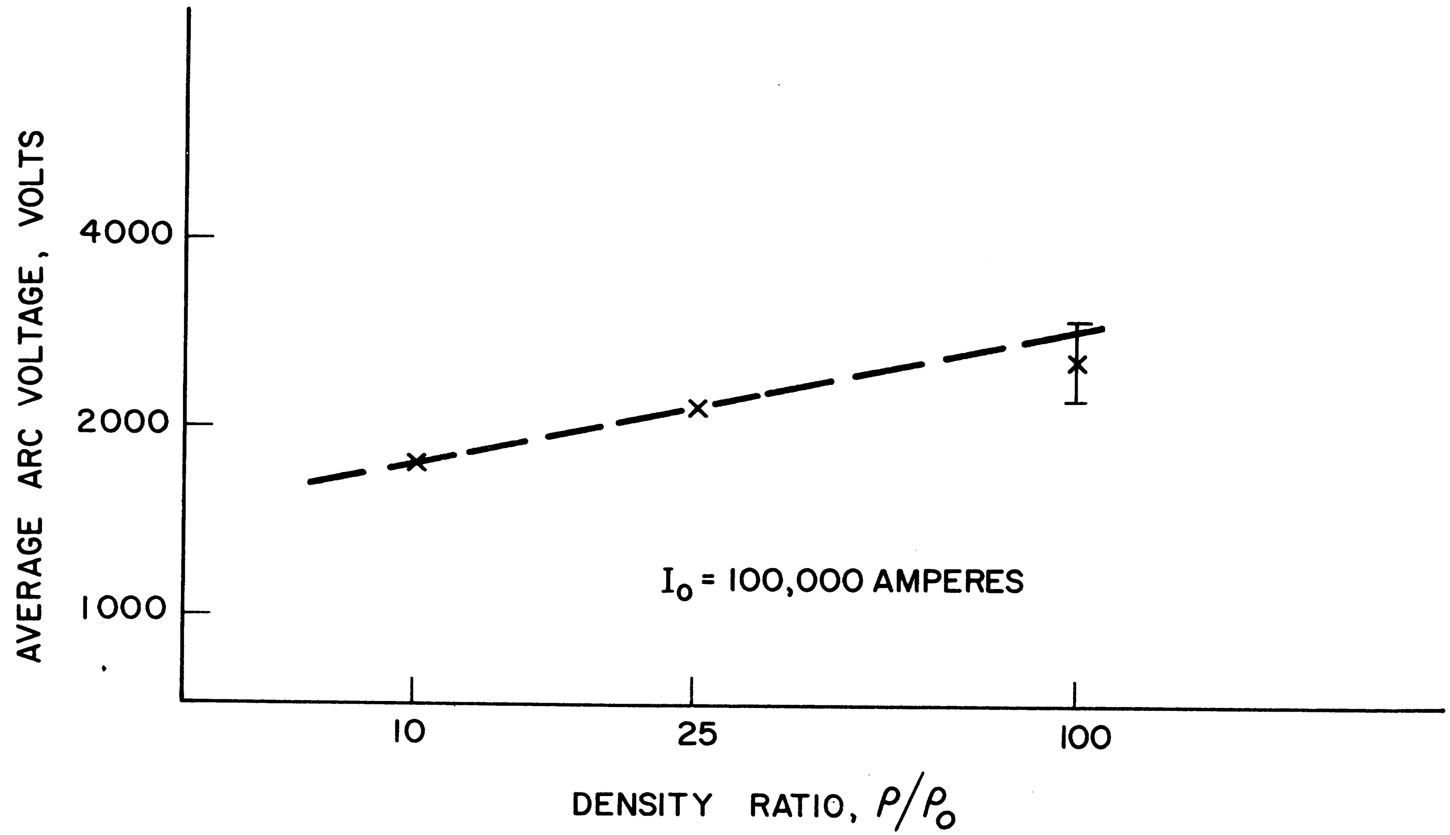


Fig. 15. Voltage dependence on initial chamber gas density.

