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OPTIMIZATION OF A PULSED AIR CORE TRANSFORMER FOR LOW IMPEDANCE INDUCTIVE IGNITION

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A design analysis was conducted to develop an inductive ignition concept for ordnance. Mathematical models were developed to examine the transformer for optimum performance. Results indicate that significant energy transfer to an electric primer can be obtained using a simple pulse transformer. Experimental results using a capacitive pulsed power supply indicate reasonable agreement with the models for short times. Deviations from the theoretical model can be explained by nonlinear materials effects and field diffusion in the surrounding barrel walls.

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I. INTRODUCTION

Alternative approaches to gun propellant ignition have been explored to ease automatic handling and loading of large caliber ammunition. Automatic loading is facilitated by incorporating a sliding block breech mechanism. Advantages obtained by eliminating a separately loaded primer for the ammunition include advanced rapid fire artillery systems, integrated system simplification, and, increased reliability and safety. A further advantage is the lower strength requirement on the propellant case due to the absense of a percussion pin.

This investigation addresses a low impedance inductive ignition (LI^3) concept design using a simple pulsed air-core transformer to transfer energy to an electrically initiated primer embedded in an igniter pad at the base of the propelling charge. The transformer consists of a single turn copper primary coil in the breech face and a consumable lead secondary in the charge as shown in Figure 1. Because of the transformer's topology, conventional percussive primer ignition systems can still be used. Low transformer efficiency coupled with high capacitive energy densities makes this an attractively insensative ignition scheme.

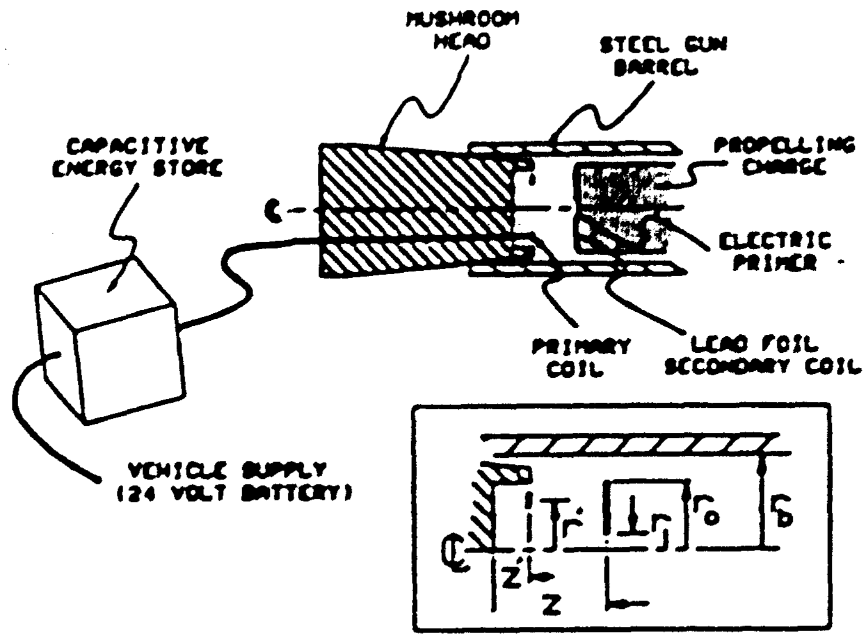

Figure 1. LI³ concept.

Previous work [1] on the LI^3 ignition system examined the capacitive energy store, charging circuit, and electric primer requirements using a lumped parameter electric circuit model. To improve performance, it was desirable to configure the circuitry for the fastest possible pulse. The coupling between coils and barrel, a complex function of geometry, substantially determines transformer performance, and was modeled using a single, average mutual inductance obtained from various experimental data. This paper describes an analysis of the magnetic fields of the pulse transformer directed towards an accurate prediction of primary/secondary coil coupling and optimum performance.

