Output conditioning techniques for flux compression generators

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OUTPUT CONDITIONING TECHNIQUES FOR FLUX COMPRESSION GENERATORS

By

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A doctoral thesis submitted in partial fulfilment of the requirements for the award of Doctor of Philosophy of Loughborough University

March 2003

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In
Memory
Of My
Father
This thesis discusses, evaluates and, where possible, demonstrates the opening and closing stages of switching techniques and circuit arrangements needed to condition (sharpen) the output current pulse of a helical flux compression generator. The sharp rise in resistance that accompanies the rapid fusing of a thin metal foil (termed a fuse) opens the circuit in the initial stages, and a fast acting plasma erosion switch (PEOS) whose output circuit is magnetically insulated at the end of its opening phase provides the same duty in the final stage.

The study is directed towards a generator having a 1 to 2 MJ and multi-MA rating accumulated in about 150 microseconds, the ultimate aim being to produce an output current pulse rising to several hundred kiloamperes in approximately ten nanoseconds in a short circuit load, the accompanying loss of energy being of secondary importance. Additionally, the output circuit needs to be able to sustain high-voltages (MV) rising in a few nanoseconds across a purely resistive load.

Brief considerations of flux compression technology and experimental methods (Chapter 1 and 2) and various switching processes (Chapter 7) are included to provide a wide perspective to the main objectives. The capacitor banks, associated pulsed power, firing control and diagnostic tools developed in support of the experimental programme are described, and the calibration methods adopted and the procedures followed when conducting experiments both in the laboratory and on the firing range are discussed.

Details are presented of the design, construction and testing of both a helical flux compressor (called Flexy) and a PEOS switch. The bulk of the thesis concentrates however on an extensive experimental programme, conducted both in the laboratory and on the firing range.

The characteristics of the conditioning circuit switching components are derived from the experimental results and subsequently used in a computer program for numerically simulating the behaviour of conditioning circuits. The thesis concludes with a proposed design for a conditioning circuit that satisfies the central aim of the investigation.
Acknowledgements

Many people deserve recognition for their contribution to the work described in the thesis, and to the thesis itself. These include my friends and colleagues Peter Senior, and Peter Butterfield, and various laboratory technicians who assisted with the experiments conducted on the firing range and Mehdi Miran who assisted in the laboratory. My thanks are also due to Bucur Novac for many fruitful discussions and for his interest in and valuable contribution to the computer modelling aspect of the work. I am indebted to my supervisor Ivor Smith for his encouragement and support, and to his personal efforts in proofreading and correcting the text, and to Lynn O’Byrne for typing the first draft. The staff of the departments engineering workshop must also be recognised, and I am pleased to note that they offered their help whenever it was needed despite many unreasonable demands on their time. John Lyons of the Military Division of DRA at Fort Halstead Kent who organised the funding for the work and maintained a close interest in its progress. Finally I must mention my brother Keith and other close friends; without their encouragement, support and patience I could not have done it. I wish my dad could have seen the result.
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Chapter 1

INTRODUCTION

A flux compressor can be loosely described as a current charged (or primed) inductor, which is closed to trap the internal magnetic flux before being rapidly reduced in volume, usually by means of explosives. The consequent increase in the internal energy density and the net energy in the final volume of the inductor provide a very useful high energy (MJ) and current (MA) inductive energy source for many pulsed power applications.

An inductive energy source offers a number of advantages over a capacitive one for high-current short-duration applications. Perhaps the most important one of these is that the maximum energy, which is limited by the current-carrying capacity and the mechanical strength of the materials involved, can be orders of magnitude greater than that of a capacitive source density, where the limitation depends solely on the electrical breakdown strength of the materials. However, to transfer current from a closed inductive source to a load requires the circuit to be opened, whereas a capacitive source can be connected directly to the load.

The time to achieve the maximum current output (termed the run-time) of a high energy flux compressor can be up to several hundred microseconds, which means that the output current must be conditioned (or sharpened) to suit the many applications that need a current pulse rising to a maximum value in microseconds or even nanoseconds.

The resistance characteristics of thin metal foils (conventionally termed fuses) connected in the inductor circuit provide a simple but effective means of reducing the rise time of the output current. The fast rise in resistance and consequent high voltage that accompany the explosive action of a fuse effectively open the circuit, thereby enabling current to be rapidly switched to a load. Several stages of this process, each providing additional conditioning, may be needed to bring about the necessary shortened rise time of the output current. Where nanosecond rise times are required, a plasma erosion opening switch, which can open a high current circuit in about ten nanoseconds is included in the final conditioning stage.

1.1. Aims of thesis

The bulk of this thesis concentrates on an extensive experimental programme that was undertaken in relation to a 1 MJ flux compression generator, both in the laboratory and
on a firing range, to demonstrate where possible the performance of the individual components of a conditioning system and to model their characteristics so that an assessment can be made of proposed overall circuit arrangements. The computer modelling and numerical programming for this aspect of the work was provided by Dr B M Novac of the Electronic and Electrical Engineering Department at Loughborough University. A central aim of the thesis is to produce an output current from the generator driven conditioning system rising to several hundred kiloamperes in about ten nanoseconds, and to have the ability to produce a pulse of several megavolts across a purely resistive load.

1.2. Structure of thesis

Chapters 2, 3 and 7 provide a wide perspective to the main objectives of the thesis, and will serve as background information for ongoing research or applications. Chapter 4 describes the different facilities and diagnostic tools developed during the course of the research, and explains the various calibration procedures that were required.

The firing range procedures adopted and the experimental results obtained from several quite different flux compressor designs are presented and discussed in Chapter 5. These are followed by an outline of the design, construction and appraisal of the 1 MJ generator in Chapter 6.

In Chapter 8, the fuse exploding phenomena are described and the performance of both capacitor bank and flux compressor driven fuses are demonstrated. Opening and closing switch current transfer techniques are also explained, and a numerical fuse model is derived from experimental results for use in performance prediction of the current transfer circuits.

The information and procedures needed to design a plasma erosion-opening switch and to predict its performance with a fast rising input pulse are presented in Chapter 9. This chapter also contains an outline of the design and construction of such a switch for use with the 1MJ flux compressor, together with an assessment of its performance when driven by a capacitor bank.

A number of different conditioning circuits are studied in Chapter 10, as possible candidates for achieving the central objectives of the overall research programme. Chapter 11 presents the overall conclusions drawn from the research and suggestions for its continuation. Chapter 12 lists the relevant publications that have been produced during the course of the work.
1.3 Research commissioning agency

The research programme was commissioned by the Defence Research Agency (DRA) following revived interest in the production of large electrical pulses and the techniques needed to condition and sharpen the pulse profile. Dr J.R. Lyons of the Military Division at Fort Halstead Kent organised the funding for the work and maintained a close interest in its progress.
OVERVIEW OF FLUX COMPRESSION GENERATOR TECHNOLOGY

It is on occasions necessary to undertake high risk, high energy pulsed power experiments at remote sites, or even in outer space, or to test new devices for which a high-pulsed power and energy source is needed. In either situation a capacitor bank may be too bulky to be transported or too costly to be developed, especially at the multi-megajoule energy level of many proof-of-principle experiments. In these circumstances the flux-compression generator (FC) can provide a compact and relatively inexpensive alternative energy source. Although ‘flux compression’ is a common term it can be quite misleading and care should be taken with its use, as in typical simple high current (multi-MA) generator design the final magnetic flux may be only about 15% of the initial value. A more appropriate term is energy density concentrator, since the action of these devices is to generate a high energy density in the final (or load) volume remaining after the compression action is complete.

The basic mechanisms involved in flux compression may be understood by considering a closed conducting cage surrounding a region in which a magnetic field is established by either a current flowing through the cage or some external priming source. The process of flux compression is introduced when the volume of the cage is reduced by the action of an externally applied pressure. The compression time must be sufficiently short, of the order of microseconds, to prevent massive magnetic diffusion through the walls of the surrounding cage. In addition, the cage material, and its thickness, must be designed to satisfy the dynamic conditions, and also to ensure that there is an insignificant increase in its resistance due to Joule heating. A wide variety of different geometrical arrangements for the conducting cage have emerged from experimental work, including cylindrical, coaxial-cylindrical, helical cylindrical (considered below), plane, bellows, spherical, Archimedes-spiral-like and disc shaped. [Herlack 1979] [Reinovsky et al 1993]. The force that accelerates the conducting cage to compress the magnetic field can be produced by an explosive (solid, liquid, gaseous or even nuclear) or by electromagnetic means. In the first case, a part (possibly up to 50%) of the chemical energy released by the explosive (typically 5 MJ/kg), is transformed into the kinetic energy of the moving cage. As this compresses the magnetic field, and does work against the internal magnetic force, a significant part of the kinetic energy is transformed into magnetic energy.
The high electrical loss mechanisms inherent in the dynamic process lower the magnetic flux within the cage and reduce the potential energy gain. Much research in recent years has therefore been directed towards understanding and minimising these losses. Although overall efficiency (electromagnetic output energy)/(chemical energy released by the explosive) is a consideration in the design of a flux compression generator, it should be noted that only about 10 kg of explosive is needed to produce 1 MJ of electrical output at an overall efficiency of 2%. The size and weight of the energy source needed to provide the initial magnetic field (termed the priming source) is also a vital consideration in some applications. For instance, in a throwable device, small novel FCs arranged in cascade can provide the priming source for a compact and lightweight pulsed power package.

Because of their explosive nature, generators must be operated on a firing range, in a bomb chamber, or at a remote site where the explosive environment can be easily handled and contained. Most, if not all, of the generator components, together with any associated sensors, are destroyed following the generation operation. The magnetic flux density achieved may exceed 100 T, representing an energy density of 4 GJ/m³, with the consequent magnetic pressure on the conducting surfaces of forty thousand atmospheres.

Table 2.1: The Megagauss Club: maximum magnetic field, energy and current achieved with flux-compression generators

<table>
<thead>
<tr>
<th>Programme</th>
<th>Magnetic field density MG(10^7T)</th>
<th>Energy MJ</th>
<th>Current MA</th>
<th>Start</th>
</tr>
</thead>
<tbody>
<tr>
<td>Russia (USSR)</td>
<td>17/28*</td>
<td>100</td>
<td>&gt;300</td>
<td>1952</td>
</tr>
<tr>
<td>USA</td>
<td>10*/14*</td>
<td>50</td>
<td>320</td>
<td>1950</td>
</tr>
<tr>
<td>France</td>
<td>11.7</td>
<td>8.5</td>
<td>24</td>
<td>1961</td>
</tr>
<tr>
<td>EURATOM (Frascati)</td>
<td>5.4/7*</td>
<td>2</td>
<td>16</td>
<td>1961</td>
</tr>
<tr>
<td>UK</td>
<td>5</td>
<td>10</td>
<td>20</td>
<td>1956</td>
</tr>
<tr>
<td>Romania</td>
<td>5/7.5*</td>
<td>0.5</td>
<td>12</td>
<td>1982</td>
</tr>
<tr>
<td>Japan</td>
<td>5.4</td>
<td>□</td>
<td>□</td>
<td>1970</td>
</tr>
<tr>
<td>Poland</td>
<td>3.5</td>
<td>□</td>
<td>0.8</td>
<td>1973</td>
</tr>
<tr>
<td>P R China</td>
<td>□</td>
<td>□</td>
<td>2(?)</td>
<td>1967</td>
</tr>
<tr>
<td>Germany</td>
<td>-</td>
<td>-</td>
<td>1.2(?)</td>
<td>1975</td>
</tr>
</tbody>
</table>

*obtained only once □ data not available * much higher figure probably obtained + inferred from X-ray pictures and a numerical simulation code

5
being sufficient to destroy the cage. Fortunately, these destructive forces do not pose any serious problem during the brief period of energy production in a well-designed generator.

Experiments on flux compression began in earnest in the 1950s and formed part of the atomic weapons program. Since then experimental work has been undertaken in more than ten countries, and Table 2.1 provides a summary of the outputs obtained in the various programs by 1996. It will be noted that in this area of technology very large units are involved: MJ, MA and MG, and with MV being generated for microsecond time-scales power is often measured in GW and sometimes even in TW.

As a general rule, a flux compressor fulfills one of two distinct sets of requirements.

(i) Those that serve as sources of very high pulsed-power. The initial magnetic field is compressed into an inductor forming part of an external load circuit and thereby produces megamperes of current and megajoules of energy within it, and

(ii) Those designed to generate ultra-high magnetic fields. The most common arrangement for this is to compress and concentrate the magnetic field within a cylindrical conducting cage into a small volume at its centre, by rapidly imploding the cage using explosive or electromagnetic techniques [Knoepfel 1970].

2.1 The helical flux compressor

A flux compressor in cylindrical helix geometry is used as a high pulsed-power source for the studies described in this thesis. This relatively simple arrangement is particularly suited for providing MJ energies and MA currents into inductive loads. The basic and more detailed arrangements of an end-initiated helical device shown in Figs 2.1 and 2.2 can be used to illustrate the action and, by considering the flux and energy changes involved, the general principle of flux compression.

The action of the generator begins with the creation of an initial linkage flux (described later) normally by the discharge of a capacitor bank through the circuit formed by the helical stator coil, the load inductor (coaxial in Fig 2.2) and the tubular coaxial cylinder (termed the armature).
The explosive within the armature expands it into the conical form of Fig 2.1(b) that contacts the coil (at time $t = t_1$ in Fig 2.2) and moves along it changing its inductance and resistance as the detonation front progresses towards the load. Initiation of the explosive is timed so that the cone contacts the crowbar bolts (insulated to prevent arcing)

![Diagram](image.jpg)

**Fig. 2.1:** Basic end-initiated helical generator: (a) prior to initiation; (b) following initiation.

shown in Fig 2.2, when the priming current ($I_0$) reaches its maximum value at $t = t_0$.

Contact with the crowbars provides a low resistance path between the coil and the armature that both short-circuits the current ($I_0$ in Fig 2.1) from the priming source and
diverts and confines the priming current in the generator to circulate within its internal circuit. The point of contact made by the expanding armature with the helical coil moves progressively to the right from its position at time \( t = t_1 \), until the remaining flux is finally contained at \( t = t_2 \) within the load inductor. Throughout this time a current path is completed through the helical coil, the load inductor and the armature, and the current that circulates through this path increases progressively as the point of contact moves toward the load.

The energy generation process can be explained by considering the flux compressor as an electromagnetic circuit. To give a clear understanding of the process, a brief introduction to the magnetic and linkage flux concept and a simple derivation of the accumulated magnetic energy is given below. This is followed by the presentation and solution of the equation for a simple FC, together with some examples of the overall current and energy gains.

It is customary to regard the magnetic field produced by the current \( I \) flowing in a circuit as lines of flux, giving the field direction by their direction and the magnitude by their density. The flux lines do not originate from a magnetic source and so are continuous in space and join back on themselves, with consequent linking with part or all of the circuit. The units of flux (\( \Phi \)) are Webers, their density (\( B \)) are Webers/\( m^2 \) or Tesla and the total flux linkages (\( \Phi_l \)) are measured in Weber links with the circuit.

The circuit inductance (\( L \)) is defined as the flux linkage per unit current,

\[
L = \frac{\Phi}{I}
\]  

(2.1)

Considering the situation where the inductance is constant, then from Faraday's law of induction, the emf induced in \( L \) by the changing current is

\[
e = -\frac{d\Phi}{dt}
\]  

(2.2)

It follows from Lenz's law that, to establish \( \Phi \), the applied voltage \( (e_a) \) across \( L \) must at every instant be equal and opposite to \( e \), thus

\[
e_a = \frac{d\Phi}{dt}
\]  

(2.3)

and from eqn (2.1) \( e_a = L \frac{dI}{dt} \)
From eqns (2.2) and (2.3), the rate at which energy is accumulated in the magnetic field of the inductance is
\[
ed I = I \frac{d\Phi_t}{dt} \text{ or } LI \frac{dI}{dt}
\] (2.4)
so that the total energy \( W \) accumulated and stored in the inductance at time \( t \) when the current is \( I_t \) is
\[
W_t = L \int_0^{I_t} IdI
\]
Integrating gives
\[
W_t = \frac{II_t^2}{2} - or \frac{(\Phi_t)I_t}{2}
\] (2.5)
Equation (2.5) shows that \( W_t \) depends upon the instantaneous values of the flux linking the circuit, and the current.

In the case of a flux compressor where the inductance is a function of time

The basic circuit equation becomes
\[
\frac{dLI}{dt} + IR = 0 = \frac{LdI}{dt} + \frac{IdL}{dt} + IR
\] (2.6)
where \( R \) = the effective resistance (see chapter 6) of the FC, and \( L = L(t) \)

The solution of equation (2.6) is
\[
L_t I_t = L_0 I_0 \exp(-\frac{IR}{L}dt)
\] (2.7)
where \( L_t I_t \) is the linkage flux at time (t), \( L_0 I_0 \) is the initial value, and \( \exp(-\frac{IR}{L}dt) \) is the proportion of flux left in the system. This analytical solution is particularly useful when \( R \) and \( L \) are simple functions of time, for example when the pitch of the FC coil remains constant.

From eqn (2.7) the proportion of the original flux linking the load inductance \( L_t \) at the end of the FC run when \( I_t = I_t \) (t = t2 in fig 2.2) is
\[
\frac{I_t L_t}{I_0 L_0} = \exp(-\frac{IR}{L}dt)
\] (2.9)
denoting this by the symbol \( \eta \), the current gain of the FC is
\[ \frac{I_1}{I_0} = \frac{L_0}{L_i} \eta \]  \hspace{1cm} (2.10)

substituting \( I_i = \frac{L_0 I_0}{L_i} \eta \) into eqn (2.5), with \( L = L_i \) gives

\[ W_i = W_o \frac{L_0}{L_i} \eta^2 \]  \hspace{1cm} (2.11)

so that the energy gain is

\[ \frac{W_i}{W_0} = \frac{L_0}{L_i} \eta^2 \]  \hspace{1cm} (2.12)

If the load inductance is very much smaller than the initial inductance say \( L_0/L_i = 500 \), then from the equations above,

(i) For a lossless generator the flux is conserved and \( \eta = 1 \), giving current and energy gains of 500

(ii) For a well designed high current flux compressor, with say 30% of the initial flux remaining in the load circuit at the end of its run, \( \eta = 0.3 \), giving current and energy gains of 150 and 45.

2.1.1 Some practical limitations

It is common practice to restrict the line current density in a flux compressor to less than 0.2 MA cm\(^{-1}\) in order to avoid excessive resistive losses (see section 6.3.1). In high current designs the width of the coil conductors and the consequent winding pitch progressively increase to satisfy this restriction as the armature cone moves along the coil and the current rises. The increase in the pitch also produces a reduction in the time rate of change of inductance produced by the movement of the cone.

The conversion of kinetic to electrical energy and the voltage induced in the circuit both depend upon the current and the rate-of-change of inductance as the cone moves along the coil, optimum conversion being obtained when the voltage between the coil and armature is close to the breakdown value throughout the run-time. In medium current flux compressor designs (MA), these optimum conditions are easily met; however in large multi-mega-ampere designs, an imposed current density restriction progressively reduces the energy conversion from the optimum value in the high current region of generation. These considerations are discussed in greater detail in chapter 6.
2.1.2 Machined generators

The cylindrical helix flux compressor can be constructed using insulated wire and a few simple machined components, as described in the next section. A numerically controlled winding machine however can be programmed to follow the design requirements more precisely, thus producing an accurate alignment between the coil and the armature (which is a very important consideration in small diameter compressors) and an improved performance.

Machined coils with turn size variations that closely satisfy the current density limitations are cut from copper cylinders, potted with epoxy resin in a vacuum chamber to exclude air bubbles, and skimmed internally to remove the resin from the inside surface. Flanges attached at each end of the coil connect to the priming source and the load. When complete, the coil is overlaid with thick epoxy resin or concrete (depending on the current rating) to inhibit movement of the windings by the magnetic pressure. The copper or aluminium armature is skimmed internally to fit the explosive charge, which is usually cast, and externally to provide a constant diameter. When the load is a closed cylinder, the armature is extended into it and attached to its end plate, thereby allowing the tip of the cone to enter. (see Fig 2.2).

A winding machine can also introduce changes in the diameter of the coil/armature to compensate for the decrease in inductance referred to in section 2.1.1, and thereby to produce a significant increase in the current gain [Boriskin et al 1994]. Tilting of the coil turns to match the angle of the cone in the high current stages of a simple helix coil is also beneficial. Fig 2.3 is a diagram of a generator (described in chapter 5), which has

![Fig 2.3: MJ flux compressor with tilted turns](image)

both a progressive increase in the coil winding width and tilted turns towards the end of coil. Such designs are both costly and extremely difficult to construct. However, in
practice, experimental requirements are now met by simple and inexpensive systems, employing hand-wound coils with constant diameter armatures. Machined generators could only be seriously considered if the energy-to-bulk ratio was an overriding consideration in a final engineering specification.

2.1.3 Hand-wound flux compressor

Fig 2.4 shows a hand-wound coil as used for the study described in this thesis. It employs a simple constant diameter armature, and an insulated-wire coil wound on a mandrel, and overlaid with epoxy resin to form a rigid coil structure. The increasing current in the coil is accommodated by dividing it into a number of sections, with both the diameter of the conductors and the number in parallel increasing toward the load end, and this is referred to as turn-splitting. Care must be taken to stagger the wire joints around the circumference to ensure a smooth transaction as the cone enters a new section. The energy gain of a hand-wound flux compressor is modest, the construction fairly straightforward, and the cost relatively low.

2.1.4 Coupling of helical flux compressors

The value of the flux conservation efficiency of a flux compressor defined by equation (2.9), represents the flux that remains at the end of the action and thus provides a useful figure of merit. Practical values range between 0.2 and 0.4 in the best generators and decreases with an increase of the inductance ratio $L_1 / L_0$. Where high-energy output and gain are required, it is advantageous to use several coupled generators, each with a modest inductance ratio, to keep the overall figure of merit high. Transformer coupling can also be used to match the generator to a high impedance load.
In the most common method of coupling, the output current of one generator is conveyed to the next by coaxial cables, where it produces the priming current using a standard transformer coupling circuit. Successive generators are triggered, so that one begins to generate immediately the previous one finishes its run. Coupling has the advantage that each armature diameter needs only to be sized to satisfy the maximum current density value for the current at the end of its run-time. The cascaded generators increase in size towards the final load. Fig 2.5 shows a cascade of coupled generators with an output of 100 MJ [Pavlovskii 1991] from an input of 2 kJ.

Another method of coupling, more suited to small diameter generators with modest output currents, is shown schematically in Fig 2.6. It is described variously in the literature as flux-capture, a dynamic transformer technique, or given the name FLUXAR [Novac et al 1997]. The cascaded generators are conveniently arranged on a common armature, so that the triggering of each is automatic. The load inductor of one compressor...
is directly coupled to a section, or to the whole, of the helical coil of the next section, which remains open circuit until it is crowbarred. When a generator completes its run, and its load current is a maximum, the next compressor circuit in the cascade is closed by the action of its crowbar. The flux-turns then present within its volume are automatically trapped, causing current to flow in the coil as the generation commences.

A beneficial increase in the initial flux-turns in successive generators, leading to an overall increase, can be obtained in both coupling methods described above by employing a step-up ratio in the coupling transformers.

Very high current sources can also be provided for low inductance applications by connecting a number of flux compressors in parallel.

2.1.5 Priming source

Most firing sites where explosive pulsed-power research is conducted have a large and heavy capacitor bank, with energies ranging from 0.2 MJ to a few MJ. These can provide the priming energy for a single large generator, which is able to produce the many megajoules of energy and megamps of current required for experimentation. In some applications however when light-weight and size are overriding factors, it is impractical to employ a capacitor bank as the priming source. In such cases, a few stages of small FLUXAR generators, initially primed by a battery or a small capacitor, are sufficient to supply the priming energy required by a multi-megajoule device, enabling a compact pulsed power package to be designed. Fig 2.7 shows such an arrangement.

2.2 Applications

Among the most important features of a flux compression generator are its low weight, compactness and autonomy since, if necessary, the initial primary source could even be a magnet or a shocked piezo-electric device. These are important assets when high values of pulsed power are needed in remote places, for outer space and military applications.
using rockets, and for proof of concept experimentation. The availability of very large levels of output current and energy, has led to their use as pulsed X-ray and neutron sources in the thermonuclear fusion program, and for the production of high magnetic fields. They are also widely used in the development of powerful lasers and electromagnetic launchers. In a number of applications the generator output needs to be sharpened, so that the current rises to its maximum in a few microseconds or even nanoseconds, by the use of opening and closing switch techniques involving exploding metallic fuses and plasma devices.

2.2.1 Cylindrical implosion techniques

Magnetic fields exceeding 2000 T have been reported near shaped rods carrying very fast-rising currents [Spielman et al 1990], and fields of the order of $10^4$ T are believed to exist in both fast-moving high-density hot magnetised plasmas, and in short pulse-compression laser experiments. The only means so far known of producing ultra-high magnetic fields in volumes sufficiently large for practical application (more than $10^{-6}$ m$^3$) however, is to use a converging implosion generator. The imploding metallic cylinder is

![Diagram](image)

**Fig. 2.8**: Electromagnetic flux compression devices for fields up to 5 MG (500 T):

(a) Z geometry. Linear squeezed by the field $B_0$ produced by the current $I_z$. High $I_0$ (not shown) flow in liner as the central field builds up; (b) $\theta$ geometry. Liner squeezed by $B_\theta$ field produced by currents $I_0$ and $I_0'$ flowing in the primary coil and the liner.

termed a liner, and an outer coil energised by either a capacitor bank or a flux compressor provides the initial magnetic field. For magnetic fields up to about 500 T the electromagnetic forces produced by discharging a capacitor bank or the sharpened output of a flux compressor into the type of z-pincho or $\theta$-pincho loads shown in Fig 2.8 have been used to collapse the copper liner. For high magnetic fields, explosive rather than electromagnetic effects are used to produce the implosion forces. Although fields
exceeding 2000 T have been claimed exceptionally, magnetic fields up to about 1000 T can be produced consistently by the simple arrangement of Fig 2.9, which accelerates copper cylinders to more than 4000 m/s by external explosive charges. With advanced firing techniques used to ensure that the charges are detonated with a high degree of simultaneity, the time for implosion is about 10 μs and the field exists for the order of hundreds of nanoseconds. A limitation to this technique lies in the melting and vaporisation of the inner surface of the liner and, as a consequence, the loss of stability of the metallic/field interface, with the onset of Rayleigh-Taylor instability as the cylindrical geometry is lost, see Fig 2.9 (a).

To obtain magnetic fields exceeding 1000 T, in volumes sufficiently large for useful application, the so-called cascade system of liners shown in Fig 2.9(b) is adopted. Each of the internal system of coaxial liners consists of an unconnected axial copper wire structure, held in position by epoxy resin and transparent to the magnetic field. When an imploding liner hits the next liner of the cascade, this is melted and transformed into a homogenous copper liner, which can allow circling currents to flow, with consequent further compression of the magnetic field. The design and positioning of the individual liners is such that the contact is made before geometrical stability of the previously accelerated liner is lost. The maximum reported reproducible magnetic field of nearly 1700 T achieved with this technique [Pavlovskii et al 1994] corresponds to an energy density of 106 TJ/m³, and is produced in about 20 μs as shown in Fig 2.10.

Fig 2.9: Explosive-initiated implosive device. Very high circular currents (not shown) are developed in the liner during the implosion.
The first reported application of the implosive flux compression technique was in the 1940s, when it was used in the Y-Manhattan atomic bomb project to study the cylindrical implosion of a metallic shell [Hawkins 1946]. More recent applications have included the study of materials under pressures of many millions of atmospheres, in particular in seeking the transition of hydrogen to the metallic state. For these experiments the probe of Fig 2.9 consisted of a copper cylinder squeezed by the magnetic field developed inside the flux-compression generator in a manner similar to that of the copper liner of Fig 2.8(b) Implosive techniques are also used to produce high magnetic fields for plasma research, for the acceleration of microspheres and in the study of a wide range of magneto-optic phenomena.

Between 1985 and 1990, many billions of dollars were spent in the USA on developing energy weapons technology in the Star Wars/SDI project. Several of the concepts underlying flux-compression generators figured prominently in that programme, where they were used as power sources in proof-of-concept experiments and the development of high energy technology. At the same time, about the same level of effort was being expanded in the USSR. However, in the present era of collaboration and reduced spending on weapons, both types of implosion device described above have been used in controlled thermonuclear fusion research. Using many disc-type flux compressors connected in parallel as a power source, liners were electromagnetically accelerated to more than 50 km/s and then made to compress pre-heated deuterium plasma to generate a high burst of neutrons.

![Fig.2.10: Typical flux-compression characteristics for the growth of current in a helical generator and magnetic field for a cylindrical implosion device](image-url)
A sharing of the high costs involved in this fusion research (estimated at more than $11 billion) is one objective of the recent scientific collaboration between the USA and Russia [Lindemuth 1997]. A 1 GJ 100 TW flux compressor has been produced as the power source for a proof-of-concept break-even fusion experiment. In addition, the moratorium on nuclear testing has meant that the flux compressor is being considered as the large, compact and inexpensive power source needed for the X-ray, neutron and EMP pulse generators (including a sharp high voltage pulse as in this study) used to provide strong pulsed radiation for weapon effect studies.

2.2.2 Soft X-ray source

A good illustration of the flux compressor used as a source in a proof-of-concept experiments is found in the Trailmaster project conducted in the USA at the Los Alamos National Laboratory in New Mexico. The objective is to produce a large pulse of soft X-rays (from 50 to above 500 eV), by applying a 15 MA pulse axially along a short and very thin aluminium cylinder in some hundreds of nanoseconds. The cylinder rapidly becomes a plasma, which is then imploded in a fraction of a microsecond by the z-pin

![Cross section of the Procyon explosive pulsed power system](image-url)

Fig 2.11 Cross section of the Procyon explosive pulsed power system (not to scale). The three major segments are noted, as well as the input header and the components: A-Typical input cable; B-Typical resistor in a 4Ω array; C-Input insulator; D-MK-IX armature; E-MK-IX stator; F- MK-IX explosive; G-Teflon EFF forming die and storage inductor insulator; H-EFF switch element; I-Detonator actuated closing switches; J-closing switch insulation; K-radiation baffles; L-PFS conductor and barrier film; M-4-cm radius load slot; and N-free volume housing the firing unit for EFF detonators.

Forces. When the plasma arrives on the axis, its kinetic energy is converted into internal energy, heating the plasma and radiating X-rays in a complicated interplay of processes.
Fig 2.11 [Goforth et al 1993] is a line diagram of the explosive device (called Procyon). It shows the helical generator, that provides 20 MA into the output conditioning circuit needed to provide the sharp current pulse required by the z-pinch section. The circuit consists of an explodingly formed fuse (EFF) opening switch, followed by an explosively activated closing switch that connects current to a plasma flow switch (PFS) that opens and feeds the sharpened pulse along the aluminium cylinder. (A full description of the switches is given in a later chapter)

Experiments at lower energy and current levels using a capacitor bank source are being conducted in a parallel supporting programme to determine the characteristics of the sharpening circuit components. Fig 2.12 shows the device assembled on the firing site.

2.3 Sources of information

Chapter 3

OUTLINE OF FLUX COMPRESSION EXPERIMENTAL METHODS

The history of pulsed-power research in general, and that of flux-compressors in particular, contains numerous examples of the development of dedicated special-purpose instrumentation (or the adaptation of previously developed equipment) for the measurement and/or analysis of the rapidly changing phenomena that are involved. The spectrum begins with a wide range of transducers, and continues through the x-ray machines needed for dynamic radiography up to the ultrafast oscilloscopes produced during the 1960s. Most of the techniques and instrumentation described below were developed to fulfil various special needs that arose during major research investigations.

Flux-compression generator research is basically a blend of detonics and electromagnetism. Detonics, as the science of detonation and explosive performance, provides the knowledge needed to understand the initial energy transformation process in the generator, from the chemical energy stored in the high explosive charge to the kinetic energy of the moving generator armature. Electromagnetism complements this, by providing a description of the generation of high and ultrahigh magnetic fields in the final energy transformation process, from the kinetic energy of the armature to the output electromagnetic energy.

3.1 Electromagnetic experimental techniques

A number of diverse experimental techniques have been developed for use during the electromagnetic phase of the generator action. These are based on familiar electromagnetic or photonic principles and are explained below.

3.1.1 Flux probes

The flux (or magnetic induction) probe described in detail in Chapter 4 Section 4.7.1, is the most frequently adopted device used to measure high pulsed magnetic fields. An effective common ground point is difficult to establish in a fast very-high-current circuit, and pick-up probes minimise the problems encountered with ground loops when a direct connection is made to the circuit. Although they can be as simple as a single turn of thin wire, a properly chosen number of turns should be used to provide increased sensitivity. The probe responds to the rate of change of magnetic flux density with time (commonly
referred to as B-dot), and to obtain the flux density a passive high-impedance RC-integrator is normally employed, inserted at the oscilloscope end of the coaxial cable used for the probe connection. Although the frequency bandwidth of the probe can be several hundreds of megahertz, it can on occasions be difficult or even impossible to use. The most common drawbacks arise from electromagnetic pick-up, electrostatic coupling and electrical breakdown, due to an unexpectedly high rate-of-change of the magnetic field inducing an excessive voltage in the probe. A further problem may arise from the length of the coaxial cable that is sometimes necessary and which can adversely affect the overall frequency response. In addition, in ultrahigh magnetic-field compression experiments (and in some special flux compression generators) the electromagnetic forces acting on the current-carrying conductors can alter the geometry of the measuring circuit and thus change the initial calibration, while on occasions the currents may be sufficient to fuse the wire from which the probe is made.

Although flux probes are normally used for field measurements they can easily be adapted to measure current, either by calibration using a known current source or, for simple conductor geometries, by calculating the current from the measured field strength.

3.1.2 Rogowski coils for electrical currents

The typical self-integrating Rogowski coil is toroidal in shape and surrounds the conductor in which the current is to be measured. Application of Ampere’s Law shows that the voltage induced in the coil is independent of its position relative to the conductor.

The construction of a Rogowski coil is less simple than that of a pick-up probe, but the many improvements achieved over the years are collected in monographs which detail the techniques needed to achieve a required bandwidth and even to self-integrate the signal. Although calibrated Rogowski coils are commercially available, the particular features of an experiment may force a special-purpose unit to be constructed. Many of the drawbacks associated with pick-up probes (electromagnetic noise, effect of long connecting cables, etc) also have to be considered when using a Rogowski coil. As with the pick-up probe, the Rogowski coil can be used, when properly calibrated, to measure a quantity other than current. For example the voltage across a known resistance can be obtained from a measurement of the current flowing through it.
3.1.3 Voltage dividers

Although voltage dividers may be thought of as representative of an old technology, at least one new concept is still presented at almost every one of the biennial IEEE International Pulsed Power Conference. Dividers capable of use in megavolt circuits and with rise times of nanoseconds (or even shorter) have already been developed. Few of the many types of resistive, capacitive or hybrid devices described in the literature however are suitable for use in flux-compressor research. In fact the measurement of voltage pulses at the remote sites where explosive devices are normally detonated presents an extremely complex challenge, since the usual laboratory conditions (short signal cables, a single good earth point, etc) cannot be achieved.

Electromagnetic methods still provide the most common, inexpensive and convenient approach to measuring the pulsed magnetic fields and currents encountered in flux-compressor experiments. However, in the last decade they have progressively been replaced by photonic methods, to provide the reliable and accurate measurements needed in proof-of-principle experiments that may be large scale and single shot and involve considerable expense.

3.2 Photonic methods

All the drawbacks mentioned above in connection with electromagnetic methods of instrumentation are eliminated in sensors based on photonic techniques. The advantages these offer may be summarised as:

- immunity to electromagnetic interference
- no ‘interaction’ with the pulsed magnetic field (no forces act on the instrumentation circuit, no heating because of diffusion)
- electrical isolation
- no ground loops
- miniaturisation
- high bandwidth (depending mainly on the opto-electronic converter)
- high sensitivity
- the ability to address measurement problems that are inaccessible using electromagnetic technology (eg, very small spaces accompanied by very high voltages
and electromagnetic discharges, such as a fast plasma opening switch or a Marx generator).

The only disadvantage is the rather high cost of the components involved (eg a Pockels cell) in sensors which may be destroyed when used with an explosive generator, restricting their use to experiments in which the total cost is high.

3.2.1 Faraday-rotation effect

Measurement methods using this effect are based on the rotation produced in the polarisation plane of light travelling through the electro-optical sensitive element of a sensor, when a magnetic field is applied parallel to the direction of travel. The rotation angle $\theta$ is proportional to the length $l$ of the light path in the element and the magnetic field intensity $H$, or

$$\theta = VlH$$

where $V$ is the Verdet constant of the element material. Depending on the application, the length of an optical material with an appropriate value of $V$ is adjusted to give the required size of signal. Fig 3.1 shows how a fibre-optic cable is used to transmit light both from and to the Faraday sensor to ensure the advantages listed above. It is probably the only technique possible for measuring ultrahigh magnetic fields above 1000 T (10 MG); with the highest recorded fields of 1700 T (17 MG) the element material was flint.

Faraday-rotation sensors to measure fast-rising, and very high currents were developed from special twisted single-mode
fibres, taking advantage of the (low) Verdet constant of the fibre material. The fibre is positioned in a similar manner to a Rogowski coil, with a number of turns surrounding the current-carrying conductor. Extreme care is needed in a sensor used in a flux-compression generator to avoid any mechanical contact with the moving conductors (some movement may even be caused by the very large magnetic pressures that are produced), which may induce additional birefringence in the fibre and thereby alter the measured signal.

