

# Practical Considerations in Helical Flux-Compression Generator Design

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## Abstract

In recent years, the detailed mathematical modelling and design of helical flux compression generators has been the subject of many technical publications. A number of significant and important issues that arise during the design are not however always fully appreciated, and the paper aims to remedy this by addressing these issues and discussing how their various effects can be identified.

## 1. Introduction

The necessity for the adoption of a systematic design procedure for flux compression generators (FCGs) is becoming increasingly evident, as many modern applications for explosive-driven power supplies require design teams with a wide range of skills. However, as a consequence of the vast amount of information published in the last three decades, it is extremely difficult for anyone entering this activity to obtain an overview of all that is now involved.

This paper presents techniques that are useful in the design of an extremely light and compact explosive power source, and highlights some common errors

that can occur in their numerical modelling. Novel experimental techniques are proposed that enable the major design parameters to be determined before beginning the main design process.

## 2. Choice of Appropriate Explosive

In practice, FCG designers are often required to make a choice between different explosive formulations and it is therefore important to know which property or properties of an explosive are important for use in the generator.

The total magnetic energy in the load  $W$  is related to the total chemical energy in the explosive charge  $Q$  by  $W = \eta Q$ , where  $\eta$  is the global efficiency of the generator. In addition,  $Q = M_{ex} \Delta H_{det}$  where  $M_{ex}$  is the explosive mass and  $\Delta H_{det}$  is the specific heat of detonation. The explosive mass  $M_{ex}$  is given by the product of the initial explosive charge density  $\rho$  and the volume of the explosive, which is the product of the cross section of the explosive charge  $A$  (assumed constant for now) and its length. In turn, the length of the explosive charge is given by the product of the detonation velocity  $D$  (also assumed constant) and the time of burning  $t_f$ , which is approximately the compression time of the generator.

The above discussion shows that the energy finally stored in the load can be expressed as

$$Q = ADt_f \rho \Delta H_{det} \quad (1)$$

and that it is therefore proportional to

$$Y = \rho D \Delta H_{det} \quad (2)$$

where  $Y$  is termed the explosive intensity ( $W/m^2$ ) and combines the properties of the explosive charge that influence the FCG performance. The best explosive charge is therefore one that maximises  $Y$ , and the values of  $Y$  for a number of well-known explosives are recorded in Table 1. An illustration of the benefits that can be obtained by changing to a different explosive is given in [1].

Table 1. Characteristic intensity of some familiar explosives (derived from [2]).

Explosive formulation	Intensity $Y$ (TW/m <sup>2</sup> )
Nitromethane	37
Composition B	88
PBX-9404	106

### 3. Minimisation of the Explosive Charge and the Generator Weight

Once the required explosive properties have been established, it becomes important to attempt to maximise the energy efficiency of the explosive charge. Increasingly however many modern applications also face the designer with the problem of coping with an upper limit to the explosive charge. This clearly is not an easy task, as it is often accompanied by the necessity of minimising the mass of the generator.

One possible solution to these apparently conflicting demands is presented below, but before doing so a brief outline is necessary of a simple way of calculating the kinetic energy of the armature under the exploding loading.

#### 3.1. Gurney Model

The Gurney model was developed during World War II and it represents the earliest design tool for dealing with the first stage of energy transformation (from chemical to kinetic) that occurs in a FCG. It is based on two main hypotheses:

- (i) Only part of the chemical energy available per unit mass of the explosive charge  $\Delta H_{det}$  is transformed into kinetic energy. This part  $E$  is termed the Gurney specific energy and for most of the explosives used in FCGs it is about 70 % of  $\Delta H_{det}$  [3].
- (ii) The velocity distribution within the detonation products is assumed to be linear, a hypothesis that has subsequently received strong support from hydrodynamic modelling [3].

Calculation of the final kinetic energy using the second hypothesis is not difficult. Thus if  $M$  is the armature mass per unit length and  $r_0$  is the initial charge radius, then the gas velocity at a radius  $r$  within the detonation products is

$$v(r) = V \frac{r}{r_0} \quad (3)$$

where  $V$  is the final armature velocity.

