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NUMERICAL STUDIES OF HELICAL CMF GENERATORS*

J. R. Freeman, J. M. McGlaun, S. L. Thompson, and E. C. Gnare
 Sandia Laboratories, Albuquerque, New Mexico 87185

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ABSTRACT

The COMAG-III [1] code has been used to model the dynamic behavior of a small CMF generator having a helical coil. The results have been compared with experiments which employed light pipe diagnostics of the explosive timing together with flash radiography to time correlate the mechanical behavior with the previously reported electrical output [2]. The code has also been used to study and compare the importance of various loss mechanisms in these generators, including ohmic heating and the flux lost by the switching action of the armature. A parameter study in which the injection current was varied is described. The possible importance of electrical breakdown is also discussed.

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1. Introduction

The COMAG series of 2-D MHD codes were constructed to provide a tool for studying the relative importance of a variety of competing physical effects in helical compressed magnetic field (CMF) generators. The initial comparisons with experiment [2] gave excellent agreement for high impedance loads but the computed results overestimated the peak current delivered to small impedance loads, a common result in earlier studies [3,4]. The comparisons described in Ref. [2] had no way to time-correlate the mechanical and electrical response of the generator, so that only the peak currents could be compared. A second series of experiments was conducted in which a light pipe oriented to observe the HE burn-through was added and multiple pulse flash radiography performed to provide the required time-synchronization. This work is described in Sec. 2.

The comparisons with the new series of experiments showed that the computed results were, in fact, time synchronized and in good agreement throughout most of the current pulse, deviating only during the shorting of the last few turns. Despite the numerical improvements described in the preceding paper [1], significant differences remained in the peak current comparison for the particular generator shot described in Sec. 2. Part of the difference can be ascribed to the lack of repeatability commonly found in generators which drive low impedance loads [5]. The experimentally determined current gains vary by about 50% due to turns-skipping and "clocking" from inaccurate assembly. Part of the difference can also be attributed to the simplified ideal sweeping contact model used in COMAG for most of the comparisons. Although the switching loss of the trapped flux in the armature and coil conductors is

treated by the COMAG circuit equation, there are several reasons to believe that additional flux will be lost in the real devices. First of all, defects and irregularities in the armature [4,6] provide a means for trapping and removing additional flux. Armatures which are not perfectly coaxial with the coil will also increase the amount of flux lost in the switching process. Third, flux loss mechanisms emerged from the experimental studies of R. Graham [7], who found that the actual electrical breakdown occurred at a time when the armature and the coil were separated by a gap of about 1 mil. The flux remaining between the conductors at the time of electrical breakdown would be lost from the system. Graham also found that there was a delay in the breakdown of 100-200 ns, equivalent to the time for shorting 2-4 turns. This delay would increase the amount of series coil resistance in the circuit.

Rather than try to include a complete model of the delayed shorting and flux loss process, which would have to include the quantitatively unknown losses due to armature defects and asymmetries, it was decided to compile a series of COMAG runs for the final current amplification phase in which the parameters of simple models for the flux-loss and coil resistance increase could be varied. These results are described in Sec. 3.

Another possible flux loss mechanism is electrical breakdown [3,5], either internal to the generator or at the load connection. A routine to provide a simple estimate of the electric field distribution in the generator was added to study this possibility. This topic is discussed in Sec. 4. In Sec. 5 the relative importance of ohmic losses is studied by comparing the current gain at normal injection current levels with that computed at very high injection levels, where resistive heating and non-linear diffusion should be of greater importance.

2. Comparison with Experiment

Although comparisons of COMAG calculations and experimental data have been presented previously, there were some factors which made an absolute comparison difficult. In particular, no experiments done before this year had time-correlated electrical and mechanical measurements. In previous comparisons with COMAG, the relative timing was adjusted to obtain the best fit.

A light pipe technique, summarized in Fig. 1, was employed to eliminate this problem. Timing in the high explosive charge was measured by placing a 0.5 mm diameter Crofon light-pipe on axis at the end of the generator explosive opposite the detonator to detect the arrival of the explosive detonation front. A second light pipe at the detonator end of the HE measured the passage of the detonation wave normal to the HE axis. By recording these light pipe outputs with a high speed streak camera together with the light of an electrically exploded bridgewire, the progress of the detonation front was synchronized to ± 8 ns with respect to the generator electrical output. Light pipe #1 recorded a 50 ns duration light pulse as the detonation front swept across its end and light pipe #2 recorded a 16 ns pulse normal to the detonation front. Some uncertainty exists in the exact axial positioning of the HE with respect to the coil so that the overall uncertainty in the synchronization is ± 20 ns.