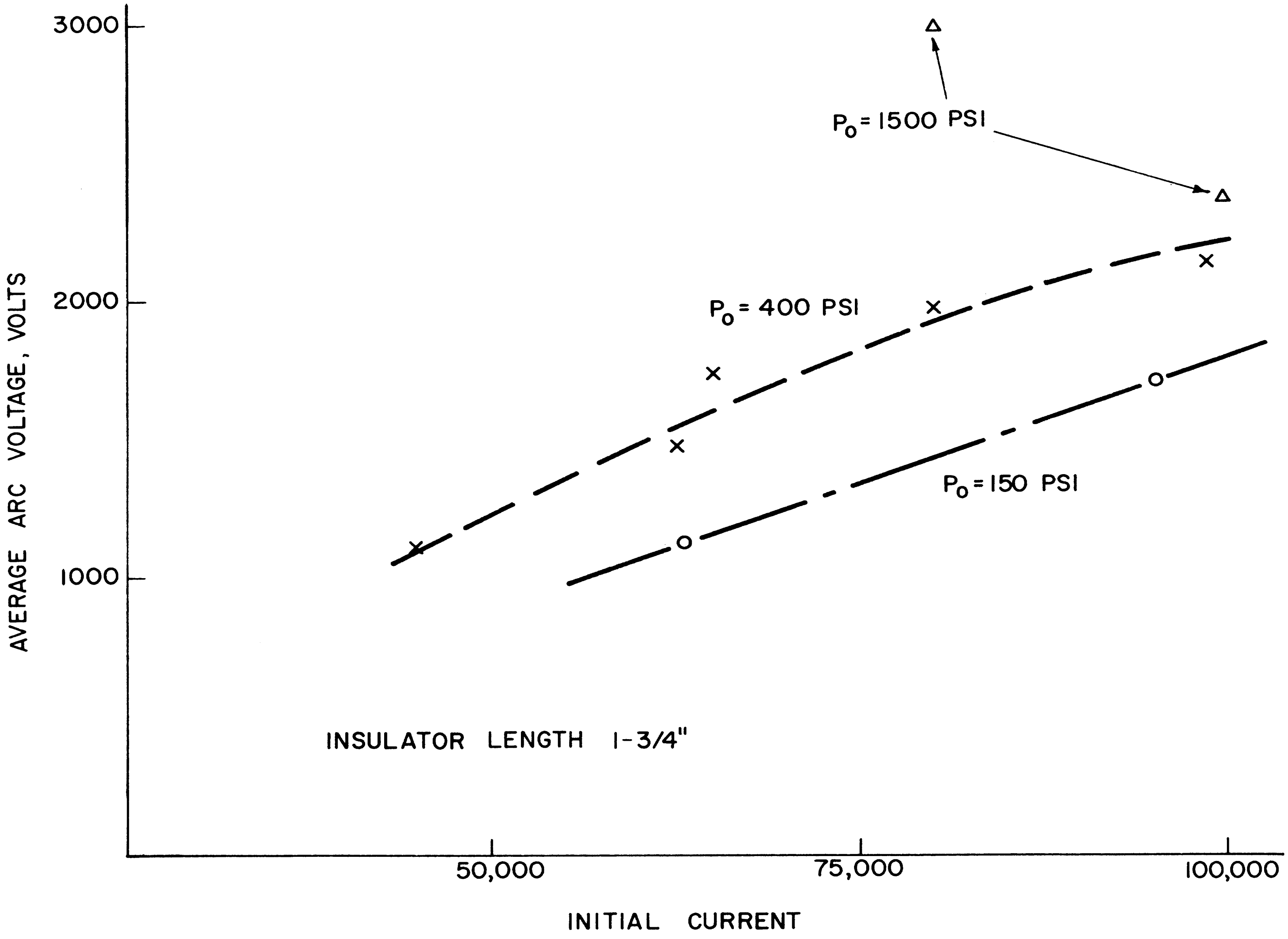


Fig. 16. Arc chamber voltage.

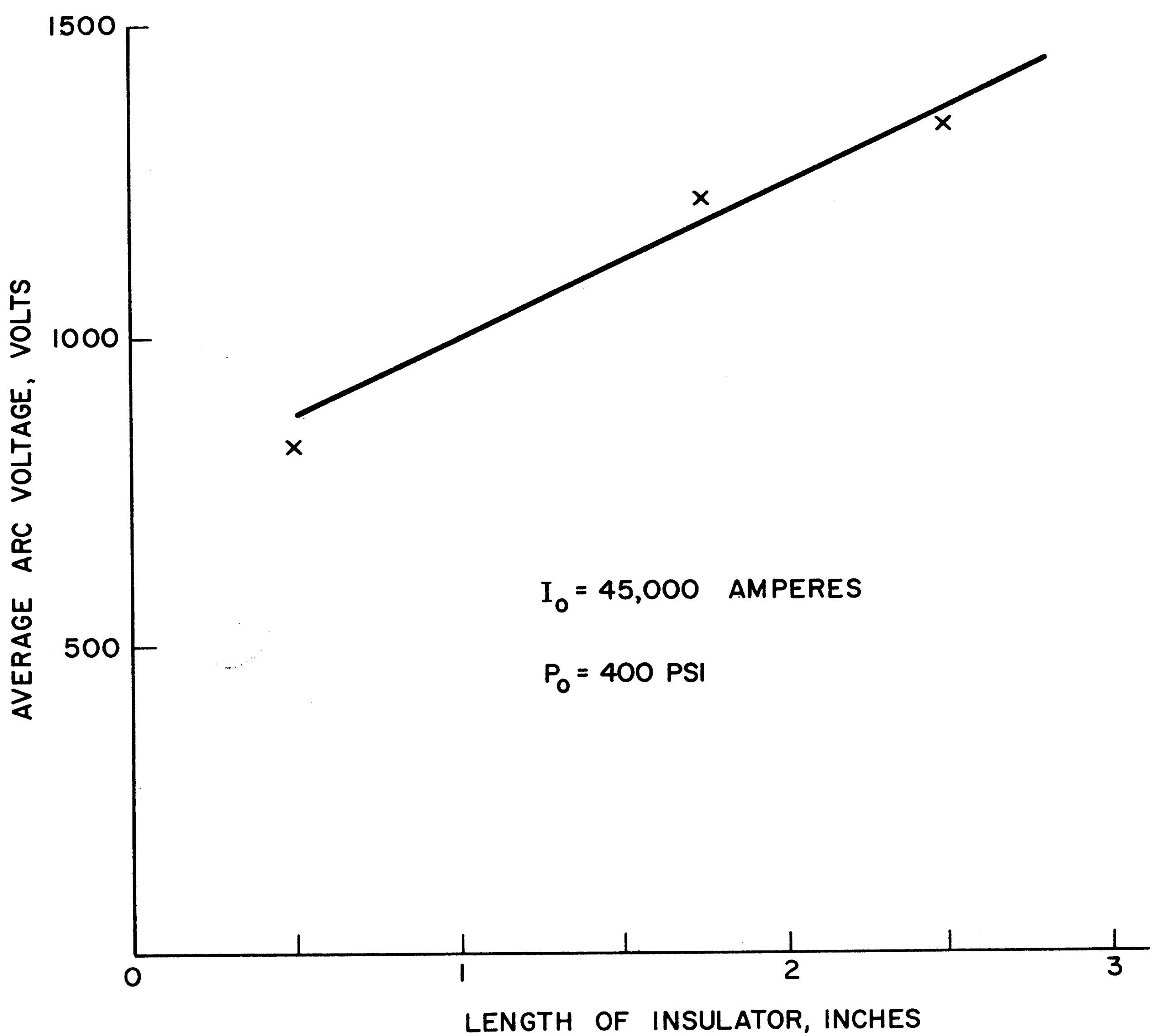


Fig. 17. Insulation effect on arc voltage.