3.2.2 Pockels effect

The Pockels electro-optic effect can be used to obtain a measure of the strength of an electric field. When a field is applied in the X direction to a crystal, the refraction index $n_0$ increases in that direction, whilst that in the Y direction remains constant. A light beam polarised along the X direction then propagates along the Z direction with a slower speed than that of a beam polarised along the Y direction. A phase shift will thus appear between the components in these two directions which is proportional to the field strength $E$ and the crystal length $l$, and is referred to as an induced electrical birefringence $\Gamma(E)$, where

$$\Gamma(E) = \frac{2\pi n_0^3 r l E}{\lambda}$$

in which $r$ is an electro-optic coefficient and $\lambda$ is the wavelength of light emitted by the source. To measure voltage, two electrodes at a potential difference $V$ are deposited on the crystal surface giving an electric field, of $E = V/d$ where $d$ is the separation of the electrodes. The phase shift resulting from the application of this voltage is detected by optical intensity measurements.

A Pockels cell is an assembly of the crystal, the electrodes and specially coated optical windows, with the crystal being sometimes immersed in a liquid to reduce the piezoelectric effect and to stabilise its temperature. The input characteristics of the cell of a few picofarads capacitance and more than ten gigohms resistance are better than those
of the great majority of recording instruments. As seen in Fig 3.2 a typical Pockels cell will be coupled by long fibre-optic links, with a laser at one end and an optoelectronic converter at the other. For a linear cell response, and to avoid its destruction, the voltage across the cell needs to be reduced to less than 2 kV by a special purpose capacitive divider, with a typical system having a rise time as low as 1 ns. The use of a Pockels cell provides all the characteristics required from the measuring equipment and gives full protection to the recording instruments.

3.3 Experimental methods in detonics

This section describes a number of the measurement techniques associated with the science of detonics, as used in the monitoring and control of the explosives used for high-energy and high-current flux-compression generators or for ultrahigh magnetic-field production.

Much of the technology arose during various nuclear weapons programmes, and this has no doubt also been responsible for the very rapid advances made in flux-compressor technology. Although the design of small devices is straightforward, considerably more complexity is necessary for larger and more advanced units to provide a particular waveform of load current or voltage or a high chemical-to-electromagnetic energy conversion efficiency. To obtain design data from tests, it is necessary to measure and control all the parameters of the generator dynamics. Some of these are related directly to the explosive (as in the detonation front velocity) while others (much more difficult to measure) are related to the corresponding acceleration of the generator conductors and insulators. Not only are their bulk velocities required, but also information of such quantities as the time-changing geometry, the surface condition and the temperatures at various locations is often sought.

3.3.1 High-speed photography

High-speed photography is the most commonly used method of obtaining experimental information on the dynamics of the armature of a flux-compression generator. From the data obtained it is possible to determine the detonation velocity, the chemical energy released, the uniformity of the moving surfaces and much else besides.

Rotating cameras and electronic image converter cameras are both used, housed in specially built laboratories (bunkers) with optical portholes enabling the process to be observed through the thick walls. Telescopic lenses are normally provided to enable the camera to focus on the experimental device, which may be several tens of metres distant.
The core of the rotating camera is a rotating mirror, which can run at hundreds of thousands of rotations per minute. It can be used in the frame mode (in excess of one hundred frames), with frame rates up to some millions of frames per second, or the streak mode with writing speeds of between 10 and 20 mm/μs.

Although much higher speeds can be obtained with electronic image converter cameras, this is not unusually necessary in detonics. The advantage of using such cameras (shown being aligned in Fig 4.23 section 4.6), lies in the greater detail that can be captured (a larger image plane) and the much greater light sensitivity, which permits a magnification of the object being photographed. A drawback is the much reduced number of frames obtained, as is evident in the photographs of Fig 3.3.

Light flashes are often necessary, and these can be made by simply detonating a small charge of plastic explosive inside a cardboard cylinder, with an inner aluminium wrapping and filled with argon gas. The shock wave produced by the explosive excites the argon atoms, giving a high intensity flash with a duration that depends on the distance the shockwave travels through the gas. Synchronisation between firing the main charge of the object studied, the flash and the camera operation can be achieved using high-precision detonators or simply detonating cords.

### 3.3.2 Interferometry

Interferometry is a very complex and costly technique that provides complete data on the hydrodynamic flash processes of an accelerating armature. It can be either a laser-based Fabry-Perot interferometer or a system based on a Michelson interferometer and using diffuse surface interferometry (the so-called VISAR system). This latter device has a time resolution of 1 ns, and the capability to determine surface motions for any kind of surface, even those that are highly tilted.
Hydrodynamic data obtained from separate armature experiments in the absence of magnetic fields is insufficient for the design and optimisation of either high-energy and high-current flux-compression generators, or cascaded ultrahigh magnetic field generators, since the very high magnetic fields developed alter dramatically the shape of the accelerated conductors. Although 2-dimensional hydrodynamic codes can provide an estimate of what is occurring inside the generators, the best information is undoubtedly obtained directly from flash radiography. Fig 3.4(a) shows a tangential x-ray still of a thin copper-foil cylinder used in a compression experiment. Instabilities developing as the cylindrical foil is imploded by electromagnetic forces are clearly seen in the dynamic result of Fig 3.4(b). [Miller et al 1965]

For simple, small devices a small x-ray source (such as a SCANRAY CADEX-200kV x-ray generator) can be used in conjunction with an IMACON camera and a supplementary electronic intensifier. For the high-energy sources used at the laboratories undertaking research in nuclear weapons hydrodynamics, large high-energy (20-50 MeV) electron accelerators have been constructed as powerful pulsed high-energy radiographic machines. Such sources generate hundreds of rads at a distance of 1 m, with a pulse duration of some tens of ns and a 1 mm diameter spot size.

3.3.3 Methods for velocity and acceleration measurement

Almost all research laboratories have developed their own approaches to the measurement of the detonation velocity and the armature movement of a generator, and only the most common methods are presented below. Other methods using devices such as phase transition probes, displacement capacitors and electromagnetic velocity gauges are more difficult to use and are restricted to specialised applications.

3.3.3.1 Pin contact method

Although a long-standing technique, the pin method remains the simplest and most frequently used method of measuring precisely both armature movement and detonation
velocity. The principle is straightforward, with the arrival of a shock wave making a contact and generating a small voltage pulse by a capacitor discharge.

The four basic types of pin illustrated in Fig 3.5 can be identified from the many descriptions that have appeared in the literature. Fig 3.5(a) shows the simplest of these. It consists of a number of thin wires, each of which is in a circuit containing a small capacitor charged to some tens of volts, with the circuit being closed by contact with the expanding armature. The wires can be either positively or negatively charged, and can be identified (for example) by the shape of the discharge pulse recorded on a raster oscilloscope. For the detailed modelling of spherical implosions, as many as 500 wires were used in a dome arrangement with 50 raster oscilloscopes, and the accuracy of positioning was within a few microns.

Fig 3.5: Pin contact method for velocity measurement: (a) basic arrangement; (b) coaxial arrangement with gas chamber and insulated cap; (c) coaxial arrangement without cap; (d) use of piezo- or ferroelectric pins

The second type of pin, shown in Fig 3.5(b) is similar but slightly more complex. Because not all armature monitoring can be performed in evacuated chambers, the strong ionisation shockwave that precedes the expanding armature causes the pin to be shorted prematurely. The solutions to this problem involves the use of special gas chambers, anodised pins or a self-shorting arrangement, in which the pins have a coaxial electrode structure with a cap insulated from the central electrode by (say) Mylar tape. Such pins are also useful when, for example, an accelerated non-metallic object such as a generator insulator has to be monitored.
The third type of pin, shown in Fig 3.5(c), is similar to that of Fig 3.5(b) but without the cap and insulator, with a fully ionised detonation wave being sufficient to short the electrodes. The final type of pin, shown in Fig 3.5(d) uses pins made from piezo or ferroelectric materials or plastic tape, all of which produce a small voltage when shocked. This effect is useful in a number of special situations, such as to provide a trigger pulse when small amplitude waves are being monitored. As an alternative to recording the pin output on a raster oscilloscope, its arrival time can be measured using an electronic counter or a specially built and costly multichannel time measurement system.

The optical pin provides the most recent technique. Small-diameter optical fibres are illuminated by a laser, with the output light monitored by either a photomultiplier tube or a fast PIN photodiode. The loss of the light signal accurately determines the time and position at which each fibre is broken by the expanding armature. This technique is particularly useful in difficult situations (such as very high-current generators) which prevent the use of an electric pin. A version of the optical pin which uses light from ionised microspheres filled with argon gas (ie a microflash) attached to the end of a fibre is used to measure detonation arrival times.

### 3.3.3.2 Detotachograph method

The pin method gives only arrival times, from which the average velocity between two pins can be obtained. Continuous monitoring of the acceleration can however be achieved by the so-called detotachograph method.

![Fig 3.6: Detotachograph probes: (a) and (b) different probe arrangements; (c) typical output signal from a type b probe](image-url)
There are two types of such transducers, both using a technique in which a high-resistance wire of known value and length, and carrying a constant current, is shorted by the shockwave, with the resulting fall in voltage being proportional to the velocity. Small changes in the velocity can be observed more easily by imputing the probe output voltage profile together with a sawtooth waveform into a differential amplifier. Velocity variations can be enhanced by a factor of up to a hundred at the amplifier output.

Fig 3.6(a) shows the first type of detotachograph probe, which has the wire implanted into an explosive charge within an earthed metallic cylinder, and uses the highly ionised detonation-front to short-circuit the probe and so provide a voltage output. The second type uses an insulated conductor contained within a small diameter thin metal tube, as shown in Fig 3.6(b). This tube is deformed by the shockwave, with the point of contact made between the tube and the conductor moving at the same speed as the shockwave. The probe can measure the velocity of a shockwave travelling through any kind of material in contact with the probe, even when it changes direction. A typical result has the form shown in Fig 3.6(c).

3.3.4 Pressure and temperature measurement

Although many forms of pressure gauges are available, the most common techniques for shockwave measurements use either quartz for low pressures (up to several GPa) or manganin for higher pressures. This latter approach uses the change in resistance of a very small diameter (tens of microns) wire embedded (for example) in an epoxy resin insulator, and needs a special power supply similar to that used in a detotachograph. The photograph of Fig 3.7(a) shows a typical (low-cost) manganin gauge, and the output signal that is obtained is presented in Fig 3.7(b).

Temperature is a difficult parameter to measure. Normally the spectrum of radiation is recorded using a spectroscope and an optical multichannel analyser with an intensified target detector. For nontransparent probes, such as a metallic conductor, the temperature can be measured by placing it in close contact with a
material such as $\text{Al}_2\text{O}_3$. This retains its transparency under shock and provides a near match in shock impedance, and enables the interface temperature to be measured.

### 3.3.5 Explosive cord delays

Explosive cords generally consist of metal, plastic or fabric tubes filled with high explosive usually DETN or RDS. They have both civil and military applications, and can be used as delays in explosive devices, for instance to trigger explosive switches between stages in flux compressor conditioning circuits. Repeatable delays of $100 \mu s$ or so can be obtained with a precision of a fraction of a microsecond.

### 3.3.6 Detonation wave shaping

Flux compressors are built with a number of different geometries (cylindrical, planar, etc) and for each of these a special explosive device is needed to produce the detonation wave shape that accelerates the armature into its final geometry.

The required shape of the detonation wave can be obtained in several ways. A distribution of precisely exploding bridgewire detonators is one possibility, but this technique is both expensive and difficult to use in the presence of the high currents, voltage and electromagnetic noise experienced with a flux compressor. A more practical solution is to use a single detonator together with a number of explosive wave generators, producing line, plane, cylindrical or even spherical wave shapes. To arrive simultaneously at the necessary shape, the construction of such generators involves either the use of accelerating metal plates to initiate the main charge (type I), the insertion of interrupter (air holes) in the path of the detonation wave (type II) or explosive lenses made from a combination of slow and fast explosives (type

![Fig 3.8: Detonation wave-shaping techniques: (a) line-wave generators; (b) plane-wave generators; (c) cylindrical implosion. Type 1: accelerated liner; Type 11: interrupter; Type 111: explosive lenses](image-url)
A range of typical examples is shown in Fig 3.8. A more recently used method of generating any desired wave shape employs the array of exploding-foil-mesh-initiated surface detonators as shown in Fig 3.9 with mesh densities as high as $3 \times 10^5$ m$^{-2}$. Surface areas of explosive up to 0.75 m$^2$ have been successfully initiated in this manner.

[Moore et al 1988]

### 3.3.7 The explosive pulsed-power laboratory

As described previously, experiments with flux compressors require both a laboratory in which the explosive charge can be detonated and a means for providing the initial priming current. The output load may sometimes need to be protected from the effects of the explosion. In practice, a laboratory will require three main elements. One of these is the firing site, which is normally outdoors and comprises a firing table (up to several tens of kilograms of TNT equivalent may be involved) sited in close proximity to the laboratory buildings. For larger explosive charges (up to several hundreds of kilogramas of TNT equivalent) the firing site is normally remote from the buildings. Occasionally the very costly alternative of a large bomb chamber, a sphere up to 10.5 m in diameter and weighing more than $10^5$ kg, is used for charges equivalent to up to 40 kg of TNT.

The second element is the initial energy source, which is normally a capacitor bank and may need to store up to 1 MJ. All the control functions are transmitted through either fibre-optic cables or pneumatic systems. The third element is normally termed the bunker and since this contains the control room with its data recording and diagnostic equipment, it needs to be located as near as possible to the firing site. A workshop and provision for the storage and assembly of explosive charges complete the minimum laboratory requirements.

The ideal laboratory arrangement of Fig 3.10 will have further buildings to accommodate an x-ray machine and the load, such as a powerful laser and electromagnetic gun or a large plasma device. This latter situation may require electrical connections to the firing
site, using possibly hundreds of coaxial cables or a large parallel-plate transmission line directly, or via a transformer, to the load.

The history of a complex firing experiment can be divided into a number of phases, with durations decreasing by orders of magnitude from months to nanoseconds as the programme proceeds. Design and manufacture of the component parts may take up to one year, following which a few days are required for the flux compression generator and its output circuits (conditioning, load, etc) to be assembled. After positioning the generator on the firing table, the instrumentation for the experiment is connected and all other connections are made. All the instrument and oscilloscope settings and connections are checked and rechecked in a series of tests with no explosives, performed in accordance with an established experimental protocol. These may even include discharge of the main capacitor bank. Once they are complete, the initial conditions (vacuum, gas pressure, voltages, etc) for the explosive test are set and the experiment can proceed. Once the start button is pressed all control is lost, except for some independent electronic fast-delay units. The flux compressor generates an output pulse in perhaps tens of microseconds, with the output circuits conditioning this for the load in a matter of nanoseconds in some applications. All that eventually remains after a long and costly experimental programme, apart from the debris, is a collection of photographs and oscilloscope records!

Fig 3.10: An ideal high-energy flux-compression laboratory arrangement
3.4 Sources of information

(1) For electromagnetic measurement, The International Megagauss and IEEE Pulsed Power conferences referred to in section 2.3 contain many relevant papers.

(2) For x-ray and optical measurements, see [Miller et al 1965] [Novac et al 1995]

(3) For hydrodynamic measurements see for example [Graham et al 1978]

(4) For detonic measurements refer to [Johannson et al 1970 1994]
CHAPTER 4

PULSED POWER FACILITIES AND MEASUREMENT TECHNIQUES

This chapter presents details of the pulsed power facilities and the basic measurement techniques assembled for the studies described later in this thesis. They include the capacitor banks used during explosive experimentation on the firing range and in the laboratory at the University, together with their associated operational equipment, crowbarring devices and safety arrangements. The voltage and current sensors and calibration procedures needed to obtain a clear and accurate interpretation of the experimental results are presented and discussed, and a 2-stage vacuum rig and chamber with a low-inductance high-current input feed-through connection for plasma switching experimentation is described. Arrangements were made for the occasional use of a high-speed Imicon camera for photographic studies.

4.1. Mobile pulsed-power facility

A capacitor bank providing up to 200 kJ of stored energy at 40 kV was assembled inside a 5 m long steel container, equipped with heating, lighting and electrical supplies to full laboratory standards. Together with a smaller and similarly equipped container that housed the control, operating and recording equipment, this provided a complete mobile pulsed power facility (MPPF) for deployment at any suitable firing range. Fig 4.1 shows the MPPF being secured on a transporter.

![Fig. 4.1: MPPF on its transporter](image)

4.1.1. Capacitor bank design details

The bank consists of eight 30 μF capacitors, each of approximately 190 μH internal inductance, mounted vertically in two parallel lines of four. Connections to a wide and rigid aluminium strip transmission line positioned above the capacitors complete a robust and compact unit. The discharge current is fed to a firing bunker 25 m away, by up to 24 high-voltage coaxial cables connected in parallel. A series resistive-capacitive circuit
with a low resistance and short time constant, is connected across the end of the cables, to avoid any serious mismatch that may cause excessive voltage spikes across the lines when feeding into a high impedance load.

4.1.2. The start switch

A versatile dry-air pressurised 3-electrode spark-gap switch (termed the start switch) is used to initiate the capacitor discharge. The switch has an inductance of 18 nH, and it can be operated at voltages of between 12 and 60 kV. An automatic flush of high-pressure air occurs after every discharge, removing the switching products and allowing the switch to pass currents of up to 600 kA many times, with only occasional maintenance. Fig 4.2 illustrates the annular cross-section and connections of the switch. The output pulse from a high voltage thyatron circuit, triggered from a standard pulse generator, provided the necessary trigger signal to the central trigger electrode of the switch.

4.1.3. Bank protection

An automatic crowbar (explained later) and an inductance that can be bypassed for very high-current work are inserted between the output of the bank and the cables leading to the experiment. These serve to protect the bank from both excessive current and high voltage reversals, due to short-circuits occurring beyond the output cable connectors. Such unplanned events are likely of course to occur in the all-weather environment of a firing range.
Fig 4.3 is a schematic circuit diagram of the capacitor bank components, and Fig 4.4 shows the protective components arranged above the bank.

Low-inductance fuses are inserted between each capacitor terminal and its connection to the main overhead strip-line. A series of experiments was conducted to determine a suitable copper fuse that would allow the working current to pass without damage, but that would fuse at about five times this value. The final design consisted of a 25 μm thick, 12 cm long and 8 cm wide foil, mounted in a cassette with silicon beads packed around with glass-fibre matting. The packing inhibits any voltage restrike across the fuse and completely absorbs and contains the fuse explosive energy; refurbishment needs only replacement of the fuse. Fig 4.5 shows the fuse cassette, mounted between current return lines on either side to balance the forces on the foil. Mylar sheets on each side of the cassette provide insulation between the lines. Each fuse arrangement is housed in a sturdy and compact container. The performance of the fuse as an open-circuiting device is illustrated in Fig 4.6. Fig 4.6 (a) shows the 290 kA maximum output current waveform from a capacitor discharge circuit and Fig 4.6 (b) is the same circuit with a fuse inserted.

![Diagram](image)

**Fig 4.5:** Capacitor fuse arrangement; B-fuse cassette, A-strip line connections

**Fig 4.6:** Capacitor discharge current cutting action with a fuse: (a) without a fuse (b) with a fuse.
4.1.4. The charging and dumping system

A high-voltage supply using switch-mode techniques and capable of providing an output at up to 6 kJ/s is used to charge the capacitors. This circuit has the advantages of accurate pre-setting of the voltage by the operator and minimum delay in attaining it, both of which are of particular benefit on a firing range.

Perspex tubes, filled with deionized water doped with copper sulphate, and with large area copper electrodes at each end, are used both as a charging resistance of around 25 kΩ, to protect the charger from excessive initial short-circuit currents and unintentional large voltage reversals, and as low resistance energy dumps for the bank.

4.1.5. The safety system

Personnel safety is ensured by a pneumatic interlock fail-safe system. Pneumatic switches capable of holding off 40 kV, and operated from the bank control equipment, prevent charging of the bank when personnel are within the capacitor bank enclosure.

4.1.6. Control cabin

Throughout the test program, the control cabin was positioned some 25 m from the capacitor bank. The cabin contains the electrical mains distribution equipment for the two containers, controls for charging the bank and firing the explosive detonators, and recording equipment. This includes storage oscilloscopes and pen recorders, as well as the pulse delay generators needed to provide the various triggering pulses in the correct timing sequence. Power is provided via an isolating transformer and a number of mains filter circuits to reduce any pick-up of spurious signals.

The trigger for initiating the firing sequence is obtained from a pick-up coil located in a tunnel in the main capacitor bank transmission line, to ensure that if the start switch flashes over before the firing button is pressed no results are lost. Optical isolators at a number of triggering points prevent large switching transients from interfering with the triggering sequence.

4.2. Recording cables and earthing arrangements on the firing range

A large underground earthing mat beneath the range storage and explosive magazine buildings, and situated some 5 m from the cabin, provides a good single earth connection to the MPPF via thick copper bars. Good quality 1.27 cm diameter 50 Ω impedance
coaxial cables convey the sensed waveforms from the explosive device to a junction box at the earth point, then back to a termination box attached to the cabin. Short coaxial fly leads pass the signals to an internal distribution board for connection to the oscilloscopes. Spark gaps with breakdown voltages of 2 kV are connected across each cable in the termination box, to prevent any excessive voltage pulses from finding their way into the recording circuits. Additional large-pulse arrestors connected across the oscilloscope inputs provide protection for the amplifiers.

4.3. Crowbarring

Crowbarring or clamping is a short-circuiting technique that isolates a current source, thereby trapping energy in the load circuit. It is usually introduced when the current is at a maximum and when the source is a capacitor bank that is required to provide a unidirectional load current. A crowbar also protects the capacitors from the large reverse voltages that would otherwise reduce their working life. A schematic diagram of a discharge circuit with a crowbar is shown in Fig. 4.7.

Two crowbarring designs are used, one operated automatically by an exploding foil and the other based on a detonator and triggering circuit. Both designs provide a short circuit, by punching through and severing the Mylar insulation (typically 4 sheets 125 μm thick) between a strip-line. The resistance introduced by the crowbars was estimated from capacitor discharge waveforms as about 1 mΩ.

The crowbarring topologies are also easily adapted to act as closing switches in high current circuits.

4.3.1. Automatic crowbar switch

In this design, the explosive forces that sever the insulation are provided at a number (determined by the fusing current) of fuse sites (termed bridges) distributed evenly in parallel across, and connected in series with one of the 250 μm thick copper strip-line conductors at the switching location. Brass plates at the top and bottom of the switch
complete the crowbarring assembly. The 1 cm long, 0.5 cm wide bridges are formed by etching them across a 100 μm thick 6 cm long aluminium foil, the same width as the line, and removing the unwanted areas. The automatic crowbar operates satisfactorily for discharge waveforms with quarter periods up to about 20 μs. For longer periods, the sharp impulsive intensity of the explosion progressively weakens, with a consequent reduction in the switching performance. Fig 4.8 shows an expanded schematic view of the component parts of the fuse sandwich. The foil bridges are also shown seen in Fig 4.4.

When the fuse bridges explode, they drive the interleaved aluminium foil, seen connected to the load side of the switch in Fig 4.8, onto the edge of a groove machined across the bottom thick brass plate (termed the anvil) connected in the return line, and so form a low-resistance (less than 1 mΩ) metal-to-metal contact between the lines. The whole assembly is compressed with a clamp or weight as shown.

4.3.2. Detonator crowbar switch

This switch, which can be triggered at any chosen time, was usually used for waveforms with quarter periods longer than 20 μs. Fig 4.9 is an expanded schematic drawing of the arrangement of the switch component parts, and Fig 4.10 shows the switch connected in a 20 cm wide strip line, prior to inserting the insulation and detonators.

Detonators (typically two), are mounted in balsawood plugs, set firmly in holes in a plate connected in the earth return line (cold line). They provide the large impulsive forces needed to drive a thick aluminium plate connected to the load side of the switch, together with the lower main insulating sheets, onto replaceable machined knife edges attached to the anvil, to provide a metal-to-metal connection. The switch assembly is held together with clamps. To avoid electrical connection with the plate, the detonators are set back by approximately 3 mm from the end of the balsa plugs. The load current waveforms shown in Fig 4.11, obtained from the typical discharge circuit of Fig 4.7, both without (a), and with (b), a crowbar, illustrate the crowbarring action.
4.4. The laboratory capacitor bank

A very low-inductance capacitor bank capable of storing 135 kJ at 30 kV and delivering 2 MA into a low inductance load was assembled in the laboratory, to satisfy all the demands envisaged in the laboratory-based experiments. The 27 µF coffin-shaped capacitors contain a number of parallel-connected low-inductance individual capacitors. These are immersed in caster oil in a number of compartments along the length of the capacitors, with each compartment having several small stud output terminals to connect to a high-voltage strip-line; the low voltage terminals are connected to the metal coffin case. A low-inductance connection between the capacitor compartments is provided by a 0.25 m wide closely spaced strip-line along the top of the capacitors, with the hot line connected to the stud terminals and the return line to the case.

Two parallel lines of five capacitors are mounted vertically on thick PVC covered marine plywood supported by concrete blocks, Short strip-line connections are made to the main
250 µm thick 0.6 m wide copper transmission lines which pass between them. The closely spaced lines are insulated with sheets of Mylar. A spongy rubber sheet positioned over the line with plywood and concrete slabs on top is used to inhibit any movement produced by the electromagnetic forces. This arrangement has an inductance of less than 10 nH for each capacitor and its connections, and a total inductance for the bank of ten capacitors of a few nanohenrys. Together with a low inductance start switch (described later) which can operate between 6 and 30 kV, this satisfies the high current specification.

A 40 kV short-term rated transformer with output rectifier and variac control provides the negative capacitor charging voltage. Personnel safety is ensured by a fail-safe pneumatically operated dumping and charging water resistor system, and a laboratory door lock that prevents charging of the bank until the door is closed. Fig 4.12 shows the completed bank.

Earthing is provided by a thick copper strip attached to the laboratory walls and terminated at the local mains supply earth. The bank controls and recording equipment are located in a small room adjacent to the laboratory. High quality 50 Ω impedance coaxial recording cables are fed into the laboratory through an overhead aluminium pipe, and connected to terminal boards. Coaxial fly leads are used for the connections at either end.

4.4.1. The multichannel solid dielectric start switch

4.4.1.1. The switch cassette

The high-voltage output line of the capacitor bank is connected to the upper jaw of a 0.6 m wide brass start switch cassette, with the bottom jaw insulated from it and connected via an automatic crowbar switch (which can be shorted out if required), to the load circuit. The jaws open to receive a replaceable switch sandwich, and they are closed over it and firmly weighted together when the bank is fired. A slot 1 mm deep and 2 cm
wide is milled along the cassette faces, giving additional control of the breakdown of the switch, and connecting holes leading to a rear cavity provide an exhaust route for the pressure and debris produced. Fig 4.13 shows the switch cassette with the jaws open.

**4.4.1.2. The disposable sandwich and switching action**

The action of the switch relies on maintaining the enhancement of the electric field that occurs at the sharp edges of a 5 μm thick aluminium trigger strap, which tapers from 0.015 to 0.002 m over its 0.8 m length. The trigger is sandwiched between 15 cm wide 175 μm thick upper and 75 μm lower Mylar sheets, whose inner surfaces are oiled to exclude air and so lessen the corona effects that would otherwise degrade the enhanced electric field. The Mylar overlaps the length of the switch cassette to provide insulation between the cassette jaws. Aluminium contact electrodes 50 μm thick and 5 cm wide are lightly attached by a thin layer of oil to the outer surfaces of the sandwich, with blotting or masking paper surround limiting enhancement of the electric field at the sharp electrode edges. This technique reduces the likelihood of corona leading to breakdown across the surface of the insulation between thin strip-line conductors. Fig 4.14 shows; (A) the switch sandwich with the top insulator and electrode removed, together with (B) a completely assembled unit and (C) a unit recovered after a discharge showing the multichannel nature of the switching action.

The DC potential of the trigger is set by a potentiometer connected to the hot line of the bank, with the voltage level determined such that the dielectric sheets are equally stressed in the static condition.

---

**Fig 4.13:** The start switch cassette

**Fig 4.14:** The disposable start switch sandwich
The switch is triggered (refer to Figs 4.13 and 4.14), by injecting a sharp rising (ns) pulse of about 30 kV into the increasing impedance transmission line formed by the tapered strap and its adjacent electrode. As the leading edge of the pulse propagates along the switch it produces an enhanced field at the edges of the strap, with a consequent initiation of breakdown channels in the upper thick Mylar sheet (see comment on trigger potential below), the switching time being of the order of 1 ns. Because of the finite resistive phase time of the switch channel, aided by the increasing impedance, the pulse front is able to continue to travel along the switch sandwich, with only a slight reduction in its amplitude, with its trailing edge being eroded by each insulation breakdown. At each breakdown channel, as the trigger electrode is connected to the main electrode of the upper thick Mylar sheet, overvoltting of the thinner sheet occurs, with subsequent rupture. The energy carried by these ruptures is sufficient to initiate intense shock waves which, in turn, enhance the original breakdown channels in the thick Mylar.

The thin aluminium electrodes, with a 1 mm deep 2 cm wide air gap above, serve to broaden the channel current, thereby producing a more uniform current and little electrode damage at the brass groove edges. In the later switching phase, when the resistance of vaporizing aluminium is introduced as the conducting channels develop into main current carriers, adjacent underdeveloped ones are assisted to become main carriers by a preference of the current to flow in their lower resistance paths.

Since breakdown in plastics occurs more readily when they are stressed in the positive direction, and the capacitor bank is negatively charged, a positive trigger pulse that takes the trigger strap potential away from that of the electrode backing the thicker Mylar is needed to satisfy the switching conditions.

The arrangements described above provides a fast, high current (MA), low inductance (nH) switch, with as many as 30 main current channels distributed along the 0.8 m length and occurring with a high degree of simultaneity.

### 4.4.1.3. Start switch triggering generator

The switch triggering pulse is generated by a 0.8 m long 0.2 m wide low impedance strip-line in a Blumlein configuration [J H Crouch et al 1972]. When charged to −30kV and triggered, this provides an 8 ns wide positive-going 30 kV output pulse into a matching load. Over voltages of approximately 45 kV occur however when connecting to the switch, due to the mismatch of the Blumlein to the higher impedance of the start switch
sandwich. Fig 4.15 is an expanded diagram of the Blumlein with a simple solid dielectric triggering switch sandwich (described below in Section 4.5.2), awaiting insertion to make close contact with the copper lines when the open mouth seen in the figure is closed over it. A fast small-capacity discharge circuit with a surface start switch (see next section), followed by a pulse transformer, provides the triggering pulse for the switch.

A variac controlled -30 kV voltage multiplying circuit [Craggs & Meek 1954], and an 8 kV spark gap with a triggering circuit [R J Rout 1969] shown in Fig 4.16 and Fig 4.17, were constructed to complete the start switch components. Fig 4.18 is a schematic diagram of the start switch circuit arrangement.

**Fig. 4.15:** Expanded isometric diagram of the Blumlein

**Fig. 4.16:** Circuit diagram of voltage multiplier
Fig. 4.17: The 8kV pulse generator circuit diagram

Fig. 4.18: Schematic diagram of the start switch circuit arrangement
4.5 Simple fast trigger switches

In situations where fast triggered switching is needed, but multichanneling is not essential, simplified and easily constructed versions of the solid dielectric and surface switches are used. In similar circumstances, but where an untriggered switch will suffice, a simple nail switch is used.

4.5.1. Triggered surface switch

A line diagram of a multichannel surface switch is shown in Fig 4.19. The gap in the copper line holds off the switch potential, and is made to arc across in many channels with nanosecond simultaneity. The gap edges are formed by curved electrodes, which ensure that a firm and uniform contact is made with the Mylar line insulation. The switch is triggered by applying a fast rising pulse, of equal value but opposite in polarity to the switch potential, to a long thin metal strip laid between a Mylar sandwich, and placed immediately below and parallel to the gap. The gap has an inductance of a few microhenrys and has been shown to be very effective and reliable for voltages up to 10 kV. Fig 4.20 is a filmed sequence taken with a framing camera (0.136 µs frame interval) of the breakdown across a 0.66 m wide switch, taken at about the 1.2 MA peak value of a discharge current waveform [Miller & Stewardson 1965]. The plan view shows multichannel arcs, and the end view obtained via a mirror, illustrates their small “lift-off” from the insulator surface.

Simple narrow versions of the switch with flat electrodes were used for fast circuit triggering.
4.5.2. Triggered single channel solid dielectric switch

A simplified version of the multichannel solid dielectric disposable sandwich described earlier was used to switch voltages of up to 30 kV. A drawing of the switch sandwich is shown in Fig 4.21. The switch is arranged as a thin strip-line (see Fig 4.15), with the switch sandwich inserted between its insulating sheets. When the sandwich is triggered, it connects the line conductors together. The sandwich consists of a short tapered 5 µm thick trigger strap oiled between two sheets of 15 cm square Mylar (typically 75 µm thick). Small coincident cut-outs in the top and bottom line insulation, with copper inserts, bring the plates into contact with the switch sandwich over a small area at the sharp end of the trigger taper. The DC trigger voltage is set so that, when the voltage is applied across the switch strip-line, the Mylar sheets are equally stressed at close to the breakdown electric field over their small area of contact with the line. The switch is triggered by application of a sharp pulse to the trigger electrode. The pulse appearing at the end of a 5 m long cable, charged to one half the switch hold-off potential and discharged by a ball-gap is sufficient to operate it.

Masking tape at the edges of the copper inserts, and strips of polythene faced Mylar covering all but a centimetre length at the narrow end of the tapered trigger electrode, are ironed on to both outer surfaces of the switch sandwich. These prevent long premature arcs forming between the enhanced electric fields at the sharp edges of the trigger strap and copper inserts. Fig 4.22 shows the breakdown damage caused at the trigger edge, close to its narrow end.
4.5.3. Nail switch

This very simple switch consists of a sharp steel nail driven through the insulation of a strip line. A circular plastic plug, with a sliding fit hole for a 1 mm diameter nail drilled axially through it, ensures that the nail is positioned vertically above one of the lines. The point protrudes through a tiny hole in the top line to rest lightly on the Mylar insulation. When the nail is in position, a rigid plastic pipe approximately 60 cm long, with an inner diameter slightly larger than that of the plug, and a steadying collar at the bottom, is positioned over the plug. The switch is operated by a steel weight secured at the top of the pipe by a pin crossing beneath it. The top of the weight is shaped so that it enters the core of a solenoid. When energised from the control room the pin is removed, allowing the weight to fall on to the nail and drive it though the insulation and to make contact with bottom line. The switch is capable of rapidly switching (ns) currents up to many hundreds of kiloampes, but with the switching action accompanied by a short burst of high frequency switching transients.

4.6. The vacuum rig

A 2-stage vacuum rig capable of evacuating a large steel belljar to a pressure of $10^{-3}$ Pa was used in plasma open circuiting switch (POS) experiments. Currents of a few 100 kA can be fed into the belljar though a low-inductance connection in the base, that consists of a sandwich of disc-shaped electrodes and thick plastic insulators with o-rings between them. Extensions in the side and top, with ports at their ends, allow high-speed camera or flash x-ray records of the switching process to be obtained. Fig 4.23 shows the rig, with an Imicon camera being aligned for a still picture prior to a dynamic firing.
4.7. Measurement and calibration techniques

Obtaining accurate and repeatable current and voltage measurements in high pulsed power experiments is a demanding task, that is only achieved by good design and careful calibration of the measuring probes and recording equipment. In situations where power and energy are to be measured to within 5%, individual measurements need to have an accuracy of better than 2%. Local earthing mats, usually via the electrical supplies supplemented with earthing rods, provide circuit earthing. Such arrangements inevitably lead to earth loops in the measuring circuits, introducing extraneous waveforms, which complicate the analysis of results. Spurious voltages are always present, but they can be minimised by extracting results via pick-up coils to avoid a direct connection to the pulsed power circuit.

The photonic method of current measurement (discussed in section 3.1.4) eliminates these problems but was unavailable during most of the experimental studies.

4.7.1. Flux probes

4.7.1.1. General design details

Solenoids with a small cross sectional area, often referred to as \( \bar{B} \) probes, were used to sense the derivative of the magnetic field in the majority of experiments. They were positioned at points in the circuit where the magnetic field can easily be deduced from Amperes circuital law

\[
\oint \bar{B} \cdot dl = \mu_0 I \tag{4.1}
\]

where \( \bar{B} \) is the magnetic flux density and \( I \) is the current contained within the closed path of integration. Within a small diameter tunnel formed across one of the conductors of a thin strip-line, with a line separation much less than its width (\( w \)), the flux density within the tunnel is reasonably constant over its cross sectional area at

\[
\bar{B} = \mu_0 \frac{I}{w} \tag{4.2}
\]

where \( I \) is the current in the line.

The flux linkage with a \( \bar{B} \) probe having \( N \) turns and a cross-sectional area \( A \), placed along the tunnel, is therefore

\[
\psi = ANB = \mu_0 AN I/w = MI
\]

50
where \( M = \mu_o AN/w \) is the mutual inductance between the probe and the line.

If the maximum value of the current derivative is known, a probe can be designed to suit the amplitude limits of the recording system, since the induced probe voltage is \( M\dot{I} \).

Similarly, between the conductors of a coaxial line

\[
B = \mu_o \left[ \frac{I}{2\pi R} \right]
\]

(4.4)

where \( R \) is any radius between the two conductors. A probe of radius very much less than the separation between the lines, with its axis positioned circumferentially between the lines at the radius \( R \), would have a flux linkage of approximately

\[
\psi = \mu_o \left[ \frac{AN}{2\pi R} \right] = MI
\]

(4.5)

with a probe output voltage of

\[
V_p = \mu_o \left[ \frac{AN}{2\pi R} \right] \dot{I}
\]

(4.6)

The flux probes were calibrated after positioning and securing in the circuit at the start of a series of experiments, by temporarily reducing the complexity of the circuit so that \( \dot{I} \) is easily calculated (see section 4.7.4), and \( M \) can be determined from \( V_p/\dot{I} \).