Although in general both impulse and energy conservation considerations are needed, for most FCGs, including the helical arrangement, the only equation to be solved is that for energy conservation

$$CE = \frac{MV^2}{2} + W_k \quad (4)$$

where  $C = \rho \pi r_0^2$  is the explosive mass per unit length and  $W_k$  is the kinetic energy of the detonation products per unit length. In cylindrical coordinates  $(r, \theta, z)$ ,  $W_k$  is readily calculated as

$$\begin{aligned} W_k &= \int \frac{mv^2}{2} dS = \frac{1}{2} \rho \int_0^{r_0} \int_0^{2\pi} v(r)^2 r dr d\theta \\ &= \frac{1}{2} \rho \frac{V^2}{r_0^2} \int_0^{r_0} \int_0^{2\pi} r^3 dr d\theta = \frac{CV^2}{4}. \end{aligned} \quad (5)$$

Substituting eqn (5) into eqn (4) gives the familiar result [3]

$$\frac{V}{\sqrt{2E}} = \frac{1}{\sqrt{\frac{M}{C} + \frac{1}{2}}}. \quad (6)$$

### 3.2. Minimising the Explosive Charge

One technique that allows the mass of the explosive charge to be reduced is to introduce a metallic bar of radius  $r_1$  ( $r_1 < r_0$ ) along the axis of the charge. If it is assumed that the bar acts as an incompressible medium, then eqn (3) will be replaced by

$$v(r) = V \frac{r - r_1}{r_0 - r_1}. \quad (7)$$

Again based on the conservation of energy, and following the same calculation techniques as before, the result obtained for the armature velocity is

$$\frac{V}{\sqrt{2E}} = \frac{1}{\sqrt{\frac{M}{C} + \frac{3r_0^2 - 2r_0r_1 - r_1^2}{6(r_0^2 - r_1^2)}}} \quad (8)$$

which, when  $r_1 \rightarrow 0$ , becomes identical with eqn (6).

The use of a metallic bar will obviously reduce the amount of explosive used in the generator, but this is achieved at the expense of a reduction in the expansion angle. This is well illustrated by consideration of the MURI-TTU FCG [4], which has a 19.05 mm outer radius 3.175 mm thick aluminium armature and an explosive charge of composition C-4. Using data from [2] and [3] it follows that  $M/C = 0.716$  and  $\sqrt{2E} = 2.68$  km/s and eqn (6) then gives the Gurney estimate for the armature expansion velocity as 2.4 km/s. If  $D = 8.04$  km/s [2] this corresponds to an estimated angle of  $17^\circ$  (we note that this figure does not compare closely with the measured value of  $14.5^\circ$ ). If a bar is introduced with a radius 50 % of that of the charge the explosive mass is reduced by 36 %, with the slightly lower armature velocity predicted by eqn.(8) corresponding to a reduction in the expansion angle of only about 10 %.

Introduction of the metal bar inevitably means that the overall weight of the FCG is substantially increased. If however it is necessary to conserve the total mass, the bar could be replaced by a water-filled, 0.5 mm thick steel cylinder, which will have exactly the same weight as that of the explosive that is displaced.

## 4. Physics of Flux Loss at the Armature/Coil Contact Point

There is an obvious need for a proven, accurate and detailed numerical code whenever an FCG is being designed *ab initio*, without any similar designs having been produced and verified. Such a code can also be used subsequently to interpret the experimental data, to identify the main loss mechanisms and to improve the design. There is however also a need at the design stage for a much simpler but also very fast and highly

versatile code, not necessarily of extreme accuracy, to perform the tens to hundreds of computer runs that are needed to undertake the thorough examination of the large multi-parameter space that is involved. This is an important process that will avoid the production of a large number of different design investigations or improving the FCG characteristics through a trial and error experimental process.

A very effective fast code is described in a companion paper (A Zero-Dimensional Computer Code for Helical Flux-Compression Generators) . This demonstrates that the major loss factor for most designs reported in the literature appears to be a continuous loss of magnetic flux at the armature/coil contact point, and that this represents an important issue that must concern the designer. Many published papers identify this process as related to the magnetic flux that is diffused and trapped inside the conductors, starting from the original work at the Sandia National Laboratories [5]. The flux is assumed to be lost 'naturally' from the system with the movement of the armature/coil contact point. It is however demonstrated in the Appendix that *this mechanism need not be taken into account in a code* if the inductances are calculated without taking into account the diffusion process, which is common in the numerical codes reported in the literature (the present authors are unaware of any detailed discussion of a technique to calculate FCG inductances taking magnetic diffusion into account). Therefore, an alternative mechanism needs to be proposed for the continuous loss of flux that is observed experimentally.