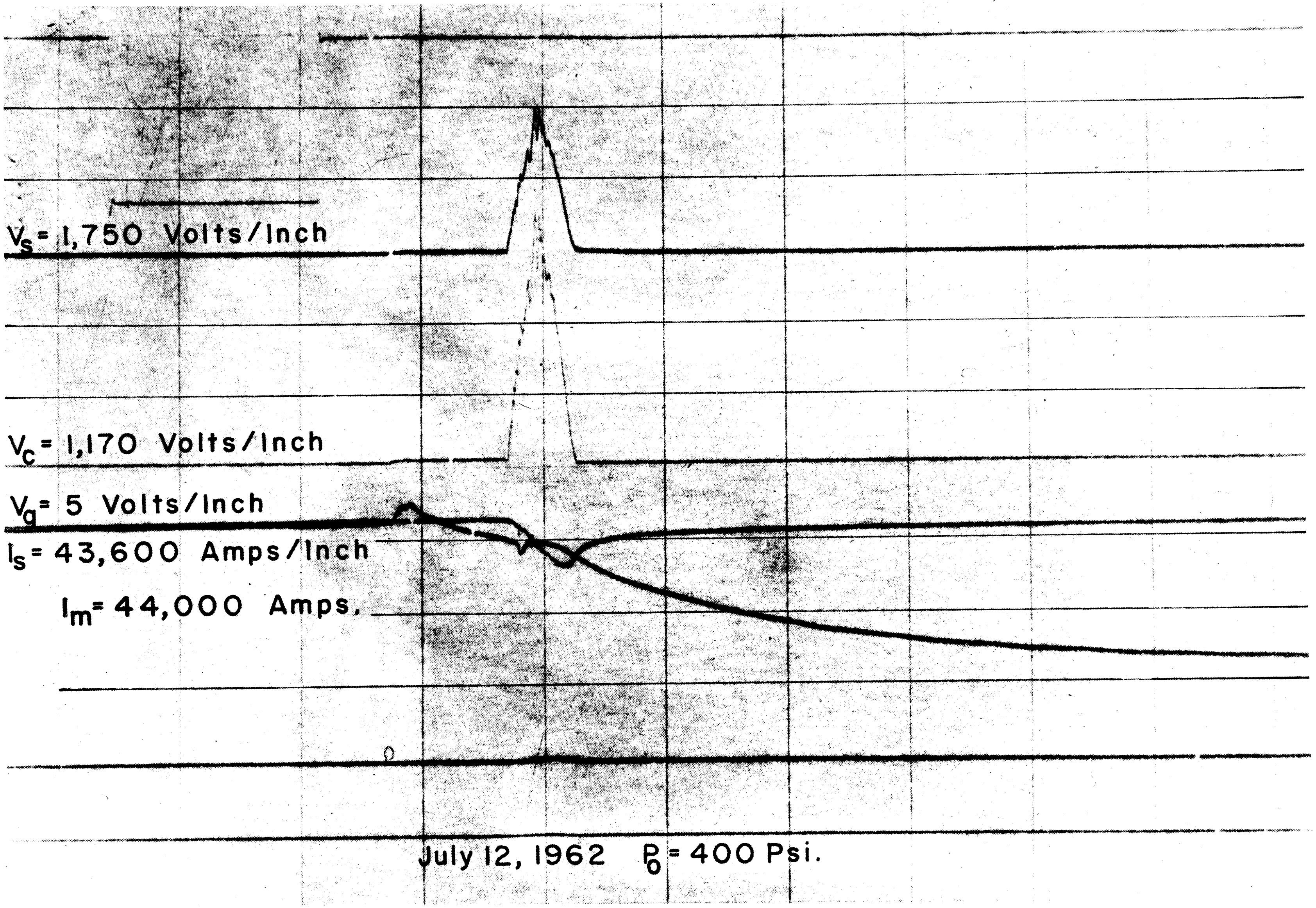


Figure 18. Original data.