### 4.7.1.2. Attenuation and integration of the probe output signal

The probe output is fed to an oscilloscope via a 50 \( \Omega \) coaxial cable, matched at its end by the compact combination of a simple attenuator and RC integrator as shown in Fig 4.24. \( R_1 \) and \( R_2 \) are 0.5 W low-inductance carbon-composition resistors, with the signal attenuation factor \( k = \frac{V_{out}}{V_{in}} = \frac{R_2}{(R_1 + R_2)} \) giving a signal recorded on the oscilloscope of

\[
51
\]
Tests with a fast pulse generator confirmed that, for values of \( k \) down to 0.05, compensating the attenuator for stray capacity or inductance is unnecessary, and that the output rise times were adequate for the study waveform profiles envisaged.

The capacitor voltage \( V_c \) in Fig 4.24 is an integrated version of the probe voltage \( V_p \) when the time \( R_3C \) is very large in comparison with its duration. Under these circumstances, the voltage drop across the capacitor is very small in comparison with that across the resistor \( R_3 \) and the current flowing into the capacitor \( I_c \) can be regarded as \( V_p / R_3 \). The capacitor voltage is therefore

\[
V_c = \frac{1}{C} \int I_c \, dt = \frac{1}{R_3C} \int V_p \, dt
\]

since \( V_p = ML \)

\[
I = \frac{V_c R_3C}{M} \quad (4.8)
\]

For \( V_c \) to have the same profile as a sensed ramp current waveform of duration \( T \), \( R_3C/T \) should be \( >> 1 \). For discharge waveforms of period \( T \) a time constant \( > 20 \) \( T \) should be used. [Millman et al 1965]

4.7.1.3. Design considerations for the probe

(a) The magnetic field \( H_b \) produced by the \( \dot{B} \) probe coil opposes \( H_t \) due to the current in the strip-line. It will therefore have an effect on the field that is being measured, and this effect should be minimised by ensuring that \( H_t \) is at least 100 times \( H_b \).

For a probe of length \( l_p \) placed along a tunnel formed across a strip-line of width \( w \)

\[
H_b = \frac{NI_b}{l_p} \quad (4.9)
\]
and since

\[ I_b = \frac{M \dot{I}_l}{R_L} \]  \hspace{1cm} (4.10)

where \( R_L \) is the characteristic impedance of the probe output cable, which is typically 50 \( \Omega \), and \( I_b \) is the probe current.

Then

\[ H_b = \frac{N M \dot{I}_l}{l_p R_L} \]  \hspace{1cm} (4.11)

and since

\[ H_I = \frac{I_I}{w} \]  \hspace{1cm} (4.12)

it follows that for \( H_b << H_I \)

\[ \frac{N M w \dot{I}_l}{R_L l_p I_t} << 1 \]  \hspace{1cm} (4.13)

(b) The output voltage of an RC integrator \((V_c)\) is much less than its input level \((V_p)\). The maximum input level should therefore be as large as possible, in order to raise the output voltage to well above the level of any spurious pick-up voltages.

(c) Shielding the probe to reduce capacitive coupling may be necessary, when it is positioned close to circuits subject to high voltage transients.

(d) The probe must be held securely in the circuit, so that its position with respect to the current paths remains constant. It should have a low time constant, to ensure it to record faithfully fast transients.
**4.7.1.4. Typical probe design for insertion into a strip line**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gauge of wire</td>
<td>40 SWG</td>
</tr>
<tr>
<td>Coil diameter</td>
<td>$3 \times 10^{-3}$ m</td>
</tr>
<tr>
<td>Coil cross-sectional area</td>
<td>$7.1 \times 10^{-6}$ m$^2$</td>
</tr>
<tr>
<td>Coil length</td>
<td>0.2 m</td>
</tr>
<tr>
<td>Number of turns</td>
<td>20</td>
</tr>
<tr>
<td>Overall probe diameter</td>
<td>$8 \times 10^{-3}$ m</td>
</tr>
<tr>
<td>Width of strip line</td>
<td>0.3 m</td>
</tr>
<tr>
<td>Probe output resistance</td>
<td>50 Ω</td>
</tr>
<tr>
<td>Mutual inductance</td>
<td>0.595 nH</td>
</tr>
<tr>
<td>Self inductance</td>
<td>12 nH</td>
</tr>
<tr>
<td>Time constant with 50 Ω load</td>
<td>0.24 ns</td>
</tr>
</tbody>
</table>

The experimental ranges of $I_i$ and $\dot{I}_i$ were expected to be

$$I_i = 0.1 \text{ to } 8 \text{ MA}$$
$$\dot{I}_i = 10^3 \text{ to } 10^6 \text{ MAs}^{-1}$$

and inserting the worst case of $\dot{I}_i/I_i = 10^7 \text{ s}^{-1}$ into equation 3.13 gives

$$\frac{H_p}{H_i} = 3.610^{-3} \ll 1$$

**4.7.1.5. Probe construction**

Fig 4.25 shows a typical 40 cm long flexible flux probe. It consists of a short length of coaxial cable, terminated at one end by a BNC plug and bared at the other to reveal the inner insulation. The coil is wound on the insulation, with one end connected to the cable braiding and the other returned via the inner conductor. A sleeve shrunk over the coil and connections both fixes and protects them, and a thick pliable plastic tube over the probe insulates it from the line. Stiff plastic or glass insulating tubes are used when a rigid probe is preferred.
In situations where excessive pick-up is produced due to the capacitive coupling between the coil and the line, a shield of thin aluminium (kitchen foil) with 12.5 μm Mylar underneath (to avoid a shorted turn, which would inhibit flux entering the coil area) is wrapped around the coil insulating sleeve and connected to the cable braid. Fig 4.26 shows the general details of the rigid and shielded flux probe assembly.

4.7.2. DC voltage measurement

It is important that the high DC voltages provided by the many voltage setting circuit during the experimental studies are accurately known, and an inexpensive DC measuring circuit was therefore constructed and used as a standard against which the set voltages were calibrated. The circuit contained a 450 MΩ chain of good quality high voltage (HV) resistors, together with a high quality rotating mirror galvanometer to measure the series current. The 0.75 m long chain was mounted inside a 5 cm diameter Perspex tube, set up on a metal stabilising base connected to ground. The resistor chain was connected to a HV terminal at the top of the tube, and via a 1 MΩ resistance to the base. A BNC socket mounted in the base and connected across the 1 MΩ resistor provided the output connection for the galvanometer current. The measurement accuracy was checked against a 1 kV commercial voltage standard source, and later by a HV Techtronix probe.
4.7.3. Measurement of pulsed voltage

A resistive 30 kV pulse attenuator with a fast response was constructed, tested and used to measure voltages with respect to earth on discharge circuits with good earthing arrangements. When used to measure the voltage between any two points in a high current discharge circuit, with poor earthing conditions and consequent earth loops, it introduced excessive switching transients and base line shifts into the recorded traces. Most HV measurements are however made by connecting a water resistor between the measuring points in the circuit, and sensing the current needed to calculate the voltage across it using a self integrating Rogowski coil. Tests on a fast discharge circuit confirmed that its rise-time response is adequate for the range of pulses envisaged in the studies.

4.7.3.1. HV Resistive probe (HVRP)

The 30 kV attenuator seen in Fig 4.27, consists of a chain of resistors soldered closely together, mounted inside a 51 cm long 1.25 cm diameter copper pipe and insulated from it by nine wrappings of 50 μm Mylar sheet. Thirty 47 Ω, 1 W carbon resistors make up the high resistance arm R₁, and a 33 Ω output resistor comprises the low resistance arm R₂. Thin copper strips 1.5 cm wide and 15 cm long soldered to the end of the resistor chain and the copper pipe form the input connection. The attenuated waveform was fed to a 50 Ω (R₃) recording cable via a BNC socket, let into and soldered to the pipe.

This arrangement resulted in a low resistance arm of \( R₂ \times R₃ / (R₂ + R₃) = R₄ \) giving a “resistive attenuation factor” \( k_I = \frac{V_{output}}{V_{input}} \) of \( R₄ / (R₁ + R₄) = 1.275 \times 10^2 \). An additional attenuator (with an attenuator factor \( k_2 \)) that was matched to the impedance of the output cable to avoid reflections, was
positioned at the input to the recording oscilloscope to reduce the output voltage \( V_{\text{input}} \) to an acceptable level.

The current received at the earthed end of a length of underground cable resulting from the application of a Heaviside unit function of voltage at the sending end was used to establish the minimum voltage rise time that was recordable. The minimum duration of the input function to give a peak current of \( \frac{E}{R \ell} \) flowing to ground at the receiving end is [Warren 1942]

\[
t = \frac{6}{\pi^2} (R \ell)(C \ell)
\]

where

\begin{align*}
E & = \text{the raised potential at the sending end} \\
R & = \text{resistance/unit length of cable} \\
C & = \text{capacitance/unit length of cable} \\
\ell & = \text{length of cable}
\end{align*}

Tests with the 1 W resistors dissipating all the energy stored in a simple series connected resistive and capacitive discharge circuit confirmed that they were capable of maintaining their values accurately, while passing large currents and holding-off more than 1 kV, providing that their temperature rise was small. The voltage profiles envisaged in studies with durations from 1 to 100 \( \mu \)s satisfied these conditions. The \( C \ell \) product was calculated as 55 pF and the \( R \ell \) product was measured as 1538 \( \Omega \), giving a minimum duration of the input pulse of 54 ns.

A balance of both the resistive and capacitive attenuation is needed for this large value of attenuation of the input signal. For correctly balanced conditions \( C_1/(C_1+C_2) = R_2/(R_1+R_2) \) [Millman et al 1965],

![Fig 4.28: HVRP output response (b) to a sharp input pulse (a)](image-url)
where \( C_1 \) and \( C_2 \) are the capacitances across the high and low resistance arms. Tests of the response of the HVRP using a fast rise-time pulse generator showed that it was under capacitive compensated and \( C_2 \) needed to be increased. The correct balance was obtained by squeezing silicon grease around the low-resistance arm to increase the dielectric constant.

Fig 4.28(b) shows the output response voltage to the input pulse voltage Fig 4.28(a), of an HVRP with an additional attenuation factor \( k_2 = 0.21 \) at the oscilloscope input, to give an overall calculated pulse attenuation of \( k_1k_2 = 2.68 \times 10^{-3} \). The measured attenuation is \( 2.72 \times 10^{-3} \), and it is clear from a comparison of the leading and trailing edges of the waveforms that the minimum recordable rise-time of the HVRP is close to the minimum calculated duration of the input function of 54 ns.

4.7.3.2. Water resistor voltage probe (WRVP)

The probe uses a self integrating Rogowski coil to sense the current flowing in a water resistor passing through its central hole and connected between the measurement points in the circuit. The resistor consisted of small diameter plastic tubes, typically 0.5 cm internal diameter and 20 cm long, containing deionized water, doped with copper sulphate and sealed at each end by a copper electrode to form a resistance \( R_w \) of approximately 450 \( \Omega \). The energy dissipated in the resistor by a high-pulsed current produces a negligible rise in either its temperature or its resistance. The Rogowski sensor has a peak current capability of 5000 A, a sensitivity when matched at the end of a 50 \( \Omega \) output cable of 0.05 V/A, and a minimum recordable rise time of 20 ns. The voltage to be measured \( V_x \) is the product of the current and the water resistance \( R_w \) so that \( V_x = 20 V_{out} R_w \). Fig 4.29 shows the WRVP used to measure the high output voltage of the pulse transformer in the experiment discussed later in Chapter 8. The machined grooves on the load and the
guard rings on each side of the current sensor were needed to increase the surface tracking path length.

The water resistor is checked at regular intervals, by measuring the current flowing when 20 V DC is applied briefly to its electrodes. Both the electrodes and the fluid were changed whenever chemical deposits were observed on the electrodes.

Measurements made over the range of voltage amplitude, rise time, and pulse shape expected in the experiments, showed that the WRVP accurately recorded the calculated values. Some of the results obtained at suitable current and energy levels were further confirmed, by substituting carbon composition resistors.

In conclusion, Fig 4.30 shows the output of the WRVP (with $R_w = 394\,\Omega$), when connected via a nail switch across a low inductance $0.22\,\mu F$ capacitor charged to $20\,kV$. The resulting rise time of less than $10\,ns$ clearly demonstrates both the rapid switching time of the nail switch accompanied by the switching transients mentioned earlier, and the rise time response of the probe. A contribution to the initial overswing by the probe may also be present, since the Rogowski coil is being driven below the minimum rise time for which it was designed.

4.7.4. Determining the bank discharge characteristics and circuit values

The inductance and the resistance of an LCR discharge circuit with constant components can be readily determined from recordings of the damped sine wave profile of current made using a $\dot{B}$ probe, together with the initial bank voltage. By inserting these results into the circuit equations, peak values of the current waveform and its derivative can be determined and used to calibrate the $\dot{B}$ probe.

However, the experimental discharge circuits assembled for the studies have additional components with resistance values that change significantly during an experiment. In these cases, the constant values required for calibration and result analysis were obtained.
from discharge tests, with the variable components temporarily replaced by copper foils 
having an insignificant resistance.

When a capacitor C charged to a voltage $V_0$ is discharged through a series combination of 
resistor R and inductor L, from a time $t = 0$, the time variation of the subsequent current is

$$I(t) = I_o \exp(-\alpha t) \sin \omega t$$  \hspace{1cm} (4.15)

where

$$I_o = \frac{V_o}{\omega L}$$

$$\omega = \frac{2\pi}{T} = \sqrt{\frac{1}{LC} - \frac{R^2}{4L}} = \omega_o \sqrt{1 - \gamma^2}$$

$$\omega_o = \frac{1}{\sqrt{LC}}$$

$$\gamma = \frac{R}{2\sqrt{L}}$$

$$\alpha = \frac{R}{2L} = \gamma \omega_o$$  \hspace{1cm} (4.16)

The measurements required from the test discharge waveform are the current period $T$ 
and the ratio $F$ of consecutive current or current differential peak values. In the majority 
of tests $\gamma \leq 0.1$ and $\omega$ can be regarded as equal to $\omega_o$. It is then easily shown from 
equations 4.15 and 4.16 [Novac et al 1995] that

$$T = \frac{2\pi}{\omega_o}$$

$$L_i = \frac{1}{C} \left( \frac{T}{2\pi} \right)^2$$

$$R = \frac{2L}{T} (\ln F)$$

$$I_{\text{max}} = V_o \sqrt{\frac{C}{L}} \exp\left(-\frac{\alpha T}{4}\right) \text{I peaks} = \frac{V_o}{L} \exp(-\alpha t')$$  \hspace{1cm} (4.17)

where $t' = 0, T/2, T$ etc-----------------

and if $V_o$ and C are known, and T and F are measured from the discharge results, the 
expressions listed in (4.17) can be evaluated.
In test circuits with $\gamma > 0.1$, the equations listed in (4.16) were used when evaluating the peak values. The current waveform, although remaining periodic, departs slightly from a pure sine wave, with the first current maximum occurring at $< \frac{T}{4}$ [Knoepfel 1970]

When the inductance $L^1$ between two points of a circuit is required, a voltage probe is connected between these points and the corresponding voltage $V^1$ and $\hat{I}$ at the zero current crossing points of the waveform are measured. The required inductance is the $L^1 = \frac{V^1}{\hat{I}}$. A useful expression for $I_{\text{max}}$ can also be derived and used for initial design purposes when $\alpha t << 1$ by approximating $e^{\alpha t}$ to $(1+\alpha t)$, leading to

$$I = \frac{V_o}{1 + \alpha t} \sqrt{\frac{C}{L}} \sin \omega t$$

or

$$I_{\text{max}} = \frac{V_o}{1 + \frac{R \pi}{4L} \omega_o} \sqrt{\frac{L}{C} + 0.8R}$$

Inserting the following typical circuit values for the laboratory experiments into eqn 4.17 for $I_{\text{max}}$, $V_o = 20$ kV, $C = 270 \mu$F, $L = 200$ nH, $R = 3.2$ m$\Omega$ gives $\alpha t = 0.092$ and $I_{\text{max}} = 670$ kA. Using these values in eqn 4.18 then gives $I_{\text{max}} = 671$ kA.

4.8. Recording facilities and analysis of results

The recording facilities (transported between the laboratory and the firing range as required) consisted of three digital storage oscilloscopes each having a 100 MHz bandwidth and a 400 MH/s sampling rate. There were eight recording channels, and the stored waveforms were recorded on paper plotters. A further two oscilloscopes, with a 200 MHz bandwidth and a 1 GH/s sampling rate, were used for recording the fast waveforms produced in the plasma switching studies. The experimental waveforms were first analysed manually, before the results were input into a computer for final analysis and presentation.
CHAPTER 5

FLUX COMPRESSOR FIRING STUDIES

5.1 Flux compressor (FC) firing facilities

A number of members of the Megagauss Club listed in Table 2.1 still have ongoing FC firing programmes conducted at static purpose-built firing ranges, equipped with the necessary pulsed power, priming, recording, and diagnostic facilities, and manned by experienced staff who determine and control the firing procedures. Fig 5.1 shows such a range at the Los Alamos National Laboratory NM USA, with an FC driven device on the firing platform. The capacitor bank, recording and FC firing equipment are all housed in a bunker beneath the platform.

![Fig 5.1: View of firing range at Los Alamos](image)

Similar facilities were assembled at the Atomic Weapons Research Establishment on Foulness Island in Essex for the UK FC research programme that ran for about ten years from the mid 1950s. The firing range facilities were however dispersed and the staff disbanded when the programme ended.

At the start of the present research programme, a number of manned firing sites and bomb chambers were available for hire within a practical distance of travel from the University, some with pulsed-power equipment for specific purposes such as rail gun research. None of these were however able to offer the combination of explosive weight capability and facilities needed for firing FCs with megajoule output energies, and the explosive weights up to 15 kg that were required for the studies.

5.1.1 The mobile pulsed-power facility (MPPF)

The self contained MPPF described in Chapter 4 and used for the firings described later, was assembled as a multipurpose facility able to provide the pulsed power, recording and firing controls needed for the FC studies. It is shown in Fig 5.2 deployed on the military
controlled Proof and Experimental firing range at West Lavington Wiltshire. The capacitor bank and control cabin containers are shown shielded from the blast and fragments created by the explosion. Large specially designed concrete blocks (termed Pendine blocks) line the sides and ends, with steel plates and sandbags above to protect the roof.

The MPPF offers a number of advantages for a tightly budgeted research programme. It was constructed for a small fraction of the cost of a static purpose built system mentioned earlier, could easily be transported and deployed on an open firing range having a mains supply or generator nearby, and needs only a simple firing bunker with a platform below ground level to confine the blast and fragments. An explosive magazine with safety officer within reach, access to a small mobile crane, and the occasional use of some local range workers to refurbish the bunker after a firing complete the requirements. This self contained unmanned system, with virtually unlimited access, and minimum range costs, enabled firings to be conducted at short notice using 3 or 4 people drawn from the Loughborough Pulsed-Power Research Group. These arrangements also enabled close contact to be maintained with the experimental procedures and details of the work.

5.1.2 The firing bunker

Fig 5.3 shows the excavated firing bunker, with Pendine blocks faced with wooden railway sleepers supporting the walls. The firing platform rests on sleepers and is supported each side by Pendine blocks, leaving an accessible cavity below the platform in which some experimental components could be positioned and recovered. The firing platform consists of two sandwiches of 2" thick steel plates, each with car tyres spread between the plates to
cushion the explosive shock. The sandwiches are positioned side-by-side with a small gap between them, through which a transmission line can convey current to a load circuit positioned in the cavity. A sloping path from ground level provides access to the platform.

5.2 Layout of the FC firing and recording circuits

Fig 5.4 is a schematic representation of the priming, firing and recording equipment layout, for firing FCs (shown in truncated form) with megajoule outputs and priming currents greater than 30 kA.

An output pulse from the thyatron (triggered via the firing button) operates the start switch to turn on the FC priming circuit current (coloured green). Standard battery-operated firing equipment is used in the firing circuits (coloured red) to fire the detonators that initiate the explosive in the FC armature. A flux probe positioned between the capacitor output lines triggers the firing circuits and the oscilloscopes. This ensures that if the start switch pre-triggers before the set capacitor charging voltage is reached the correct firing and recording sequence is nevertheless maintained.
When conducting pre-tests of the complete firing system for a megajoule size FC, the detonator crowbar (shown in Fig 5.5, without its detonators or insulating Mylar) is inserted in the priming discharge circuit, at a position close to the firing bunker. The position chosen is where the coaxial feeder cables reduced from 12 to 6, and the detonator is operated by the redirected FC detonator firing cable shown dotted in Fig 5.4. The crowbar simulates the FC crowbar and enables the timing conditions to be monitored by the FC flux probes.

An alternative firing circuit and a detonator start switch with initiating cable are also shown dotted in Fig 5.4. This is used for low energy FCs requiring small priming currents, at capacitor voltages less than the 12 kV minimum needed for reliable operation of the capacitor bank start switch. The switch is accordingly short circuited, the firing ignition pulse link disconnected, and an appropriate reduction is made to the number of priming current feeder cables.

A Rogowski self-integrating current sensor records the current flowing in the bared inner conductors of two of the feeder cables. An ionisation probe (described in Section 3.3.3.1), positioned on a detonator at the end of one of the firing cables, provides a timing pulse occurring at the same time as the armature is detonated. Ionization probes are also used on some FCs to record time of arrival of the detonation wave at points close to the beginning and at the end of the armature explosive.

The flux probes shown in the FC load in Fig 5.4 were designed using the procedures described in Chapter 4 and inserted during its construction. They provide sufficient output voltage (within the capability of the recording equipment and associated attenuator/integrators), to record adequately the wide range of initial to final current and its derivative determined from the design figures, that approach 200 and 1000 respectively in some of the FCs fired in the study. The FC current and its derivative are
obtained from the integrated / attenuated recordings of the waveforms produced by the flux probes using the equations of Section 4.7.1.2.

The HV optical isolators shown in Fig 5.4 are included to prevent switching transients interfering with the triggering sequence. Fig 5.6 is a view inside the control cabin, showing the recording equipment assembled for a firing and secured to the benches by straps that minimise any movement resulting from ground shock and blast waves.

5.3 Estimating the priming current waveform characteristics

The priming current quarter period and peak value for a given capacitor bank voltage are estimated from the calculated or measured values of the inductance, resistance, and capacitance of the discharge circuit and the FC. The results are confirmed, and the flux probes calibrated using measurements of the recorded waveforms (see Section 4.7.4) obtained from a priming current discharge conducted during the firing procedures.

5.4 Firing and recording procedures

There is only one chance to obtain results when firing an FC, since it is totally destroyed by the explosive charge. Some of the more pertinent reasons for losing results are errors in the setting of the firing circuit delay or in the estimation of the time between initiation of the armature explosive and operation of the FC crowbar (termed crowbar delay), needed to ensure that crowbarring occurs at the peak value ($I_{p_{max}}$) of the priming current. High voltage breakdown, and incorrect oscilloscope time base, recording amplitude and polarity settings are others. Loss of results for any reason is frustrating, time consuming, and costly.

To minimise the possibility of mistakes during the busy firing activities, instructions and schedules detailing the delay and oscilloscope settings are drawn up for each firing. Pre-tests of the complete firing and recording system are also conducted close to the firing time to check the crowbarring and $I_{p_{max}}$ simultaneity, and the recording equipment connections and settings.
5.4.1 Setting the firing circuit delay

Correct FC crowbarring conditions are obtained by adjusting the firing circuit delay to bring the time of initiation of the armature explosive into coincidence with that of the peak of the priming current, and then subtracting the estimated crowbar delay ($t_{cd}$).

5.4.2 The pre-tests

Prior to inserting the explosive, but with all other connections in place, the complete FC firing system is operated at a low priming current level. Errors in the recording connections, delays, and settings are identified by inspection of the output waveforms of the flux probes, recorded at appropriate oscilloscope input amplification levels. The crowbar coincidence conditions are identified from the pre-test waveforms of an megajoule size FC with the simulated crowbar inserted, and delay adjustments made as required. The crowbar conditions, when firing a low energy FC needing a detonator start switch, are however checked by noting the time difference (displayed on the same record) between the input priming current maximum and the arrival time of a pulse derived from a ionisation probe taped to the end of a detonator initiated by the FC firing cable.

The crowbarring timing and action is illustrated in Fig 5.7, which shows the attenuated and integrated waveforms recorded from the output of a flux probe during a pre-test of a 1 MJ FC at a priming discharge current maximum of 19 kA. The close coincidence between the crowbar time and the quarter period of the priming current is clearly seen in the figure.

5.4.3 Estimating the FC crowbar delay

The coincidence conditions for the pre-tests discussed above occur when the armature explosive is detonated. For the same conditions to apply when the crowbar operates in the FC, the calculated time between detonation and contact of the armature cone with the crowbar (item 7 in Fig 5.8) is subtracted from the firing circuit delay for the pre-tests.
Fig 5.8 shows a sectioned schematic view of an FC detonation and crowbar assembly with dotted arrows indicating movement of the armature. Crowbarring becomes effective when the armature reaches the crowbar contact at C. This happens when the detonation wave-front has travelled a distance $x$ from A to B at a detonation velocity $v_d$, propagated a shock-wave through the armature wall thickness $t$ at a shock velocity $v_s$ (approximately 5 and 3.7 mm $\mu$s$^{-1}$ for aluminium and copper respectively), and expanded the armature radius a distance $y$ from B to C at a radial velocity $v_r$ (ways of determining the dynamic characteristics of the expanding armature are discussed in section 6.4.5). The total crowbar delay time $t_{cd}$ is therefore

$$t_{cd} = \frac{x}{v_d} + \frac{t}{v_s} + \frac{y}{v_r} + \text{approximately } 2\mu\text{s for the crowbar closing time}$$

For example, the megajoule FC discussed later in chapter 6, has an aluminium armature and $x = 220$ mm, $t = 9$ mm, $y = 20$ mm and $v_d = 8.2$ mm $\mu$s$^{-1}$, $v_s = 5$ mm $\mu$s$^{-1}$, $v_r = 1.74$ mm $\mu$s$^{-1}$, $\alpha = 12^\circ$. This gives a crowbar delay of $t_{cd} = 42.3$ $\mu$s.

5.5 The firing programme

Firings commenced with the appraisal and comparison of the performance over a range of priming currents, of two small helical FC designs [Brooker et al 1997] with kilojoule outputs and hand wound or machined constant pitch coils. Both designs utilised 30 mm diameter aluminium armatures filled with 0.25 kg of PE4 plastic explosive.

The firings were also required for the proof tests and commissioning of the MPPF and explosive initiating facilities, and served to familiarise the supporting personnel with the firing and range procedures.

The programme continued with the appraisal of larger helical FCs with megajoule outputs, including a complex machined design intended for the high current testing of output conditioning components, and a hand wound design termed FLEXY that was
designed specifically for the research programme. The design and construction, together with the appraisal (using the procedures described above) and use of FLEXY for exploding foil experiments, are all presented in the next chapter.

5.5.1 Estimating the FC performance

From the discussion of Section 2.1 it follows that given the priming current $I_0$, the inductance of the FC plus load $L_0$ at crowbar time $t_0$, (see Fig 2.2) and the final load current $I_1$ at time $t$ when the remaining inductance $L_1$ equals the design load value, the proportion of the original linkage flux in the load inductance is

$$\text{Output flux / Input flux} = \frac{I_1}{I_0} \frac{L_1}{L_0} = \eta$$  \hspace{1cm} (5.1)

or in percentage terms

$$\text{The flux efficiency of the FC} = 100 \frac{I_1}{I_0} \frac{L_1}{L_0} = 100 \eta$$

and more specifically, the percentage of the initial flux lost by the FC loss mechanisms described in chapter 6 is

$$\text{flux loss} = 100 (1-\eta) \%$$

Furthermore

$$\text{the FC current gain} = \frac{I_1}{I_0} = \frac{L_1}{L_0} \eta / L_1$$  \hspace{1cm} (5.2)

and the energy gain

$$\text{the energy gain} = \frac{W_1}{W_0} = \frac{L_0}{L_1} \eta^2 / L_1$$  \hspace{1cm} (5.3)

$L_0$ is calculated (see Section 6.1.2) and confirmed using the methods described in Section 4.7.4. To satisfy the above equations, an accurate assessment is needed of the time $t$ at which the armature/coil contact reaches the position along the coil, where the remaining inductance $L = \text{load inductance } L_1$. A value for $t$ is found by estimating the time taken by the armature to expand radially to meet the coil after operation of the crowbar, and for the armature/coil contact to move along the coil to the position where $L = L_1$ using the radial cone and detonation velocities. Changes of amplitude on the current derivative waveform when the armature/coil contact crosses an abrupt change of inductance inherent in the FC design are also used to identify the contact position and to corroborate or improve the above estimate.

When the leading edge of the cone reaches the end of the explosive inside the armature its motion along the armature comes rapidly to rest. A further reduction of the inductance
with some increase in energy then occurs as the surface of cone is continues to be driven radially in a complex manner by the explosive inside the cone.

Time \( t \) for the small FCs (Minigen) mentioned above, with \( L_1 \) of several hundred nH connected across the end of the coil, is indicated when generation is finished and \( \text{d}l/\text{d}t \) is close to zero. The Megajoule size FCs fired during this work had integral coaxial loads with an inductance \( L_1 \) of a few tens of nanohenrys when the armature/coil contact reached the end of the coil at \( t_2 \) with the cone inside the coaxial load (see Fig 2.2). An understanding of the significance of the \( \text{d}l/\text{d}t \) and \( I \) changes of slope and level when the armature contact is approaching or is within the load is needed when determining \( t_2 \) and the corresponding value of \( L_1 \) to obtain an accurate assessment of the output energy.

The slope of the \( \text{d}l/\text{d}t \) and \( I \) waveforms reduce as the armature/coil contact approaches the end of the coil, and \( \text{d}l/\text{d}t \) is reduced abruptly to a lower positive level, with a small corresponding indication in \( I \) when the end is reached at \( t_2 \). If the leading edge of the cone reaches the end of the explosive at the same time, some additional generation with an increase of current will occur, as mentioned earlier. The \( \text{d}l/\text{d}t \) then drops to a negative value as \( I \) falls. When the armature explosive is arranged to extend further into the load, the \( \text{d}l/\text{d}t \) will increase after its reduction at \( t_2 \) as generation takes place, with a significant increase of \( I \) as the cone moves into the coaxial load reducing its inductance. Arrival of the leading edge at the end of the explosive will be indicated on the \( \text{d}l/\text{d}t \) and \( I \) waveforms as described above.

5.5.2 Performance of the Minigen

5.5.2.1 Hand-wound Minigen

The 20 cm long coil was potted in epoxy resin, had a mean diameter of 5.4 cm, and was wound with 2.64 mm round-section copper wire insulated with heat-shrink sleeving which also provided the desired spacing. It had an inductance of 34 \( \mu \)H, and a load inductance of 400 nH connected between its end and the
armature. Fig 5.9 shows the generator mounted in the firing bunker.

The inconsistencies in performance that were observed are attributed to the simplified method of construction employed, in particular turns skipping, observed on several firings due to misalignment between the coil and the armature, (described in section 6.3.2) is probably responsible for their low efficiencies. The flux efficiency dropped with an increase of input current and ranged from 23 % for \( I_0 = 1 \) kA, \( W_0 = 17.2 \) J, giving current and energy gains of 20 and 4.6 respectively, to 11.3 % with current and energy gains of 9.7 and 1.1 for \( I_0 = 9 \) kA, \( W_0 = 1.39 \) kJ. Fig 5.10 shows the \( I_0 \), \( I \) and \( \text{dl/dt} \) waveforms obtained from a typical firing with \( I_0 = 4 \) kA, \( W_0 = 0.275 \) kJ. The flux efficiency is 16 \% and current and energy gains are 13.8 and 2.2, giving \( I_1 \) and \( W_1 = 55 \) kA and 0.605 kJ.

5.5.2.2 Machined Minigen

The 27 constant-pitch turns of the Minigen coil, carefully machined from a copper cylinder, are sufficiently wide (4.4 mm) to ensure low Joule heating losses at the expected current level. The
finished coil is potted in epoxy resin, and the resin inside the coil is machined out, leaving the uninsulated inner surfaces of the windings at a finished diameter of 5.64 cm. Great care is taken to ensure concentricity of the coil/armature assembly. Some misalignment may occur however, in providing the transition needed from the coaxial generator geometry to the short parallel plate feed for the 115 nH load inductance consisting of a single turn of 15 cm wide copper foil. The initial inductance of the generator is 8.5 μH. A short cylindrical connector needed to convey the priming current to the generator, and to house the explosive initiator, completes an assembly some 36 cm long. Fig 5.11 shows the generator in the firing bunker.

The low resistance windings, and the care taken to ensure a high level of coil/armature concentricity, was expected to result in a higher flux efficiency and a more consistent performance than obtained from the hand wound Minigen.

The performance of the Minigen was investigated over a range of priming currents between 5 kA and 10 kA. The flux efficiency rose with increasing input current, from 12.8 % with current and energy gains of 9.4 and 1.2 for I₀ = 5.2 kA, W₀ = 0.115 kJ, to 19.2 % with current and energy gains of 14.2 and 2.7 for I₀ = 10 kA, W₀ = 0.41 kJ. A record of the current waveform and its derivative obtained with I₀ = 10 kA, giving I₁ and W₁ = 142 kA and 1.1 kJ, is shown in Fig 5.12. The abrupt change in the current derivative close to the end of the generator run
is thought to be due to the geometry change problems mentioned earlier.

The low efficiencies obtained for this carefully made generator, and its reduction for a decrease of input current to values less than those obtained for the hand-wound Minigen were puzzling. The \( \frac{dI}{dt} \) records were smooth, showing no indication of flux loss due to either breakdown or turn skipping.

However, an analysis of a typical low priming current result, [Novac, et al 1997] using a 2D model that takes into account the electric field distribution within the generator, showed that the experimental current waveform could be closely simulated by assuming that electrical breakdown occurred between the uninsulated helical coil and the armature during the compression process at an electric field intensity of 34 kV/cm. Fig 5.13 shows the calculated and experimental result. The analysis indicated that the unexpected results were indeed attributable to voltage breakdown, and that the efficiency would doubtless have been higher if provision had been made to fill the generator with \( \text{SF}_6 \) gas to increase the breakdown electric field.

The induced voltage in a generator is a measure of the energy conversion level, while a poor result illustrates the importance of determining its value at the design stage. A main objective of good design is to provide the maximum insulation without compromising the generator action, and for the induced voltage level to be as high as possible over the generator run while avoiding voltage breakdown. These design considerations are discussed more fully in section 6.1.

5.5.3 Machined MJ helical FC

Fig 5.14 is a sectional schematic diagram of a 1MJ FC designed by the Pulsed Power Group (of which the author was a member) at the Atomic Weapons Research

![Fig 5.14: Megajoule size machined FC](image-url)
Establishment (AWRE), for use as an energy source in weapon experiments in the 1960's. The complex design of the 20 cm diameter 86 cm long coil requires 27 turns to be machined from copper tube. The turns consist of constant and variable pitch sections followed by a region of turns tilted to match the angle of the cone. The width of the turns increases towards the load end, to handle the increase in current. The coil is overlaid with fibreglass and potted with epoxy resin and has an inductance of 30 $\mu$H.

The 10.2 cm OD diameter armature containing approximately 11 kg of composition 8 explosive completed a sealed unit through the coil and coaxial load, allowing SF$_6$ gas at a pressure of 3 bar to be contained, to increase the voltage breakdown level between the uninsulated surface of the coil and the armature. The generator was used extensively as an energy source to produce about a megajoule of energy into a range of inductive loads. With a 50 nH integral coaxial load and an initial current of 30 kA it had a measured flux efficiency of 33 %, and the current and energy gains were 200 and 60. The design evolved after some 5 years of FC research and for security reasons was not reported in the open literature.

It was agreed that the generator should be reconstructed at DRA [Brooker et al 1994], with a view to using it as a pulsed power source in this study. Drawings were available, together with additional instructions for the careful attention needed to the geometry, insulation details, and alignment of the tilted turns, in order to avoid voltage breakdown and turn skipping, and to ensure a smooth transition of the cone both into and out of the tilted turns region.

Machining and manufacturing details however had to be worked out from scratch. Consequently construction was difficult and took some 18 months. The completed coil has an inductance of 32.5 $\mu$H, and the other dimensions and details are close to the original design values. The armature which extended well over a cone length into the load was packed with PE4 plastic explosive, the integral coaxial load inductance with the armature/coil contact at its entrance is 50 nH, and free flowing SF$_6$ at up to 3 bar pressure provided internal

![Fig 5.15: Megajoule machined FC in firing bunker](image)
insulation. Flux probes placed near the end of the load provide the output waveforms required to record the current and its derivative. Fig 5.15 shows the finished generator in the firing bunker.

Fig 5.16 (a) shows the output current profile obtained with a priming current of 32 kA and an initial energy $W_0$ of 16.64 kJ. The flux efficiency when the contact reached the end of the coil ($t_2$ in Fig 5.16) and the load value was 50 nH was only 17.3%. The current gain of 112.5 and the energy gain of 19.5 give $I_1$ and $W_1$ as 3.6 MA and 0.325 MJ. The maximum value of 8.5 MA was reached when the armature cone was well inside the load, with an optimistic calculated value of 14 nH load inductance remaining. The efficiency at this time is 11.4%, with current and energy gains of 266 and 30.3.

The reason for the reduction in energy in the 50 nH load at $t_2$ from 1 to 0.325 MJ is clearly shown in the current derivative profiles of Fig 5.16 (b) and (c). The main flux losses occur, as the cone is moving through the region of tilted turns, indicated by the ragged profile on the derivative waveform. Flux losses are seen each time the cone moves to the next turn, with a major but temporary breakdown with large flux loss occurring at the end of the last turn. After the breakdown the generator is seen to recover, and continues to generate as the cone moves within the coaxial load region. The energy into an optimistic final load inductance of 14 nH is 0.504 MJ.