Additional information about the mechanical behavior of the generator was obtained by using a three channel flash radiograph. The 30 ns effective exposure time of the x-ray source was sufficiently short to stop the fastest material motion but the 4 mm effective source size and the low angle of impact

between the armature and coil did not adequately define any generator feature that could be precisely related to a time in the code predictions. Figure 2 shows flash radiographs of the generator which illustrate the progress of the armature expansion in the coil with respect to the electrical output.

The initial configuration for the COMAG calculation is shown in Fig. 3. The high explosive is PBX-9404 with an initial density of 1.84 gm/cm^3 , a detonation velocity of $8.8 \times 10^5 \text{ cm/sec}$ and a total energy release of $1.03 \times 10^{12} \text{ ergs}$ for the 18.58 gm of explosive in the generator. The detonation point is at the bottom of the figure with the detonation wave moving upward. The zero of time is defined to be the start of the detonation with about 6.9 μsec required to complete the burn. The total operation time of the generator is slightly less than 10 μs .

The armature, coil, and crowbar switch are modeled as copper. The armature thickness was determined by requiring that its mass thickness be the same as that of the layered armature used in the experimental configuration. All are treated as elastic-plastic materials. The details of the remaining materials (structural case, end caps, etc.) are not important to the calculation other than that they be sufficiently massive. For reasons of computational economy, these were taken to be the same as unburned explosive.

The zoning of these calculations is quite difficult. There are many conflicting requirements from the various computational sections but only a limited amount of computer storage. The following represents a compromise which attempts to satisfy the minimum requirements for each section and still fit the data into the CDC-7600. A constant spacing of 0.045357 cm in the y coordinate (axial direction) is used with a total of 139 meshes. The radial

zoning is more complex with a total of 104 variably spaced meshes. The smallest meshes are 0.008078 cm near the coil, while the width near the armature is 0.013 cm, with a smooth variation in between. Considerably larger meshes are used in the explosive and case material regions, with the last radial mesh line at 2.135 cm. This results in some material leaving the mesh before the end of the calculation. The total number of cells in the grid is 14456.

As discussed in Sec. 1, the exact flux loss of the switching process is uncertain at the present time. For this particular example, the flux trapped in the conductors and the flux in the initial insulation thickness ($\sim .005$ cm) was assumed to be lost from the circuit. In addition, four extra turns of coil resistance were included to model the delay of the breakdown process.

A circuit diagram of the generator and load is shown in Fig. 4. The charging source is used to establish the initial current. At the appropriate time, the charging circuit is explosively switched out of the circuit by the shorting or crowbar switch. This occurs approximately $2 \mu\text{s}$ after the start of HE detonation for the given configuration. The current at this time is used as the initial condition for the code. Between 0 and $2 \mu\text{s}$ the current is fixed at this value since COMAG does not treat the charging circuit portion of the problem.

The exact initial charging current was not known because these calculations were completed before the corresponding experimental shot. While a value of 234 A was chosen on the basis of an earlier shot, the actual experimental current at crowbar time turned out to be 224 A. Since the calculations are quite time consuming, it was not felt that this

difference ($\sim 4.5\%$) was sufficiently large to repeat the calculations or experiment. The complete initial circuit parameters used in the calculation are tabulated below in Table 1.

Table 1.

Initial Current	234 amp
Coil Inductance	79.9 μh
Load Inductance	0.69 μh
Coil Resistance*	1.12 Ω
Load Resistance	0.01 Ω

*For a uniform current distribution.

The mechanical response of the generator is shown in Fig. 5a-5f. The first contact between the armature and coil occurs at about 6.5 μs . The complete length of the coil is swept out by 9.85 μs . These plots may be compared to the flash x-rays obtained experimentally. It is of interest to note that the current is about 790 A at 8 μs and 2200 A at 9 μs . Most of the generator output is in the time period between Figs. 5e and 5f.