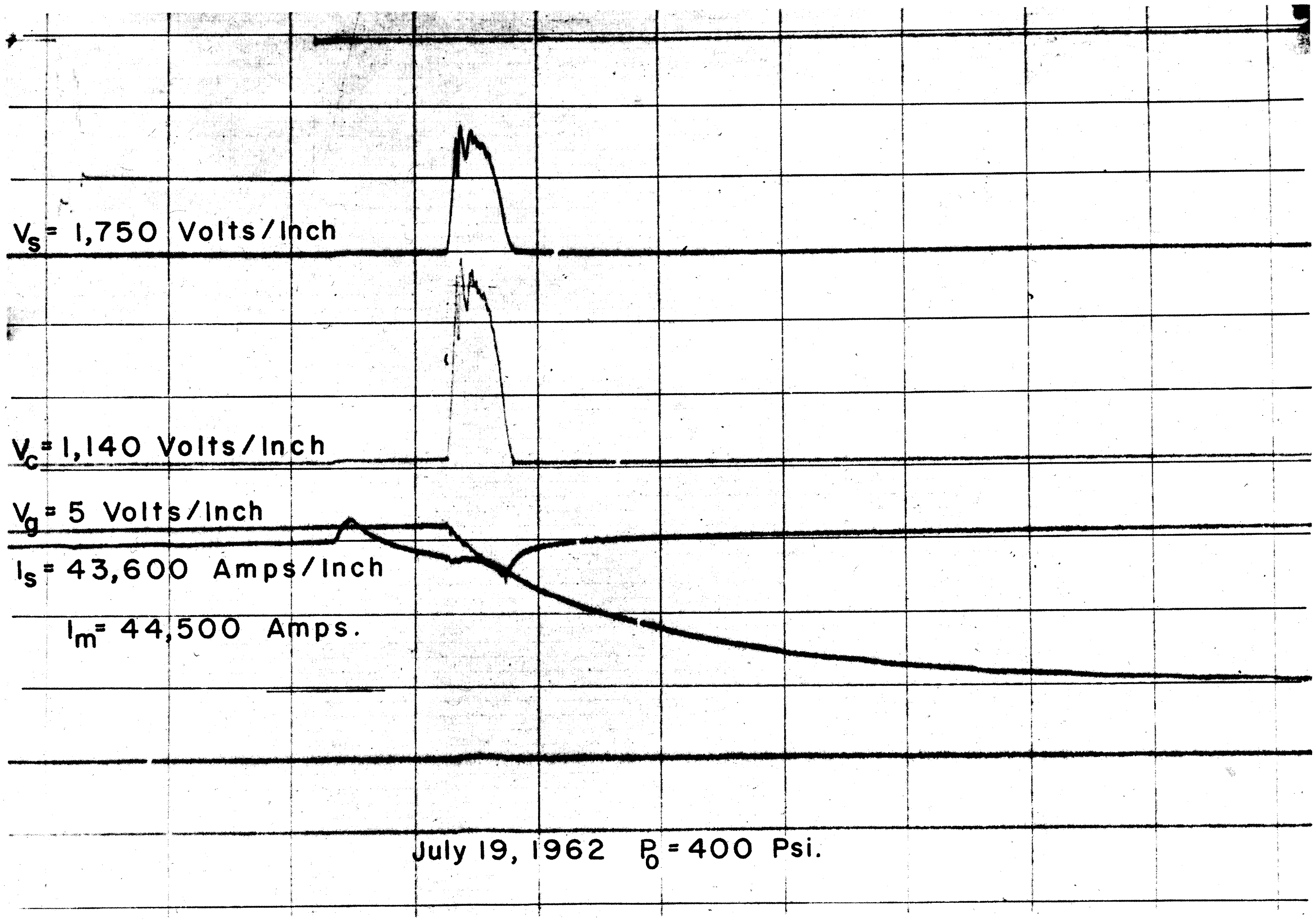


Figure 19. Original data.

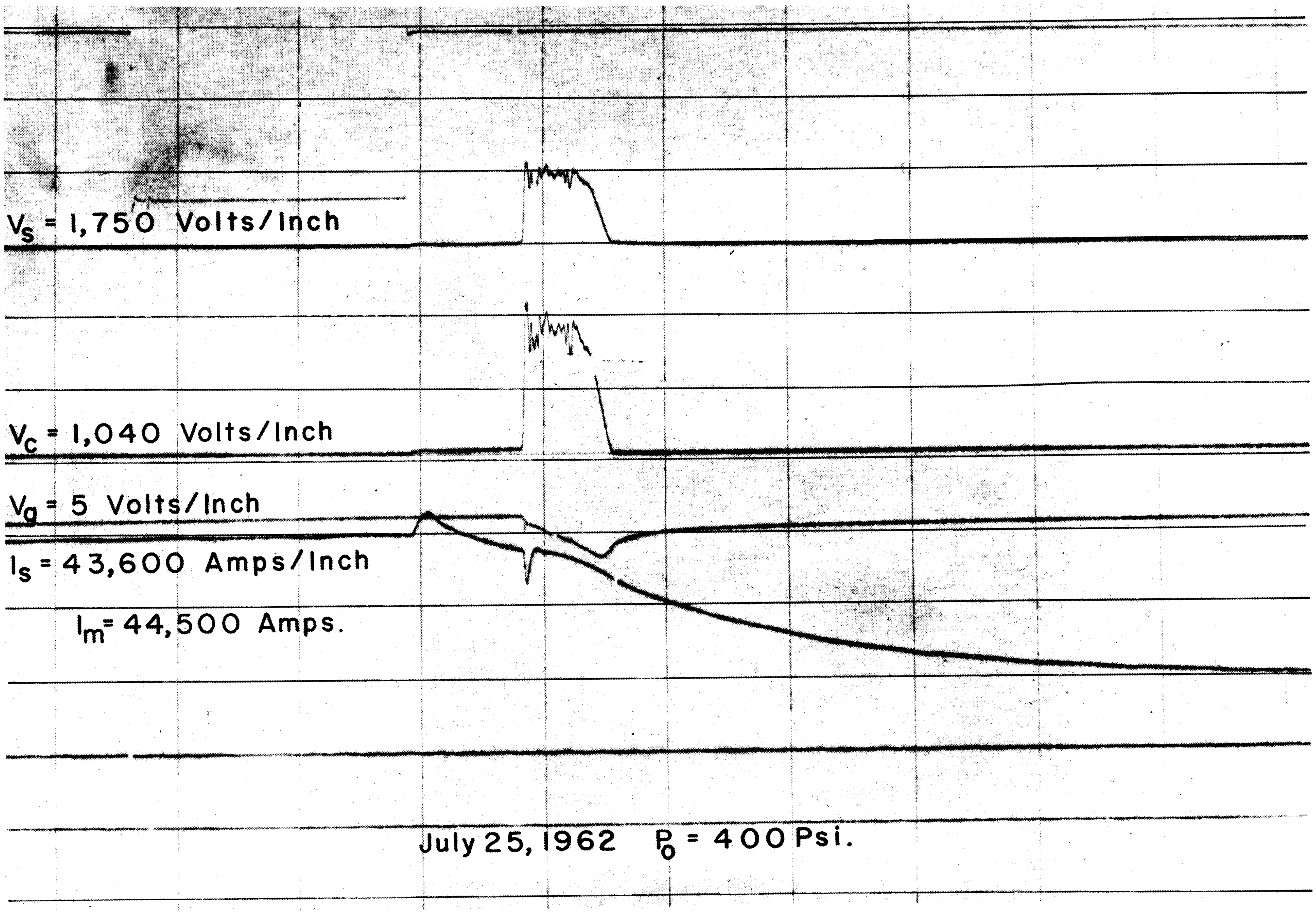


Figure 20. Original data.