**Fig 5.16:** MJ machined FC output waveforms
Improvements are needed in the alignment / insulation in the tilted turn region, and the breakdown conditions at the end of the coil, to obtain the full potential output of the generator. The difficulties and time scale of construction of this FC, and the need for further development however ruled it out as a high-pulsed power source for the present experiments, and therefore it was necessary to design the Flexy megajoule FC mentioned earlier in order to fulfil the research programme.
HELICAL FC DESIGN CONSTRUCTION AND APPRAISAL

At the onset of this study, it was envisaged that the machined 1 MJ AWRE flux compressor described in the previous chapter would be used as the pulsed-power source for high-current testing of the output conditioning components, and that a 10 MJ version would provide the energy source in a final design. However, the poor experimental performance of this compressor and together with the other factors described in section 5.5.3, ruled it out as a potential energy source. A much simpler hand-wound compressor was therefore designed, constructed and tested. Although the use of hand-wound devices is common, and many details have been published, no comprehensive account appears of any successful and proven design and construction process. It was imperative therefore for the necessary techniques to be developed.

When designing a flux compressor, it is important to have available a mathematical model of adequate accuracy to determine the initial parameters and to predict the likely performance. Unfortunately, most existing models are very detailed [McGlaun et al 1980, Freeman et al 1980], requiring long run times on large and fast computers and yet yielding results of limited accuracy for the final load current and energy. A simple but efficient mathematical model was therefore developed and verified using previously published data and results. The computing requirements are much reduced in comparison with existing models, and a good quantitative guide is provided to both the different phenomena that are involved and the measures needed to keep the various losses under control.

A number of the new design of 1 MJ compressors was constructed and tested on the firing range. The helical stator coil used commercially available insulated cables (which led to the design being termed FLEXY). While performing the essential series of pre-tests a number of manufacturing problems associated with the coil were highlighted. Once these had been overcome, an extremely successful series of firings was obtained. Detailed considerations of the experimental results showed that the intended output was obtained, with the results being in generally good agreement with predictions based on the design code.
This chapter describes the mathematical model, the input data that is required and the verification procedures that were followed. Details of the design, construction and testing of the generator then follow.

6.1 A simple generator model

Although high-intensity magnetic and electric fields may are produced during flux-compression, it is possible to neglect the effects of the magnetic field on the armature dynamics and the conductor assembly for both low- and medium-energy generators in which the magnetic field intensity is kept low by a suitable choice of turn-splitting (sic) [Tucker 1980, Pavlovskii et al 1980]. The effects of the electric field cannot however be similarly neglected, and early considerations that ignored the high voltage developed between the helical coil and the armature led either to extremely inaccurately predicted results [Cummings et al 1966] or to results that could not be reproduced regularly in practice [Shearer et al 1968]

In considerations in which the effects of the electric field are properly included [Pavlovskii et al 1980, Chernyshev et al 1980], an upper limit is imposed on the maximum voltage $V_{\text{max}}$ that is allowed to develop between the coil and the armature. This leads to an expression for the time variation of the generator inductance $L$ following crowbarring at time $t = t_0 = 0$ as [Chernyshev et al 1980]

$$\frac{dL}{dt} + \frac{dL}{dt} + RL = 0$$  \hspace{1cm} (6.1)

where $I$ is the load current, subscript $0$ denotes initial values and $\gamma$ is the assumed constant ratio of resistance $R$ to inductance $L$. Although equation (6.1) has been employed in generator design it is not generally satisfactory, since in practice the value of $\gamma$ varies widely with time. A better representation of the generator parameters [Novac et al 1985] is obtained from the simultaneous solution of the equations

$$L = R(t, I, dI/dt)$$  \hspace{1cm} (6.2)

$$L = L(t)$$  \hspace{1cm} (6.3)

in which the functions for $R$ and $L$ are obtained as explained below. In eqn (6.2), the second term, which represents the maximum value of the voltage induced in the generator circuit, is the energy conversion term. The energy multiplication ratio of the
The generator is therefore directly related to the maximum permitted value of this voltage. These basic considerations together with the limitations of helix flux compressors [Novac et al 1997] are presented in Appendix A.

The model developed below considers a generator with the stator divided into a number of equal length sections, each having a constant winding pitch and with the same number of turns in any parallel current paths. The axial dimensions of the crowbar and the output ring shown in the typical 4-section generator of Fig 6.1 will be neglected and the behaviour during two different time periods considered separately.

![Diagram](image)

**Fig 6.1:** A four-section generator: (i) the armature cone at the crowbar position; (ii) the armature cone at the final position of period (1); (iii) the armature at the intermediate position of period (2); and (iv) the armature cone at the final position of period (2).

a) The first period (period 1), while the armature cone is expanding from the crowbar position (i) of Fig 6.1, until contact is made with the helical coil at position (ii).

b) The second period (period 2), while the point of contact between the armature cone and the helical coil moves along the coil length to the final position (iv).

Another important feature of generator design is the length of the explosive charge. If the end of this lies at the output ring of the stator, any additional compression of the flux after this point, when the cone is driven by its own inertia, is ignored in the design.

### 6.1.1 Ohmic resistance

Calculation of the ohmic resistance of a generator is based normally on a unidimensional model for diffusion of the magnetic field into both the coil conductors and the armature wall. The skin depth $\delta$ that is involved is determined from [Knoepfel 1970]
\[
\delta = \left( \frac{I}{(dI/dt) \mu_0 \sigma_0} \right)^{1/2}
\]  
(6.5)

where \( \mu_0 \) is the magnetic permeability of free space and \( \sigma_0 \) the conductor conductivity at room temperature. Eqn (6.5) is valid only for an exponentially rising current, which occurs only approximately in practice, and it gives a somewhat pessimistic estimate during the final moments of compression when the current growth may be far from exponential.

For initial resistance calculations, the skin depth that is effective during discharge of the priming capacitor bank is [Knoepfel 1970]

\[
\delta = \left( \frac{2(L_0 C)^{1/2}}{\mu_0 \sigma_0} \right)^{1/2}
\]  
(6.6)

where \( C \) is the bank capacitance.

The resistance \( R_c \) of one section of the helical stator coil is determined using a skin effect factor \( f_s \) and a proximity effect factor \( f_p \), such that [Welsby 1964]

\[
R_c = R_{DC} f_s f_p
\]  
(6.7)

where \( R_{DC} \) is the DC coil resistance and

\[
f_s = \frac{\Phi}{(4\delta)}
\]  
(6.8a)

\[
f_p = \begin{cases} 
1 + \frac{\Phi n^2}{4p^2\delta^3} & \Phi < 2\delta \\
1 + \left( \frac{\Phi n}{\delta} \right)^2 & \Phi \geq 2\delta
\end{cases}
\]  
(6.8b, 6.8c)

where \( \Phi \) is the wire diameter, \( n \) the number of parallel paths in a section, and \( p \) the pitch of that section. Both skin and proximity effects are described by [Welsby 1964], from which the formulae above are adopted.

The current in the generator armature may be considered as the summation of an axial current with circular currents developed by induction from the stator. The total current then
clearly forms a helix with the same pitch as the corresponding stator section, and the armature resistance is calculated using the formulae above, but with $f_p$ equal to unity.

A significant phenomenon that occurs during the first period of the flux compression process is a rapid increase in the generator resistance. The armature is expanding and the corresponding longer path of the helical current through the cone is evaluated in Appendix B.

### 6.1.2 Inductance

Many methods exist [Fowler et al 1975, Grover et al 1980, Jones 1980, Tucker 1980, Cowan et al 1984, Fowler et al 1989] whereby the inductance of the individual stator sections of a helical generator may be calculated. That used in the present design process [Fowler et al 1975] has the advantage of being relatively simple, with an error of less than 5% provided that the ratio of the diameter of the helical coil to the length of a section is less than about 1.5. On this assumption, the inductance $L_s$ of a stator section of length $l$ is

$$L_s = \frac{k_1 l^2 (r_c^2 - r_a^2)}{p^2 [1 + k_2 (r_c - r_a)]} \quad (6.9)$$

where $r_c$ and $r_a$ are respectively the coil and armature radii (see Fig 6.1) and the constants involved are $k_1 = 0.003948$ and $k_2 = 0.45$.

The mutual inductance between adjacent sections of the stator coil (x and y, say) is much less than the self-inductance of either, and may conveniently be calculated [Novac 1989] from

$$M_{xy} = \frac{k_2 L_x p_x (r_c - r_a)}{p_y [2l + k_2 (r_c - r_a)]} \quad (6.10)$$

For coil sections that contain an expanded armature within their axial length, and possibly an armature/coil contact, the correspondingly reduced inductance is obtained by subtracting from the value given by eqn (6.9) the quantity [Novac 1989]

$$\Delta L_s = \frac{k_1 [r_a (r_1^2 - r_2^2) + \frac{1}{3} (r_1^3 - r_2^3)]}{p^2 \tan \alpha \left[1 + k_2 \left(r_c - \frac{1}{2} (r_1 + r_2) \right) \right]} \quad (6.11)$$
where \( r_1 \) and \( r_2 \) are the radii of the armature cone at the section ends and \( \alpha \) is the cone angle (see Fig 6.1 position (iii)). In eqns (6.9)-(6.11), all lengths are in millimetres and the inductances are obtained in microhenrys, and when the contact is within a section the length \( l \) is decreased accordingly. All three equations are deduced under the assumption that the flux density is constant throughout the volume between the armature and the coil. In eqns (6.9) and (6.11) the value used is that produced at the centre of a simple helical coil and in eqn (6.10) it is that produced at the same position but by an adjacent coil. Further approximations [Fowler et al 1975] were introduced into the above equations to improve the accuracy of prediction, with most importantly \( (l^2 + 4r_c^2)^{1/2} \) being replaced by \( (l + 0.9r_c) \).

Because the self-inductances of the stator winding are so much greater than the mutual inductances, it is unnecessary to calculate any variation in the latter due to the expanding armature. However, this, together with the approximations used in deriving eqns (6.9)-(6.11), leads to a small discontinuity in the theoretical inductance variation each time the contact point moves from one section to another.

Values for the resistance and inductance obtained from the equations in sections 6.1.1 and 6.1.2 are used in a simultaneous solution of eqns (6.2)-(6.4) to provide time variations of the current, flux density, stored energy etc. The solution takes only a few seconds using a FORTRAN language program implemented on a PC-386 computer.

**6.2 Verification of the model**

Generally speaking, experimental and other generator data available in the literature are inadequate to provide a satisfactory basis for establishing the accuracy of prediction provided by the equations of the previous section. Nevertheless, a few important results (described in Sections 6.2.1 and 6.2.2) exist that can be used for this purpose.

**6.2.1 The Mark IX generator**

The Mark IX generator [Fowler et al 1989] developed at the Los Alamos National Laboratories (USA) provides a high-current output into a low-inductance load, and is a low-gain device needing a large input energy. Published information on this generator, together with details of the way by which the following input data for the design program were derived, are given in Appendix C.
The helical coil is of length 1085 mm, inside diameter 356 mm and it has five equal-length sections, 217 mm each, see Table 6.1. The copper armature is of outside diameter 173 mm and thickness 9 mm. The cone angle $\alpha$ is 14°, the detonation velocity is $9 \text{ mm} \mu \text{s}^{-1}$, the crowbar diameter is 252 mm, the load inductance is 60 nH and the priming current of 413 kA is obtained from a 1500 $\mu$F, 40 kV capacitor bank.

<table>
<thead>
<tr>
<th>section</th>
<th>pitch mm</th>
<th>number of parallel cables</th>
<th>cable diameter mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>54.25</td>
<td>5</td>
<td>9.3</td>
</tr>
<tr>
<td>II</td>
<td>108.5</td>
<td>10</td>
<td>9.3</td>
</tr>
<tr>
<td>III</td>
<td>223</td>
<td>20</td>
<td>9.3</td>
</tr>
<tr>
<td>VI</td>
<td>461</td>
<td>40</td>
<td>9.3</td>
</tr>
<tr>
<td>V</td>
<td>461</td>
<td>40</td>
<td>9.3</td>
</tr>
</tbody>
</table>

Table 6.1. The five sections of the helical coil of the Mark IX generator.

Details of the generator inductance variations, obtained using a far more complex model than that described above, are also available [Fowler et al 1989] and Fig 6.2(a) and (b) show that very good agreement exists between the values of both the inductance and its rate-of-change with time calculated using this model and the present design program. Fig 6.3(a) shows that good agreement also exists between the measured and calculated load currents for the first 100 $\mu$s following the crowbar action at time $t_0$. At time ($t_2$ for the actual generator, $t_{2m}$ for the model) the armature cone enters the output ring (see Appendix C) and the subsequent error in the predictions increase slightly. However, it still remains at only about 20% at the end of the period (2) ($t_3$ for the actual generator, $t_{3m}$ for the model), when the detonation process is complete. The unusual form of the experimental rate-of-change of current
characteristics of Fig 6.3(b) has been noted previously [Degnan et al 1983] and is confirmed by the corresponding computed results. This indicates that it is a normal part of generator action, quite unrelated to any unusual loss mechanisms.

Fig 6.3: Characteristics of the Mark IX generator: (a) the load current variation with time; (b) the rate of change of load current variation with time; (c) the resistance variation with time; and (d) the maximum internally generated voltage variation with time; The lines are (----), the design program and (-- -- -- -- --), measured [Fowler et al 1989].

The very high experimentally obtained generator resistance evident in Fig 6.3(c) results from the very high voltage developed internally while the armature cone is expanding from the crowbar position, which can be seen in fig 6.3(d). Substantial energy losses occur when the armature cone makes contact with the uninsulated input ring (see Appendix C, Fig C.1) but these disappear gradually as the contact point moves along the cables comprising the helical stator winding.
6.2.2 The EF-3 generator

The EF-3 generator delivers a medium-level current to a load of fairly high inductance and is primed from a low-energy capacitor bank. It was developed at the Institute of Atomic Physics (Romania) and, from the data available [Novac 1989, Ursu et al 1990, Novac et al 1991] the following information can be extracted for use as input to the model design program.

The helical coil is of length 1500 mm and inside diameter 160 mm. It has 15 equal-length sections; of 100 mm, see Table 6.2.

Table 6.2 The 15 sections of the helical coil of the EF-3 generator.

<table>
<thead>
<tr>
<th>section</th>
<th>pitch mm</th>
<th>number of parallel cables</th>
<th>cable diameter mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>3.2</td>
<td>1</td>
<td>2.5</td>
</tr>
<tr>
<td>II</td>
<td>3.5</td>
<td>1</td>
<td>3.0</td>
</tr>
<tr>
<td>III</td>
<td>4.0</td>
<td>1</td>
<td>3.0</td>
</tr>
<tr>
<td>IV</td>
<td>4.7</td>
<td>1</td>
<td>4.0</td>
</tr>
<tr>
<td>V</td>
<td>5.5</td>
<td>1</td>
<td>4.5</td>
</tr>
<tr>
<td>VI</td>
<td>6.3</td>
<td>1</td>
<td>4.5</td>
</tr>
<tr>
<td>VII</td>
<td>7.9</td>
<td>2</td>
<td>3.0</td>
</tr>
<tr>
<td>VIII</td>
<td>9.5</td>
<td>2</td>
<td>4.0</td>
</tr>
<tr>
<td>IX</td>
<td>11.1</td>
<td>2</td>
<td>4.5</td>
</tr>
<tr>
<td>X</td>
<td>14.3</td>
<td>3</td>
<td>4.0</td>
</tr>
<tr>
<td>XI</td>
<td>19.0</td>
<td>3</td>
<td>4.5</td>
</tr>
<tr>
<td>XII</td>
<td>25.4</td>
<td>4</td>
<td>4.5</td>
</tr>
<tr>
<td>XIII</td>
<td>35.0</td>
<td>6</td>
<td>4.5</td>
</tr>
<tr>
<td>XIV</td>
<td>51.0</td>
<td>8</td>
<td>4.5</td>
</tr>
<tr>
<td>XV</td>
<td>76.0</td>
<td>12</td>
<td>4.5</td>
</tr>
</tbody>
</table>

The copper armature is of outside diameter 80 mm and thickness 3 mm. The cone angle is 13°, the detonation velocity is 7.7 mm$\mu$s$^{-1}$, the crowbar diameter is 107 mm, the load inductance is 110 nH and the priming current is 5 kA (from a 1650 $\mu$F, 4.5 kV capacitor bank).
Fig 6.4(a) and (b) show respectively the calculated time variation of the generator inductance and of its rate-of-change with time, and the presence of the fifteen stator coil sections is clearly shown by the discontinuities of Fig 6.4(b). Figs 6.5(a) and (b) compare experimental and computed time variations of both the generator current and its rate of change with time, and it can be seen that good agreement is obtained until the end of the detonation period. Beyond this considerable divergence appears, with the experimental current reaching a peak and declining, but with the computed current continuing to increase. It is clear that, to obtain better agreement, generator losses other than the ohmic effects so far included must be taken into account, and this is considered in some detail in the next section.

Although these effects are not significant in the 1 MJ generator designed later, they are included here for the sake of completeness. Fig 6.5(c) and (d) show respectively the variations in the generator resistance and the generator internal voltage, and the presence of the separate stator sections is again apparent. Towards the end of the flux compression this voltage reaches about 110 kV, which was too high to be sustained by the insulation of the cables, even when a 1.2 mm layer of polyethylene foil was added along the coil interior [Novac et al 1991]
6.3 The inclusion of non-ohmic resistance

The computed results presented in the previous section are substantially in agreement with corresponding measured results, at least to the accuracy achieved in previous studies. Nevertheless, the inclusion in the theoretical considerations of the non-ohmic loss mechanisms discussed below enables an even closer comparison to be readily obtained.

Fig 6.5: Characteristics of the EF-3 generator: (a) the load current variation with time; (b) the rate of change of load current variation with time; (c) the resistance variation with time; (d) the maximum internally generated voltage variation with time. The lines are (-----), the design program; (-----), measured [Novae et al 1989, 1991] and (------), the modified design.

Fig 6.6: The mechanism for voltage breakdown losses, using a sinusoidal model for armature cone defects.
6.3.1 Non-linear diffusion of the magnetic field

Two ways of calculating the effective resistances and inductances of a helical generator circuit have been described [Antoni et al. 1975]. The first of these considers the diffusion process when determining the inductances and assumes a permanent loss of energy as the stator turns are removed from the circuit. The alternative, and more commonly adopted approach, neglects the diffusion process when calculating the inductances but includes it in the resistance calculations. There is no loss of energy at the contact point when the diffusion process is kept linear by the imposition of a low value for the current density. However, additional losses occur at the point of contact between the armature cone and the coil, where a high magnetic field is developed as the volume of the wires heated under a nonlinear diffusion process is removed from the circuit. Under these conditions, a modified skin depth $\delta^*$ must be calculated from [see Knoepfle 1970]

$$
\delta^* = \delta \left[ \frac{1}{T_0} \left( T_0 + \frac{B^2}{\mu_0 \rho c_v} \right) \right]^{1/2}
$$

(6.12)

where $T_0$ °K is the initial coil temperature, $B$ is the magnetic flux density arising from the coil current and $\rho$ and $c_v$ are respectively the density and specific heat of the coil conductors. The additional loss is calculated as the magnetic energy contained in the volume between $\delta$ and $\delta^*$ and the equivalent resistance is (see Appendix D)

$$
R_{nd} = \frac{2\mu_0 v_{det} [\delta^* (\phi - \delta^*) - \delta (\phi - \delta) ]}{\pi \phi^2 \rho \cos \beta}
$$

(6.13)

where $\beta$ is the angle made by the coil turn relative to a plan normal to the coil and $v_{det}$ is the detonation velocity.

For a generator design in which the linear current density along the coil axis is kept below about 20 MAm$^{-1}$, calculation and experiment have both shown that, in fact, little loss is produced by this process [Antoni et al. 1975, Pavlovskii et al. 1980].

6.3.2 The $2\pi$-clocking mechanism

This loss appears whenever a coil section is not coaxial with the armature, and when the eccentricity exceeds $(p/2\pi) \tan \alpha$ (where $p$ is the coil pitch) the contact point jumps unexpectedly ahead by one turn. The theory of this mechanism and a useful nomogram for determining the corresponding loss is given elsewhere [Herlach 1979]. If the coil pitch
is small, as may be the case in the early sections of a stator winding, this loss mechanism is often the only one that needs to be considered.

6.3.3 Geometric defects in the armature

Most generator armatures are machined from a thick tube, when vibration of the cutting tool can lead to sinusoidal modulation of the armature diameter along the generator axis. It has been shown both theoretically and experimentally [Antoni et al 1975], that these defects are amplified by a factor of between seven and nine as the armature expands. This process can generate multiple contact points and as a consequence leads to additional nonlinear losses. Similar defects can also result either from the use of an inhomogeneous armature material or too large a ratio of coil diameter to armature diameter.

6.3.4 Voltage breakdown

Overall, the most significant of the various non-ohmic loss mechanisms in a high-inductance generator is the voltage breakdown that occurs between a coil and the armature cone. Near the contact point the electric field is sufficiently intense for a plasma region to be formed and, with ideal geometry, this ensures good electrical contact and prevents the lines of force leaving the region [Pavlovskii et al 1980] However, if armature defects are present and the generator geometry is not coaxial, electrical breakdown is likely to occur ahead of the contact point and the plasma region. In practice, the high voltage effectively amplifies the loss mechanisms that have already been described. For instance, the effective amplitude of the sinusoidal armature defects becomes the sum of the actual amplitudes and the breakdown distance V/E, where V is the voltage between the coil and the armature at the point considered and E is the breakdown field of the air or insulating gas under these particular conditions, see Fig 6.6. For a detonation velocity \( v_{\text{det}} \) the equivalent non-ohmic resistance is (see Appendix D)

\[
R_{\text{vb}} = 2\pi \mu_0 v_{\text{det}} A \cos \alpha (2r_e - A)/p^2
\]

(6.14)

where A is the effective amplitude of the armature expansion defects in the presence of a voltage breakdown.

The total non-ohmic resistance term of a generator is the sum of the components due to the effects described above, or

\[
R_{\text{no}} = R_{\text{nd}} + R_{2\pi} + R_{\text{vb}}
\]

(6.15)
which is effective only after the armature has made contact with the coil, when it has a significant effect on the current that is calculated. Results obtained using a more complicated generator model, in which the effects summarized by eqn (6.15) are included, are added to Fig 6.5, and it is clear that, although an improved accuracy is achieved, this is somewhat less than might be expected. However, although the maximum current prediction error is 35%, the error in the maximum current is less than 10% while, during practical testing, the generator, even with improved stator winding insulation, had about a ±15% variation in the peak-current reproducibility.

6.4 The helical generator design (FLEXY)

The previous sections have demonstrated how various important features of a helical generator and its detailed performance can be calculated, and have confirmed the accuracy of these results by comparison with measured results. A formalization of the design process will now be presented, and its application illustrated in a new design of 1 MJ generator.

6.4.1 Basic input data

The basic data needed for a generator design are summarized below, with the information for the specific design considered being given in parentheses.

6.4.2 Capacitor bank

The Mobile Pulsed Power Facility described in chapter 4 was used to prime the generator. The practice adopted here follows that used in most generators, where the input cables are connected to the armature and the coil. Systems also exist however in which the current is returned directly to the bank and not through the armature [Grove et al 1980, Pavlovskii et al 1980], thereby reducing the resistance of the generator.

6.4.3 Load information

A load inductance $L_1$ of 40 nH and a maximum current $I_{\text{max}}$ about 7 MA were chosen to satisfy the envisaged output conditioning experiments. The need to keep the linear current density to an acceptable level (ie below 0.2 MAcm$^{-1}$) imposes a minimum outer diameter on the armature (106 mm). Normal practice is to produce the armature from tube that only needs machining on the outside, and to use copper rather than aluminium.
6.4.4 High-explosive characteristics

A plastic explosive, (PE4), which was easily packed into the armature, was selected. It has an initial mass density $\rho_{\text{ex}}$ of $(1.71 \times 10^3 \text{ kgm}^{-3})$, and a detonation velocity $v_{\text{det}}$ of $(8.2 \text{ mm} \mu\text{s}^{-1})$. For helical generators, the output magnetic energy depends directly on the product $\rho_{\text{ex}}v_{\text{det}}\Delta H_{\text{ex}}$ where $\Delta H_{\text{ex}}$ is the characteristic heat of detonation. (see Appendix A)

6.4.5 Armature dynamics

These are preferably obtained from field experiments using high-speed photography. Otherwise, a simple numerical code [Novac et al 1989] or a calculation based on the Gurney model [Kennedy 1972] can be used. From the armature dynamics, the optimum armature wall thickness (9 mm) and cone angle $(12^\circ)$ are obtained. For an aluminium armature, the inner coil diameter (212 mm) should be about twice the armature outside diameter [Chernyshev et al 1980, Fowler et al 1989] while for copper a higher ratio is possible [Pavlovskii et al 1980, Chernyshev et al 1980, Antoni et al 1989]

6.4.6 Maximum explosive charge

A maximum permitted charge weight on the firing range of about 15 kg restricted the armature length to 1350 mm. A length from the initiation point to the crowbar of 100 mm and the need for the end of the explosive charge to be one cone length (250 mm) beyond the output ring of the coil to avoid inertial movement set the maximum coil length at 1000 mm.

6.4.7 Crowbar

If no measures are taken to match the input cables, the armature-crowbar distance must be sufficient to withstand the reflected input capacitor bank voltage $(V_{\text{cb}})$ of about $2V_{\text{cb}}$.

6.4.8 Summary

The above data define fully the generator geometry and provide sufficient information to enable the helical coil to be designed.

6.4.9 Helical coil design

A number of rules must be observed during the coil design.
(i) The constant voltage (or constant electric field intensity) rule [Pavlovskii et al 1980, Chernyshev et al 1980]. The requirement that the generator voltage should not exceed a set value $V_{\text{max}}$ is a major design concern, since the higher the value the greater is the energy multiplication ratio. Although values in excess of 150 kV have been used, it was shown in section 5.2.1 that voltages exceeding about 125 kV are accompanied by large increases in the generator resistance if inadequate insulation is provided to prevent premature breakdown. The use of a working gas at high pressure, such as freon or SF$_6$, can reduce the chance of electrical breakdown [Shearer et al 1968, Degnan et al 1983]. To satisfy the requirement that the present design should be simple, a value of 100 kV was chosen for $V_{\text{max}}$.

(ii) The constant linear current density rule [Pavlovskii et al 1980]. It is common practice with helical generators to restrict the current density to less than 0.2 MA cm$^{-1}$ as mentioned earlier, to avoid the development of nonlinear diffusion resistances.

(iii) The containment rule. Radial cable movement within any section of the coil during the flux compression process, supposed asymmetric for conservative reasons, should be less than the $2\pi$-clocking eccentricity allowable for that section.

To simplify the coil construction, the various section pitches are chosen as integer multiples of the cable diameter adopted. Additionally, the number of parallel cables was doubled between adjacent sections, whenever possible, although in the final section consideration had to be given to both the largest diameter available and the current to be carried. Furthermore, the overall winding was divided into a number of equal-length sections, as required by the computer code. The maximum possible number of sections was used, to ensure a smooth inductance-time characteristic, subject to the constraint discussed in section 6.1.2 that the ratio of the coil inside diameter to the section length should not exceed 1.5. To provide a reasonable current density margin, the design was based on a maximum load current of 9 MA, and designs were sought for different values of $V_{cb}$. In fact no design was found for $V_{cb} = 20$ kV, while $V_{cb} = 30$ kV was regarded as too near the primary source limitations.

The final design was obtained at $V_{cb} = 25$ kV, at which, to obtain the necessary final parameters, a 1120 mm stator coil becomes necessary. The design parameters of the FLEXY are summarized below.
The helical coil is of length 1120 mm and inside diameter 212 mm. It has eight equal sections, 140 mm (each), see Table 6.3

The armature is of outside diameter 106 mm and thickness 9 mm. It is made from aluminium. The cone angle is 12°, the detonation velocity is 8.2 mm/μs⁻¹, the crowbar diameter is 172 mm, the load inductance is 40 nH and the priming current is 53 kA when the mobile capacitor bank is charged to 25 kV.

Table 6.3 The sections of the helical coil in FLEXY

<table>
<thead>
<tr>
<th>Section</th>
<th>Pitch (mm)</th>
<th>Number of parallel cables</th>
<th>Strands/diameter (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>12</td>
<td>3</td>
<td>7/0.85</td>
</tr>
<tr>
<td>II</td>
<td>18</td>
<td>3</td>
<td>7/1.35</td>
</tr>
<tr>
<td>III</td>
<td>27</td>
<td>3</td>
<td>7/2.14</td>
</tr>
<tr>
<td>IV</td>
<td>36</td>
<td>6</td>
<td>7/1.35</td>
</tr>
<tr>
<td>V</td>
<td>54</td>
<td>6</td>
<td>7/2.14</td>
</tr>
<tr>
<td>VI</td>
<td>84</td>
<td>12</td>
<td>7/1.7</td>
</tr>
<tr>
<td>VII</td>
<td>140</td>
<td>20</td>
<td>7/2.14</td>
</tr>
<tr>
<td>VIII</td>
<td>180</td>
<td>20</td>
<td>7/2.14</td>
</tr>
</tbody>
</table>

6.4.10 Design Limitations

Computed results for the FLEXY generator, both with and without the inclusion of the non-ohmic resistance terms, are presented in Figs 6.7 and 6.8. It is evident from Figs 6.8(a) and (d) that it was not possible to meet both the constant voltage and constant current density rules. This arises since the conducting cross sections corresponding to the coil pitches required to satisfy the constant voltage rule and the skin depth, in both the coil and the armature, are insufficient to fulfil the constant current density rule. The high current required (7 MA) can only be obtained at the expense of a progressively decreasing internally generated voltage,
and thus a decreasing energy multiplication. Practical evidence confirms that simple high-current, high-input-energy generators have low energy multiplication (see Appendix A). To prevent the decrease in generated voltage and energy requires the use of complex and costly techniques described in chapter 2 section 2.1.2, to provide the increase in the \( l(\frac{dI}{dt}) \) term of eqn (6.2) while satisfying the current density rule. To satisfy both the constant voltage rule and the constant linear current density rule requires either that the current is maintained at a reduced total energy gain or that the energy gain is achieved at a reduced current. If only high-energy multiplication is required then it may be obtained, if the current is kept low, by imposing a high-inductance load [Chernyshev et al 1980].

6.5 Construction

The physical arrangement of the FLEXY generator is shown in Fig 6.9. During manufacture the stator coil was wound on a special-purpose cylindrical mandrel, with the
diameter tapered slightly along the axis and the surface liberally waxed to facilitate

**Fig 6.9:** Practical arrangement of FLEXY: 1, the detonator, pellet holder and Pellet; 2, high explosive; 3, the armature (aluminium) with extensions welded at each end; 4, the start plate (aluminium); 5, the separator plate (polyvinylchloride); 6, insulator (polyvinylchloride); 7, the start ring (aluminium); 8, insulated crowbar bolts (three); 9, the helical coil; 10, fibreglass reinforcement; 11, the inertial mass (concrete); 12, the end ring (aluminium); 13 and 17, load attachment rings; 14, the coaxial load (aluminium); 15, the armature end plug (aluminium); 16, the probe Holder (polyvinylchloride); 18, the end plate (aluminium); and 19, the end screw. Broken lines show the positions of the armature cone at times \( t = t_0, t_1 \) and \( t_2 \).

removal of the finished coil. To assist further, the mandrel could be cooled by liquid nitrogen. After positioning the start and end rings of the generator (7 and 12 respectively in Fig 6.9) the correct distance apart on the mandrel, the eight sections of the coil were wound in sequence. Single core, PVC-insulated, non-sheathed, stranded cables were used, having the number of strands and size of wires shown in the design parameters for FLEXY.

Fig 6.10 shows an interconnection being made between the cables of adjacent sections, using the special soldering technique described elsewhere [Reinovsky et al 1985]. Supplementary insulation was provided at the joints, and the input and output cables of the first and last sections of the coil were terminated on lugs and

**Fig. 6.10:** The joining of cables
bolted to the appropriate ring. It was found considerably more convenient to commence winding the coil at the load end (the bigger cables) and to join the cables of the last two sections prior to winding on the mandrel. To reduce the likelihood of voids, the several interconnections between sections having different numbers of turns were in different planes normal to the coil axis. On completion, each section was bound with layers of fibreglass glued with epoxy resin, with additional reinforcement provided adjacent to both the start and end rings. Fig 6.11 shows the completed coil on the mandrel, prior to application of the concrete casing (see section 6.6).

Unmachined aluminium tubing 106 mm in diameter and having a sag of less than 3 mm over a length of 1500 mm was used for the armature. However, to obtain the length required by the design, it was necessary for the welded extensions shown in Fig 6.9 to be added. Plastic explosive (PE4) was hand-shaped into lumps about the size of a tennis ball and packed into the armature to provide the explosive charge. For simplicity the crowbar consisted of three insulated and symmetrically mounted bolts, positioned at an effective diameter of 146 mm, rather than in the more usual ring.

When the armature cone made contact with the crowbar (at time t = t₀ = 0 in Fig 6.9), the combined coil and load inductance was 37.5 μH (the value for the modified generator, see Section 6.6) and when the contact point entered the end ring (at time t = t₂) the load inductance alone was 38 nH. Calibrated Rogowski coils mounted inside the load provided measurement of both the generator current and of its rate of change with time, and the total error in the current measurement was estimated at below ±5%. The space between the armature the coil and the load was filled with SF₆ gas via inlet and outlet valves in order to increase the voltage breakdown level for the interior of the generator. The initial priming energy for the generator was obtained from the mobile capacitor bank via twelve parallel-connected coaxial cables. (Refer to Chapter 5 for a detailed description).

Crude estimates had predicted that the coil would survive the required 50 kA priming current discharge obtained with the capacitor bank charged to 25 kV without the addition of any supplementary inertial mass. During a preliminary test however, intended to
calibrate the flux probes it however failed, with the first section being destroyed. A similar problem has been noted elsewhere [Goforth et al 1987] during a full-energy firing test, and the solution adopted there was to change the capacitor bank to a Marx configuration, so as to inject the priming current at an increased rate. The approach followed here, was to leave the capacitor configuration unchanged and to use the inertial containment rule to determine the changes needed to the generator design. The modifications required are discussed in the following section.

6.6 An application of the containment rule

The containment rule must be observed if the coil geometry is to be preserved during generator testing. Although both axial and radial forces are present, the former are mainly important in the final stages of complex variable-geometry multi-megajoules generators [Cherynshev et al 1990].

Since the tensile forces produced in the coil following a capacitor discharge are much greater than the yield point for copper, elastic forces will be neglected and calculations made on the conservative basis of considering only inertial movement [Herlach 1968]. Thus, if \( B_i \) is the flux density at the coils of the \( i \)th section, with an inner surface area \( S_i \), inner radius \( r_i \) and mass \( m_i \), the radial force and corresponding radial acceleration of this section are

\[
F^i_r = \frac{B^2_i S_i}{2\mu_0} \quad (6.16)
\]

and

\[
\ddot{r}_i = \frac{F^i_r}{m_i} \quad (6.17)
\]

During the capacitor bank discharge the time variation of the flux density is

\[
B^0_{0i}(t) = B_{0i} \sin \left( \frac{\pi t}{2\tau} \right) \quad (6.18)
\]

where \( B_{0i} \) is the value corresponding to the maximum discharge current and \( \tau \) is a quarter period of the discharge. Integrating equation (6.17) gives the coil displacement [Herlach 1968] as

\[
\Delta r^0_i(t) = \frac{B^2_{0i} S_i \tau^2}{4\mu_0 m_i \pi^2} \left[ \frac{\pi^2 t^2}{2\tau^2} + \cos \left( \frac{\pi t}{\tau} \right) - 1 \right] \quad (6.19)
\]
Subsequently, between the armature making contact with the crowbar and the end ring, the flux density may be assumed to rise exponentially, from the value $B_i^0(\tau)$ corresponding to the maximum discharge current, so that

$$B_i(t) = B_i^0(\tau) \exp(\Lambda t) \quad (6.20)$$

From the equations above, the total radial displacement is

$$\Delta r_i(t) = \Delta r_i^0(\tau) + v_0^i t + \frac{(B_i^0(\tau))}{8\mu_0 m_i \Lambda^2} \exp(2\Lambda t) \quad (6.21)$$

where $v_0^i$ is the radial velocity at the end of the capacitor discharge. A satisfactory constant for $\Lambda$ can be estimated at the design stage and verified by experiment. The containment rule requires the final radial movement of each section to be less than the permitted eccentricity $\varepsilon_i$ for that section, or

$$\Delta r_i \leq \varepsilon_i \quad (6.22)$$

where $\varepsilon_i$ is given by the $2\pi$-clocking relationship presented in section 6.3.2.

Calculations using eqn (6.21) established that it was impossible for the first coil section to meet the containment rule, even with a covering of concrete to provide additional inertial mass. The generator design was therefore modified, with the cables of the first section made identical to those of the second section and an overall 40 mm concrete sheath added to increase the inertial mass to 10 kg for each section. Fig 6.12 shows the completed FLEXY coil, part concreted. This modification caused a reduction in the rate of change of the generator inductance, thus lowering the interior voltage and the overall performance.

Fig 6.13 shows the radial movement of the eight coil sections throughout a generator firing test, calculated for the modified coil design using eqn (6.21) and 25 kV capacitor voltage. The left-hand result applies for the two identical first coil sections, and the other six results apply in sequence for the remaining six sections. Scaling of the time axis is from the instant of closure of the capacitor switch, the times $t_0(=\tau)$ and $t_2$.
are as identified in Fig 6.9 and the Roman numerals correspond to the instant at which the armature cone leaves the corresponding coil section. The eccentricities of the different sections are included in Fig 6.13, which shows that for the new design all the coil sections satisfy inequality (6.22) for the maximum calculated capacitor current (shown later in Fig 6.15 by a broken line).

Further calculations showed that a current of 30 kA produced a maximum tensile stress in the coils of 30 kg mm$^{-2}$, sufficiently below the yield point of 40 kg mm$^{-2}$ for it to be acceptable for pre-test purposes. This limited the corresponding capacitor voltage to about 14 kV.