Pavlovski et al [6] proposed a loss mechanism associated with the finite time necessary for the electrical contact to be established through the rather thick insulation used in high current/energy FCGs. The distance at which this contact is established is a function of the electric field and the electric strength of the insulation, and is used as an adjustable parameter to match experimental and theoretical data.

A proposal from Loughborough [7] has suggested a more general electrical flux loss mechanism related to the electrical discharges that can appear in the FCG, either through the cable insulation or even well ahead of the contact point if the electric field stress is sufficiently large. In the companion paper, values of this stress (termed there the EBS) are obtained by matching predictions from the fast code with design data available in the literature.

The simple technique proposed here enables the quantified value of the electric flux loss to be obtained from specially designed experiments.

Usually the total experimentally obtained resistance of an FCG (including Joule heating,  $2\pi$ -clocking, electric breakdown flux loss, flux pocketing by armature defects, etc) can be obtained from the experimental data by calculating

$$R(t) = -\frac{L(t)\frac{dI}{dt} + \frac{dL}{dt}I(t)}{I(t)} \quad (9)$$

where  $I(t)$  and  $dI/dt$  are obtained from the experiment and  $L(t)$  and  $dL/dt$  are either calculated or taken from preliminary inductance measurements using special purpose expanded armatures.

The proposal requires an FCG to be built in which *all inductive terms are absent*, by constructing the stator coil from paired sets of parallel conductors through which the current flows in opposed directions. A constant-current power source will replace the load, and the measurement of voltage drop at the output will provide data from which the resistance variation can be easily obtained. As the speed of the contact point is constant and known in each constant-pitch section, the data provided will allow the mainly geometric contact point losses to be quantified without any interference from the electric field effects. Use of both normal and non-inductive stator coils will therefore enable the electric loss effects to be much better understood.

## 5. Design Methodology

Before embarking on any discussion of design methodology, it is important to distinguish between the two main possibilities that exist. The first of these requires a *general-purpose* design that can be viewed as a natural enhancement of the initial power supply, which is typically a capacitor bank. The role of the generator is to compress the magnetic flux and energy into a *static load*, often termed a ballast inductor. Only after the generator action is complete is the output conditioning system or the load itself activated. Such a requirement leads to a simple, straightforward and *stable* design.

A design is seen as stable when one or both of the initial current or the load parameters can be changed over a range, without affecting unduly either the current or energy multiplication ratios. The Sandia National Laboratories family of double end-initiated helical generators [8] provides an excellent example of a stable design.

The second possibility is a *high-efficiency* design, in which the generator is in tandem with a particular conditioning system and/or a time-varying inductive and/or a resistive load. The very high rate of energy transfer is achieved only under the specified design input and time varying output characteristics, and the design is therefore *unstable*. An example of this type of design is given in [9], with Fig. 1 showing the arrangement of the very unusual 1 MJ generator.

The adoption of tilted turns seen in Fig. 1 is a costly and complex method of satisfying design Rule

(i) (explained below), which requires the armature expansion angle to exactly match the angle of tilt and does not allow for changes in the explosive charge (as would be the case with any stable design). Towards the end of the generator action, the expansion angle falls due to the high magnetic pressure, and since the required matching angle will not then be achieved for any change from the designed input current, such changes (if made) will lead to severe  $2\pi$ -clocking. However, all the drawbacks associated with this design are balanced by a high-energy gain at very high current levels, and with the very high  $dL/dt$  in the final moments of the generator action ensuring that a fast time-varying inductive load can then be attached and efficiently driven.