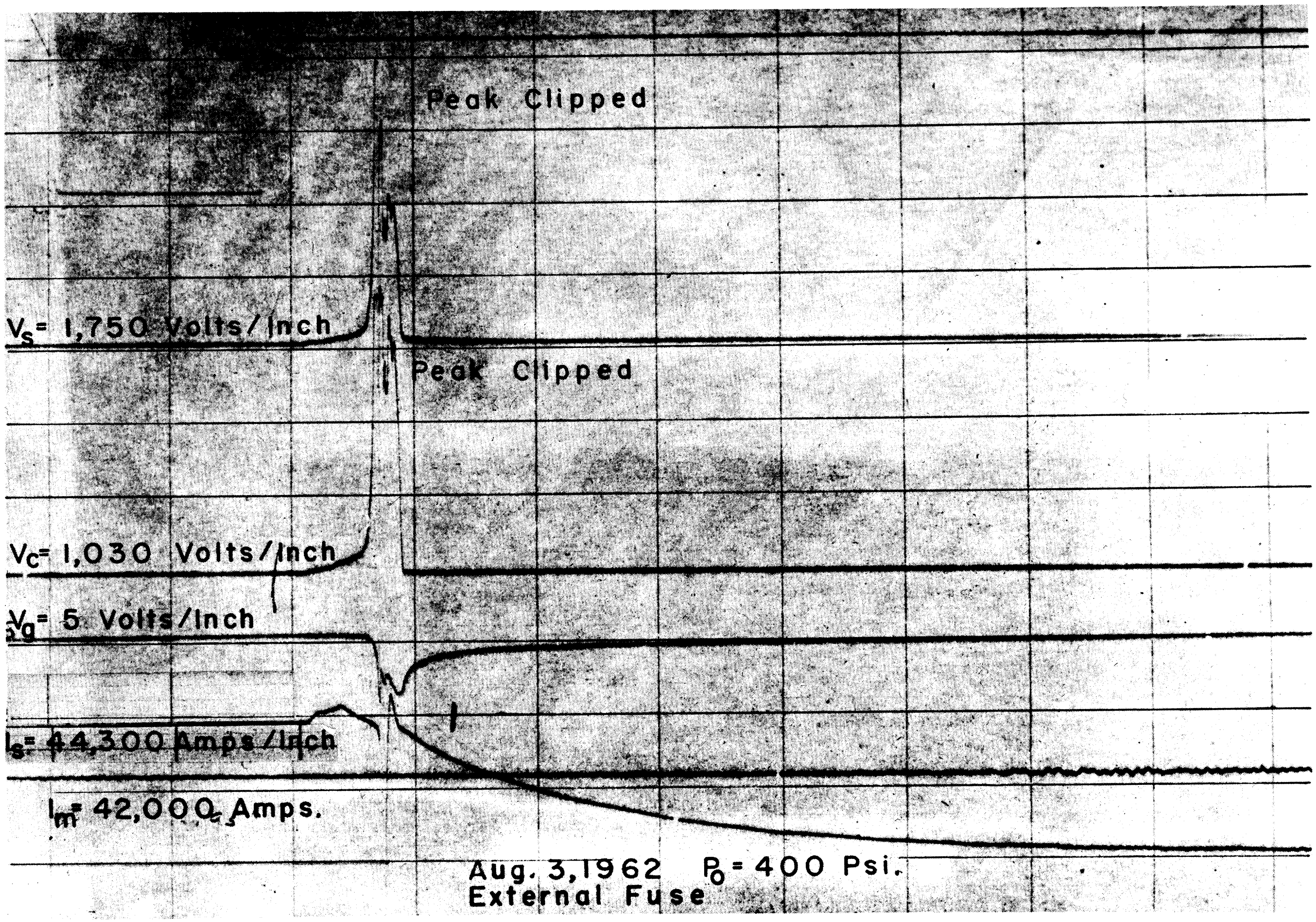


Figure 21. Original data.