II. ANALYTICAL MODEL

The electromagnetic field analysis of the transformer requires a mathematical derivation of the vector potential inside the combustion chamber and subsequent computation to determine transformer geometry for optimum energy transfer to the electric primer load. The impedance of the charging circuit is considered dominant, resulting in the primary current pulse profile being nearly independent of the transformer geometry. The duration of this event is on the order of tens of microseconds; consequently, the magnetic flux is substantially excluded from the interior of the conductors. In addition, the primary coil is taken to be filamentary, minimizing the reluctance inside the chamber.

The vector potential, A, inside the combustion chamber is the solution to Poisson's equation in cylindrical coordinates. The magnitude of the azimuthal component is expressed as follows

$$\frac{\partial^2 A}{\partial r^2} + \frac{1}{r}\frac{\partial A}{\partial r} + \frac{\partial^2 A}{\partial z^2} - \frac{A}{r^2} = -\mu J \tag{1}$$

where A vanishes on the surface of the chamber. The permeability is μ and J is the primary coil current density. The secondary coil is a very thin foil, and its contribution to the vector potential is considered negligible. Equation 1 is solved with a Green's function, $G(r,\acute{r})$, constructed from the electrostatic potential of a point charge at $\acute{r}$, $\acute{\phi}$, and $\acute{z}$, inside a conducting cylinder comprising surfaces $r = r_b$ and $z = 0$ at zero potential [2]

$$G(r,\acute{r}) = \frac{1}{2\pi r_b^2}\sum_{n=1}^{\infty}\sum_{m=0}^{\infty}(2-\delta_m^{\circ})[e^{(-x_n|z-\acute{z}|)} - e^{(-x_n|z+\acute{z}|)}]\left[\frac{J_m(x_n\acute{r})J_m(x_n r)}{x_n[J_{m+1}(x_n r_b)]^2}\right]\cos(m(\phi-\acute{\phi})) \tag{2}$$

where J_m and J_{m+1} are Bessel functions, x_n is defined by $J_1(x_n r_b) = 0$, and δ_m° is the Kronecker delta. The solution to the Poisson equation (1) is given by

$$A = \mu I_1 \oint_{\ell} G(r,\acute{r})\,\acute{r}\,\cos(\phi - \acute{\phi})\,d\acute{\phi} \tag{3}$$

where I_1 is the primary current. Substituting the Green's function (2) into equation (3) and performing the integration yields

$$A = \mu I_1\left(\frac{\acute{r}}{r_b^2}\right)\sum_{n=1}^{\infty}[e^{(-x_n|z-\acute{z}|)} - e^{(-x_n|z+\acute{z}|)}]\left[\frac{J_1(x_n\acute{r})J_1(x_n r)}{x_n[J_2(x_n r_b)]^2}\right] \tag{4}$$

where $\acute{r}$, $\acute{z}$ are the cylindrical coordinates of the primary coil and r_b is the radius of the combustion chamber. The current in the secondary coil, I_2, may be calculated from Faraday's law

$$\dot{\phi} + \frac{2\pi r J_{sec}}{\sigma} + I_2 R_{load} = 0 \tag{5}$$

where σ is the conductivity of the secondary coil, J_{sec} is the secondary current density, and R_{load} is the resistance of the electric primer.

Substitution of $2\pi r\dot{A} = \dot{\phi}$ and subsequent integration in terms of thickness δ, and inner and outer radii r_i and r_o of the secondary coil yields the transfer function [3]

$$\alpha = \mu\left(\frac{\acute{r}}{r_b^2}\right)\left[\frac{1}{\sigma\delta} + \frac{R_{load}}{2\pi}\ln(r_o/r_i)\right]^{-1}\sum_{n=1}^{\infty}\left[e^{(-x_n|z-\acute{z}|)} - e^{(-x_n|z+\acute{z}|)}\right]\left[\frac{J_1(x_n\acute{r})[J_0(x_n r_o) - J_0(x_n r_i)]}{x_n^2[J_2(x_n r_b)]^2}\right] \tag{6}$$

where α is the ratio $I_2/\dot{I}_1$.