### 5.1. Basic Design Rules

The following considerations relate to the design of an all-purpose, stable generator, for which the basic data required are the input and output parameters i.e., the priming energy/current and the energy/current to be delivered to a *constant* load. Preliminary experimentation (presented later) enables the expected flux conservation to be estimated and the initial inductance of the generator to be defined. The final load current indicates the approximate radial dimensions required for the armature, and the coil diameter is often conventionally taken as twice the armature diameter. The final dimensions are of course determined by the availability of good quality materials in the form of aluminium/copper tubing, cables with high electrical breakdown strength, insulation, etc. Except for micro-generators, the coil will be multi-sectioned, for reasons explained later, and wound with cables to minimise any leakage of magnetic flux.

The three basic generator design Rules are [7]

- (i) The magnetic field intensity/linear current density should be maintained below a specific limit to avoid excessive heating and unwanted non-linear diffusion. For copper the figure is approximately 0.34 MA/cm [10].
- (ii) The electric field should be below a specific limit to avoid premature internal breakdown. The limit depends on the specific conditions of the design and a straightforward method for determining it is presented later.
- (iii) The effects of magnetic pressure should be restricted by supplementary inertial mass to avoid excessive movement leading to flux pocketing ( $2\pi$ -clocking).

In condensed form these three rules can be stated as [11]

$$\begin{aligned} \mathbf{H} &= \text{constant}, \\ \mathbf{E} &= \text{constant}, \\ \mathbf{E} \times \mathbf{H} &= \text{constant}, \end{aligned}$$

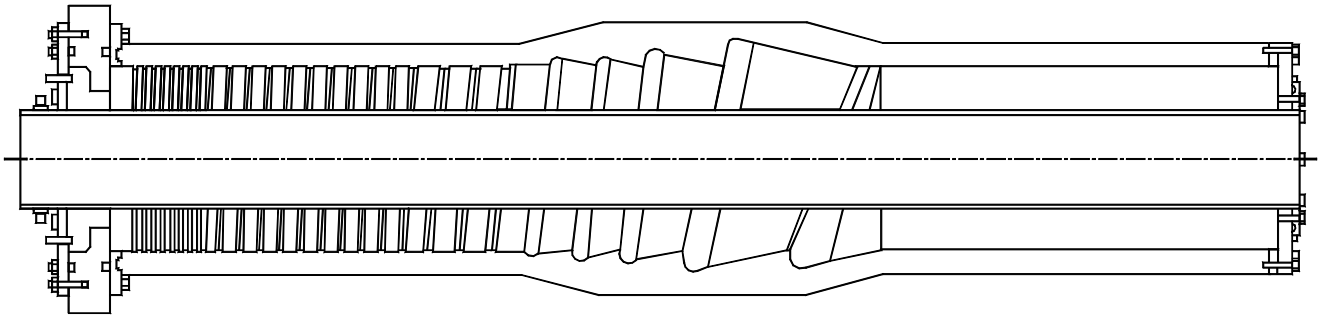


Fig. 1. 1 MJ generator (from [9]).

## 5.2. Coil Design

The best generator design is based on preliminary experimentation using available materials. In its simplest form this will involve building a *long single-pitch generator*, in which the internal voltage ( $IdL/dt$ ) increases continuously with the current (since  $dL/dt$  is constant) An analysis of the experimental  $dI/dt$  signal using, for example, the code presented in the companion paper, will identify the instant at which the internal voltage can no longer be sustained and severe breakdown occurs, reducing the EBS factor described in the companion paper. The final design will certainly need to use an internal voltage lower than the figure thereby obtained.

The aim of the final design is to maintain the inner voltage as near as possible to this maximum internal voltage throughout the compression time, in order to maximise the energy gain. This will normally require a large number of sections of different pitch windings to match the exponentially increasing rate-of-change of current, although this may be limited by the need for a simple and inexpensive generator construction.

Maintaining a flat internal voltage characteristic will not however be possible when the application requires a very high current output into a low inductance load. As a consequence of design Rule (i), the coil sections will need a progressively increasing number of parallel turns, with a corresponding decrease in  $dL/dt$  and the internal voltage.

Examples of both designs (high-energy gain and high current) are given in the companion paper.

## 6. Conclusions

The paper has highlighted some important design aspects of helical FCGs, and has presented novel experimental methods to facilitate the understanding of the electrical losses and to quantify the maximum internal voltage limitation. Space restrictions have however prevented any consideration of such important issues as the crowbar and the coaxial end section or other load attachment solution.