6.7 Generator testing

To enable the generator to be easily transported and manoeuvred, it was contained in the wooden frame seen in Fig 6.14, and usually mounted vertically on the firing platform with the explosive initiated at the top. This orientation with the explosive detonation front progressing towards the platform was regarded as the best for containing the explosive effects within the bunker. Mounting in this manner, with the load close to the gap in the platform sandwich, also allowed additional load components to be positioned in the cavity below the platform as described in section 5.1.2. Prior to firing, the interior of the generator was pressurized to 3 atm with SF$_6$ gas. Synchronization between closure of the switching connecting the generator to the capacitor bank and firing of the detonator for the high-explosive charge was based on data provided by the design code, and did not
account for either the breakdown time of the crowbar insulation or any variation in the
detonation velocity of the high explosive. Nevertheless, it is evident from the capacitor
current waveform of Fig 6.15 that the armature cone makes contact with the crowbar
very near the predicted time \( t = t_0 = 0 \) at which the maximum priming current was
obtained. Fig 6.15 illustrates also the effect of the generator action on the current profile,
due to armature movement. At the instant of the cone-crowbar contact, the capacitor
supplies 48 kA to the generator, giving a priming energy of 43.2 kJ and an initial flux of
1.8 Wb.

![Fig 6.15: Test results – the current waveform measured by a self-integrating Pearson-Rogowski
coil at the capacitor bank output. The broken line
is a prediction not accounting for any generator
resistance variations due to armature movement
after firing. After cone-crowbar contact \( (t = 0) \),
the waveform corresponds to a 48 m\( \Omega \), 1.4 \( \mu \)s,
250 \( \mu \)F discharge.](image)

Measurements of the load current and its
rate of change with time obtained from the
output of a flux probe are shown in Fig
6.16, with the small irregularities in the
current waveform of Fig 6.16(a) being due
to the interconnection discontinuities
described in section 6.5. The disruption
towards the end of the rate of change
waveform of Fig 6.16(b) is probably

![Fig 6.16: Test results – measurements from a flux probe coil positioned within the load: (a)
the current waveform and (b) the rate of change
of current waveform; \( t = 0 \) and \( t = t_2 \) as shown
in fig 6.9.](image)
caused by arcing between the armature and the outer uninsulated coaxial structure and the weld required for the armature extension, with the weld being unable to sustain the high final current density while expanding under the pressure of the exploding products.

As described elsewhere [Goforth et al 1990] a complex design is necessary to ensure optimum coupling of the load to the armature. Nevertheless, as Fig 6.16(a) shows, the load current when the armature cone makes contact with the end ring is 7.3 MA, giving 1 MJ for the final energy in the load. With the corresponding flux of 0.28 Wb giving a magnetic flux conservation efficiency of 15.4% and a chemical-to-magnetic energy conversion efficiency of 1.5%, FLEXY had an overall energy gain of twenty three, and a current multiplication factor of one hundred and fifty two Also, calculations showed that about 1 MJ of energy was dissipated in the various resistances discussed earlier

6.8 Theoretical/experimental comparison

Fig 6.17 presents the principal theoretical prediction for the generator performance, including only simple ohmic loss. Experimental results are added for comparative purposes, with the uncertainty in these being indicated. The equivalent resistance variations of Fig 6.17(e) were calculated from

\[ R = -\frac{L(dI/dt) + I(dL/dt)}{I} \]

using values of I and dI/dt from Fig 6.16 and L and dL/dt from Fig 6.17. A number of the experimental data points exceed the predictions, possibly as a consequence of axial coil movement, since electrical breakdown, severe 2\pi-clocking, and so on are unlikely to be important due to the correct application of the rules for generator design. There is also some error between the predicted and measured current profile of the generator, although, as with other designs investigated, the final value is accurately predicted.

Fig 6.17(f) shows that the maximum voltage internally generated during the experiment was less than 70 kV, a rather low value imposed by the containment rule. The higher voltage necessary for a greater output, with the FLEXY geometry unchanged, will require cables with increased insulation. Other more complex designs [Chernyshev et al 1991] have used cables with a breakdown voltage of 60 kV, rather than the 30-40 kV of the present cables. Additionally, an increased energy output could be obtained by using a modified coil design, without exceeding the current waveform of FLEXY, by using an increased load inductance.
Fig 6.17: Predictions of FLEXY performance with experimental data added: (a) the inductance; (b) the rate of change of inductance (c) the load current; (d) the rate of change of load current; (e) the equivalent resistance; and (f) the maximum internally generated voltage. The time is measured from armature cone-crowbar contact. Error bars on the symbols indicate the possible range of results.

6.9 Conclusions

This study has produced a simple and novel design method for explosive-driven helical generators, which can be used for either the performance prediction of existing generators or the design of future generator of high-energy output. Various practical rules, which should be followed when designing generators, were presented. For properly
designed high-energy generators, the insignificant non-ohmic resistance contribution enabled an accurate prediction to be obtained of the current history of the Mark IX generator. (Very good agreement for the peak load current and magnetic energy were also obtained for the HEG-24 generator [Chernyshev et al 1980], for which insufficient information is available for a more detailed comparison). For smaller low-energy generators, for which it is difficult to avoid $2\pi$-clocking, (and sometimes electrical breakdown of inadequately insulated coils), the basic design model can substantially overestimate the measured load current. When considerations of various non-ohmic resistances are included, good agreement was obtained with the final values of current and energy.

No complex manufacturing techniques were required for the generator that was designed, with neither tilted pitch nor variable diameter coils being used, while the armature was unmachined and filled manually with explosive. Six sets of component parts were manufactured, four FLEXY generators were assembled and tested, and three of these were filled with explosive and fired. The main features of the first generator are summarized below.

- **Overall length:** 1.8 m
- **Maximum diameter:** 0.45 m
- **Armature plus coil weight and associated components:** 65 kg
  
  (excluding explosive and added concrete)
- **Explosive weight:** 15 kg
- **Concrete weight:** 80 kg
- **Initial energy/current:** 40 kJ/50 kA
- **Final energy/current:** 1 MJ/7 MA
- **Time of operation:** 160 $\mu$s

**Cost of materials for one generator:** approximately £600

**Time for manufacture:** two technicians working for 2 weeks
OVERVIEW OF HIGH-ENERGY PULSED POWER CONDITIONING AND SWITCHING FOR FLUX COMPRESSION GENERATORS

One particular feature of the flux-compression generator is that the final energy is contained in a magnetic field, where the stored energy is many times greater per unit volume than it would be in the electric field of a capacitor. However, when the energy is released as a pulse, the rise time is far too long to meet the nanosecond output rise time requirement of the present study, for other applications such as X-ray or high-energy particle generators, or for proof-of-principle experiments in thermonuclear research for which flux-compression generators are otherwise well suited. It is necessary therefore to condition the output pulse by compressing it in time, so that the energy is transferred to the normally inductive load at a greatly enhanced rate. A circuit using one or more stages of conditioning, with each stage comprising an opening and a closing switch, conveniently accomplishes this process.

This chapter examines firstly the concepts involved in the magnetic energy transfer process, and then describes briefly typical opening and closing switches for use in each stage of conditioning. Those switches most relevant to the present study are described more fully in a later chapter.

7.1 Magnetic energy transfer

In the basic arrangement for the pulsed transfer of energy shown in Fig 7.1, the aim is to transfer to the system \( W_2 \) (the load), as quickly as possible, the magnetic energy that has been accumulated relatively slowly in the system \( W_1 \) (the source). Using the opening switch \( Q \) and the closing switch \( S \) in sequenced operation, increases the volume occupied by the magnetic flux, and leads therefore to a decompression of the energy and a reduction in the overall energy density.
An illustration of a simple energy transfer circuit is given in Fig 7.2, where it is required to transfer energy very quickly from the inductor $L_1$ (ie the source $W_1$) to the inductor $L_2$ (ie the load $W_2$). It will be assumed that the energy deposited in the resistor $R(t)$ causes it to change in resistance very quickly, to a value sufficiently high for the current through it to be neglected as soon as the switch $S$ is closed. The resistor therefore effectively functions as the opening switch $Q$.

If the current flowing in the circuit formed by $L_1$ and $R(t)$ before the switch $S$ is closed is $I_1$, and that flowing in both $L_1$ and $L_2$ immediately after it is closed is $I_2$, constant flux-linkage considerations show that

$$L_1 I_1 = (L_1 + L_2) I_2 \quad (7.1)$$

while energy conservation considerations show that

$$\frac{1}{2} L_1 I_1^2 = \frac{1}{2} (L_1 + L_2) I_2^2 + E_Q \quad (7.2)$$

where $E_Q$ is the energy lost in the resistance during the transfer process.

It follows from eqns (7.1) and (7.2) that

$$E_Q = E_1 \left[ \frac{L_2}{L_1 + L_2} \right] \quad (7.3)$$

and also that

$$E_2 = E_1 \left[ \frac{L_1 L_2}{(L_1 + L_2)^2} \right] = E_Q \left[ \frac{L_1}{(L_1 + L_2)} \right] \quad (7.4)$$

where $E_1 = \frac{1}{2} L_1 I_1^2$ and $E_2 = \frac{1}{2} L_2 I_2^2$ are respectively the energy initially stored and that delivered to the load. Consideration of eqn (7.4) shows that the maximum output energy for a given input energy is obtained when $L_1 = L_2$ and $E_2 = \frac{1}{4} E_1$,.
demonstrating that only 25% of the energy available in $W_1$ can be transferred to $W_2$ and that 50% of it has to be lost. There are, however, a number of important practical situations in which this result is not relevant. For example, the initial storage inductor $L_1$ may be part of an active device, such as a flux-compression generator, for which $(dL_1/dt) < 0$. This will cause a supplementary "pumping" of magnetic energy to the load, once any opening and closing switches have functioned, which can lead to an increased efficiency of the transfer process. Additionally, it is not always the highest efficiency that is of prime importance, and sometimes it is the maximum possible time compression of the output current pulse delivered to the load that is sought.

At first sight the energy loss in the circuit of Fig 7.2 resulting from the switching operation is puzzling, and it is informative to consider the analogy between magnetic energy transfer and the operation of a heat engine, and to recall the statement of the second law of thermodynamics that the total entropy of a thermally insulated system is increased in any process in which there is a transfer of energy between one or more of the subsystems of the overall system. A thermal system receives energy in the form of heat and delivers part of this to another cooler system. In order to do work, as energy is transferred from the source to the load, a further part of the initial energy must also be delivered to some medium. This is because as the entropy of the whole system ($W_1 + W_2 + Q$) increases, due to the interaction between the subsystems, the preservation of the entropy of some subsystems ($W_1$ and $W_2$) requires that of the other ($Q$) to be increased. Interpreting this concept for the magnetic energy transfer process of Fig 7.2 shows that, in order to deliver "high-quality" magnetic energy to the inductor $L_2$, it is necessary to lose "low-quality" heat energy in the resistor $R(t)$.

The very short rise time of the energy transfer to the load of Fig 7.2, required in a number of practical situations, is a complex function of the voltage produced across $R(t)$, the ratio $L_1/L_2$ and the magnetic energy accumulated in $L_1$. However, in situations where a fast transfer of energy is not important, replacement of the resistor $R(t)$ by a capacitor enables the theoretical efficiency of the transfer process to be raised from 25% to 100%. As the first part of this process, the inductive energy initially existing in $L_1$ is all transferred to the capacitor as electrostatic energy, and by a subsequent switching process it is then all passed on to $L_2$ as inductive energy. This introduction of a stage of complementary energy in the capacitor has a clear counterpart in mechanics where, in
order to transfer all the kinetic energy from one body to another, a stage of complementary energy produced by elastic forces must be introduced.

7.2 Opening switches (OSs)

When a switch is used to ‘open’ a circuit, it is necessary for its resistance to increase rapidly by at least two orders of magnitude between the initial conductive state and the final open state, and also for it to withstand the high voltage to which it is subjected. In high-current (MA) applications the open resistance of the switch may be much less than 1 Ω. Many possible forms of opening switches have been proposed, with those discussed below all having found extensive use in practical flux-compressor applications.

7.2.1 Exploding metallic fuses (EMFs)

Exploding metallic foils (often termed fuses) have been by far the most commonly used type of opening switch throughout the history of pulsed power research. Metallic conductors of copper, aluminium, etc, are used, in the form of wires at low energy levels (kJ) and as foils at high energy levels (MJ). Foils are normally embedded within an insulating medium, which can serve to both inhibit electrical breakdown and to minimise the damage caused by the explosive nature of the fusing process. The medium can be solid (quartz sand granules, alumina, polythene, Mylar, etc) liquid (water, etc) or gaseous (air, nitrogen, etc) or sometimes a vacuum. In addition to the use of many different foil geometries, a range of techniques, such as the use of an external cooling agent, a multiple-layer surrounding medium or a laterally applied pressure, have all been used to increase the resistance ratio that is obtained. The ratio itself is dependent on the rate of energy input to the fuse with time, with a typical figure of between 200 and 300 for fuses exploded in microseconds being sufficient in some cases to generate more than 300 kV across the switch as it opens [Reinovsky 1987].

Simple phenomenological models for exploding foils can be developed from capacitor-powered experiments and subsequently used to predict and optimise the results of similar experiments. However, when foils are used with flux-compression generators, the very much longer time profile of the current pulse that is then produced causes the foil behaviour to be significantly affected by hydrodynamic action in its gas phase. For
many associated conditioning applications a detailed numerical model, using an advanced atomic database, must be developed for accurate performance prediction.

7.2.2 Explosively formed fuses (EFFs)

To overcome the above problems, and to provide accurate control of the burst time, the explosively formed foil (EFF) opening switch shown in part in Fig 7.3(a) has been developed. The initial explosive action of the detonator E is constrained to force the relatively thick foil F, which the current by itself will not cause to explode, into a series of insulated grooves I. As a consequence thinner regions are formed in the foil, as shown in Fig 7.3(b), which themselves behave as exploding foils. However, although this arrangement achieves a performance much improved over that of a simple exploding foil, it is difficult to implement because of the complex technology that is involved. Either linear or circular geometry can be used for an EFF.

7.2.3 Plasma opening switches (POSs)

When very short duration output pulses are needed, with rise times of the order of nanoseconds, the plasma-opening switch provides the only solution so far known. The two very different switches described below have been developed for quite different applications.

(i) Plasma flow switch (PFS):

Fig 7.4 (a) shows the typical coaxial 'gun' electrode geometry of a plasma flow switch, with a cylindrical load connected at the muzzle-end. Current from the power source flowing through the fine metallic wire array causes this to explode, and the annular plasma sheath that is formed is accelerated longitudinally, as shown in Fig 7.4(b), by the magnetic force that arises from interaction between the current and the magnetic field that it produces. The opening action of the switch occurs as the plasma emerges axially through the muzzle of the coaxial gun, where the magnetic forces cause the low mass density components of the plasma to turn the corner at X. Fig 7.4(c) shows that these
components collapse radially inwards to the load, thus connecting it into the circuit. This type of switch has been widely used in the production of large quantities of soft X-rays.

![Diagram](image)

**Fig 7.4:** Plasma flow switch (PFS): (a) electrode geometry; (b) initial movement of plasma; (c) plasma collapsing onto liner.

(ii) Plasma erosion opening switch (PEOS):

Both the coaxial geometry of this type of switch and its method of operation can be visualised from Figs 7.5. Low-temperature carbon plasma is produced by the flashlightboard (a special-purpose plasma gun) and injected at a drift velocity $v_d$ into the space between the electrodes. The physical principles of the ensuing switch action are complex and, although it is now almost twenty-five years since the first working model was produced, no complete and universally accepted explanation has yet appeared. Nevertheless, the simplistic representation given below for a four-phase opening process, used as a basis for a zero-dimensional model [Ottenger et al, 1984] for the PEOS, provides an insight into the phenomena that underlies the switch operation for a
fast rising input current pulse, (about 100 ns) where plasma displacement due to magnetic forces can be neglected.

During the initial conduction phase of the PEOS, Fig 7.6(a), the plasma behaves as a good conductor, connecting the electrodes. As a result of the plasma ion motion, high electric fields are developed at the cathode surface, generating electrons with a bipolar space-charge-limited conduction through a neutral region (the gap) as in a conventional diode. When the input current $I_{in}$ exceeds a critical threshold value $I_{op}$ that is too great to be maintained by the drifting plasma ions the gap widens (ie it is ‘eroded’), and the switch starts to open, Fig 7.6(b). A high voltage is generated across the increased resistance of the switch and transfer begins of the input current from the plasma to the load. In Fig 7.6(c) $I_{in}$ has increased beyond a value $I_c$ at which the magnetic forces deflect the electrons from their radial paths, with consequent enhanced erosion, and rapid increase of the load current. In the final stage shown in Fig 7.6(d) the electrons change direction, and return to the cathode before they reach the plasma, thus establishing a very effective ‘magnetic insulation’.

For longer rising input pulses the plasma is displaced both axially and radially due to the magnetic forces, making details of the opening mechanism more speculative. An explanation of the opening conditions is gradually emerging from measurements (described in Chapter 9) of the plasma movement and density distribution.

Presently, the PEOS provides the fastest opening switch available. By generating a voltage exceeding 10 MV it has enabled currents up to about 5 MA to be transferred with a rise time of less than 10 ns.
(iii) Inductive switching (IS)

In the opening switches considered above, the high voltage necessary to transfer current from the switch to the load was developed across a resistive element. Alternatively, this voltage can be produced using a ballast inductor $L_b$ fed from a flux-compression generator. With $L_b$ replacing $R(t)$ in the circuit of Fig 7.2, the transfer voltage becomes $L_b(dI_1/dt)$, since the switch $S$ is open. In practice, the voltages that can be produced by this technique are lower than those in the case of, say, a fuse, leading to a correspondingly longer rise time of the transferred current.

An inductive switching technique is possible only when using a flux-compression generator or other power source that (unlike a capacitor bank) generates a high rate-of-change of current at the time when the current is near a peak. It is clearly difficult to define the transfer efficiency for this process, as the flux-compressor is active during the transfer.

7.3 Closing switches (CSs)

Although closing switches are much simpler in construction than opening switches, they pose the very difficult problem of synchronisation, with the load circuit having to be connected to the conditioning circuit at a predetermined switch voltage. The switches described below have been selected from the many that are available, as those that are most useful and relevant.

7.3.1 Detonator-activated closing switch (DACS)

Possibly the simplest and most frequently used method for closing a pulsed-power circuit is to use the detonator arrangement shown in Fig 7.7 to destroy the insulation between two conductors, by penetrating it with an explosively activated metallic jet.

A fast and precise detonator, with a timing jitter of only some tens of nanoseconds, fires a small explosive charge to create the

Fig 7.7: Detonator activated closing switch (DACS): (a) before activation; (b) after activation.
jet by means of the shape charge or Monroe effect.

Although highly effective, this type of switch can only be used in situations when it is possible to know in advance the precise timing of the associated opening switch. For very high current transfer, several DACS will be used in parallel.

7.3.2 Dielectric breakdown closing switch (DBCS)

Although a very simple closing switch with a preset breakdown voltage is obtained by the use of a dielectric situated between two electrodes, significant technical difficulties arise when it is used in high-current, low-inductance systems.

7.3.3 Surface discharge closing switch (SDCS)

This type of switch is made by correctly positioning two electrodes on an insulated surface, along which a multichannel discharge is self-triggered at a preselected electrode voltage. The switch is simple to make and, since the jitter is small if it is used repetitively, accurate auto-trigger control can be added. The inductance is low, and switches can be are used at voltages up to 200 kV, when the currents may reach several megampere.

A number of unresolved difficulties however remain when an SDCS is used in single-shot applications, arising mainly from the dependence of the switch behaviour on the polarity profile of the applied voltage.

7.4 Bibliography


3. Proceedings of the Megagauss Conferences held between 1965 and 2002
CHAPTER 8

EXPLODING CONDUCTORS AS OPEN AND CLOSING SWITCHES

Exploding foils (EFs) are possibly the most useful devices to produce the sharpening required in the initial opening circuit stages of a conditioning circuit when the specific energy input rates are high, although as demonstrated later, they are less effective at lower input rates. Explosively formed foils (EFFs) can be used to overcome this difficulty, by forcing a thick current-carrying conductor into insulator grooves to generate thin conductors which act in a similar manner to conventional foils. This enables the EFF to control the instant at which the circuit is opened. However a complex triggering circuit is required to initiate the explosion, while further difficulties can arise in flux compressor applications through the necessity to accommodate the EFF and its initiating circuits within the total explosive package.

Explosive foils are simple, operate automatically and give reproducible results, and the use of multiple stages of thinned opening fuses can minimize the inherent deterioration in performance at low rates of specific energy input. Automatically operating conditioning circuits using exploding foil opening (FOS) and closing (FCS) switches can be designed for a wide range of input current profiles.

Since in general the performance of a foil as an opening switch improves with a reduction of its thickness [Wilkinson et al 1985], 17 μm copper foils were chosen to satisfy both the practical use and the foil area requirements for the experiments envisaged.

8.1 Résumé of chapter contents

8.1.1 Chapter aims

(i) To obtain, from the results of capacitor-bank driven experiments, a simple empirically derived curve of dynamic resistance against energy input for the copper foil and subsequently to use this as a sub-routine in a numerical solution of the foil switching circuits.
(ii) To investigate the behaviour of foils driven by the large currents provided by the Flexy flux compressor.

(iii) To investigate and to demonstrate the use of exploding foil opening and closing switching in current transfer circuits.

(iv) To demonstrate the characteristics and practical implementation of the EFF and to provide data for its use in a numerical circuit solution.

(v) To investigate and to demonstrate the use of transformers and crowbarring techniques in fast current-switching circuits.

8.1.2 Preliminary investigations

The experimental studies are preceded by a brief historical survey of the progress of exploding wire and foil (fuse) research, and a list of important information sources. This is followed by a general description of fuse behaviour in relation to the dissipated energy and circuit specific action. Information, empirical data and details of the methods used to dimension the foil in capacitor bank and flux compressor driven circuits complete the information required for designing the exploratory experiments.

8.1.3 The experimental studies

The studies begin with a description of experiments aimed at demonstrating the opening techniques and providing information for the EF empirical model. Comparisons between the experimental and predicted results obtained from a computer program follow.

The chapter continues with a description and analysis of the results of FC driven EF experiments conducted on the firing range. A study of a capacitor bank driven EFF also conducted on the firing range then follows. The circuit layout and electrical and explosive components are described, and an empirical model extracted from the results is given.

An automatically operated EF closing switch used in capacitor bank driven current transfer circuits is described next. An empirical model, and details of its operating time extracted from the experiments for inclusion in the numerical solution of the transfer circuit are given. The equations and method used in the numerical solution of switch transfer circuits follow, together with comparisons between predicted and experimental
results. A detailed discussion of both simple and more efficient but difficult to arrange EF circuit layouts completes the studies of EF current transfer circuits.

The chapter concludes with a discussion and presentation of some faster switching techniques. These include a comparison of the predicted and experimental results of an EF conditioning circuit with an output transformer matched to a load, together with a theoretical study of the optimum performance of the EF, and a novel crowbarring technique for FC driven conditioning circuits.

8.2 Historical survey

True scientific interest in the electrical disintegration of fine wires and foils began in the 1950s, although the experiment itself is old. In 1774 [Mairme] reported the experimental use of an exploding wire positioned at different points in a series circuit to prove that the exploding phenomena and hence the current was the same. However, neither his nor subsequent papers, failed to generate much scientific interest in the phenomena until [Anderson 1920], often referred to as the father of scientific exploding wire studies, showed that temperatures approaching those of the sun could be produced in thin wires. Further progress was slow, due to a lack of equipment suitable for the study of processes as rapid as a wire explosion. The first scientific investigation of exploding wires for their own sake is attributed to (Kleen 1931). Other significant papers followed, but it was not until after World War II that new and fast microsecond techniques and the availability of fast capacitor banks enabled rapid progress to be made.

8.2.1 Information sources

Excellent accounts of the experimental and theoretical work undertaken in the 1950s and 60s are contained in the proceedings of four conferences devoted exclusively to exploding wire phenomena. These were all held in the USA and published in (Exploding Wires ed by W G Chace and H K Moore 1959-64) and in numerous articles in research journals. The proceedings of the biennial [IEEE Pulsed Power and Megagauss Conferences] are also valuable sources of exploding wire and foil applications.
8.3 Behaviour of electrically self-heated wires and foils

The behaviour of fine wires and thin foils (termed fuses) heated by their own current depends upon a complex mixture of their material (often copper or aluminium), geometry, (particularly thickness) [Wilkinson et al 1985], surrounding medium, current density, and the temporal hydrodynamic processes applicable to the particular conditions [Chace G W 1962]. The time taken to pass through each of the thermal phases between the solid and vapour conditions, depends on the rate of specific energy input in each phase. Low energy rates, with consequent long thermodynamic time scales (particularly in the boiling phase) allow hydrodynamic effects to play a significant part in the disintegration phenomena, causing the material to rupture in a haphazard manner as illustrated by the common household fuse. For large input current densities, say $10^5 \, \text{A/mm}^2$, with fast rise times such that the fuse reaches its boiling temperature within 10 $\mu$s or so, the vapour phase is completed in about $1 \mu$s. The hydrodynamic effects during this brief timescale are inhibited by the material inertia and the larger magnetic forces, and under these conditions the fuse explodes in a violent but orderly manner.

Fig 8.1 shows the radial expansion of the vaporised copper and incident blast-wave, produced by a 22 SWG 13 cm long copper wire that exploded in 4 $\mu$s. The event was viewed via a single-lens Schlieren system and 50 $\mu$s xenon flash and recorded on an Imicon image-converter camera operated at $10^6$ frames per second with an exposure time of 0.2 $\mu$s. The result, which is repeatable, clearly demonstrates the orderly nature of the explosive behaviour. The radial blast wave that can be seen moving ahead of the vapour on the original negative, accounts for some 30% of the energy dissipated in the wire, and was used to impact plastic cylinders mounted coaxially with the wire, in an investigation of their dynamic properties at high rates of strain. [Parry et al 1990]
8.3.1 Fuse resistance

The marked increase in a fuse resistance during the vapour phase can reach a few hundred times its cold value when the explosion occurs in microseconds, and has proved to be useful as an open-circuiting switch. The phenomena has been attributed to a low conductivity vaporisation expansion wave proceeding inward from the fuse surface [Bennet et al 1964], that effectively reduces the high conductivity cross-sectional area. This thereby increases the current density in the fuse and, as a consequence, raises the temperature ahead of the wave by many orders of magnitude.

8.3.2 Restrike

Restrike or reignition is the name given to a fuse phenomena resembling voltage-breakdown that can occur when the fuse becomes fully vaporised, or later as the low conductivity metal vapour is expanding. It has been shown to be a complex function of the vapour density and temperature, the enclosing medium, and the voltage across the fuse. Reignition may be due to ionisation occurring within the vapour [Chace et al 1959]. Chace asserted that the initial vapour density is too high for electrons moving in the electric field of the fuse to acquire sufficient energy between collisions to produce ionisation by impact, but as the vapour expansion continues and its density falls the mean free path of the electrons increases and ionisation by impact and attendant avalanching occurs. Ionisation of the air around the fuse may also induce restrike [Reithel et al 1962], with the ionisation attributed to the ultraviolet radiation and the shock-wave produced by the explosion process. If the voltage across the fuse is sufficiently high restrike around the fuse can occur.

8.3.3 Tamping or quenching

Restrike can be inhibited by enclosing the fuse in a medium that serves to restrain (tamp) the expanding vapour, thereby keeping its density and pressure high, or cooling (quenching) it. A range of gaseous, liquid, solid, and granular media have been tried [Guenther et al (Ed) 1987]. Small glass granules packed around the fuse is the medium used in a large number of applications, when electric fields up to 6 kV/cm can be maintained across the fuse before restrike occurs. The granules also provide an excellent attenuator of the attendant shock wave and thereby allow the fuse to be assembled in a non-destructive cassette form.
8.3.4 Action and energy

8.3.4.1 The circuit action

The specific action $g_{vm}$ accumulated in an exploding wire circuit, carrying a fast rising current up to the time $t_{vm}$, when the voltage across the wire reaches a maximum value $V_m$, can be regarded as substantially constant and determined only by the nature of the material [Tucker et al 1959, Andersson et al 1959]. Specific action is defined by

$$\int_{t_{vm}}^{t(t)} \frac{J^2(t) \, dt}{A^2} = g_{vm} \tag{8.1}$$

where $A =$ the cross section of the wire

and this relationship was found experimentally to hold for a wide range of wire cross-sections and lengths and for current densities between $0.15 \times 10^6 \text{ A mm}^{-2}$ and $10^6 \text{ A mm}^{-2}$, which are typical of those used in the present studies.

Action is a very convenient function, since it only involves a measurement of current. The use of energy as a fuse variable would require measurement of both current and voltage to determine its value, and unlike action, its value at $t_{vm}$ increases with both current density and wire diameter. The constancy of the specific action is due to the fact that the largest proportion is accumulated during the solid and liquid heating phases, and that the value is fairly insensitive to the complex changes take place during the brief vaporisation phase. The major fraction of the total energy is however deposited during this final phase.

The basic assertions outlined above have been verified in many experiments and have been shown to apply also to thin foils typically $20 \mu \text{m}$ thick [Dimario et al 1970].

The specific action assumes its most convenient value for design purposes when evaluated at the time $t_b$ (referred to as the time of burst), at which the foil has reached its boiling temperature at a dissipated energy close to the handbook value. The foil is just entering the vapour phase and is starting to increase rapidly in resistance, and

$$\int_0^{t_b} \frac{J^2(t) \, dt}{A^2} = g_b \tag{8.2}$$
The specific action at the time of burst for copper and aluminium foils are given in Table 8.1 [Tucker et al 1975]. The ranges of expected maximum resistance ratios (M_r) for fuses in typical circuit arrangements, are also included in the table.

It is useful to introduce an "equivalent action" time $\tau_{ea}$ to account for the variety of current source profiles that can deliver action to the fuse, eg a capacitor bank or a flux compressor, as will be seen later.

$$\tau_{ea} = \frac{1}{I^2} \int I^2(t) dt$$

(8.3)

where $\tau_{ea}$ can be regarded as the time over which the action would accumulate if the current was constant at $I(t)$. Eqn 8.3 can be evaluated analytically for a sinusoidal current waveform of quarter period $\tau_q$, giving $\tau_{ea} = \tau_q/2$.

8.3.4.2 Dissipated energy

The specific energy $\varepsilon_b$ needed to bring a fuse to the burst condition determined experimentally, approximates to the calculated one using tabulated values of the specific and latent heats for slow adiabatic heating at atmospheric pressure for the fuse material [Keilhacker 1959, Removsky et al 1982]. The specific energy for copper and aluminium at burst are $\varepsilon_b \approx 1.3 \times 10^6$ J kg$^{-1}$ and $\varepsilon_b \approx 3.2 \times 10^6$ J kg$^{-1}$. Beyond burst the energy required to vaporise foil fuses increases with both thickness and current density [Dimareo et al 1970].

For fuses approximately 25 μm thick and current densities within the range of (1.5 to 4) x $10^5$ A mm$^{-2}$, a mean energy ($\varepsilon_v$ in Table 8.1) for vaporisation of $5.8 \times 10^6$ J kg$^{-1}$ and $9.8 \times 10^6$ J kg$^{-1}$ for copper and aluminium is regarded as acceptable for approximate initial fuse circuit design [Tucker et al 1975].

<table>
<thead>
<tr>
<th>Table 8.1: Foil material and fusing constants</th>
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<tbody>
<tr>
<td>Density ($\rho$) kg m$^{-3}$</td>
</tr>
<tr>
<td>--------------------------------</td>
</tr>
<tr>
<td>8.9 x $10^3$</td>
</tr>
<tr>
<td>Initial Resistivity $\mu_\Omega$ m</td>
</tr>
<tr>
<td>Action constant at burst ($g_b$) A$^2$ sm$^{-4}$</td>
</tr>
<tr>
<td>Specific energy to vaporise the foil ($\varepsilon_v$) J/kg$^{-1}$</td>
</tr>
</tbody>
</table>
8.4 Estimating the fuse dimensions

The aim of the initial experiments with exploding foils was to provide data for compiling and testing an empirical model of an exploding foil over a range of circuit conditions. Optimisation of the foil and circuit parameters was a secondary consideration.

An important consideration, when a foil is used as an opening switch in an inductive circuit, is the maximum voltage $V_{\text{max}}$ that occurs at time $t_{\text{vm}}$ during the rapid increase of its resistance $R_f$ and the consequent reduction of its current during the vapour phase. When transferring current rapidly into a low inductance load $L_1$ connected across the foil at time $t_{\text{vm}}$, the initial transferred current derivative $\dot{i}_i = V_{\text{max}}/L_f$ needs to be as large as possible.

8.4.1 For capacitor bank driven experiments

The cross-section of a fuse in a discharge circuit was determined from Eqn (8.2), with $\int_0^{t_{\text{b}}} I^2 dt$ equated to the action accumulated at the selected time of burst on the waveform profile. The fuse length $l_b$ was obtained by equating the energy to vaporise it to a substantial (see later) fraction $\alpha$ of the energy $E_b$ remaining in the circuit at burst. Then

$$A = \sqrt{\int_0^{t_{\text{b}}} I^2 dt}$$  \hspace{1cm} (8.4)

or from Eqn (8.3)

$$= I_b \left( \frac{\tau_{\text{ea}}}{g_b} \right)^{\frac{3}{2}}$$  \hspace{1cm} (8.5)

where $\tau_{\text{ea}} = \tau_q/2$ for a discharge current increasing sinusoidally and

$$l_o = \frac{\alpha E_b}{A \rho \varepsilon_r}$$  \hspace{1cm} (8.6)

where $\rho$ is the density of the fuse material.
Burst for the initial exploding foil experiments was arranged to occur in a range of times about the first current maximum \(I_m\) where from eqn (8.3) the action accumulated by a sinusoidal waveform of quarter period \(\tau_q\) is \(I_m^2 \tau_q / 2\), and from eqn 8.4 the foil cross section will be

\[
A = I_m \left( \frac{\tau_q}{2g_b} \right)^{\frac{1}{2}}
\]  

(8.7)

In most cases the energy lost up to the time of burst makes little difference to the current or waveform profile and is normally neglected in first order calculations.

### 8.4.2 For flux compressor driven experiments

Equations (8.4) to (8.7) were used to dimension the Flexy driven fuses. Values for the action and energy accumulated at the time selected for burst were extracted from the experimental results of a Flexy firing. It seemed reasonable to suppose that the best time to select for burst to occur with a consequent maximum voltage \(V_m\) across the fuse was close to the end of the Flexy run where the current and energy are high. Consideration of the equation derived below for \(V_m\) [Reinovsky et al 1989] however shows that this is incorrect.

A simple model for the fuse, that assumed that the resistance increase beyond the burst condition at \(t_b\) can be approximated as a linear rise to time \(t_{vm}\) when the voltage across the fuse reaches its maximum value \(V_m\), at a fuse resistance \(R_{vm}\) and an overall resistance ratio of \((M_1)_{vm}\) was compiled. It was deduced from the model that \(V_m\) will be produced across the fuse when the current and dissipated energy are 0.61 and 0.63 of their value at \(t_b\) respectively. It follows therefore that \(\alpha\) (mentioned earlier) should be at least 0.63, and that \(V_m = 0.61 I_b R_{vm} = 0.61 I_b R_o (M_1)_{vm}\) where \(R_o = \mu_0 I_0 / A\), and by substituting eqns (8.5) and (8.6) for \(A\) and \(l_0\) gives

\[
V_m = \frac{0.3k_o \alpha (M_f)_{vm} \Phi_b}{\tau_{eq}}
\]

(8.8)

where \(k_o = \mu_0 g_b / \varepsilon, \rho = 0.0423\) consists of material constants listed in Table 8.1, and \(\Phi_b\) is the flux linkage \((I_b l_b)\) at burst.
Clearly $V_m$ can be increased by increasing $(M)_{vm}$ and $\Phi_0$ or by decreasing $\tau_{ca}$. Fig 8.2 shows $\tau_{ca}$ as a function of time for the Flexy flux compressor, with $\tau_{ca}$ rising as the generator is primed and then decreasing to a minimum value. As the derivatives of $L$ and $I$ decrease near the end of the generator run $\tau_{ca}$ begins to increase rapidly again. A burst time during the generator run when $\tau_{ca}$ has a minimum value should therefore be selected to maximise $V_m$.

8.5 Empirical modelling of exploding foil

8.5.1 Experimental assembly

Experiments to obtain data for the foil model were performed using the circuit of Fig 8.3, with the energy source provided by the laboratory capacitor bank. The start switch $S$ connects the charged bank to the foil of inductance $L_f$ and resistance $R_f$ (which varies considerably during an experiment) through a parallel-plate transmission line arranged in the simple physical layout described in Section 8.9.2. The inductance $L_t$ and resistance $R_t$ include contributions from both the capacitor bank and the transmission line, as well as the ballast inductance included to restrict the short-circuit current to about 800 kA. A flux probe $T_1$ located in a tunnel in the copper conductors of the transmission lines was used to measure both the current and its rate of change with time.

Following closure of switch $S$ at $t = 0$ the foil voltage $V_f$ is given in terms of the current $I_f$ by

$$V_f = R_f I_f + L_f \frac{dI_f}{dt} \quad (8.9)$$
and was measured using a water resistance voltage probe, parallel-connected to the foil. The capacitor bank and diagnostic tools used are described in Chapter 4. Data for calibration were obtained from recordings of the current discharge from the capacitor, with the thin foil replaced by a much thicker foil of the same length and width. The foil inductance $L_f$ was then obtained from Eqn 8.9 by assuming that $R_f$ now remains constant. To ensure good electrical contact and precise geometric definition, the foils were firmly secured between heavy copper mounts, contained within a plastic cassette and surrounded under pressure by a 100 μm glass bead medium at the centre of a glass fibre/glass bead assembly. Fig 8.4(a) shows a foil resting on the lower section of the glass bead packing before an experiment and Fig 8.4(b) the characteristic two-layer split produced in the foil after it has vaporised and reformed with, for clarity, the upper layer having been slightly displaced. In both figures the upper section of the glass bead packing is absent.

Table 8.2: Parameters for exploding-foil experiments. $V_0$ = initial capacitor voltage $R_f(0)$ = initial fuse resistance.