The transfer function equation (6) is evaluated for a 155 mm Self Propelled Howitzer with a combustion chamber radius, r_b, equal to 60.3 mm. On the basis of materials considerations, performance criteria, and breech design constraints, a primary coil of 12 gage square magnet wire is selected and mounted in a 12.7 mm recessed section. The mean axial location of the primary coil from the breech is 11.7 mm. The secondary coil is made from 0.1 mm thick lead (Pb) foil and its axial position from the breech is denoted by z. All radial dimensions for the coil geometries are normalized to the combustion chamber radius. A numerical technique was implemented to find the optimum primary coil mean radius and the secondary coil inner and outer radii when the transfer function is maximized. The primary/secondary coil separation distance is defined by

$$z_{1,2} = z - \acute{z}. \tag{7}$$

The optimum value for α at a separation distance of 2 mm is 3.480×10^{-7} seconds. Using the optimized coil dimensions for 200 mm of separation, and calculating α at 2 mm gives 2.837×10^{-7} seconds, only 18.6% less. This deviation from optimum quickly falls below 5% for $z_{1,2}$ greater than 12.7 mm and indicates the insensitivity of the transfer function to radially varying coil dimensions. For the three coil parameters, $\acute{r}$, r_i, and r_o, the optimum coil dimension variation as a function of primary/secondary separation is plotted in Figure 2. In addition, transformer performance degrades with increasing primary/secondary separation distance as illustrated in Figure 3.

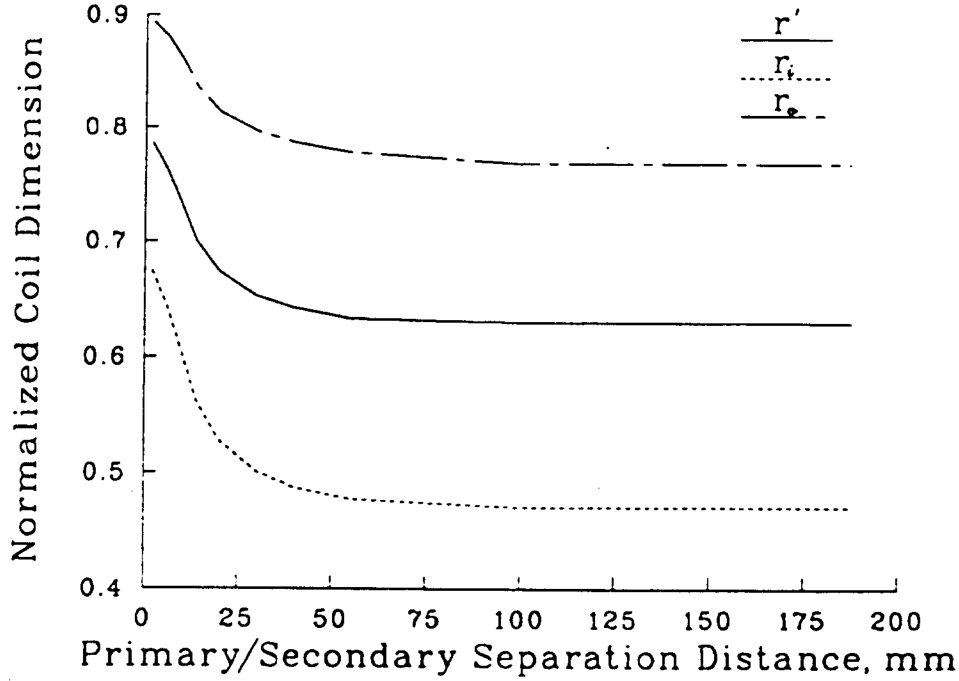

Figure 2. Coil dimensions normalized to r_b versus separation distance.