Much of the information contained in the paper is

not original, although this is probably the first time that it has been presented in a coordinated way. The authors hope that this will assist those who are new to the design of FCGs, and who do not have time to study the considerable volume of literature that is devoted to the topic.

The authors were encouraged to consolidate into a paper their thoughts on design and numerical modelling by comments received during a seminar they presented on FCG techniques at Kirtland AFB, Albuquerque, N.M., under the auspices of the WOS programme of EOARD.

## Appendix: Mechanism of Flux

### Loss at the Armature-Coil

### Contact

### Point Due to Magnetic Diffusion

The following demonstration was first given in [12] but unfortunately remains unpublished.

The magnetic definition of the skin depth  $\delta(t)$  is [10]

$$B(0,t)\delta(t) = \int_0^{\infty} B(x,t)dx$$

where  $B(0,t)$  is the flux density at the conductor surface. For exponentially varying fields and currents as generated during flux compression, the skin depth can be approximated as [10]

$$\delta(t) = \sqrt{\frac{1}{\mu_0\sigma} \frac{I}{dI/dt}}$$

where  $\mu_0$  is the magnetic permeability and  $d\sigma$  is the conductor electrical conductivity.

For simplicity, consideration is given to the simplest flux compression generator, having the form of a parallel transmission plate with a constant width  $w$  and a plate separation  $s$  along its length. The short circuit advances toward the load end with a constant detonation velocity  $D$ , reducing the initial generator length  $z_0$  according to  $z(t) = z_0 - Dt$ . At time  $t$ , the remaining inductance can be approximated

by  $L(t) = \mu_0 s z(t)/w$ , where the missing correction factor [10] is regarded as not relevant in this analysis.

The ohmic resistance of the generator is given by  $R(t) = 2z(t)/[\sigma\delta(t)w]$ , where the factor 2 is due to the presence of the two plates. The fundamental circuit equation for the generator is [10]:

$$\frac{dL}{dt}I + L\frac{dI}{dt} + RI = 0$$

where the time variation of the inductance is given by  $dL/dt = -\mu_0 s D/w$ . This is the *normal procedure* adopted in the numerical modelling of flux-compression generators, where *the inductance is calculated without taking into account any diffusion of the magnetic field into the generator conductors.*

To calculate the generator inductance  $L^*$  taking into account the skin depth we write  $L^*(t) = \mu_0[s + 2\delta(t)]z(t)/w = L(t) + 2\mu_0\delta(t)z(t)/w$ . Differentiating this equation yields  $dL^*/dt = dL/dt - 2\mu_0\delta(t)D/w$ , leading to the following circuit equation

$$\frac{dL^*}{dt}I + L^*\frac{dI}{dt} = \frac{dL}{dt}I + L\frac{dI}{dt} - \frac{2\mu_0\delta(t)DI}{w} + \frac{2\mu_0\delta(t)z(t)}{w}\frac{dI}{dt}.$$

Introducing the skin depth to replace the  $dI/dt$  in the last term on the right-hand side of this equation gives

$$\frac{dL^*}{dt}I + L^*\frac{dI}{dt} + R^*I = \frac{dL}{dt}I + L\frac{dI}{dt} + RI$$

which demonstrates the complete equivalence of the two methods outlined. However  $R^*(t) = -2\mu_0\delta(t)D/w$  is not now an ohmic resistance but instead is related to the *magnetic flux loss at the contact point due to diffusion of the field into the conductors*, i.e.

$$R^* = \frac{d}{dt}(L^* - L).$$

In conclusion, there are two very different ways to take into account the various parameters when modelling an FCG

- i) calculate the inductance without taking into account magnetic diffusion, in which case the equivalent resistive term only includes the Joule energy deposited in the skin depth and other loss phenomena described in [7] such as  $2\pi$ -clocking and electrical breakdown
- ii) calculate the inductance taking into account diffusion, in which case the "resistive" term now contains the magnetic flux loss at the contact point due to the diffused field.

The second of these ways is obviously far more difficult than the first. However, if the first way is

adopted, any continuous magnetic flux losses at the contact points due to the diffused magnetic field are automatically taken into account. Therefore, when calculating the overall resistance, the term relating to the rate of energy removal from the flux loss-layer is absent.

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