<table>
<thead>
<tr>
<th>Copper foil</th>
<th>$V_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Foil</td>
<td>$L_t$</td>
</tr>
<tr>
<td>I 17.0 28 12</td>
<td>86 3.58</td>
</tr>
<tr>
<td>II 17.0 18 11</td>
<td>353 3.20</td>
</tr>
<tr>
<td>III 17.0 9 11</td>
<td>353 3.20</td>
</tr>
<tr>
<td>IV 25.4 18 12</td>
<td>86 3.50</td>
</tr>
</tbody>
</table>
8.5.2 Experimental results

The broken curves in Fig 8.5(a) are experimental results for the variation with time of the current discharged from the capacitor bank into three 17 μm thick copper fuses having different widths and lengths. Table 8.2 summarises the main parameters of the test circuits and also those used for a similar experiment using a 25.4 μm thick foil.
The current profiles for the three foils are quite different, as are the results in Figs 8.5(b) and 8.5(c) for the variations of the rate-of-change with time of the foil current and the foil voltage respectively. The corresponding variations in the specific action are shown in Fig 8.5(d), with the values at the burst times being in good agreement with those in Table 8.1 (eg 1.27 x 10\(^{17}\) A\(^2\) s m\(^{-4}\)). In curves derived for the increase in dynamic resistance ratio with time, shown in Fig 8.5(e) for the foil I experiment, the liquid and gaseous phases of the foil behaviour can be clearly distinguished. The increase in dynamic resistance ratio with specific deposited energy characteristics for the three 17 \(\mu\)m copper foils coalesce into the single curve shown in Fig 8.6, a feature observed elsewhere for aluminium foils [Roderick et al 1983, Reinovsky 1987].

### 8.5.3 Foil model

The many parameters determining the behaviour of an exploding foil were described in section 8.2. Fig 8.6 shows additionally that, although the characteristics for the 17 \(\mu\)m and 25.4 \(\mu\)m foils have the same general shape, that for the 17 \(\mu\)m foil reaches the same resistance ratio at a lower deposited energy. A similar phenomenon has been observed elsewhere for aluminium foils [Bueck et al 1985].

The results given in Fig 8.6 for the two foil thickness were used in a computer program (described later in Section 8.9) to predict the experimental behaviour, for the range of foil widths and lengths needed for the fast opening switches required by the present application. For purposes of comparison, results provided by the program for the 17 \(\mu\)m foil are added as
full curves to Figs 8.5(a)-(e). In addition Fig 8.7 presents computed and measured results for the current and voltage time profiles of the 25.4 μm foil of Table 8.2. Figs 8.5 and 8.7 together confirm the ability of the program to predict accurately the measured characteristics of the foils.

8.6 Compressor/opening switch experiments

Having established the accuracy of the computer program, it was used to simulate experimental results provided by a 17 μm exploding foil, in which the FLEXY flux compressor described in Chapter 6 supplied the power. Various manufacturing improvements in this have resulted in the experimental performance being consistent and close to that predicted by theory, and it is now regarded as providing a reproducible energy enhancement of the firing range capacitor bank by which it is primed.

Fig 8.8 shows diagrammatically the equivalent electrical arrangement used for those tests, in which the compressor is primed by the capacitor C when the capacitor bank start switch S1 is closed. The compressor is subsequently self-crowbarred by the switch S2 (at t = 0), and feeds energy to a single-turn load coil of inductance L1 through a parallel plate transmission line of inductance L4. The circuit also includes a laterally pressurised EF package, in which parallel connected copper foils are separated by three layers of polythene faced sheets of Mylar insulation to a total thickness of 760 μm. Currents and their rate-of-change with time were again measured by means of flux probes. Table 8.3 summarises the main circuit and foil data for the two experiments. The foil dimensions and burst times were estimated by the methods described in Section 8.4.2. Fig 8.9 shows the experimental arrangements at the firing site.
Table 8.3 Parameters for compressor/exploding foil experiments

<table>
<thead>
<tr>
<th>Test</th>
<th>Initial Current $I_0$ (kA)</th>
<th>Load Inductance $L_1$ (nH)</th>
<th>Line Inductance $L_4$ (nH)</th>
<th>Foils</th>
<th>No in Parallel</th>
<th>Thickness ($\mu$m)</th>
<th>Width (cm)</th>
<th>Length (cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>F3</td>
<td>50.46</td>
<td>40</td>
<td>6</td>
<td>4</td>
<td>17</td>
<td>60</td>
<td>25</td>
<td></td>
</tr>
<tr>
<td>F4</td>
<td>50.03</td>
<td>42</td>
<td>11</td>
<td>2</td>
<td>17</td>
<td>52</td>
<td>42</td>
<td></td>
</tr>
</tbody>
</table>

Results from the two experiments F3 and F4 are presented in Figs 8.10(a) and (b), together with the current waveform F1 from an earlier lower performance compressor experiment without a foil. During approximately the first 100 $\mu$s the load parameters (including the foil resistance) do not play a major role in the functioning of the compressor, and a valid comparison can therefore be made with results provided by the different experiments. The improved performance of the two later compressors and the excellent reproducibility achieved before the increasing foil resistance becomes significant, can clearly be seen.

To fit the experimental data to the theoretical predictions, it was necessary to modify the mathematical function that describes the variation of the compressor resistance with time (see Chapter 6 Section 6.3) This changed function, together with the corresponding calculated variation of both...
the inductance and its time rate-of-change fully describe the compressor, and their variations are presented in Fig 8.11 throughout the 160 μs operating time. Data analysis of the two EF experiments was performed using this numerical data, together with the 17 μm foil model of Fig 8.6. Fig 8.12 compares experimentally obtained values with values calculated for the foil voltages. Although good agreement is clearly obtained for the burst times, the foils restrike early in the vapour phase and at a much lower voltage than is expected. The maximum restrike electric field occurring in both experiments was about 1.2 kV cm⁻¹, much below the 3 to 4 kV cm⁻¹ that can be sustained by foils in capacitor powered experiments when surrounded by a glass bead/glass fibre medium. Further capacitor powered experiments with a scaled down version of the same foil package design used in the flux compressor experiments, and containing the same polythene faced Mylar insulation, confirmed that electrical fields exceeding 3 kV cm⁻¹ could again be sustained. Numerical simulations using the theoretical model presented above, with assumed electrical breakdowns at different regions of the circuit, produced results widely different from the experimental results, confirming that the much reduced voltage had undoubtedly occurred across the foil.

A major difference between capacitor powered experiments and those using flux compressors is that the rate at which specific energy is deposited in the foil between the melt and vapour phases is between 2 and 5 MJ kg⁻¹ μs⁻¹ for the capacitor drive and only
0.4 and 0.8 MJ kg\(^{-1}\) \(\mu s\)^{-1} for the flux compressors. The lower restrike fields observed are regarded therefore as due to the resulting differences in the thermal and hydrodynamic behaviour of the foils in the vapour phases. Similar low restrike fields (1.16 kV cm\(^{-1}\)) were also noted in results from the earlier Laguna foil experiment [Reinovsky et al 1990], using the Mark IX flux compressor [Fowler et al 1989]. As shown in Fig 8.13, predictions of these results using the code described in Chapter 6 were in excellent agreement with measured results until the foil vaporised. After this, as in the present experiments, the calculated maximum voltage was much higher than the experimental figure, and it was suggested [Lindemuth et al 1985] that, for accurate modelling of the post-burst phase, a complex code based on atomic data would be needed.

8.7 Explosively formed fuses (EFFs)

Explosively formed fuses (EFFs) (described in Chapter 7), can be used to overcome the deterioration of performance mentioned in Section 8.6.1, and to provide accurate control of the burst time by forcing a thick current-carrying conductor into a die of insulator grooves to generate thin conductors, which then act in a similar manner to conventional foils. The EFF switch can be triggered to open in a few microseconds, after passing multimegamp currents accumulated in hundreds of microseconds, with dynamic resistance ratios of a few hundred times, while holding-off voltages greater than 8 kV/groove and dissipating energies up to about 0.78 kJ/cm along the groove length. Input to output pulse compression of several hundred times are possible when using it together with a suitable closing switch to transfer current into low inductance loads. Designs have been produced in both plane and cylindrical geometry [Goforth et al 1990].

These advantages however are outweighed somewhat by the requirement of a complex explosive geometry/trIGGERing assembly needed to provide a detonation front of close
simultaneity over the fuse surface area, while further difficulties can arise in flux compressor applications through the necessity to accommodate and operate the assembly within the total explosive package.

8.7.1 Study of an EFF in plane geometry

The aims of this study are to demonstrate and gain a direct insight into the practical implementation of the technology. With these aims in mind an EFF was constructed and appraised. Fig 8.14 shows the experimental layout with a "mouse trap" explosive plane-wave shaper (see also Chapter 3 Fig 3.8). Fig 8.15 is a sectioned end diagram of the experimental arrangement, Fig 8.16 are views of the EFF experimental assembly, and Fig 8.17 is the equivalent circuit diagram. The crowbar was included to give a unidirectional current in the load circuit. Fig 8.18 shows the EFF resistance and voltage waveforms obtained from the results. Table 8.4 lists the circuit constants and component values calculated from discharge tests made both with and without crowbarring the load circuit.

Fig 8.14: Schematic diagram of the EFF experimental layout in plane geometry with mousetrap plane wave shaper. (a) sectioned side elevation. (b) plan view.
Fig 8.15: Schematic sectioned end view of the experimental arrangement of (a) the mousetrap plane-wave shaper, (b) the EFF components. (c) is a diagram of the PTFE extrusion die; (i) plane view, (ii) front elevation (iii) end elevation.

Fig 8.16: Views of EFF components; (a) end view, (b) side view with explosive disk stack displaced.
Table 8.4 Circuit constants and calculated values

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bank voltage</td>
<td>20 kV</td>
</tr>
<tr>
<td>Bank capacity</td>
<td>250 μF</td>
</tr>
<tr>
<td>Total circuit inductance</td>
<td>1090 nH</td>
</tr>
<tr>
<td>Maximum current</td>
<td>0.256 MA</td>
</tr>
<tr>
<td>1/4 Period of current waveform</td>
<td>26 μs</td>
</tr>
<tr>
<td>Total circuit resistance</td>
<td>15 mΩ</td>
</tr>
<tr>
<td>Inductance of load circuit</td>
<td>840 nH</td>
</tr>
<tr>
<td>Resistance of load circuit</td>
<td>7.8 mΩ</td>
</tr>
<tr>
<td>Current at start of switching process</td>
<td>0.239 MA</td>
</tr>
<tr>
<td>EFF aluminium fuse</td>
<td>38.5 mm (l), 40 mm (w), 0.8 mm (th)</td>
</tr>
<tr>
<td>EFF Resistance at start of switching process (t = 0 in Fig 8.18)</td>
<td>&lt;1 mΩ</td>
</tr>
</tbody>
</table>

![Fig 8.17: Equivalent circuit diagram for the EFF experiment](image)

8.7.1.1 Circuit characteristics

When $S_s$ in Fig 8.17 is closed a discharge current $I_d$ with the characteristics listed in Table 8.4 flows through the thick foil fuse in the load circuit. Closing the crowbar switch $S_c$ at the first quarter period of the waveform (26 μs) results in a unidirectional current $I_d \max e^{-R/L}t$ with a time constant $L/R = 107 \mu s$ flowing

![Fig 8.18: Experimental results: (a) resistance, and (b) voltage across the EFF](image)

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through the fuse. The cross-sectional area of the aluminium crowbar foil needed for switching to occur at $I_d \text{ max}$ was determined from Eqn 8.7.

The delay between initiation of the explosive and closing of $S_s$ was set for the formation of a thin extruded foil (EFF) within the die, with its resistance beginning to rise significantly (a condition akin to burst in a simple foil) ($t_0$ in Fig 8.18) to occur shortly after $S_c$ is closed. This timing is not critical because of the long time constant of the load current.

### 8.7.1.2 Characteristics of the explosive assembly

The mousetrap plane wave generator seen in the Figs 8.15 and 8.16 is a copy of a design developed at RARDE Fort Halstead. The design ensured that the glass fragments (see Fig 8.15) that initiate the explosive stack arrive at the same time over its surface area. Given this, estimates of the velocity (2.6 mm μs) and time of arrival of the fragments at the stack (35 μs) were made using the explosive detonation velocity (7.95 mm μs$^{-1}$), the angle ($\alpha = 19^\circ$) of incline of the glass plate overlaid with SX2 sheet explosive, and the stand-off distance of the stack from the mousetrap.

Addition of 3 μs for the detonator time plus 25 μs and 3 μs for the explosive propagation along the line wave front forming tracks and through the stack, and allowing 5 μs to extrude the fuse in the die and obtain burst conditions, resulted in a total estimated time between firing the detonator and burst of 68 μs.

To satisfy the timing conditions described in section 8.7.1.1 above, a delay of 38 μs was introduced between firing of the detonator and closing of the capacitor start switch $S_s$.

### 8.7.1.3 Experimental results

Fig 8.18 (a) shows the resistance and Fig 8.18 (b) the voltage across the EFF extracted from the results. The electric field across the switch, and energy dissipated per cm length of each extrusion groove when the resistance and voltage had reached their peak values and had begun to fall, were calculated for design purposes as $8.7 \text{ kV cm}^{-1}$ and $0.75 \text{ kJ cm}^{-1}$.
8.8 Closing switches

Synchronisation provides one of the most difficult problems for any conditioning scheme that involves closing switches. For capacitor-powered experiments, the optimum time for closure can be established from preliminary tests, but this is clearly not practicable in single-shot flux compressor experiments. The use of dielectric switches with a pre-set breakdown voltage [Vedel et al. 1971] provides one possible solution, Explosively-operated switches [Goforth et al. 1991] are another possibility, but synchronisation problems can arise when transferring very fast rise-time short duration pulses. Surface tracking switches offer a further possible solution, although they present a number of unresolved difficulties that arise from their dependence on the polarity of the switch voltage profile [Reinovsky et al. 1987]. Possibly the most practical solution is to use an EF closing switch as described below, which does not require synchronising, since it operates automatically once its correct dimensions have been established.

Fig 8.19 shows a circuit in which exploding foils are used as both opening and closing switches, with the latter comprising the assembly of parallel connected, small dimension aluminium bridges seen in Fig 8.20(a) connected in series with the main copper foil. The geometry of the small bridges enhances their explosive action, by an effect probably similar to that used in detonics for shaped-charge devices. The dimensions of the bridges are such that they burst close to current maximum, and subsequently penetrate the insulation between them, with the following conditioning stage being introduced just before the main foil completely vaporises.

The bridges were made by removing sections from a 100 μm aluminium foil, leaving a number of strips 9 mm long and 5 mm wide to carry the current. The enhancing action of the bridges is brought about by bending them into a 2 mm wide by 2 mm deep slot milled in a 15 mm wide insulator, as seen in Fig 8.20(b). Brass electrodes, positioned on each side of the insulator, complete the connections between the foil and the main strip-line. Mylar sheets initially insulate the fuse assembly from an anvil connected in the strip-line.
of the circuit into which contact is subsequently made. The explosive debris is released into a groove in the anvil, while a 100 µm aluminium foil laid across it helps to maintain a low switch resistance. Fig 8.20(c) shows the result of the foil explosion on the switch insulation, which was made from six sheets of Mylar having a total thickness of 762 µm. The models extracted from the results for the switch delay and dynamic resistance ratio of the bridges shown in Figs 8.21 and 8.22 were used later in the numerical analysis of a complete conditioning circuit. The close correspondence of the lower values of dynamic resistance for the aluminium bridges to results obtained by [Roderick et al 1983, Bueck et al 1985] [R] and [B] in Fig 8.22
is also demonstrated. The difference at higher values is attributed to the absence of any tamping material to restrain bridge movement.

8.9 Computer program for current transfer switching action

A computer program was written in FORTRAN to simulate a single stage of foil opening followed by a closing switch to transfer current into a load. The resistance of the closing switch (FCS of Fig 8.19), which introduces the load $R_2$, and $L_2$ into the circuit is insignificant in comparison with the other resistive factors in the circuit. After the action of the closing switch, the system can be described by equations (8.10) to (8.15) below.

\[
\frac{dI_1}{dt} = \frac{dI_f}{dt} + \frac{dI_2}{dt} \quad (8.10)
\]

\[
\frac{dI_f}{dt} = \frac{L_2(V_0 - Q/C) - I_f R_f (L_1 + L_2) - I_1 R_1 L_2 + I_2 R_2 L_1}{L_1 L_2 + L_f (L_1 + L_2)} \quad (8.11)
\]

\[
\frac{dI_2}{dt} = \frac{R_f I_f}{L_2} - \frac{R_2 I_2}{L_2} + \frac{L_f}{L_2} \frac{dI_f}{dt} \quad (8.12)
\]

\[
\frac{dQ}{dt} = I_1 \quad (8.13)
\]

\[
\frac{dW}{dt} = \frac{I_f^2 R_f}{M_f} \quad (8.14)
\]

\[
\frac{dg}{dt} = \frac{I_f^2}{A_f^2} \quad (8.15)
\]

where $Q$ is the charge given out by the capacitor, $M_f$ and $A_f$ are the mass and cross sectional area of the fuse, $g_f$ is the specific action and $W$ the specific energy deposited in the fuse.

The above set of six first-order differential equations was solved using a FORTRAN subroutine [Kahaner et al 1989]. At each time step of the solution, the specific energy deposited in the foil was calculated, with the data of Fig 8.6 being used to provide the corresponding resistance. The program takes some tens of seconds to run on a PC386 computer.
8.9.1 Experimental and computed results

Fig 8.23 presents results from three identical experiments using the circuit shown in Fig 8.19, together with corresponding theoretical predictions. The main parameters for the experiment are given in Table 5.

Fig 8.23(a) shows that good operating time reproducibility was observed in the experiments with, in two cases, the switch FCS operating about 11.25 \( \mu s \) after the main switch (S of Fig 8.19) and in the third after about 11 \( \mu s \). This small time difference enabled consideration to be given to the accuracy with which the predictions made using the numerical code of the previous section described the actual switch behaviour. Thus the small jump in the rate-of-change of current with time (arrowed in Fig 8.23(b)), correctly predicted when the switch operated after 11.25 \( \mu s \), was absent from both theoretical and measured results when the switching occurred after 11 \( \mu s \). In addition, Fig 8.23(c) shows that the maximum-recorded foil voltages are closely predicted for both operational times.

The use of an EF closing switch together with an EF opening switch suggest the novel and faster techniques explained in section 8.10.

Fig 8.23: Results from exploding foil opening and closing switch experiments: (a) foil current \( (I_f) \) and transferred currents \( (I_1) \); (b) rate-of-change of foil currents \( (dI_f/dt) \); (c) fuse voltage \( V_f \) (e— — , maximum experimental value for 11\( \mu s \) experiment; — — — , maximum theoretical value for 11\( \mu s \) experiment). Maximum voltage shown for 11\( \mu s \) switching; for one fuse voltage signal only the points shown as full squares are available; — — — , experimental; — — — , theoretical.
Table 8.5 Parameters for exploding copper foil switch experiment

<table>
<thead>
<tr>
<th>Thickness (μm)</th>
<th>Width (cm)</th>
<th>Length (cm)</th>
<th>$I_L$ (nH)</th>
<th>$R_L$ (mΩ)</th>
<th>$R_{f}(0)$ (mΩ)</th>
<th>$L_2$ (nH)</th>
<th>$R_2$ (mΩ)</th>
<th>$V_o$ (kV)</th>
</tr>
</thead>
<tbody>
<tr>
<td>17</td>
<td>12</td>
<td>10</td>
<td>353</td>
<td>3.2</td>
<td>35</td>
<td>0.84</td>
<td>60</td>
<td>1</td>
</tr>
</tbody>
</table>

It has been observed that when the current is interrupted before it would otherwise reach its first maximum, both computed and measured values of the maximum foil voltage exceed those predicted using an earlier and simple theory [Reinovskiy et al 1989], whereas if the current is interrupted after the maximum, there is good agreement. The cause of this is attributed to the influence on the circuit behaviour of the non-linear interaction between the foil and the capacitor discharge (fully accounted for in the program) being significant when the interruption is early in the discharge cycle.

8.9.2 Simple and advanced foil conditioning circuit layouts

Fig 8.24(a) represents a single stage of simple fuse (R) or EFF conditioning, with $L_1$ and $L_3$ being the energy storage and load inductances and $L_2$ the inductance of the fuse. Beyond burst the fuse resistance is assumed to rise in a finite step to $R^*$ at time $t_{vm}$ (when the voltage across it is a maximum) and to remain constant. The value of $I_1$ at $t_{vm}$ when the switch $S$ is closed and current $I_f$ is transferred to $L_3$ is assumed to be $I^*$. Fig 8.24 (a) depicts the exploding foil opening switch and its physical layout in circuits discussed thus far. By a physical re-arrangement that requires some extra design, fabrication and implementation considerations, the circuit components can be arranged in the more advanced layout shown in Fig 8.24(b). Here, the symbols represent the same quantities as in Fig 8.24(a) but the switch inductance $L_2$ is now included as part of the inductance store. An indication of the advantages gained by adopting the layout in Fig 8.24(b) can be obtained by solving the circuit equations for the assumed fuse conditions.
It can be shown by application of the energy and linkage flux conservation relationships, and analytical methods for solving circuits with constant components, that the energy dissipation $\Delta E$ in the opening switch during a current transfer operation, and the transferred current $I_3$ for the circuits in Fig 8.24 are for Fig 8.24(a),

$$\Delta E = \frac{1}{2} (I^*)^2 \frac{L_1 L_2 + L_1 L_3 + L_2 L_3}{L_1 + L_3}$$

(8.16)

$$I_3 = \frac{L_1}{L_1 + L_3} I^*(1 - e^{-\alpha(t-t_m)})$$

(8.17)

where $\alpha = \frac{R^* (L_1 + L_3)}{(L_1 L_2 + L_1 L_3 + L_2 L_3)}$ is the reciprocal of the time constant.

However, for the circuit of Fig 8.24(b)

$$\Delta E' = \frac{1}{2} (I^*)^2 \frac{L_1 L_3}{L_1 + L_3}$$

(8.18)

and

$$I_3' = \frac{L_1 + L_2}{L_1 + L_2 + L_3} I^*(1 - e^{-\alpha'(t-t_m)})$$

(8.19)

where $\alpha' = \frac{R^* (L_1 + L_2 + L_3)}{(L_1 L_2 + L_1 L_3 + L_2 L_3)}$.

For a large storage inductor, the energy dissipation $\Delta E$, and time constant $1/\alpha$, reduce to

$$\Delta E = \frac{1}{2} (I^*)^2 (L_2 + L_3), \alpha = \frac{R^*}{(L_2 + L_3)}$$

(8.20)

$$\Delta E' = \frac{1}{2} (I^*)^2 L_3 and \alpha' = \frac{R^*}{L_3}$$

The advantages are readily apparent: Both the energy dissipation in the switch and the load current rise-time are reduced by a factor of $L_2/(L_2 + L_3)$. For large scale devices $L_2$ is typically $\geq 35 \text{ nH}$ and loads of interest are 20 to 150 nH which result in savings of 20 to 64% in both energy and rise-time.

The advanced design although more efficient is difficult and expensive to implement, and is only chosen when optimum performance is vital. Figs 8.25 show expanded...
diagrams of the physical layout of simple and advanced current transfer circuits in plane geometry. In Fig 8.25 (b), S can be replaced by FCS bridges at the end of the FOS indicated in Fig 8.25 (b), that automatically make contact to L₃ by severing the above insulation as discussed earlier, thereby forming a very convenient and efficient current transfer arrangement.

![Diagram of current transfer circuits](image)

Figure 8.25: Expanded isometric layout of exploding foil current transfer circuits in plane geometry; (a) simple design, (b) more advanced design; current paths —— 250μm thick copper foil, insulation —— a sufficient number of 125μm mylar sheets.

8.9.3 Cylindrical EFF conditioning circuit layout

Cylindrical geometry offers a more compact package than the planar geometry, and is preferred for large area EFFs [Goforth et al 1989]. Figs 8.26 show cylindrical designs in both simple and advanced circuit layout. In both versions, close spaced exploding foil sites initiate sensitive explosive pellets. These detonate the cylindrical explosive shown along its axis with a high degree of simultaneity. In the simple layout of Fig 8.26(a), coaxial cables connect the EFF to the current source, and the smooth detonation front generated by the cylindrical explosive contacts directly with the EFF to drive it into the die. A triaxial circuit arrangement is necessary in this design to enable the load to be connected directly across the EFF when transferring current. The outer conductor of the
input cable is connected to the end flange, with the inner conductor and insulator passing through it to complete the EFF circuit. The flux residing in the EFF circuit after the closing switch is operated is lost in this arrangement.

Fig 8.26: Schematic cut away diagram of cylindrical EFF current transfer circuit layouts in simple and more advanced format; (a) simple layout itemizing components, inductances and current paths; (b) advanced layout itemizing additional components and differing current paths.

Higher current transfer efficiency is obtained by arranging the EFF in advanced open circuit format; also, this circuit layout can be directly connected to the output of a helical flux compressor. A complex explosive assembly is however required (as with a plane EFF) arranged in this format. An additional machined explosive shown in Fig 8.26(b) sandwiched the EFF foil surface and annular main current line insulator, and initiated by
the shock wave travelling through the insulator, is needed to provide the smooth detonation front over the foil surface.

8.10 Faster switching techniques

8.10.1 Transformers

When a flux compressor feeds a high-impedance load, an intermediate transformer is necessary to match the load to the generator. Useful characteristics can be obtained when the load is introduced by means of a switch, which shapes the power source waveform and applies a fast-rising high-voltage pulse to the load.

<table>
<thead>
<tr>
<th>Table 8.6 Parameters for the transformer experiment. ( M_t = ) theoretical mutual inductance, ( M_e = ) measured mutual inductance.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Transformer ( L_p ) ( M_t ) ( M_e ) ( M_c )</td>
</tr>
<tr>
<td>( nH )</td>
</tr>
<tr>
<td>180</td>
</tr>
</tbody>
</table>

A transformer was developed on the basis of previous work [Reinovsksy et al 1987], with both the primary and secondary windings made from two 16.5 cm wide copper strips laid side-by-side and the inter-winding insulation comprising six layers of 125 \( \mu \)m thick Mylar. The strip used for the 25 cm diameter single-turn primary was 500 \( \mu \)m thick and that for the six-turn secondary was 51 \( \mu \)m thick. The secondary turns were thermally bonded between 50 \( \mu \)m Mylar foil faced with 100 \( \mu \)m of polyethylene, with additional layers of the same material used to give a separation between turns of about 600 \( \mu \)m.

Fig 8.27 shows the circuit for an experiment (driven by the laboratory capacitor bank charged to 12 kV) in which a transformer was connected in series with a foil. If \( I_1 \) and \( I_2 \) are the primary and secondary currents, the behaviour of the overall system after closure of the untriggered solid dielectric closing switch (SDS) is determined as in section 8.9 but with Eqns (8.10) (8.11) and (8.12) replaced by the following two equations.
\[
\frac{dl_1}{dt} = \frac{1}{M} \left( (L_2 + L_s) \frac{dl_2}{dt} + R_2 I_2 \right)
\]

\[
\frac{dl_2}{dt} = \frac{M(R_1 + R_f)I_1 - (L_1 + L_f + L_p)R_2 I_2 - M(V_o - Q/C)}{(L_2 + L_s)(L_1 + L_f + L_p)} - M^2
\]

and with \( I_f \) in equations (8.14) and (8.15) replaced by \( I_1 \).

The primary and secondary inductances of the transformer, \( L_p \) and \( L_s \), and the primary/secondary mutual inductance \( M \) were calculated using the method described in Appendix E. The model for the transformer as validated using data available elsewhere [Reinovsky et al 1987], and the main parameters for the circuit of Fig 8.27 are recorded in Table 8.6.

The solid dielectric switch (SDS) in the secondary circuit of the transformer consisted of a 305 mm square of 1.59 mm thick polythene, with a number of 0.75 mm indentations on one side. The switch breakdown voltage between circular electrodes was 70 kV.

Measured and computed results obtained from an experiment in which the crowbar switch \( S_2 \) remained open are compared in Fig 8.28. Figs 8.28(a) and 8.28(b) confirm that the model determines most of the circuit quantities accurately, although in Fig 8.28(c) the rate-of-change of transferred current \( dl_2/dt \) — — experimental; — — theoretical.
current is only obtained accurately for the very short initial period before electrical breakdown occurred. Nevertheless, it is believed that a load voltage approaching the maximum calculated value of 150 kV was actually generated, causing destruction of the equipment designed to measure safely voltages of about 100 kV.

8.10.2 Optimisation of exploding foil

To drive a transformer-based conditioning system requires a high voltage to be developed by the EF. Although an EF to produce, say, six times the initial capacitor voltage can be determined experimentally, this is time consuming, costly and potentially damaging to the experimental circuit.

A preferred approach is to use a theoretical model for the EF similar to that developed in section 8.5.3 and the results from such a study using 17 μm foils are summarised in Table 8.7. The objective of the study was to determine the optimum EF if the maximum current is not to be less than 600 kA, and the table shows that for each width there is a different optimum length which gives the maximum voltage.

Table 8.7 Maximum circuit voltages and currents for optimum 17 μm foils. \(C = 238 \ \mu F, L_1 = 86 \ \text{nH}, R_1 = 3.0 \ \text{mΩ}, L_t = 20 \ \text{nH}, V_0 = 20 \ \text{kV}\) for the circuit of Fig 8.3

<table>
<thead>
<tr>
<th>Foil Width (cm)</th>
<th>15</th>
<th>16</th>
<th>17</th>
<th>18</th>
<th>19</th>
<th>22</th>
</tr>
</thead>
<tbody>
<tr>
<td>Optimum length (cm)</td>
<td>22</td>
<td>30</td>
<td>40</td>
<td>40</td>
<td>40</td>
<td>38</td>
</tr>
<tr>
<td>Voltage (kV)</td>
<td>100</td>
<td>116</td>
<td>122</td>
<td>119</td>
<td>116</td>
<td>98</td>
</tr>
<tr>
<td>Current (kA)</td>
<td>600</td>
<td>600</td>
<td>606</td>
<td>624</td>
<td>640</td>
<td>691</td>
</tr>
<tr>
<td>\left</td>
<td>d/dt \right</td>
<td>(TA s(^{-1}))</td>
<td>1</td>
<td>1.15</td>
<td>1.2</td>
<td>1.12</td>
</tr>
</tbody>
</table>

An experiment using a 17 μm foil, 22 cm long and 17 cm wide, was performed at an initial capacitor voltage of 17 kV. The theoretically predicted maximum voltage for this (78 kV) agreed well with the corresponding measured value of 82 kV, and the maximum predicted value of the rate-of-change of current with time of -0.78 TA s\(^{-1}\) was also in good agreement with the measured value of -0.75 TA s\(^{-1}\). It is important to note that when used in a switching experiment, an optimised EF does not produce voltages in excess of 80 kV, and thus will not damage the capacitor bank circuit. Experiments using transformers, as described in the next section, need to be conducted using a crowbar technique to protect the power system.
8.10.3 Crowbar switch technique

The insight provided by the investigation into the flux compressor/exploding-foil circuit has led to a novel method of significantly increasing the value of the negative current derivative in the output of the conditioning circuit of Fig 8.27, when powered by a compressor rather than a capacitor. If the foil and transformer circuit is crowbarred by switch $S_2$, when the foil voltage has reached its maximum value, the final stage of the circuit is effectively disconnected from the compressor and the early stages of the conditioning circuit. The current in the primary circuit will remain approximately the same, but its rate of change with time will become more negative due to the reductions in the total circuit inductance. Either the armature cone of the compressor or an EF closing switch can provide the crowbarring.

To illustrate the action of the crowbar technique, the dimensions of a foil to give a maximum voltage $V_m$ at 2 MA current were calculated on the basis of the Flexy flux compressor described in Chapter 6.

The foil cross section was the same as used in test F3 of Table 8.3 but the length was increased by a factor of six. The results obtained for the foil performance and presented

![Predicted waveforms for FLEXY/foil conditioning: (a) FLEXY current $I_t$; (b) foil voltage $V_f$; (c) rate-of-change of FLEXY current $dI/dt$; time origin as in figure 8.10](image)
in Table 8.8 are somewhat surprising, since the 17 kJ cm\(^{-3}\) (or a total of 1MJ of energy dissipated at maximum voltage) is only about one third of that expected from previous capacitor bank experiments to vaporise the foil fully. This also demonstrates the ability of a compressor to deposit large quantities of energy into a resistive load.

Fig 8.29(a) and (b) present results predicted for the transferred current and the foil voltage in the circuit of Fig 8.27 when crowbarred but using a flux compressor as the power source. Fig 8.29 (c) shows that the effect is to increase the negative rate-of-change of current to a maximum value of -3 T A s\(^{-1}\), a figure that amply achieves the rise time required to operate a plasma erosion opening switch (PEOS) connected in the secondary circuit. A foil closing switch (FCS) can serve to connect and close the secondary before the foil voltage has achieved the maximum value.

### 8.11 Conclusions

Models obtained experimentally for copper fuses 17 μm and 25.4 μm thick of the increase of resistance ratio with deposited energy, were shown to apply over a wide range of fuse dimensions, and were used in a computer program compiled to closely predict fuse performance. The two models demonstrate a characteristic of fuses during the vapour phase, the thinner fuse needing much less energy for the same dynamic ratio. It was shown that the reduced rate of energy input when a flux compressor, rather than a capacitor bank, was used as the power source resulted in a reduced value of voltage generated by the foil.

The EFF was readily implemented, its high performance characteristics were confirmed, and since it does not have the energy rate problems mentioned above, it can serve as a good first stage of flux compressor conditioning.

The opening and closing switch techniques presented can be used in conditioning circuits for both flux compressors and capacitor banks, to provide very sharp output current and voltage waveforms. Where energy rates are acceptable, the circuits will operate automatically. In particular, predicting the performance of the output conditioning circuits for a flux compressor using the techniques described, followed by proof tests using a capacitor bank, will maximise the probability of a first time firing success.
STUDIES OF THE PEOS OPENING SWITCH

The Plasma Erosion Opening Switch is acknowledged to be the fastest high current opening switch presently available. Experiments undertaken with the input current rising to the opening current level in less than 100 ns (referred to as the conduction time), have demonstrated opening times of approximately 10 ns to a resistance of 5-50 Ω, with resistance gradients of 1 Ω/ns in the final opening stage [Mesyats et al, 1986, Myamoto et al, 1987, Stennet et al, 1987]. The physical processes involved are not however well understood, and research being pursued at a number of centres into the basic operating mechanisms aims to produce similar output characteristics for input pulses with lengthened rise times. A number of explanations have been advanced for the action of the PEOS with a variety of input conditions, and a physical picture based on different in-situ measurements is gradually emerging.

Switches built for input opening current levels (threshold levels) up to several kilo-amperes have demonstrated that output pulses of many megavolts can be produced, due to the magnetic insulation phenomenon that accompanies the final opening phase [Weber et al, 1991]. Theoretical OD studies at the Naval Research Laboratory (NRL, Washington DC) have led to a switch model for fast input pulses [Ottinger et al, 1984], which has been verified experimentally for switches with input conduction times of about 100 ns. Results from experiments (described later) to determine the characteristics of the PEOS, and to observe the plasma density in the switch channel, have shown that for longer input current rise-times (up to 1 μs), the associated increase in the duration of the magnetic forces in the switch produces a significant change to the distribution of the plasma, and that the rise-time of the output pulse increases.

This chapter describes work leading to the production of a prototype PEOS for the final stage of conditioning a 1 MJ output flux compressor (FC), and intended for proof testing in a capacitor-driven pulse conditioning circuit. It begins with a description of the PEOS design objectives and the basic assumptions for switch operation. This is followed by a brief account of the experimental results and the conclusions reached, in a number of research papers.
These provided the insight into the plasma number density and opening processes that were needed to design and build the switch. The chapter continues with a description of plasma sources, together with calculations of the plasma densities needed to satisfy the design objectives. A description follows of the source system used in the PEOS design, the methods used to estimate the range of plasma density available, and the corresponding limits of input current level that are able to open the switch are described. Assessment of the plasma source concludes with a description of experiments to determine certain important flashboard characteristics, together with a satisfactory layout for the system and its associated components and connections in the final design. Details are given of the design and construction of the PEOS, and results obtained from it when driven by the conditioned output of the capacitor bank. The chapter concludes with a description of the procedure used to develop a model for the 2-stage conditioning system that supplies a fast rising input pulse to the PEOS, and the changes in the assumed plasma values needed to produce a close fit to the slower rising experimental results.

9.1 Design objectives and basic assumptions

9.1.1 Design objectives

The objectives of the PEOS design were to build a switch that opened both to an input current rising to several hundred kiloamps in 100 ns and, for test purposes, an input rising to about 200 kA in approximately 300 ns. The former current pulse to be provided by a proposed Flexy driven conditioning circuit (described in the next chapter), and the latter by a conditioning circuit (as discussed in Chapter 8) driven by the laboratory capacitor bank.

9.1.2 Basic assumptions

In the cylindrical switch arrangement of Fig 9.1, low-density carbon plasma is injected towards the cathode through the conductive-gauze covered apertures in the anode of a coaxial vacuum transmission line. The injected plasma is produced by a flashboard (or alternatively a plasma gun) and it is assumed to drift towards the cathode to fill the annular region between the anode and cathode and so provide a low resistance path that virtually short circuits the load. It is also assumed that the high-density plasma that is formed on the cathode surface provides an electron source.
Although the opening processes are not clearly understood, they nevertheless differ for the above two requirements. The following section includes a brief account of the experimental results that are recorded and the conclusions that are reached in a number of research papers. These provided sufficient insight into the opening processes for an assessment to be made of the plasma requirements for the switch to be built.