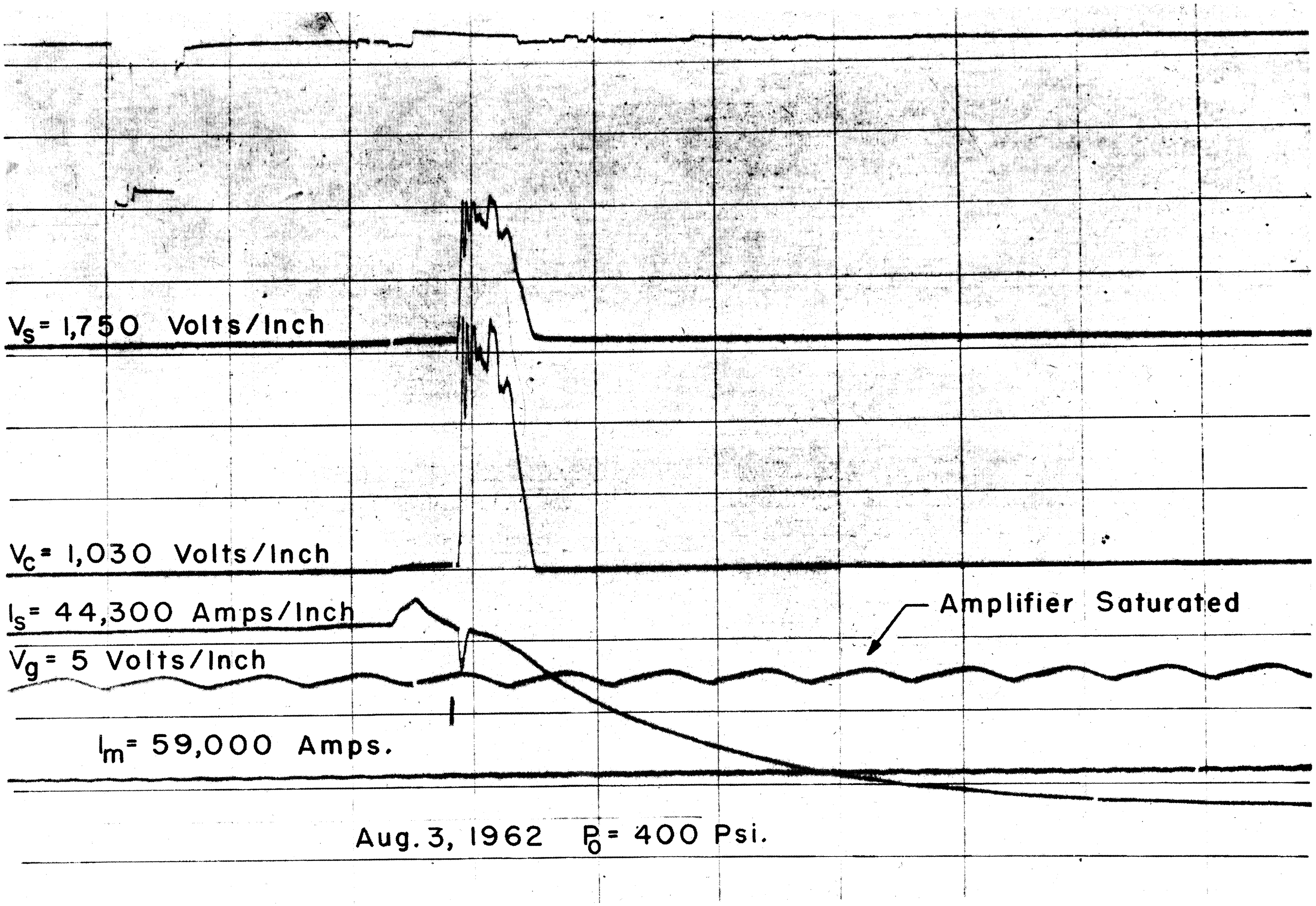


Figure 22. Original data.

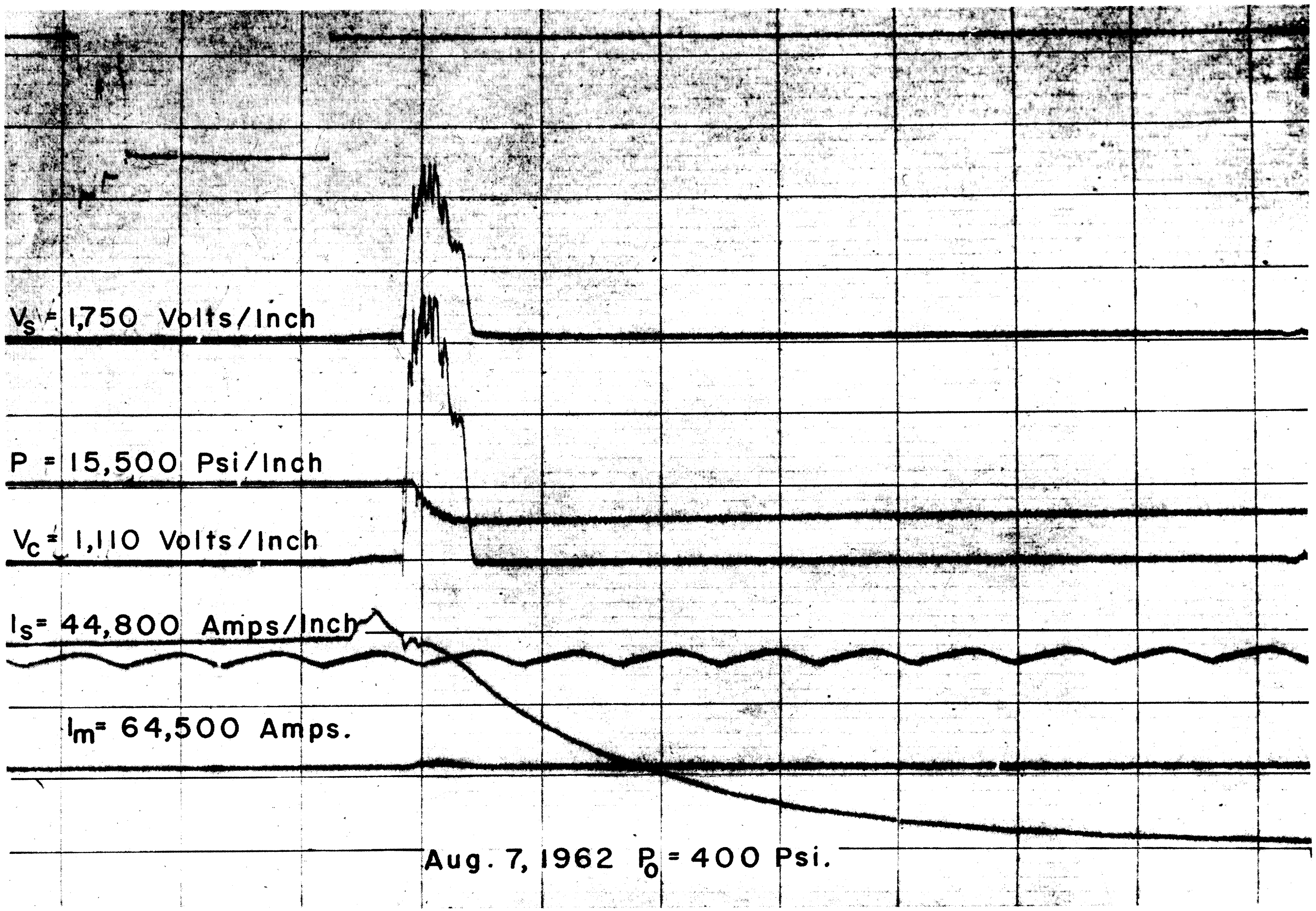


Figure 23. Original data.