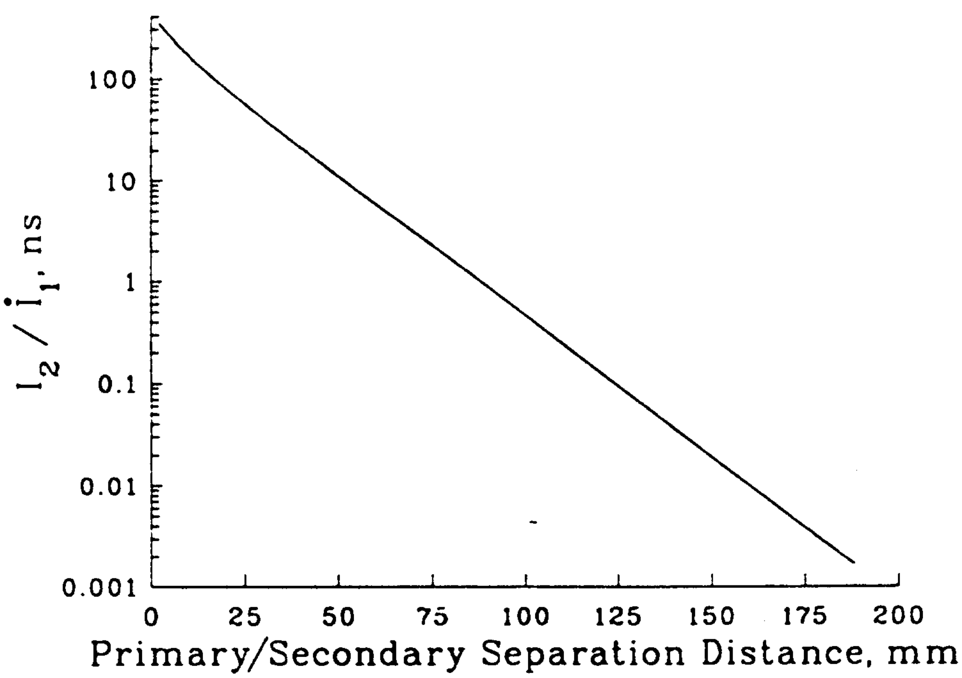

Figure 3. Transfer function versus separation distance.

Relaxation of design restrictions on the secondary circuit impacts system effectiveness. Optimum values for the transfer function were calculated with $z_{1,2}$ fixed at 100 mm. Results from varying the foil thickness and electric primer load resistance are illustrated in Figure 4. Thicker secondary coils have reduced ohmic losses but may leave large amounts of residue in the barrel. In all cases, lower electric primer resistance yields a larger value for the transfer function. The optimum, normalized, primary coil radius for this variational analysis remained constant at 0.63. However, the secondary foil width $(r_o - r_i)$ decreased with an increasing mean secondary radius for thicker foils.