9.2 The initial literature survey

The most important tasks posed during the survey, were to enable estimates to be made of either the plasma ion (\(n_{\text{ion}}\)) or the related electron (\(n_{\text{cop}}\)) number density in the switch at the end of the conduction phase at \(t = t_{\text{op}}\). At this time the switch begins to open and current starts flowing to the load as indicated in Chapter 7.2.3. A further task was to find a plasma source arrangement that satisfies the needs of both the FLEXY and capacitor bank driven PEOS conditions described above. The electron number density satisfies the quasi-neutrality of the carbon plasma, and is related to it by \(n_{\text{cop}} = z n_{\text{ion}}\), where \(z\) is the number of electrons removed from a neutral ion by ionisation, leaving it positively charged (symbolized for \(z = 2\) as \(\text{C}^{++}\)). A summary of the conclusions that were reached from the survey for the first task above, is included at the beginning of the next section, to clarify the discussion that follows of the published experimental results and arguments that led up to them.

9.2.1 Résumé of conclusions reached for estimating the plasma number density

The survey yielded the two empirical equations given later in Sections 9.2.2.4 and 9.2.3.6 for estimating the plasma density. Eqn 9.1 gives \(n_{\text{cop}}\) proportional to the PEOS cathode current density (\(J_{\text{op}}\)) at \(t_{\text{op}}\) for input current rise-times of about 100 ns, and an opening process in agreement with the OD model, and is valid up to a limit of \(J_{\text{op}} t^2\) defined later. Beyond the limiting value (increase of \(J_{\text{op}} / \tau\)), the associated longer duration / magnitude of the magnetic force in the switch channel change the plasma distribution significantly in both an axial and radial direction, and thereby the opening
process. For these conditions, \( n_{\text{eop}} \) approaches the value given by Eqn 9.3 with \( n_{\text{eop}} \) proportional to \( J_{\text{op}}^2 \tau^2 \) for values of \( \tau \) up to about one microsecond. In Eqns (9.1) and (9.3), \( J_{\text{op}} \) is the current density in amps / m\(^2\) over the whole area of the switch cathode channel of length \( l_0 \).

The equation titles (emission-limited (ELC) and hydro-limited (HLC) conduction), refer to the fact that the conduction phase of the PEOS ends (is limited) for a fast input pulse (Eqn 9.1) applies, and the switch begins to open, when the cathode emission reaches a critical threshold value. For input conditions that exceed the limiting value of \( J_{\text{op}} \tau^2 \) for a fast pulse and (eqn 9.3) applies, the conduction phase ends, when the hydrodynamic displacement of the plasma by the magnetic field in the switch channel reaches a critical configuration. Eqn (9.4) for the cross-over point between an ELC and HLC process was determined by combining Eqns (9.1) and (9.3), resulting in \( J_{\text{op}}^2 \tau^2 = K_c \text{Amp m}^2\text{s}^2 \).

Eqn (9.3) shows that very large plasma densities are needed as the PEOS begins to open when its conduction phase is hydro-limited. For the case of \( J_{\text{op}}(t) \) being proportional to \( t \), \( n_{\text{eop}} \) is proportional to \( J_{\text{op}}^4 \) whereas, when it is emission limited, \( n_{\text{eop}} \) is proportional to \( J_{\text{op}} \).

Estimates of \( n_{\text{eop}} \) are made by calculating \( J_{\text{op}} \tau \) using the expected PEOS input pulse characteristics, and comparing these with \( K_c \). If \( J_{\text{op}} \) is \( < K_c \), the conduction phase is emission limited and Eqn (9.1) is used to calculate \( n_{\text{eop}} \). If \( J_{\text{op}} \tau \) is \( > K_c \), the conduction phase is hydro-limited so Eqn (9.3) is used.

### 9.2.2 Experimental results for fast input pulses

#### 9.2.2.1 The OD model

The underlying four stage-model for the opening process described in Chapter 7.2.3 that has been shown to comply with experimental results for input pulse rise times of less than 100 ns. Fig 9.2 shows the close coincidence achieved between (a) experimental and (b) simulated output waveforms for a PEOS with an input current \( I_0 \) rising to 225 kA in about 100 ns. The load currents were obtained from experiments with different delay times between initiating the plasma source and applying the input pulse. \( \Delta t \) is the delay between initiation of the plasma and opening of the switch. [Weber et al, 1987].
9.2.2.2 The axial distribution of current density

Measurements of the current distribution in the switch for fast input pulses [Weber et al, 1984] show that the current broadens and moves along the switch channel, with opening occurring when it reaches the channel end. This behaviour agrees with that predicted by the model. The current at \( t_{\text{top}} \) however is found to occupy only half of the switch channel at the load end [Hinshelwood et al, 1987]. This was attributed to either an axial displacement of the plasma in which the current flows (which is not in agreement with estimates of displacement due to magnetic forces), or to an unexplained suppression of current flow at the input end of the channel. A modification of the plasma distribution assumed by the model at \( t_{\text{top}} \) may be needed for it to comply with these findings.

9.2.2.3 The relationship between \( J_{\text{op}} \) and \( n_{\text{cop}} \)

Measured values of the integrated electron density in the plasma along the switch channel length \( l_{c} \), obtained without applying an input to the PEOS, were plotted against \( J_{\text{op}} \) for PEOS experiments with a variety of plasma injection schemes and cathode areas [Weber et al, 1991]. These graphs showed that the average value of \( J_{\text{op}} \) when the PEOS begins to open at \( t_{\text{op}} \), is proportional to \( n_{\text{cop}} \), with a proportionality constant of \( 0.6 \times 10^{12} \)

9.2.2.4 Equation for emission-limited conduction

The empirical equation satisfying the above result for a PEOS whose conduction phase ends (is limited) when the emission from the cathode reaches a critical threshold value is

\[
J_{\text{op}} = 0.6 \ n_{\text{cop}} \times 10^{-12}
\]

(9.1)

where \( J_{\text{op}} = I_{\text{op}} / 2\pi r_{c} l_{c} \) Amps \( m^{-2} \), and \( r_{c} \) is the cathode radius. \( J_{\text{op}} \) calculated for the input threshold opening current \( I_{\text{op}} \) for the OD model [Eqn 15 Ottenger et al 1984] where
can be brought into close agreement with Eqn 9.1 by using a switch channel length of $l_0/2$ in Eqn 9.2. Where $e$ is the electron charge, $V_D$ is the plasma drift velocity, and $\alpha$ is $(z \text{ Mass of an electron} / \text{Mass of an ion})^{1/2}$ and is approximately equal to 0.01. The change in channel length also agrees with the current distribution measurements given in Section 9.2.2.2. This quantitative result supports the basic physics of emission-limited conduction.

### 9.2.3 Experimental results for slower rising pulses

As $\tau$ increases, the plasma distribution is changed in both the axial and radial directions by the longer duration magnetic forces in the switch channel. Under these circumstances the switching process departs significantly from that fitting the fast pulse model. The conclusions reached from arguments made later in Section 9.2.3.6 show that the transition between the switching processes depends upon both $J_{op}$ and $\tau$, and occurs at a limiting value of $J_{op}\tau^2 = K_c$. An understanding of the complex switching processes involved is emerging slowly from the results of switch experiments under a variety of conditions, and measurements of the current, plasma density, and distribution in the switch channel. The broad conclusions drawn from the results of the various published investigations described briefly below were used as a guide to the plasma density needed, and the performance expected from the switch design.

#### 9.2.3.1 Comparison of the plasma at $t_{op}$ for fast and slow input pulses

By determining the number of plasma sources needed for a PEOS to begin opening (at $t_{op}$), at the same input current level, but with rise times of between 60 and 1000 ns. It was shown that [Hinshelwood et al., 1986, Weber et al., 1991]

(i) Much more plasma is needed at the longer times, with three sources being required to open the switch with a current rising to 240 kA in 60 ns, compared to twelve sources for 100 kA in 400 ns. Six times as many sources (18) were needed for a current rising to 750 kA in 1 $\mu$s than for a similar current input current with a 60 ns rise time.

(ii) Profiles of the load current when a slower input rise-time current pulse opens the switch resemble those produced by fast input pulses, but have a longer rise-time.
9.2.3.2 The plasma axial distribution

The plasma at $t_{\text{top}}$ (inferred from measurements of the current distribution) was shown to be distributed in half or less of the switch channel at the load end [Hinshelwood et al 1986].

9.2.3.3 The plasma density

Integrated line measurements along the switch channel [Hinshelwood et al, 1992, Weber et al, 1992] showed that electron number densities with a magnitude of between $10^{15}$ and $10^{16}$ cm$^{-3}$ were present in the switch, and that the plasma was redistributed in a radial direction in the switch channel. It was concentrated towards the anode and cathode during conduction, with a rapid density reduction (thinning) occurring in the central regions close to $t_{\text{top}}$. 

9.2.3.4 Hypothesis for the opening process

From the results mentioned in Sections 9.2.3.3 and 9.2.3.1 (ii) the switch opening was assumed to occur as a two-stage process, with a slow line density reduction in the central region of the switch due to $J \times B$ forces, followed by a rapid gap formation caused by erosion of the rarefied plasma as described by the OD model [Hinshelwood et al, 1992].

9.2.3.5 Agreement between plasma density and calculated displacement

Calculation of the centre-of-mass displacement in the axial direction of a thin plasma cylinder of radius $r$ and length $l_0$ [Weber et al, 1991.] showed that plasma electron densities of between $10^{15}$ and $10^{16}$ cm$^{-3}$ (in agreement with the measured values in Section 9.2.3.3) would be needed to restrain the plasma to one half or less of the switch channel, and so satisfy the conclusion reached in Section 9.2.3.2. A linear input current rise was assumed in the calculation for simplicity.

9.2.3.6 Empirical equation for hydro-limited conduction

The results given in Sections 9.2.3.2 and 9.2.3.5 argue that emission-limited conduction (eqn 9.1) does not apply to any situation where plasma displacement is greater than one half the switch length when the conduction phase ends at $t_{\text{top}}$. Also, from Section 9.2.3.4 that the switch conduction phase ends (is limited), and the switch begins to open when the hydrodynamic displacement and thinning of the plasma reaches a critical configuration. An empirical equation (9.3) for these conditions (termed hydro-limited
conduction) [Weber et al, 1991] was therefore defined by equating the calculated plasma displacement referred to in Section 9.2.3.5 to $l_o/2$. This resulted in a predicted scaling between the conduction current density $J_{op}$, the conduction time $\tau$, and electron plasma-density $n_{eop}$, that is insensitive to the actual displacement chosen. Thus

$$J_{op}^2 \tau^2 = \frac{12 M_i n_{eop}}{\mu_o z} \quad (9.3)$$

where $M_i$ is the mass of a carbon ion ($19.92 \times 10^{-27}$ kg) $\mu_o$ is the permeability of free space and $z = 2$ which is the predominant value for the flashboard plasma source (described later) used in the switch design. Inserting component values into the equation gives

$$J_{op}^2 \tau^2 = 9.51 \times 10^{20} n_{eop}$$

The prediction from eqn (9.3) that $J_{op}$ is proportional to $n^{1/4}$ (for $J_{op}(t) \propto t$) agrees closely with the relationship obtained by experiment [Weber et al 1991]. The intersection of the emission and hydrodynamic limited conduction in $(J, t)$ space is given by combining eqns (9.1) and (9.3) as

$$J_{op} \tau^2 = \frac{20 M_i \times 10^{12}}{\mu_o z} = K_c \quad (9.4)$$

where $K_c = 1.58 \times 10^7$ Amp m$^{-2}$ s$^{-2}$

The above equations, which are obviously somewhat speculative, were used to estimate the electron and related ion number densities, for both the PEOS design, and in relevant published experimental switch results.

**9.2.3.7 Estimating the electron density at $t_{op}$**

Eqn (9.3) was used to determine whether or not the conduction phase is emission or hydrostatically limited. If the value calculated for $J_{op} \tau^2 \leq K_c$ the switching conduction phase is emission limited and eqn (9.1) is used to obtain an approximate value of $n_{eop}$. For $J_{op} \tau^2 > K_c$ the conduction phase is hydro-limited and eqn (9.3) is used to calculate $n_{eop}$. It is readily shown from Eqn 9.3 that $n_{eop}$, in terms of a PEOS input current $I_{op}$ at $t_{op}$ when the conduction phase is hydro-limited, is given by

$$n_{eop} = 2.66 \frac{J_{op}^2 \tau^2}{r_c^2 l_o^2} \times 10^{17} \quad (9.4)$$
9.3 Plasma sources

Two different sources have been used to provide plasma in the PEOS switch channel; carbon guns [Humphries et al, 1979, Mendel et al, 1980, Weber et al, 1987] and flashboards [Renk et al, 1984, Hinshelwood, 1985, Renk, 1989]. Both sources produce plasma by causing the discharge current from a capacitor to flow across a carbon-coated insulator. A thin carbon layer is thereby heated, vapourised and ionised, and propelled away from the surface at supersonic velocity by the force resulting from the interaction between the current and its own magnetic field.

Plasma sources are simple to construct and operate. They are reasonably reliable and give reproducible results. Plasma guns have been thoroughly researched, and were used in most experimental switches aimed at determining the characteristics of the switching process. Flashboards were developed to provide a wider and relatively uniform cone of plasma, and to fill large volumes in switches for high-current systems.

9.3.1 Plasma guns

In its simplest form, the plasma gun consists of a length of semi-rigid coaxial cable that is terminated either flush or with its centre slightly protruding. The insulator is then coated with a suspension of carbon particles in alcohol.

The cable is fed via a triggered switch, to a small capacitor charged to a high voltage. When the switch is closed, a voltage appears at the remote end of the cable, and a surface flashover occurs between the inner and outer conductors. This vaporizes and ionises a layer of the carbon coating, creating a narrow cone of propagating plasma.

![Graph showing plasma gun parameters](image-url)

**Fig.9.3**: Probe measurements of carbon-gun plasma parameters in vacuum, 10 cm from the gun.
9.3.2 Plasma gun characteristics

The carbon gun has been analysed using several standard measurement techniques [Hochte-Holtgreven, 1968, Badaye et al, 1991]. Typical profiles of ion flux and plasma density are shown at a distance of 0.1 m from the gun in Fig 9.3 [Weber et al, 1987]. The fall to zero of the ion flux result was attributed to the reduction of the drift velocity with time, making Faraday cup measurements of ion current density insensitive to the ambient plasma density. The gun current can be seen to be an underdamped sinusoid, with a first quarter period of 0.5 μs and a peak value of 35 kA. Δt is the time delay measured from the start of the source current to the time the PEOS opens in an experiment.

9.3.3 Flashboards

As shown in Fig 9.4(a), a flashboard is made from a short section of flat strip-line, consisting of a thin plastic base (often Kaplon) with copper deposited on each side. Much of the side to be connected to the high voltage supply is etched away, to form a network of chains each with a number of flashover gaps in series. The gaps are coated with a carbon composition, and when the parallel chains are connected via a low-inductance line and triggered switch to a charged capacitor, carbon ions are propagated from the surface of the coated gaps by a similar process to that described above. Propagation from the surface proceeds as indicated in Fig 9.4b. Fig 9.5 shows measurements obtained from a flashboard driven by a discharge circuit that provided a current maximum of 27 kA from a 0.6 μF capacitor charged to 25 kV [Renk, 1989]. The profile amplitudes were shown to
be adjustable by changing the circuit parameters, in particular, halving the voltage approximately halves the plasma density output with little change of the drift velocity.

9.4 The PEOS design details

The PEOS produced at Loughborough was based on an NRL design (Gamble 2) that employed a coaxial configuration. For an input current rising to 950 kA in approximately 60 ns, Gamble 2 opened at values of $\Delta t$ (delay between initiation of the plasma and opening of the switch) between $1\mu$s and $1.8$ $\mu$s for input amplitudes between 600 kA and 900 kA [Neri et al, 1987, Weber et al, 1991]. The corresponding range of plasma densities accumulated in the switch channel from the three flashboards positioned 100 mm from the 25 mm radius cathode, was determined from the switch opening currents and dimensions, using the methods described in Section 9.2.3.7, as between $6.3 \times 10^{19}$ and $12 \times 10^{19}$ m$^{-3}$. When opening at 600 kA its conduction phase was emission-limited, whereas at 900 kA it was hydro-limited. The estimated plasma drift velocity at opening time was given as about 10 cm/$\mu$s.

To accommodate the anticipated conditioned outputs from the flux compressor or capacitor bank given in Section 9.1.1, a similar flashboard arrangement was used to provide plasma to a 22.5 mm radius cathode, via three gauze covered apertures 100 mm long, 50 mm wide and equally spaced round a 45 mm radius anode. An input inductance of less than 100 nH was required to ensure that a 300 ns input pulse rise-time was obtained from the capacitor bank.

9.4.1 Range of opening currents

The expected range of current amplitudes that will open the PEOS design, given the correct plasma pre-delay (described later) for the proposed input currents and rise-times, was calculated as follows. The input conditions for the FLEXY and capacitor bank driven PEOS were inserted into eqn (9.4), to establish whether they had an emission-limited or hydro-limited conduction phase. The limits calculated above of plasma densities available from the flashboard system were then inserted into the appropriate equation (9.1 or 9.3), together with the related value of input rise-time (100 ns or 300 ns). Limits for the cathode current density $J_{op}$ and hence $I_{op}$ were extracted from the equations.

The calculations showed that the FLEXY driven PEOS had an emission limited conduction phase, and will open for input current amplitudes between 346 kA and 486
kA. The capacitor bank driven PEOS is hydro-limited and can be expected to open at input current levels between 115 kA and 160 kA. These figures are based on calculations of the plasma density needed for the high current experiments conducted with the Gamble 2 PEOS. They are therefore regarded as being correct for the upper limits of input current, but the lower limit can probably be extended.

9.5 Tests of a flashboard assembly layout

Preliminary investigations were made of the plasma sources constructed for the present study, their associated charged capacitor current sources, and the triggering and connecting circuitry, with the intention of obtaining an insight into their characteristics, of highlighting any circuit layout problems, and of determining the best arrangement for the switch components in a final compact design of the FLEXY conditioning circuit. A PEOS with a trial flashboard layout was constructed and attached to connections on the base of the steel vacuum bell with integral vacuum system described in chapter 4. Fig 9.6 shows this arrangement. The flashboards were positioned 0.1 m from the cathode conductor, and were held in place facing the gauze covered anode plasma entrance ports by plastic supports attached to the

Fig.9.6: PEOS trial layout

Fig. 9.7: The flashboard capacitor discharge circuits mounted on top of the vacuum bell.

Fig. 9.8: Preparing the connecting cable ends prior to lowering the bell.
anode. The three 0.6 \( \mu F \) capacitor discharge circuits needed to feed current to the flashboards are shown arranged on top of the bell in Fig 9.7. A solid dielectric switch triggered the discharge, and the output current entered the bell via lead-through connectors in its top 0.2 m diameter Perspex plate. Three parallel-connected coaxial cables completed the internal circuit to each flashboard. Fig 9.8 shows the ends of coaxial cables being prepared prior to connection to the flashboards and lowering of the vacuum bell for a low-pressure test. A schematic diagram of the flashboard circuit together with a block diagram of the high voltage and trigger components are given in Fig 9.9

![Schematic diagram of the flashboard circuit](image)

**Fig. 9.9:** (a) Schematic of flashboard circuit. (b) Block diagram of its HV and trigger components

### 9.5.1 Flashboard construction

The flashboards were made from 1.5 mm thick double-sided printed board, 170 mm long and 80 mm wide. By etching away the surplus copper, six chains of 8 mm long and 5 mm wide copper islands, with 2 mm long gaps between them were formed. The copper near the edge of the board was also etched away, and copper foil was soldered at each end of the chains to complete the current paths. Tape was used to mask the plasma producing side of the flashboard,
leaving only the gaps between the islands to be sprayed with a carbon composition Aerodag (Acheson Colloids Company). Fig 9.10 is a front view of a flashboard

9.5.2 Results and problems arising in the trial layout

A number of possible problems in the final design were highlighted by the trial layout. In particular, the length and number of parallel-connected high-voltage coaxial cables needed inside the bell to feed the necessary current from the lead-through connectors to the flashboards, and that had to be connected each time the bell was lowered to enclose the switch, posed problems. This illustrated that in a compact final design, the flashboards should be attached to the top plate of the vacuum enclosure, and that a different method of low-inductance connection should be sought.

Repeatable values of the required discharge current were obtained (approximately 24 kA with a quarter period of 0.7 μs) from the discharge circuit and associated components, with the capacitors charged to 25 kV. Some arcing however occurred to the steel bell, when discharging with the internal pressure reduced to its working value of $10^3$ Pa.

To avoid the above problems, a cylindrical non-conducting vacuum vessel with removable top and bottom plates was used for the final design. The switch is attached to and passes though the bottom plate, and the flashboards are mounted on brackets that attach to the top plate and protrude into the vessel. Low-inductance strip-lines convey current to the flashboards via manufactured strip lead-through connectors.

9.6 Optical observation of some flashboard characteristics

The characteristics were obtained from a flashboard hung from the perspex plate at the top of the bell, with the internal pressure reduced to $10^3$ Pa. The flashboard was connected to the discharge circuits via a short internal connecting cable. Switching with an explosive detonator enabled the performance to be observed over a range of capacitor voltages. An Imicon camera operating at $10^7$ frames/second viewed the flashboard via one of the ports in the bell. Although the observed light output intensity from the propagating plasma cannot be directly related to its density, it is certain that the light indicates a region of the plasma in which there is power deposition. Difficulty was experienced with the camera light settings needed to accommodate the wide range of light intensity, and also with synchronisation of the flash waveform with the camera.
Fig. 9.11: Optical results; Front views, (a) with the discharge capacitors charged to 25 kV, (b) with the capacitor charged to 10 kV.

timing pulses. Viewing the light intensity from the six gapped chains of the flashboard confirmed the following

(i) Flashboard coating procedure

Three coatings of Aerodag with up to a day for drying between them were needed, together with a few initial “seasoning” discharges, to achieve identical and many time repeatable luminous discharges from the six chains.

(ii) Light output intensity

The light intensity profile matched that of the flash waveform, with the discharge current and light intensity maximum occurring at the same time. Fig 9.11 (a) shows the result obtained when viewing the flashboard, with the discharge current provided by the circuit capacitor charged to 25 kV.

(iii) Minimum voltage operation

Identical luminous characteristics for the six chains were not obtained when the capacitor discharge voltage was reduced from 25kV to below 10kV. Only a few chains flashed initially, followed by others after some delay. See Fig 9.11 (b)

Fig 9.11 (c) is a side view of the board a fraction of a microsecond after the flash discharge was initiated, and shows clearly a luminous front (presumably plasma) issuing from the board.

Fig. 9.11: (c) side view showing plasma issuing from the board
9.7 PEOS construction

9.7.1 General considerations

To satisfy the envisaged arrangements for flux compressor or capacitor bank drive the switch and its associated components were designed as separate compact modules. Cylindrical input connections were used for the switch to enable it to be attached directly to the conditioned output of the flux compressor. Alternatively, it can be connected via a low-inductance cylindrical-to-plane line adapter (termed a fish tail) to strip-line outputs from a flux compressor or capacitor bank. Current from a compact
triggered discharge circuit is conveyed to the flashboards via low-inductance strip-lines. Vacuum-tight strip connectors in the top plate of the vacuum vessel enable the lines to complete the circuit as described earlier. A small two-stage vacuum pump connected to the PEOS top plate through a short flexible hose completed the required switch accessories.

9.7.2 Construction details

9.7.2.1 The switch

PVC was used for the input components and the main body of the switch, with O-rings providing the required vacuum seals. Ridges were cut along some of the input surface to increase the length of the voltage breakdown path.

Fig 9.12 is a line diagram of the PEOS and the component parts can be seen in Fig 9.13. Fig 9.14 shows the internal switch assembly, without either the plastic vacuum container or the load connection. The flashboards and 70% geometric transparency gauze are clearly visible. The gauze provides a conducting path for the current, while allowing the plasma to pass through and fill the anode-cathode gap.

9.7.2.2 The flashboard discharge circuit arrangement

A compact version of the discharge arrangement shown earlier in Fig 9.7 was built. Its schematic circuit being the same as that of Fig 9.9, Three 0.6 μF capacitors triggered by
a solid dielectric switch were arranged to provide a low inductance strip-line circuit to each flashboard. The unit is seen nearing completion in Fig 9.15. The output currents are conveyed to the switch, and via vacuum tight feed-through strip-line connectors in the top plate to the flashboards as shown later in Fig 9.18. Each copper line is shrouded in thin polythene faced Mylar, which when heated forms an envelope with excellent edge insulation, when taped together the lines form a semi-rigid line harness. A Cockroft-Walton voltage multiplier provided the charging voltage, and an 8 kV pulse generator provides the triggering source. Fig 9.16 shows the discharge current recorded from one of the flashboards.

9.7.2.3 The flashboard connections

One end of each of the flashboards described in Section 9.7.1 is secured to the remote end of a pillar, as seen in Fig 9.17, which in turn is attached to a flange protruding from its high voltage feed-through line connector. The other ends of the flashboards are attached by Mylar wrapped copper strip-lines and a large central lead-through connector to their common return lines.

9.7.2.4 Flux probes

To enable the load current to be measured, two flexible probes with 5 turn 1.46 mm diameter coils are secured to the inner surface of the 90 mm diameter anode. The probe
cables are brought out through holes in the anode and attached to feed-through connectors in the top plate.

9.7.2.5 The PEOS load

An aluminium plate secured to the ends of the anode and cathode cylinders, with soft aluminium foil compressed between them to ensure a good contact, provides the short circuit load. The fully assembled PEOS with the flashboard strip-line harness attached is shown in Fig 9.18.

9.7.2.6 The vacuum system

A compact diffusion pump is used to evacuate the switch to a pressure of $10^{-3}$ Pa.

9.8 Appraisal of the PEOS on the capacitor bank

9.8.1 Experimental arrangement

Two adaptors, termed fishtails and shown in Fig 9.19, are used to provide a low-inductance connection between the cylindrical geometry of the anode and cathode of the PEOS and the plane geometry of the capacitor bank output strip-lines. A single-stage conditioning circuit provides an output current of approximately 200 kA to the PEOS in less than 400 ns, when driven by the laboratory capacitor bank charged to 25 kV.
Fig. 9.20: PEOS connected to the single-stage conditioning circuit driven by the capacitor bank.

Fig 9.20 shows the PEOS connected to the conditioning circuit, together with its associated components.

**9.8.2 Equivalent electrical circuit**

Fig 9.21 shows the equivalent electrical circuit of the two-stage conditioning system. Resistors $R_{t1}$ and $R_{t2}$ include the closing switch resistance of the corresponding circuit, while the inductance $L_t$ includes the ballast inductance needed to restrict the current to about 600 kA. Details of the opening and closing switches (OS and CS) are given in Chapter 8.

**9.8.3 Diagnostics**

The currents in the experimental circuit are measured by integrating the output of flux probes positioned in the circuit and PEOS load. The voltage across the open circuiting foil (OS), is measured by a water resistor voltage probe. However, because of the
electromagnetic noise generated by the actions of the closing switch (CS), a Faraday rotation transducer [Enache et al 1997] was also included, to provide an additional noise-free measurement of the current transferred to the PEOS. Complete noise immunity is ensured by using 30 m long optical cables to connect the probe to a battery powered laser light source at one end and an opto-electronic converter feeding an oscilloscope at the other. The components at both ends are contained within special-purpose Faraday cages. Twelve channels of recording on fast digital oscilloscopes are needed to obtain a full appreciation of the circuit characteristics.

9.8.4 Initial tests

Values of circuit parameters and calibration figures for the load probes were obtained from results of a test without a plasma and conducted at 12 kV, as described in Chapter 4.7.4, with the thin foils of the OC and CS switches replaced by thick ones of the same dimensions. Table 9.1 details the parameters of the circuit of Fig 9.21 as obtained from this test.

<table>
<thead>
<tr>
<th>capacitor bank</th>
<th>transmission lines</th>
<th>opening switch (OS)</th>
<th>closing switch (CS)</th>
<th>load</th>
</tr>
</thead>
<tbody>
<tr>
<td>C μF</td>
<td>V kV</td>
<td>L_{q1} nH</td>
<td>L_{l2} nH</td>
<td>R_{q1} mΩ</td>
</tr>
<tr>
<td>238</td>
<td>21</td>
<td>110</td>
<td>90</td>
<td>3.20</td>
</tr>
</tbody>
</table>

Table 9.1 Parameters of the two-stage conditioning switch. The opening switch was a copper foil. The closing switch had seven aluminium bridges. t, thickness; l, length; w, width, R_{l}(0), R_{c}(0) and R_{i}(0), initial calculated values

9.8.5 Plasma pre-delay

A plasma source system capable of producing the ion number densities calculated by eqns (9.1) or (9.3), propagates plasma ions across the distance between the source and the cathode in a time t', filling the switch channel with plasma of sufficient density to constitute a virtual short-circuit to the PEOS load. Ions from the source continue to accumulate in the channel and will reach the number density (n_{cop}) needed to open the switch at time t_{op}. The pre-delay between initiation of the plasma and applying the input current to the PEOS (which must be > t'), and the related time Δt to bring n_{cop} into coincidence with the time of arrival of the corresponding input current I_{op}, were determined by experiment.
9.8.6 Experimental procedure

The correct value of $\Delta t$ was determined from a series of experiments with the input current pulse applied to the PEOS at different times within a 1 $\mu$s window, starting 1 $\mu$s after the flashboards are triggered. Fine-tuning of the input current characteristics was achieved by making small adjustments to the width of the OS foil.

9.8.7 Experimental results

The PEOS opened at $\Delta t = 1.6$ $\mu$s. Figs 9.22 - 9.24 show experimental results obtained for the circuit of Fig 9.21. The circuit was intended to produce a very much faster rise in the current $I_3$ transferred by the PEOS to the load than that supplied to the circuit $I_1$ by the capacitor. Fig 9.22 shows that this was clearly achieved, with the 5.5 $\mu$s needed for the capacitor current $I_1$ to rise to about 500kA being reduced to less than 100ns for 200kA in the load current. The initial time rate of increase of current from the capacitor bank of about $1.2 \times 10^{11}$ A s$^{-1}$ (Fig 9.23(a)) has been raised to about $5 \times 10^{12}$ A s$^{-1}$ (Fig 9.23(c)) in the load. The simulated results (from a model discussed later) are also included in the figures.

The initial value of the rate of change with time of the current into the PEOS, following operation of the CS, depends on the voltage level achieved across the OS. Voltages exceeding 80 kV can be obtained with an unswitched OS using the optimal fuse dimensions obtained from the computer program given in Chapter 8.10.2, although this voltage would damage the capacitors. For an acceptable current transfer, the CS

![Fig.9.22: The variation of circuit currents with time: (a) all currents and (b) an expanded view of $I_2$ and $I_3$: (---), theory; and (---), experiment.](image-url)
characteristic must therefore be arranged such that it switches within a time window when the voltage across the OS is between 45 and 80 kV (Fig 9.24).

The relatively small discrepancies apparent between the predicted and actual capacitor current results (I₁ in Fig 9.22 (a)) are attributable to slight inadequacies in the numerical representation of the main switch S₁. Because the OS foil model was obtained from experiments that did not involve stage switching, small differences also exist between measured and predicted variations for the OS voltage after switching, as seen in Fig 9.24. This is not altogether unexpected with the quite simple model that was used. Far more complex models based on atomic data [Lindemuth et al., 1985] have needed an adjustment parameter [Lindemuth et al., 1989] to ensure a good fit between experimental and measured characteristics.

A particular feature of the results is the good qualitative agreement of the load current profile with that obtained for an emission limited opening process. The normal erosion phase that begins when the influx of ion current is insufficient to maintain a steady gap, and the enhanced erosion when the magnetic field increases the electron lifetime in the gap, greatly increasing the ion current fraction, can be clearly distinguished in both the experimental and simulated results in Fig 9.23(c). These facts are in agreement with the
hypothesis given in Section 9.2.3.4 that the final opening process of a hydro-limited switch after thinning of the plasma, is a rapid gap formation by erosion of the rarefied plasma.

A change that would improve the behaviour of the system for those applications in which a faster current rise time is needed is to adopt the advanced foil open switch design discussed in Chapter 8.9.2. The magnetic flux associated with the OS then remains within the main circuit after the action of the CS. Predictions show that this, together with minimising the inductance of the transmission-line component of the coupling to the PEOS, and optimising the circuit inductance, will produce a rise-time of the PEOS input current up to the threshold opening value of about 100 ns, giving a rise-time of the load current approaching 10 ns, which is in accordance with that used in PEOS experimentation elsewhere [Weber et al, 1987].

### 9.9 Numerical model

A complete numerical model (unpublished communication from Dr B M Novac) was developed for the two-stage conditioning system of Fig 9.21 with the system of equations being similar to those described in Chapter 8 Section 8.9 and assuming that the PEOS conduction stage was emission-limited. The equations were solved algebraically for the time of change of current, with the resulting set of first-order differential equations being solved numerically using FORTRAN software. At each time step of the solution, the OS foil resistance $R_f$ and the CS bridge resistance $R_b$, were obtained, using experimentally derived (Chapter 8) variations of the dynamic resistance ratio with specific energy deposited for the 17 μm copper foil (Fig 8.6) and for the 100 μm aluminium bridges (Fig 8.22), after first calculating the specific energy deposited. The second stage of the conditioning circuit (including the PEOS) was coupled according to the closing-switch function (Fig 8.21).
Calculation of the PEOS resistance and critical currents for the four opening phases was by the standard NRL method. This method gave a close fit to the experimental results when used to simulate the output characteristics of the Gamble 2 switch with a emission limited conduction phase for $t_{op} = 600$ kA [Neri et al, 1987 Weber et al 1991]. The load resistance of the PEOS was modelled using the exponential skin depth formula. [Knoepfel, 1970].

9.9.1 Simulating the experimental results

The model can be used to simulate a PEOS whose conduction phase is emission-limited. The conduction phase for the slower (~300 ns rise-time) input pulse provided by the conditioned output of the capacitor bank was however shown to be hydro-limited, and the switch begins to open at $t_{op}$, when the plasma in the central region of the switch channel is reduced to a low value. Beyond $t_{op}$ however, the output load current profile has been reported (Section 9.2.3.1 (ii) and Section 9.2.3.4) to resemble a lengthened version of that expected from an emission-limited plasma erosion opening process (similar to that of $I_3$ in Fig 9.22) that satisfies the OD model. An exploratory approach was therefore adopted to simulate the experimental results. The numerical model was initially assembled using the PEOS OD model with appropriate plasma values inserted to satisfy equation (9.2) for the bipolar threshold current ($I_{op}$). The equation was set equal to the experimentally derived opening current of about 150 kA, and a value for the plasma drift velocity was extracted from it. The close fit to the experimental results shown in Fig 9.23(c), was then obtained by making adjustments to the values of the plasma components, and channel length in the NRL equations. Initial and adjusted best fit values for the switch were

(i) Initial values

$n_{op} = 8.85 \times 10^{18}$ m$^{-3}$, $V_D = 7 \times 10^4$ m s$^{-1}$, $l_0 = 0.05$ m, to satisfy the agreement reached in section 9.2.2.4.

(ii) Best fit values

$n_{op} = 1.35 \times 10^{19}$ m$^{-3}$, $V_D = 9 \times 10^4$ m s$^{-1}$, $l_0 = 0.025$ m

Future analysis of this exercise may well yield additional insight into the underlying opening process.
9.10 Conclusions

As developed, the conditioning circuit is capable of sufficiently reducing the rise time of the current from several microseconds at the output of a capacitor bank to operate the PEOS satisfactorily, and to transfer current to a load with a rise time below 100 ns. It is relatively straightforward to adapt the system to high energy flux compressor drive, with simple, or explosively formed exploding foil output conditioning, or both, to provide an input pulse to the PEOS of several kiloamps rising in less than 100 ns. The resulting emission limited opening process, can be expected to result in load voltages increasing at up to 1 MV ns$^{-1}$ when working into a highly resistive load. [Weber et al 1987].
PROPOSED CONDITIONING CIRCUITS FOR FLEXY

A central aim of the thesis was to produce a current pulse rising in about 10 ns to several hundred kiloamperes, by conditioning the output of the Flexy flux compressor using exploding foil (fuse) current sharpening stages and a final stage of PEOS switching. The studies presented in Chapter 8 of the characteristics of fuses as opening and closing switches in high current circuits, and their use in transferring current rapidly to a low-inductance, have led to a clear understanding of fuse sharpening techniques. In addition the work in Chapter 9 has led to an appreciation of the design, construction and use of a PEOS and to the input conditions needed to produce an output pulse that satisfies the central aim.

The insight gained into the switching technology enabled decisions to be made on the best conditioning circuit arrangements. When used together with empirical models for the time varying components obtained from an extensive experimental program, an assessment was obtained of the performance of these circuits.

Simple fuse sharpening stages are easy to implement and, when used to condition the discharge waveform of the laboratory capacitor bank, sufficient energy input rates to the fuse were provided to enable it to pass quickly though its melt and vapour phases. Under these conditions, electric fields of between 3 and 4 kV/cm were sustained before restrike occurred across the fuse. Automatic operation of the sharpening stage was also achieved by using a fuse as a closing switch. This technique was used to provide an input to test the PEOS described in Chapter 9, and it was subsequently predicted that by improving the fuse layout and optimising the circuit inductances, a PEOS output rise-time of 10 ns would be obtained.

However, when the current source had the longer time-scale current waveform provided by the Flexy flux compressor, the energy input rate was reduced and restrike occurred at an electric field of 1.2 kV/cm. In addition, at restrike the energy dissipated by the fuse was only about one third of that necessary to fully vapourise it. The lower restrike electric field is regarded therefore as being due to differences in the thermal and hydrodynamic behaviour of the foils during the melting and vapour phases, as a consequence of the lower input energy rates. Clearly, the dynamic resistance ratio is also reduced. Replacing
\( \varepsilon_r \) by \( \varepsilon_r / 3 \) in eqn (8.6) gives \( I_0 \) as three times longer for optimum fuse sizing conditions for Flexy experiments than for those conducted using the capacitor bank.

The explosively formed fuse described and appraised in Chapter 8, overcomes the energy rate problem by changing a thick foil in the load circuit into a fuse at the most appropriate time on the Flexy output current waveform by reducing its thickness. It is however difficult to implement this technique, as it requires an explosive assembly with special initiating techniques.