The computed magnetic field lines (or lines of constant stream function) are shown in Figs. 6a to 6d. The conductors are outlined by asterisks. Figure 6a has the same field of view as Fig. 3, while the field of view in Figs. 6b to 6c is reduced in the radial coordinate for clarity. The large increase in the magnetic field strength as the calculation proceeds forces a change in the scale for the plotted field lines. The two marked lines are at the same field strength in all plots.

Figures 7 and 8 show the equivalent total generator inductance and

resistance history. The static coil resistance is 1.12Ω . The initial resistance drop in Fig. 8 reflects the fact that the initial current was assumed to be a surface current which slowly diffused into the wire. The break at $6.5 \mu s$ is the start of coil to armature shorting.

Figure 9 compares the computed and experimental currents. The light pipe data yields a timing shift of $9.867 \pm 0.023 \mu s$ for relating the computed results to the experimental value. The three experimental scope traces are labeled by A, B, and C. There are no adjustable parameters in the comparison. The 4.5% difference in the initial current which was previously discussed can be clearly seen. The calculations were stopped at the end of generator operation while the experimental data extends beyond this period of time.

The most important conclusion to be drawn from the comparisons is that the current histories are in excellent agreement, with no arbitrary time shift, until the last few turns of the generator are reached. Since most generator applications do not depend on this final output, it appears that the COMAC model provides an adequate description for this type of generator. It is nevertheless of interest to study the relative importance of the computing loss mechanisms to provide information for improved designs.

3. Switching Flux Loss

One of the dominant loss mechanisms in the generators under study is the contact switching flux loss [3,6,8]. The nature and treatment of this loss is best described by referring to the simplified two-turn helical example shown in Fig. 10. Fig. 10a shows the configuration just before the armature shorts turn A to turn B. At this time flux has diffused into the turns because of their finite resistivity.

This flux is linked to the load as shown in Fig. 10. Points A and B are positions on the turns at the same azimuthal angle; the point labeled AB is where they join. Because of the helical nature of the coil, its current must flow around the first turn (a distance of $2\pi r_c$) to get from point A on the first turn to point AB. Later in time, shown in Fig. 10b, the current entering the coil turn at point A can flow directly along the armature (assuming that the armature has shorted any insulation) to reach point AB. Since the coil wire diameter is much less than the turn radius, this direct path from point A to AB has a negligible voltage drop, so that one could redraw the coil circuit to look like Fig. 10c. The flux left in the helical turn A drives an eddy current which does not pass through the load.

The flux which is left behind the point of contact in both the coil and the armature represents a real energy loss to the coil circuit; this lost energy remains trapped in the form of magnetic field energy stored in conductors. This energy loss is separate from, and must be added to, the usual ohmic dissipation which converts magnetic field energy into internal energy by resistive heating. The flux loss due to resistive heating in a given turn occurs continuously, while the switching flux loss occurs only while that turn is being shorted and removed from the circuit linking the external load. The circuit equation used to describe the time dependence of the total flux linkage in COMAG includes a switching flux loss term; as each turn is "shorted," the trapped flux (and thus the energy associated with it) is discarded.

The switching flux loss can be expressed mathematically in the absence of other losses. If the generator coil has N turns and if each turn loses a fraction f of its local flux as it is switched, the circuit flux linkage

remaining after M turns are switched is

$$\psi_M = \frac{\psi_0 (N-M)!}{N!} \prod_{k=0}^{M-1} (N-k-f)$$

which can be expressed recursively as

$$\psi_{M+1} = \frac{\psi_M (N-M-f)}{(N-M)}$$

Fig. 11 plots ψ_M/ψ_0 for the case where $N = 70$, $f = 0.2$. Most of the loss is seen to occur when the last few turns are shorted.

As discussed in Sec. 1, there are many ways in which the armature-coil shorting process can deviate from a simple ideal switch, including flux-trapping by surface defects, clocking, delayed shorting, and gaps at the time of breakdown. Because of the quantitative uncertainties of these effects, a series of COMAG runs for the case discussed in Sec. 2 was compiled in which the magnitude of the flux loss f was varied. To reduce the volume of computing required, the runs were initiated when the current was at 3000 A; seventeen turns out of the initial seventy remained at that time.