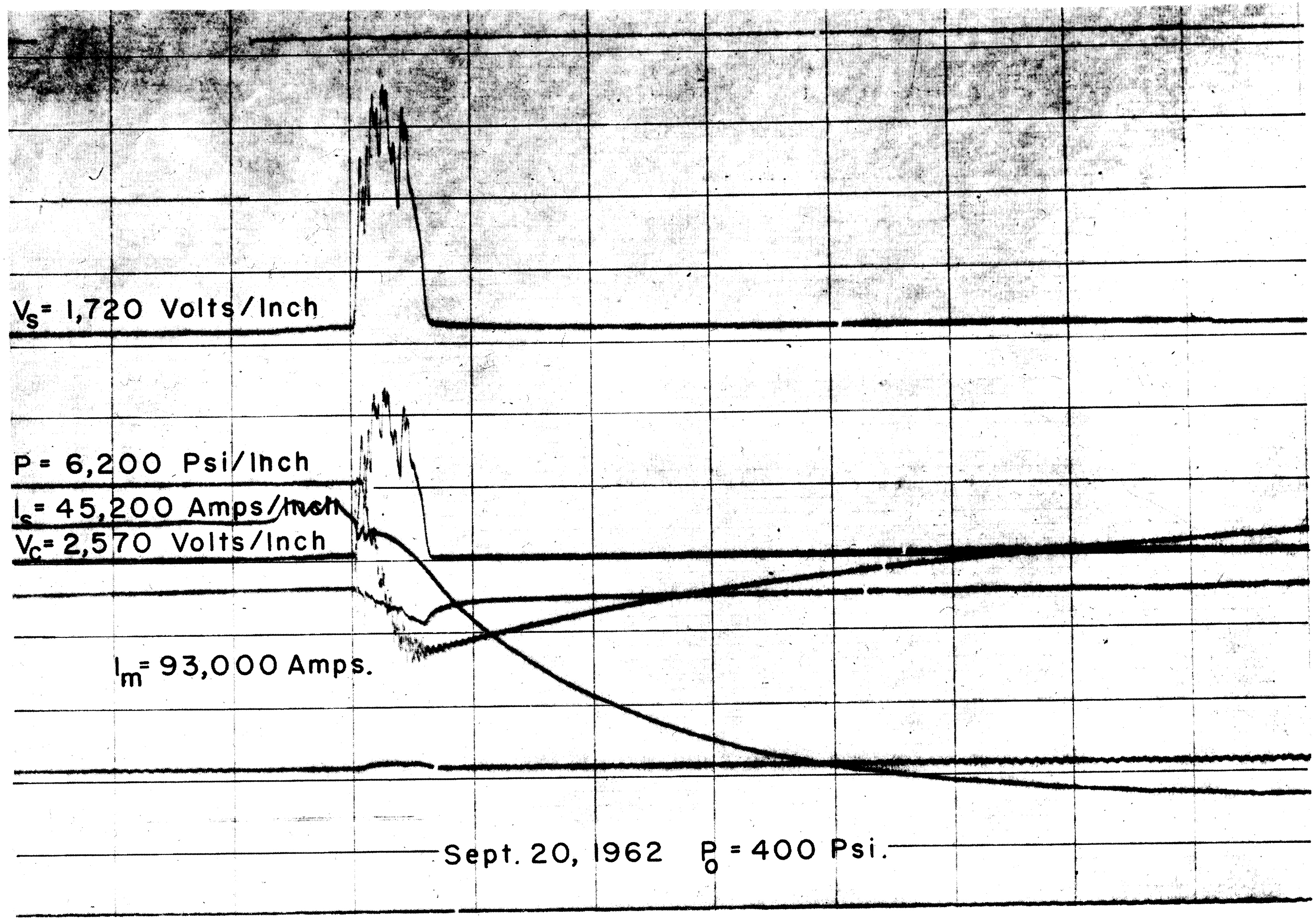


Figure 24. Original data.