It is clear from Chapter 9 that to provide an output from the PEOS rising in 10 ns, the input current must reach the threshold opening level in 100 ns or less and, if the conduction time is emission limited, the OD model can be used to determine the PEOS characteristics. Setting the minimum PEOS threshold current as 300 kA, a time input current derivative of \( 3 \times 10^{12} \) A s\(^{-1}\) is required to satisfy the central aim, and a voltage pulse across the 80 nH PEOS input inductance exceeding 240 kV is needed to achieve it. The voltage across the fuse when the closing switch is operated, must be greater than the above value to account for the voltage induced in the fuse and coupling circuit inductances.

This chapter provides a brief description of the procedure given in an unpublished communication from Dr Novac of the Electronic and Electrical Engineering Department, who carried out the numerical calculations using Fortran computer language. This is followed by two possible Flexy driven circuits that use fuse conditioning to feed the PEOS. The first of these has a single conditioning stage while the second has two stages with an output transformer to raise the input voltage to the PEOS. Both circuits fail to achieve the input voltage level to the PEOS needed to satisfy the central aim and they are therefore not presented in great detail. Comments are given on the implementation of the fuses, together with suggestions of ways of improving the performance of the circuits. A proposed Flexy driven circuit using an EFF conditioning stage to feed the PEOS follows. Calculations show that this combination can satisfy the central aim and details of a proposed future experiment are therefore presented.
10.1 The calculations

A set of first-order differential equations was derived to describe each of the proposed Flexy driven circuits, and these were solved in a similar manner to that described in Chapter 8. At each time step of the solution, the data from Figs 10.2, 10.3, and 10.6 for Flexy, the fuses and the EFF, obtained from the results of the capacitor bank and Flexy driven experiments, were used as necessary to provide their corresponding characteristic. A number of trial runs of the computer program were needed to determine the best fuse dimensions to use.

10.2 Designs with simple fuse stages feeding a PEOS

10.2.1 Single stage of fuse conditioning

The equivalent circuit for the proposed Flexy driven conditioning circuit with a single stage of current sharpening is shown in Fig 10.1. The model used for the fuse in the calculations (curve FEMF in Fig 10.3) was derived from the results of the Flexy / foil experiments of Chapter 8, during which the foil restructured at an electric field of 1.2 kV. The best fuse dimensions determined from many trial runs of the computer program had a width of 300 cm, giving a cross-section close to that of the fuse used in the Flexy experiment F3, and a length of 240 cm. It was envisaged that this large fuse could be arranged in a similar manner to that of the Flexy F3 experiment (described in Section 8.6) with six 50 cm wide foil strips in parallel but with its length accommodated by folding it concertina fashion. This would result in a flat package about 60 cm wide by 50

![Fig. 10.1: Equivalent circuit for proposed conditioning scheme, Sₙ - start switch, S₁ - crowbar switch, FCS/SDS - foil or solid dielectric closing switch, FOS - foil opening switch, Subscripts: t - transmission line, g - generator, f - foil.](image-url)
cm long or, to suit the Flexy geometry, in a packaged coaxial arrangement. Supplementary experiments will be necessary to check the model for this huge fuse, and to determine the most appropriate closing switch and its synchronisation.

The calculated current at the input to the PEOS for the opening fuse described above however rises to 300 kA in 240 ns, and so fails to meet the 100 ns rise-time specified earlier.

Fig 10.2: Time variation of flux compressor Parameters; $R_g = \text{resistance (to fit experimental data)}$,$L_g, \frac{dl_g}{dt} = \text{inductance and its rate-of-change with time (calculated)}$

10.2.2 Two Stages of Fuse Conditioning

Fig 10.4 shows the equivalent circuit for the proposed Flexy driven output conditioner with two stages of fuse conditioning followed by a transformer to feed the PEOS. The first fuse (FOS 1 in Fig 10.4, has the same dimensions as that used in the F3 Flexy / fuse experiment. Fuse FOS 2, optimised to give the best performance, is 58 cm long and 68 cm wide.

Switch $S_1$ is closed when FOS 1 (modelled by the FEMF curve in Fig 10.3) approaches its restrike electric field, having dissipated approximately a third of the energy needed to vaporize it (Chapter 8 Section 8.10.3). It was expected that current would then be transferred into the second fuse sufficiently rapidly to produce a significant increase in the rate of energy input as it passes through its melt and vapour phases. It can thus be modelled by the curve derived for the capacitor bank driven fuses (CBEMF in Fig 10.3).

It was further anticipated that the voltage developed across FOS 2 as it approaches its restrike electric field of between 3 and 4 kV / cm and $S_2$ is closed, together with a
transformer step-up ratio of 2:1, will produce sufficient voltage across the PEOS input inductance to achieve the required time rate of current increase.

![Diagram](image)

**Fig. 10.5:** Arrangement of proposed conditioning scheme. $S_0$ – start switch, $S_{C1}$ – crowbar switch, FOS – foil opening switch, FCS – foil closing switch, SDS solid dielectric closing switch. Subscripts: $t$ – transmission line, $g$ – generator, $f$ – foil, $p,s$ – transformer primary and secondary.

The calculated current characteristics at the input to the PEOS of a current rising in 300 ns to 300kA was however again inadequate. This poor result and the complicated nature of the circuit with two fuses and two closing switches to be synchronised, plus a transformer, ruled it out as a viable contender to feed the PEOS.

### 10.2.3 Discussion of the results

The capacitor bank and Flexy driven fuse experiments provided some quantitative results to support the discussion in Chapter 8 that the effectiveness of a fuse as an opening switch depends upon it moving rapidly though its melt and vapour phases, which in turn depends upon the rate of energy input provided by the current source. With the laboratory capacitor source, this happens sufficiently rapidly to produce a very effective opening and closing switching characteristic. When the current source is however Flexy, with its long run-time, the transition is slower and the fuse restrikes before it is fully vaporised. Consequently the fuse opening and closing characteristics are less effective. The dynamic resistance ratio when the energy dissipated at restrike is reached is also reduced as shown in Fig 10.3, and much longer fuses are required to satisfy optimum sharpening conditions as described in the introduction. These are clearly the reasons for the disappointing performance of the proposed fuse sharpening circuits, and although some improvement to the rise-times would be gained by arranging the fuses in the
advanced layout described in Chapter 8 it is nevertheless doubtful that the target could be achieved.

10.2.3.1 Crowbar switch technique

Another way of improving the performance of a Flexy driven circuit with a single fuse and a transformer feeding a PEOS, is to use a crowbar to effectively disconnect the Flexy from its conditioning circuit when the fuse voltage reaches its maximum. The circuit, and details of the fuse dimensions selected to illustrate this technique, together with predictions of its performance, are presented in Chapter 8. Calculations show that the 100 ns current rise-time needed at the input of the PEOS can be achieved. Further investigation is needed however to determine a satisfactory way of crowbarring the circuit to demonstrate the technique. The crowbar switch $S_{c2}$ shown dotted in Fig 10.4 would also enhance the performance in a similar manner.

10.2.3.2 Fuse driven by a fast generator coupled to Flexy.

A conditioning approach that avoids the problem of low rates of energy input to the fuse is to couple Flexy by means of a large dynamic transformer (referred to in Chapter 2) for example, to a flux compressor with a shorter run-time (less than 20 $\mu$s), with its output conditioned by a fuse stage and a PEOS. Such an arrangement would also be preferable for higher energy and performance systems, in which a very large EFF and its associated explosive assembly would be needed in the conditioning circuit proposed below.

10.3 Flexy with an explosively formed fuse (EFF) and a PEOS

Fig 10.5 shows the equivalent circuit for a proposed future experiment. Since it is regarded as the most promising for future research, when it is necessary to produce mega volts across a resistive load, it is therefore described in more detail.

Fig 10.6 shows the time variation of the dynamic resistance (curve labelled LUT) of the EFF obtained from the experimental results in Chapter 8, and also that obtained from a similar experiment conducted at LANL [Goforth et al 1985]. Both experiments were in plane geometry, with 5 extrusion grooves, and used the same standard fuse forming groove dimensions. The similarity of the two curves is clear, although their shape and maximum values differ.
Fig. 10.5: Equivalent circuit of Flexy and its conditioning components. $S_0$ — start switch, $S_c$ — crowbar switch, FCS — foil closing switch, SDS — solid dielectric closing switch. 

t — transmission line, g — generator.

The curve used as a model in the calculations was chosen conservatively and is shown in the figure; The maximum 6 kV hold-off voltage per groove and 0.6 kJ energy dissipation / cm along it used for design purposes were also below those obtainable. A simple expression to account for the compression of the flux in the grooves was also included. The four-stage OD model for fast input pulses described in Chapter 7 was used to calculate the output characteristics of the PEOS. The value of the plasma density $n_{eo}$ needed in the calculation for the PEOS to open at a threshold current $I_{op}$ of 300 kA was obtained from eqn 9.1 for an emission-limited conduction phase.

The sixty extruded 40 cm long grooves needed to produce the voltage across the EFF should be of standard groove design (shown in Figs 8.15 and 8.16), and have a total inductance of 30 nH. The explosive assembly needed to force the thick fuse into the grooves, should be initiated by an exploding-foil-mesh (Chapter 3) or other explosive plane-wave shaper for an EFF design in plane geometry, or by a line initiator using...
explosive pellets for a cylindrical design (Chapter 8). An explosive switch (S₂) should complete the circuit to the PEOS. Other components values are given in Fig 10.5.

10.3.1 Predicted Results

Predicted waveforms for the circuit of Fig 10.5 are presented in Fig 10.7. I₁ is the Flexy output current, I₂ is the current transferred to the PEOS, and I₃ is the current switched into the load, which is a short circuit to a DC current, but has an effective resistance due to the skin effect produced by the fast rising current pulse. The 340 kV peak voltage appearing across the EFF is also shown, and the change in I₁ due to flux compression into the fuse forming grooves of the EFF is evident when the current reaches about 5.2 MA. The calculations show that an input current to the PEOS rising in 100 ns to 333 kA can be obtained, and that the PEOS would open at this threshold value in 10 ns. These characteristics clearly satisfy the central aim discussed earlier.

![Graph](image)

**Fig. 10.7:** Predicted waveforms for Flexy, the EFF, and the PEOS.

Inserting the above input conditions into eqn 9.4 shows that the opening process is just within the hydro limited boundary, and consequently that using the fast model for the PEOS is not fully justified. An output rise-time a little longer than 10 ns may therefore be
expected. Emission-limited conditions and a 10 ns output rise-times would be achieved in practice however by setting the plasma pre-delay to bring $n_{op}$ into coincidence with $I_{op}$ at a threshold current level of 300 kA, which from Fig 10.7 is seen to be reached in about 90 ns. For the PEOS to open for these conditions, it may be necessary to reduce the plasma density by lowering the voltage across the flashboard discharge capacitors. This will extend the lower range of opening currents calculated in Chapter 9 for a PEOS with an emission-limited phase and an input rising in 100 ns. With appropriate plasma conditions, the output rise time would then compare favourably with a previous experiment [Neri et al 1987] using a PEOS with similar dimensions, and plasma source, but driven by a high voltage Marx bank. This produced a current rising to 700 kA in approximately 60 ns at the input to the PEOS. A voltage of 4.25 MV was obtained across a diode load, so that a voltage exceeding 1 MV across such a load is anticipated from the proposed experiment.

10.4 Conclusions

Proposed conditioning circuits for Flexy using fuse current sharpening stages to provide an input current to the PEOS rising to 300 kA in 100 ns, were presented, together with their calculated output characteristics. Reasons why the fuses were unable to produce sufficient voltage and thereby input rise-time to the PEOS were given. Ways of improving their performance were suggested including a novel crowbarring technique and by coupling Flexy to a fast flux compression generator.

A circuit with an EFF sharpening stage that was able to provide the characteristics required at the input to the PEOS with a consequent output current rising in 10 ns was also presented. Details for a future experiment, and predicted waveforms for this arrangement were given. Comparison of the results with results produced elsewhere indicated that voltages exceeding 1 MV might be anticipated across a diode load.
CONCLUSIONS OF THE RESEARCH UNDERTAKEN

11.1 Pulsed-power facilities

The pulsed-power facilities needed for the experimentation described in this thesis were constructed and assembled at Loughborough, both in the laboratory and in steel containers that were subsequently transported and deployed on the explosive firing range at the military Proof and Experimental Establishment, West Lavington, Wiltshire. These facilities, together with the variety of diagnostic and calibration techniques adopted for measuring high currents and voltages (see Chapter 4), enabled numerous experimental results to be obtained with a guaranteed accuracy of a few percent.

11.2 On the firing range

The decision to house the capacitor bank and the control and recording techniques in steel containers, and to transport them to a suitable firing site (see Chapter 4 and 5) was a good one. This 200 kJ mobile facility, able to deliver up to 600 kA into low inductance explosive devices, and also capable of priming, firing and recording the output of flux compressors containing up to 15 kg of explosive and several MJs of output energy is probably unique in the UK, where all known pulsed power facilities on suitable UK firing ranges are static. The purpose built mobile facility was particularly suitable for a low budget programme, since the firings (experiments) were conducted with a minimum of staff, access was virtually unlimited, and overheads were kept low. The experimental procedures adopted, and the tests and calibrations carried out prior to the firing (see Chapter 5) ensured that accurate results were obtained from complex single shot firings with a high probability of success.

Several low energy small diameter flux compressors (Minigens) were successfully fired during the commissioning period of the firing system (see Chapter 5). The results served in particular to illustrate the energy loss mechanism described as 2π-clocking or turn skipping (described in Chapter 6) that occurred occasionally due to a misalignment between the coil and armature, and which is difficult to avoid in small diameter hand wound generators. Voltage breakdown between the coil and armature was also experienced in a number of well aligned machined but uninsulated generators. This
emphasised the need to calculate the breakdown voltage at the design stage, and to fill the space between the coil and armature with a high voltage breakdown gas.

Reproductions of the high gain 1 MJ flux compressor with a machined coil and tilted turns in the high current section, designed and used at the Atomic Weapons Research Establishment in the 1950s, were intended to be used for the high energy experiments. Its output energy when fired was however shown to be well below the design value, due to a failure to reproduce the precise contouring and insulation requirements in the tilted turn region of the coil (see Chapter 5). The consequent need for further investigations, coupled with the time involved in its manufacture, ruled it out as an energy source for the present experimentation. These events highlighted some of the problems likely to be encountered with more complex generator designs and also the requirement to replace it with a simple hand wound source that was very much easier to manufacture.

11.3 Flexy flux compressor

The studies of the explosively driven helical flux compressor described in Chapter 6 led to a simple but efficient computer model for design purposes. This simple programme can be used for either the performance prediction of existing generators or the design of future generators with high output energy. No complex manufacturing techniques were required to construct the eight section hand-wound coil with turn splitting between sections for the 1 MJ flux compressor (called Flexy). Satisfactory results were obtained with an unmachined but annealed aluminium armature filled with plastic explosive. After solving an initial coil winding containment problem in the generator, and including various non-ohmic resistances in the numerical model, the simulated results were close to those obtained experimentally. These inexpensive and easily produced generators (the first generator was designed, manufactured and fired in less than three months, and thereafter a Flexy could be produced every few weeks) gave consistent outputs that were adequate to power the high energy and current experiments conducted on the firing range.

11.4 Fuse experiments on the capacitor bank

The performance of a thin metal foil (termed a fuse) as an open circuiting switch in high current pulse circuits was demonstrated by experiments using a capacitor bank energy source. The bank provided a rate of energy input to the fuse during its vaporizing phase beyond the burst condition (see Chapter 8) of between 2 and 5 MJ kgm$^{-1}$μs$^{-1}$ into 17 μm
and 24.5 μm thick copper foil fuses surrounded by a glass bead / fibre glass or polythene lined Mylar medium. The results showed that under these energy rate conditions the fuse resistance after burst increased rapidly by about a hundredfold in a fraction of a microsecond and then increased more slowly by a further factor of approximately two as the fuse become fully vaporised. The resulting sharp voltage (approaching 50 kV) resulted in an effective opening of the fuse circuit, with electric fields exceeding 3kV cm⁻¹ being sustained across the fuse.

Empirical curves derived from the experimental results for the increase of resistance with specific energy deposited in the fuse, were used to develop a computer program that accurately predicted the fuse performance over a wide range of circuit conditions and fuse dimensions. The program also predicted that more than double the voltage quoted above could be obtained by optimising the fuse dimensions. Although the curves for the two fuses described above were of similar shape, they demonstrated a characteristic of fuses during the vapour phase, with the thinner fuse needing much less energy for the same increase in resistance ratio.

Current sharpening using fuse opening and closing switches was also demonstrated. An untriggered fast acting exploding-foil closing switch design connected in series with the fuse avoided the problems of triggering. The switch operated automatically to transfer current into a low-inductance load when the optimum circuit opening conditions were reached, with the sharp pulse of the opening fuse voltage being close to its maximum value. A computer program for the single stage of fuse opening followed by a closing switch to transfer current into a load closely simulated the experimental results.

11.5 Plasma erosion switch

A plasma erosion opening switch (PEOS) was designed and constructed to provide a final stage of pulse conditioning. Although no complete and universally accepted explanation of the complex switching action has appeared, a survey of relevant published experimental evidence and the conclusions drawn (described in Chapter 9), confirmed that the PEOS output rise-time depended on both the input threshold current and its rise-time. The experimental results also showed that for input currents rising to threshold levels of a few hundred kA in about 100 ns, the plasma density in the switch when it opens is proportional to the switch cathode current density, and the opening time approaches 10 ns. Under these conditions the switch input conduction phase is described as being emission-limited (see Chapter 9). Also, when the product of the input rise-time
squared and the input opening threshold current density exceeds a limiting value, the plasma density needed to open the switch increases. It then becomes proportional to the product of the cathode current density times the input rise-time to the opening threshold squared, and the input conduction phase is described as being hydro-limited (see Chapter 9). Under these much greater plasma density conditions, the output current rise-times are also increased.

The information yielded from the survey enabled, in particular, the range of plasma densities provided by the selected plasma source to be estimated. This, together with some experiments conducted in the laboratory to observe some plasma characteristics, and to determine the best component layout and inter-wiring details, enabled the switch to be designed and built.

The plasma sources used for the PEOS provided a range of plasma densities. When initiated at the correct time relative to the input threshold currents, the opening threshold current level provided by both the capacitor bank with automatic fuse sharpening (rising in about 300 ns) and that anticipated from the conditioned Flexy flux compressor (rising in about 100 ns) will open the switch.

When the PEOS was tested on the capacitor bank, it opened in less than 100 ns at an input current level of 150 kA. Although it was hydro-limited its output current profile resembled that expected from a emission-limited opening process, and a computer simulation of the PEOS characteristics produced a close fit to the experimental results. Additional calculations showed that, by adopting the advanced fuse arrangement described in Chapter 8, and optimising the circuit and coupling inductance to the PEOS, the input current would rise to the threshold level in 100 ns, the input conduction would be emission-limited, and the switch opening time would approach 10 ns.

11.6 Flexy conditioning with fuses

The results from the Flexy flux compression driven fuse experiments highlighted the importance of providing a sufficient rate of input energy during the vapour phase. With the fuse dimensions arranged for burst to occur at the most beneficial time during the time scale of the current waveform, the input energy rates to the fuse during the vapour phase were only between 0.4 and 0.8 MJ kgm\(^{-1}\) \(\mu\)s. This is approximately six times lower than that provided in experiments using the capacitor bank. Under these conditions, the fuse takes a longer time to pass through its vapour phase, thus allowing more time for
differences occurring in the thermal and hydrodynamic behaviour to influence the characteristics of the expanding vapour. As a result, restrike occurred across the fuses at an electric field of 1.2 kV cm\(^{-1}\), when only about a third of the energy needed to fully vaporize it (and thereby to increase its resistance) had been dissipated. The effectiveness of fuses to open a circuit (and to automatically transfer current to a load) is reduced under these conditions. For comparable fuse voltage results, fuses provided with energy input rates of between 0.4 and 0.8 MJ km\(^{-1}\) \(\mu\)s\(^{-1}\) during the vapour phase, need to be approximately three times longer than those with energy rates six times higher.

Numerical simulations of the results using the Flexy flux compressor parameters, together with the fuse model, agreed with the fuse voltage up to restrike. Calculations of the performance of two proposed Flexy-driven first stage fuse conditioning circuits led to the conclusion that the low energy input rates provided by Flexy were unlikely to achieve the PEOS input characteristics needed to satisfy the central aim of the thesis. However, performance predictions for a fuse sharpening circuit (see Chapter 8) that included a crowbar, to effectively disconnect Flexy from the circuit when the fuse voltage reaches its maximum value, showed that the negative rate of change of current in the primary of its output transformer was increased to \(-3\) TA s\(^{-1}\). This would amply achieve the rise time needed by the PEOS connected in the secondary circuit and satisfy the central aim. Further work is however needed to confirm that the crowbar can be successfully implemented and triggered.

Current sharpening with simple automatically operated opening and closing fuse switches is easy to implement. It was shown to be very effective at the input energy rates provided by the capacitor bank, and for these reasons it is regarded as the preferred choice. A conditioning approach that would avoid the problem of low energy rates in the fuse is to feed the Flexy output current into a flux compressor with a run-time of less than 20 \(\mu\)s, and use its faster rising output current to drive the first stage of fuse sharpening. This approach is certainly worth considering in future research.

11.7 Flexy conditioning with an EFF and a PEOS

The explosively formed fuse (EFF) described in Chapter 8 overcomes the problems described above that occur when the time to vaporize a simple fuse is increased. The complex action of an EFF driven by the Flexy flux compressor can be regarded loosely as equivalent to that of a thin fuse of an appropriate size to effectively open the circuit, formed in the conditioning circuit at a chosen time during the current waveform.
Experiments to demonstrate the EFF characteristics were conducted, and a curve for its resistance variation with time, values for the electric field (about twice that sustained by fuses, due probably to a reignition inhibiting effect of the high-pressure explosive environment) and the energy dissipation in the extrusion grooves, were all derived. Conservative values of these were included in a computer simulation of the overall characteristics of a Flexy driven EFF conditioning circuit. Calculations for a proposed circuit design for this combination, together with a final stage of PEOS sharpening, showed that a current output satisfying the central aim with a rise time approaching 10 ns could be obtained.
RELEVANT PAPERS PRODUCED DURING THE COURSE OF THE WORK

Stewardson H R, Smith I R, Vadher V V, Senior P and Butterfield P G 1991 Pulsed-power technology and experimentation at Loughborough University of Technology, 8th IEEE Int Pulsed-Power Conf, (San Diego, USA) pp 411-415


Senior P, Smith I R and Stewardson H R 1996 Pulsed magnetohydrodynamic generators IEE Coll on Pulsed Power '96, (Savoy Place, London) pp 16/1-16/3


References


Antoni B and Nazet C 1975 Experimental and theoretical study of helical explosive electrical current generators with magnetic field compression Report CEA-R-4662 (Commission of Atomic Energy, France)


Dobratz B M 1974 Lawrence Livermore National Laboratory Report, No UCRL-51319


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Hawkins D: 1946 Manhattan district history, project Y, the Los Alamos project, (vol 1) Inception until 1945, Report LAMS-2532 pp141, 143, 194, 201, 228, 232


Kahaner D, Moler C and Nash S 1989 *Numerical Methods and Software* (New York: Prentice Hall)

Keilhacker M 1960 The mechanism of explosion-like vaporisation of copper wires though very intensive current surges and the behaviour of copper under the high pressures and temperatures thus set up *Zeitschrift fur Angewandte Physik* 12_(3) pp 49-59


Kleen W 1931 The passage of electricity though fine wires *Ann Physik*, 11 pp 579-605

Knoepfel H 1970 *Pulsed High Magnetic Fields* (Amsterdam: North-Holland)


Miller J R and Stewardson H R 1965 'Hypervelocities from imploding foils' UK Atomic Weapons Research Establishment, Foulness division note 11-65

Miura N and Chikazumi S 1979 Computer simulation of Megagauss field generation by electromagnetic flux compression *Japan. J. Appl. Phys.*18 553-64


Nairne E 1774 Electrical experiments *Phil Trans Roy Soc.*, (London) 64 pp 79-89


Novac B 1989 Production of ultrahigh magnetic fields *PhD Thesis* University of Bucharest


Novac B, Zambreanu V and Zoita V 1991 A pulsed power source for plasma focus single-shot experiments *Proc. 8th IEEE Pulsed Power Conf.* San Diego, CA pp 434-7

Ottenger P F, Goldstein S A and Meyer R A August1984, Theoretical modelling of the plasma erosion opening switch for inductive storage applications *J. Appl. Phys.* 56 (3) pp774-84


Pavlovskii A I: Powerful explosive pulsed sources 1991 *Proc 8 th IEEE Pulsed Power conf.* (San Diego, USA) pp1-14


Renk T J and Weber B V 1984, Plasma production from flashboards for opening switch applications IEEE Int Conf Plasma Science abstracts (St Louis M O) p 102

Renk T J April 1989, Flashboards as a plasma source for plasma opening switch applications J. Appl. Phys. 65 (7)


Shearer J W et al 1968 Explosive-driven magnetic-field compression generators J. Appl. Phys. 39 2102-16


Tucker T J and P Toth, 1975, EBW I: A computer code for the prediction of the behaviour of electrical circuits SAND-75-0041


Welsby V G 1964 The Theory and Design of Inductance Couils (London: MacDonald)

Appendix A

BASIC LIMITATIONS OF HELICAL FLUX COMPRESSION GENERATORS

Although the analysis below initially follows that of [Pavlovskii et al 1980a], in assuming an ideal generator in which all flux loss mechanisms are neglected, the later stages are novel [Novac et al 1995]. In this situation, the ratio of the maximum final magnetic energy to the initial magnetic energy $k = W_m/W_0$ can be written as $k = L_0/L_1$, where $L_0$ and $L_1$ are the corresponding initial and load inductances.

The maximum current that can be supported by the generator is $I_m = 2\pi r_c i_m$, where $r_c$ is the coil radius and $i_m$ the corresponding maximum linear current density. The global efficiency of the generator may be written as $\eta = W_m/Q$, where $W_m = L_1 I_m^2/2$ and $Q = \pi r_{ex}^2 t_m Y$ is the chemical energy stored in an explosive charge of radius $r_{ex}$. The quantity $Y$ is termed the intensity of the explosive and includes the initial mass density $\rho_0$, the detonation velocity $D$, and the characteristic heat of detonation $\Delta H_{ex}, (Y = \rho_0 D \Delta H_{ex})$ [Novac et al 1989]. If the operational time of the generator is $t_m$, then

$$\eta = \frac{2\pi L_1 I_m^2}{t_m Y} \left[ \frac{r_c}{r_{ex}} \right]^2$$

The basic limitation to the energy multiplication of a generator is given by the maximum induced voltage that can be sustained in the generator without leading to an electrical break-down. If a linear rate-of-change of current is assumed throughout the time $t_m$, then $dI/dt = I_m/t_m$ and the initial and final voltages developed inside the generator are $V_0 = L_0 I_m/t_m$ and $V_m = L_1 I_m/t_m$. The corresponding interior electric fields are $E_0 = V_0/(r_c - r_a)$ and $E_m = V_m(r_c - r_a)$, where $r_a$ is the outer radius of the armature. If $r_a$ is considered to be close to $r_{ex}$, then $k = E_0/E_m$ and the activity of the generator $A = \eta k$ is given by

$$A = E_0 i_m R(R - 1)/Y$$

where $R = r_c/r_a$ is the maximum expansion ratio of the generator armature. A high experimentally obtained activity clearly denotes a good initial design of the generator.
The presence of the explosive intensity term in the expression for \( \Lambda \) shows that a better explosive will produce a lower value for the activity, and that to obtain the same activity requires an improved design in which higher electric fields can be sustained.

The examples below illustrate the range of basic parameters reported for a range of practical generators

\[
20 \text{ kVcm}^{-1} < E_0 < 150 \text{ kVcm}^{-1} \quad \text{[Chernyshev et al 1980a, Pavlovskii et al 1980a, Novac et al 1995]}
\]

\[
0.2 \text{ MAcm}^{-1} < i_m < 1 \text{ MAcm}^{-1} \quad \text{[Morin and Vedel 1971, Freeman et al 1994, Novac et al 1995]}
\]

\[
Y(TW/m^2) = \begin{cases} 
37 \text{ nitromethane} \\
88 \text{ composition B} \\
106 \text{ PBX - 9404}
\end{cases} \quad \text{[Dobratz 1974]}
\]

\[
2 < R < 2.5 \quad \text{[Morin and Vedel 1971, Chernyshev et al 1980a, Novac et al 1995]}
\]

and these results together show that the limits for the activity of a variable-pitch helical generator are:

\[
1 < \Lambda < \text{about 40}
\]

It should be noted that for flux compressors with a constant rate-of-change of inductance with time (\( \frac{dL}{dt} \) = constant), as in a single-pitch helical generator, the maximum theoretical value of \( \Lambda \) is unity [Cummings et al 1966].

As the definition of the activity shows (\( \Lambda = \eta k \)), a choice can be made between a high-efficiency, low-energy multiplication generator (a very high-current design) or a low-efficiency, high-energy multiplication generator (a booster). As an example, the best results reported for the first case are \( \Lambda = 13 \) with \( \eta = 30\% \) and \( k = 43 \) [Morin et al 1971] and for the second case \( \Lambda = 40 \) with \( \eta = 4\% \) and \( k = 1000 \) (Chernyshev et al 1980a).
HELICAL LENGTH ALONG THE ARMATURE CONE.

Consider a truncated cone section of the armature with the radii \( r_1 \) and \( r_2 \) and \( r_2 > r_1 \) (see Fig 6.1, intermediate position (iii)). Using cylindrical coordinates \((r, \theta, z)\), a helix on this surface is described by

\[
\begin{align*}
  z &= p\theta/(2\pi) \\
  r &= r_2 - (p\theta \tan \alpha)/(2\pi)
\end{align*}
\]

from which the elemental length

\[
dl = \left[(rd\theta)^2 + (dz)^2 + (dr)^2\right]^{1/2}
\]

can be written as

\[
dl = \left[p \tan \alpha/(2\pi)\right]\left[2\pi r_2/(p \tan \alpha) - 0\right]^2 + \left(1 + \cot^2 \alpha\right)\frac{1}{2}d\theta
\]

The total length \( l_h \) of the helix in this section is

\[
l_h = \int_{\theta_0}^{\theta_1} dl = \int dl = \int_{\theta_0}^{\theta_1} d\theta
\]

where

\[
\theta_0 = 0 \quad \theta_1 = 2\pi(r_2 - r_1)/(p \tan \alpha)
\]

giving

\[
l_h = \frac{1}{2} \left[r_2 \left[1 + \cot^2 \alpha + (2\pi r_2 \cot \alpha / p)^2\right]^{1/2} + r_1 \left[1 + \cot^2 \alpha + (2\pi r_1 \cot \alpha / p)^2\right]^{1/2} + \left(p \tan \alpha/(2\pi)\right)\ln(R_2 / R_1)\right]
\]

where

\[
R_1 = r_1 + \left\{r_1^2 + \left(1 + \cot^2 \alpha\right)(p \tan \alpha/(2\pi))^2\right\}^{1/2}
\]
\[
R_2 = r_2 + \left\{r_2^2 + \left(1 + \cot^2 \alpha\right)(p \tan \alpha/(2\pi))^2\right\}^{1/2}
\]
MARK IX INPUT DATA

The active coil length of 118 mm comprises four sections with lengths 217, 217, 223 and 461 mm (see Fig C.1) but, since the design program accepts only equal-length sections, the input data was specified as five sections of 217 mm length. This will affect the final part of the computed behaviour, as the operational time is reduced by about 3.6 µs and there is a reduction in the corresponding inductance. The diameter/section length ratio exceeds 1.6, so that an error of about 10% can be expected in the inductance values.

The detonation velocity is not specified, and was obtained from the time for the expanding armature to make contact with the coil, considering the radial velocity to be given by $v_{det} \tan \alpha$.

---

**Fig C.1:** The Mark IX generator. The upper half is the actual arrangement (0, 1, 2 and 3 are the armature positions at $t_0$, $t_1$, $t_2$ and $t_3$); the lower half is the model arrangement (0, 1, 2m and 3m are the armature positions at $t_0$, $t_1$, $t_2$ and $t_3$). Here $t_0$ is the crowbar time, $t_1$ is the end of period (1) $t_2$ and $t_3$ are the end times of purely helical phase (the armature cone enters the output ring). $t_2$ and $t_3$ are the end times of period (2).

The Mark IX generator presented in [Fowler et al 1989] has no crowbar, so that the input ring has the same diameter as the coil. Nevertheless a crowbar time was introduced, based on the $dI/dt$ and resistance data, and from this the diameter of the hypothetical crowbar was calculated. Even for the first sections of the stator, some difference between experimental and computed data is therefore to be expected.
Because the model considers only the helical part of the generator, the inductance of the output coaxial ring (26 nH) must be added to the load inductance. The ring inductance was calculated from the cross section of the generator and was used for the first 102 μs (t₂).

For this particular design, the computer code included a coaxial coil, to enable a comparison to be made with the current in the final stage of flux compression (after 102 μs). The coaxial coil has 153 mm active length and 26 mm passive length (because of the output insulator). The 3.9 nH inductance of the passive length remained added to the load inductance until the end of compression. The calculation ends when the contact point reaches the output end of the helical coil.
NONLINEAR DIFFUSION AND THE MECHANISM OF VOLTAGE BREAKDOWN

When a volume \( \Delta V \) is removed from the internal generator volume, either by movement or by an unexpected jump of the contact point, the energy contained therein is lost. If the reduction in inductance is \( \Delta L \), then

\[
\frac{1}{2} I^2 \Delta L = \frac{B^2}{2\mu_0} \Delta V
\]

For nonlinear diffusion

\[
\Delta V = 2\Delta l (S^* - S)
\]

where \( \Delta l \) is the cable length removed in time \( \Delta t \) and \( S^* \) and \( S \) are circular areas inside the cable, defined by skin depths \( \delta^* \) and \( \delta \) respectively. The factor of two is introduced to include the effect of the armature.

The velocity of the contact point, approximately

\[
v_{cp} = \frac{2\pi c v_{det}}{p}
\]

can be extremely high (880 mm/s for the first section of the EF-3 generator). The equivalent non-ohmic resistance is

\[
R_{nd} = \frac{\Delta L}{\Delta t} = 2B^2 v_{cp} \frac{S^* - S}{\mu_0 l^2}
\]

If the magnetic flux density is expressed as

\[
B = \frac{\mu_0 I}{\pi n \Phi}
\]

and \( \cos \beta \) approximately by \( p/(2\pi c) \) then, for \( n \) contact points of the armature with the \( n \) cables in a section, the non-ohmic diffusion term of eqn (6.13) is obtained.

In the sinusoidal model of Fig 6.6 for armature machining defects, \( \lambda \) is the wavelength of the defect. The corresponding amplitude \( G \) is the resulting amplification of an initial amplitude \( g \) by the armature expansion.
For each voltage breakdown between the coil and a peak on the sinusoidal armature surface, the magnetic energy in the volume between the plasma and the breakdown point is lost. The new effective amplitude is now \( A = G + \frac{V}{E} \).

A volume \( \Delta V \), approximately by \( \pi \lambda A(2r_c - A)\cos\alpha \), is lost during each time interval \( \Delta t = \frac{\lambda}{v_{det}} \). If the magnetic field is regarded as being produced by a copper sheet of width \( p \), then, as an approximation to the cable lay-out,

\[
B = \frac{\mu_0 l}{p}
\]

and, with the aid of the results presented above, the result given in eqn (6.14) can be obtained.
WINDING INDUCTANCE OF HELICAL TRANSFORMERS

Fig E.1 shows a view of a typical helical transformer. If initially a transformer with a 2:1 turn ratio is considered then, to calculate the inductance, the single-turn primary winding is considered as a cylinder, and the secondary winding is decomposed into two further cylinders having radii equal to the minimum radii of the two winding turns. The z axis field distribution $H_z(r,z)$ inside a cylinder of length $l$, radius $a$, and carrying a circular current $I$ is [Miura et al 1979].

$$H_z(r,z) = \frac{1}{4\pi l} \int \left( \frac{1 - K \cos \psi}{1 + K^2 - 2K \cos \psi} \right)$$

$$\times \left[ \frac{1 - Z'}{\left[ y^2(1+K^2-2K\cos\psi)+(1-Z')^2/4 \right]^{1/2}} \right. $$

$$+ \left. \frac{1 + Z'}{\left[ y^2(1+K^2-2K\cos\psi)+(1+Z')^2/4 \right]^{1/2}} \right] d\psi $$(E 1)

where $y = a/l$, $K = r/a$ and $Z' = Z(l/2)$. The inductance of the primary winding is obtained by integrating eqn E1 over the surface of the cylinder

$$L_p = \frac{H_0}{I} \oint H_z(r,0) 2\pi r dr $$ (E 2)

Relatively simple approximations for $H_z(r,z)$ enable this equation to be readily integrated [Miura et al 1979], but in the present study a numerical solution was used. The mutual inductance $M_{12}$ between two concentric cylinders is calculated from

$$M_{12} = \frac{\mu_0}{l_2} \int H_z'(r,0) 2\pi r dr $$ (E 3)
where $a_1$ is the radius of the inner cylinder and $I_2$ and $H_z'(r,0)$ are respectively the current in the outer cylinder and the magnetic field that it produces.

In general, the inductance of a multi-turn secondary winding represented by $n$ concentric cylinders is

$$L_s = \sum_{i=1}^{n} L_i + \sum_{i,j=1}^{n} M_{ij} \quad (E \ 4)$$

where $L_i$ is the inductance of the $i$th of the secondary cylinders and $M_{ij}$ is the mutual inductance between the $i$ and $j$ cylinders. The primary to secondary mutual inductance is

$$M_{ps} = \sum_{i=1}^{n} M_{pi} \quad (E \ 5)$$

where $M_{pi}$ is the mutual inductance between the primary and the $i$th of the $n$ secondary cylinders.