Figure 12 compares the final current vs. time for several parameter variations. Case A used a model wherein the usual trapped flux loss in the conductors was augmented by an additional loss corresponding to the flux remaining in the insulator at the time of shorting, estimated from the results of Graham [7]. The computed current gain is considerably higher than seen in any experiment. Case B increased the flux-loss term to discard the local flux when the armature just made contact with the insulation under the coil.

This would in some sense model the increased losses due to 3-D effects, asymmetries, and armature surface defects. Case C used the basic flux loss model of case B, but added four turns to the coil resistance to account for the delayed breakdown of the sliding contact. Each of these losses brings the results on better comparison with the experimental results of Sec. 2, but a more consistent treatment [5] would be required to model the delayed shorting accurately. Case D is an example based on an electrical breakdown model discussed in the next section.

4. Electrical Breakdown

Electrical breakdown is always a potential failure mode for CMF generators [3,6]. Breakdown can occur at the load end where the helical coil turns must be attached to the external load; it can also occur internally near the sliding armature-coil contact. It would be expected to be of greater importance at high current and high rate of rise levels. The voltage drop around a single helical coil loop is given by (in practical cgs units)

$$\phi_j = 2\pi r_c \left[\frac{j_c}{\sigma} - \frac{10^{-8}}{r_c} \frac{\partial \psi}{\partial t} \right], \quad (1)$$

where r_c is the coil inner radius, j_c is the current density, σ is the electrical conductivity, and ψ is the magnetic stream function, all taken at the inner surface of coil to provide an estimate of the coil-to-armature potential. If the armature and the coil contact point is taken for reference as $\phi = 0$, the total voltage drop at turn J would be $V_J = \sum_{j=1}^J \phi_j$.

Figure 12 shows the distribution of internal potentials ϕ_j for the

low injection current case, Sec. 2, at a time when ten coil turns remain. The low impedance load leads to a low output voltage, so that most of the voltage generated at the sliding contact end must be distributed internally by the turns near the load end.

The principal effect of breakdown at the turn nearest the sliding contact would be an increase in the flux loss in this region. The fraction of the vacuum flux remaining in the turn at the time of breakdown would be added to the last flux trapped in the conductors by diffusion.

wire used in the actual device was coated with a layer of Kapton insulation having a thickness of .0025. In addition, a cylindrical layer of comparable thickness is placed just inside the coil. The breakdown strength of Kapton at room temperature and c. conditions is about 7 kV/mil, a field strength higher than that seen in the low injection current case. It nevertheless has been suggested [9] that the breakdown strength in the actual generator environment could be greatly reduced. To test the possible importance of breakdown, the COMAG calculations were repeated using a breakdown threshold of 1 kV/mil as a criterion for increasing the switching flux loss; the flux loss term included only the trapped conductor flux. With this threshold value, breakdown became important during the shorting of the last six turns and the final computed current, shown as Case D in Fig. 12, was very close to the experimental value.

5. Resistive Heating

Resistive heating and subsequent conductor melting has historically been thought to cause major limitations at high currents. Concern in this area led to generators in which the coil wire increased in cross-sectional area near

the load end or were split into parallel turns at several locations to reduce the current density. It was found during the comparisons for the generator described in Sec. 2 that the switching flux loss was about four times greater than the resistive losses at the normal injection current levels of about 235 A. Experiments designed to study the scaling of the current gain as a function of injection current had indicated that the gain decreased by about a factor of three at injection currents in the 1-1.5 kA range. This seemed to imply that resistive heating was becoming of greater importance at final current levels in the 50-100 kA range.

At an injection current of 235 A, the computed current gain for the generator discussed in Sec. II was 117. At an injection level of 1500 A, the computed gain decreased only slightly to a value of about 100. To understand why the increased heating and diffusion had so little effect, it is helpful to compute the skin depth

$$\delta = \sqrt{\frac{10^9 \eta \tau}{4\pi}}$$

where $\tau \approx I/(dI/dt)$ and η is the electrical resistivity. Near the end of the generator, where the amplification is greatest, $\tau \approx 0.2$ s, so that $\delta \approx 5.6 \times 10^{-3}$ cm. Although the armature thickness has decreased somewhat due to the cylindrical expansion, its thickness d is still about $d = 5.3 \times 10^{-2}$ cm, a value nearly ten times the skin depth. The coil wire thickness is about 4.5×10^{-2} cm.