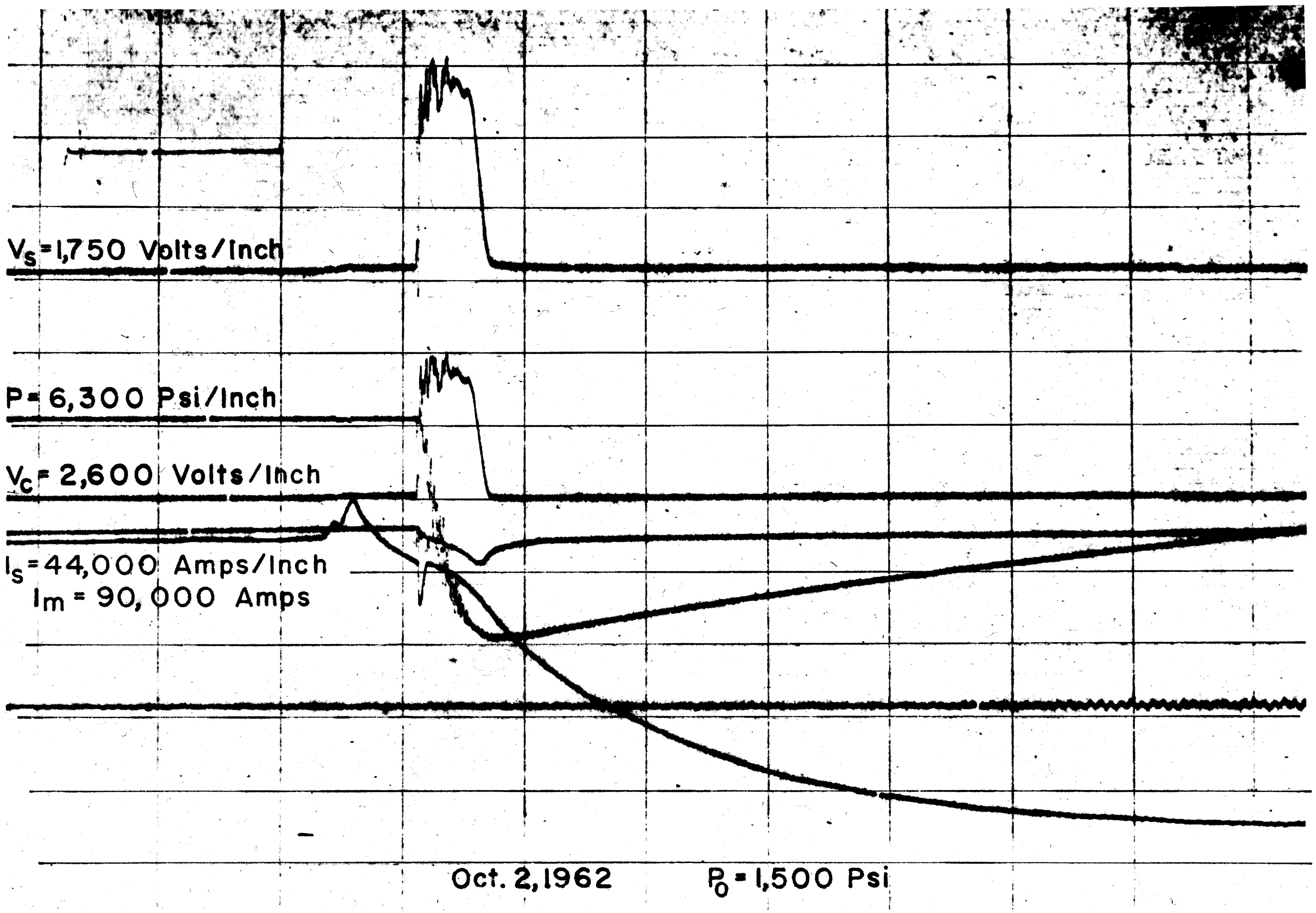


Figure 25. Original data.

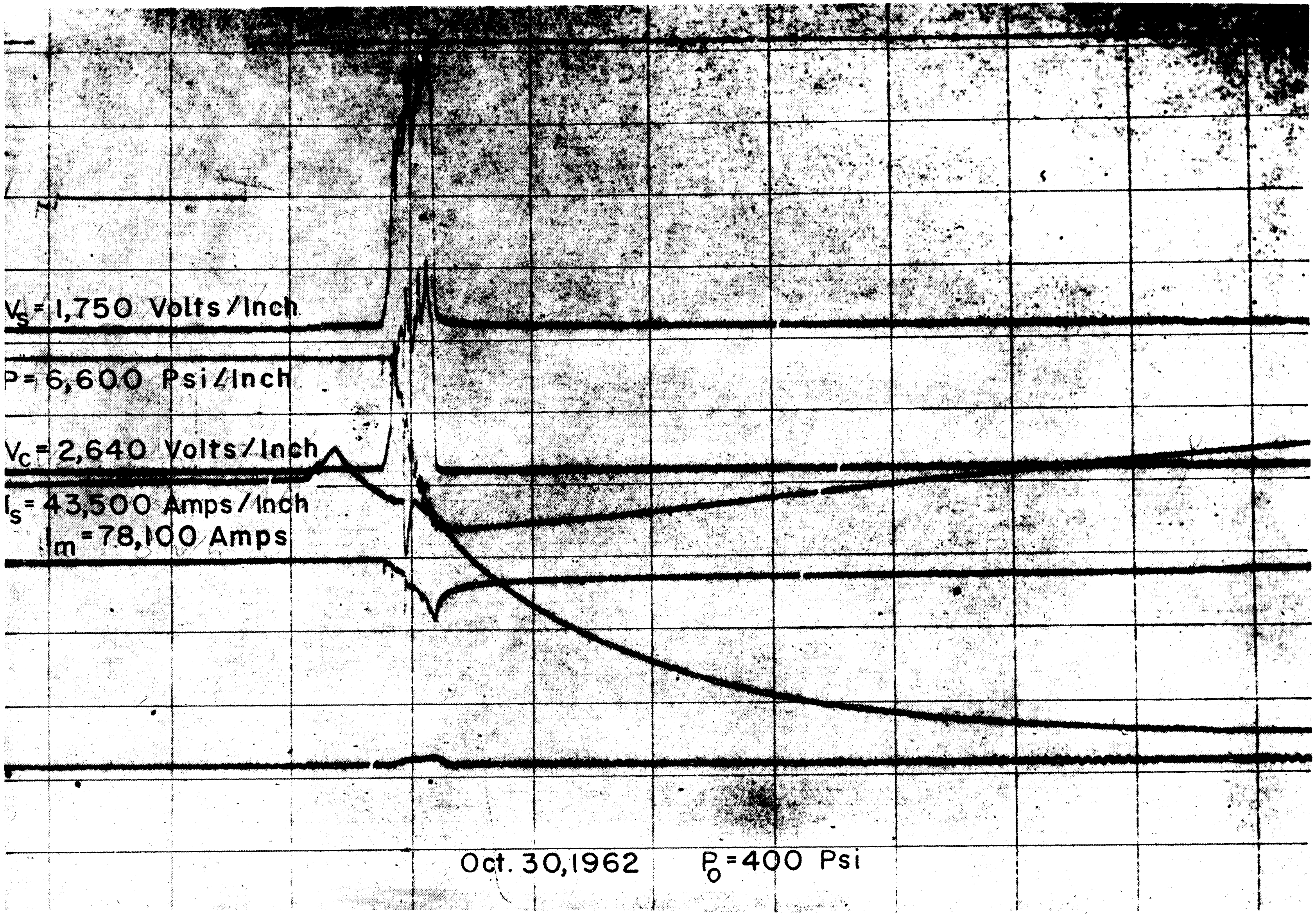


Figure 26. Original data.