The computed temperature rise near the conduction surfaces was only about 300° C. This increase was small because the time derivative of the current was very high, reducing the amount of time that the high currents had to

resistively heat the conductors. Since $\delta \sim T^{1/2}$ the temperature increase was translated into only a slight increase in skin depth and resistive dissipation. The switching flux loss was again the dominant loss mechanism for this case.

The high final current reached in the high injection current case also increased the importance of the Lorentz force. The final armature velocity at the load end was reduced by about 12% from the low injection current case which again would have little effect on the current gain. These comparisons indicate that additional losses are required to explain the current gain scaling for this particular generator. This conclusion applies only to this particular configuration; resistive losses can be very important for other generators.

6. Discussion

The comparison of COMAG results with the well-diagnosed experiments in Sec. II indicates that the basic physical model gives accurate results until the final few turns of the generator are reached. Since most generator applications do not require this final current gain, the utility of the model is established.

The switching flux loss was shown to be a dominant limitation to generator performance. The final current gain for low impedance loads depended significantly on the details of the switching model. (COMAG computations for high impedance loads have given good comparisons [2].) A more accurate treatment of the shorting process, wherein the breakdown delay and the trapped flux were treated consistently, could be done. Methods of the reducing the importance

of the switching flux loss are basically similar to the methods for reducing ohmic dissipation, such as increasing the conductivity or the armature velocity. Generators with split turn coils would also be less sensitive to the problem because the effective pitch would be increased. This would also hold for spreading out the last few turns, although both of these techniques reduce the inductance per unit length and thus the rate of rise of the current.

The potential importance of electrical breakdown near the sliding contact was studied by increasing the local switching loss at the sliding contact under the control of a breakdown threshold of 1 kV/mil. This value is much less than the d.c. breakdown strength of 7 kV/mil, and the improved comparison with the experimental current values must be considered somewhat fortuitous. The results do indicate that further research into breakdown phenomena would be warranted.

The comparison of the high and low injection current cases indicates that resistive heating and Lorentz force effects are of little importance in current gain scaling in this parameter range. The basic reasons are that the skin depth is much smaller than the conductor thicknesses and that the rapid rise of the current to its peak value reduces the action integral and the heating.

References

- [1] McGLAUN, J. M., THOMPSON, S. L., FREEMAN, J. R., preceding article.
- [2] FREEMAN, J. R., THOMPSON, S. L., J. Comp. Phys. 25, 332 (1977).
- [3] SHEARER, J. W., et al., J. Appl. Phys. 39, 2102 (1968).
- [4] ANTONI, B., NAZET, C., POBE, L., "Theoretical and Experimental Study of Explosive-Driven Magnetic Field Compression Generators," in Proc. of the Int. Conf. on Energy Storage, Compression, and Switching, Plenum Press, New York, 1976.
- [5] GOVER, J. E., STUETZER, O. M., JOHNSON, J.L., proceedings of this Conference.
- [6] KNOEPFEL, H., Pulsed High Magnetic Fields, North-Holland, Amsterdam, 1970.
- [7] GRAHAM, R. A., private communication.
- [8] CRAWFORD, J. C., DAMEROW, R. A., J. Appl. Phys. 39, 5224 (1968).
- [9] MILTON, O., private communication.

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LIGHT PIPE RESULTS

PBX-9404 DIAMETER=1.397 CM, LENGTH=6.062 CM

TETRYL BOOSTER PELLET DIAMETER=.386 CM, LENGTH=.127 CM

LIGHT PIPE #1 AT RADIUS=.625 CM

LIGHT PIPE #	DETONATION WAVE ARRIVAL TIME US
1	10.33
2	16.68

BRIDGEWIRE FIRED IN PARALLEL WITH RP-2 DET HAD BURST TIME OF 6.99 US

AVERAGE DETONATION VELOCITY=.886 CM/US

FIGURE 1

~~DOE-104X~~ RAY TEST

FIGURE 2

FIGURE 3

FIGURE 4

FIGURE 5a

FIGURE 5b

FIGURE 5c

FIGURE 5d

FIGURE 5e

FIGURE 5f

FIGURE 6a

FIGURE 6b

FIGURE 6c

FIGURE 6d

FIGURE 7

FIGURE 8

FIGURE 9

00

A.

00

B.

00

C.

FIGURE 10

FIGURE 11

FIGURE 12

FIGURE 13