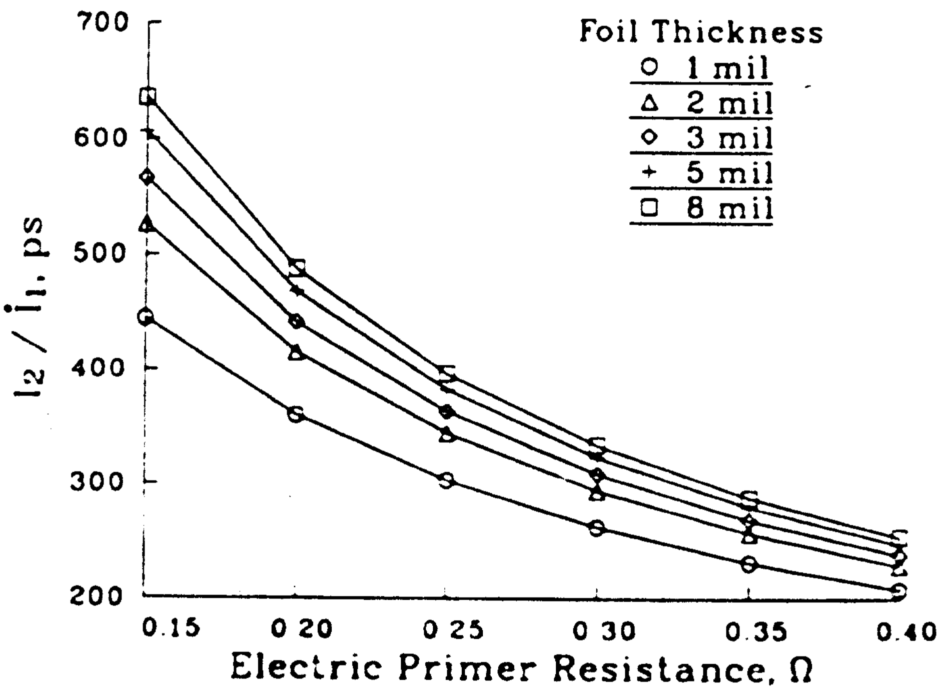

Figure 4. Transfer function versus electric primer resistance.

III. EXPERIMENTAL RESULTS AND DISCUSSION

The experiments were performed using a conventional 155 mm breech with a 12 gage square copper coil having a mean radius of 39 mm embedded in the breech face. The mushroom head recess diameter was 121 mm; the steel gun barrel which fit over the spindle had an inner diameter of 142 mm. The gun barrel radius was used for r_b in normalizing all coil configuration dimensions. The power supply specifications were developed at ARDEC and built by EML, Inc. It stores nominally 1500 Joules at 5 kV. All secondary test coils used 0.13 mm thick copper foil with a 0.174 Ω load resistor. The resulting normalized primary coil radius is 0.55 and the normalized secondary coil dimension domain is bounded by $0.36 < r_i < 0.76$, and $0.56 < r_o < 0.94$. Data on each coil configuration, identified by H, D, C, and K, are given later in Table 1.

Earlier circuit analysis had indicated that increased performance would be obtained by minimizing the primary side inductance and resistance.[1] The transmission line leads from the power supply to the breech were fabricated from high voltage coax with a length of 2.7 m. A ringing pulse discharge with the primary coil connected yielded 2.5μH and 28.6$m\Omega$ for the primary side circuit parameters. During each test, initial charge voltage, primary current, and secondary load voltage were recorded. The current was measured using a current transformer while the secondary voltage was measured directly with a voltage divider. Figure 5 shows a typical trace obtained for the measured primary current and secondary load voltage at an initial charge voltage of 1 kV. Also shown is the primary di/dt, obtained by differentiating the primary current. Differentiation is done via a numerical technique which uses a five point smoothing algorithm. During all tests the primary to secondary distance remained constant at 44.13 mm.

Twenty four tests were performed with each of the four coil configurations pulsed twice at initial charge voltages of 1, 3, and 5 kV. For each test α could be found as a function of time from

$$\alpha(t) = \frac{I_2(t)}{\dot{I}_1(t)} = \frac{V_2(t)/0.174\Omega}{\Delta I_1(t)/\Delta t}. \tag{8}$$

Typical waveforms obtained using equation (8) are shown in Figure 6 for the four coil configurations at an initial charge voltage of 3 kV. For times greater than 7μs there is very little deviation amongst the four coils. Beyond 16μs the value of $I_2/\dot{I}_1$ becomes untenably large as $\dot{I}_1$ approaches zero.

For all coil configurations, the transfer function data are only significantly different for times less than 5 μsec. The time averaged values of α, integrated over the interval .5 μsec to 22 μsec, for all coil configurations is 51.44×10^{-9} seconds with a deviation of only 2.8%. Since the mathematical model is applicable only when the vector potential on the inner barrel wall surface is zero, the data should be evaluated for short times. A time of 3 μsec was selected to analyze $I_2/\dot{I}_1$. The deviation of the transfer function among the four coil configurations evaluated at this time is 16.8%. Table 1 lists α for the four coil configurations tested in order of decreasing $I_2/\dot{I}_1$. Also, results from the theoretical model are shown to be in reasonable agreement with the experimental data.

Table 1. Coil Configuration Summary

Coil Configuration	r_i	r_o	$I_2/\dot{I}_1$, 10^{-9} seconds	
			Data @ 3μs	Calculated
H	.36	.56	30.71 ± 3.0%	28
D	.51	.87	27.37 ± 9.3%	32
C	.60	.78	24.86 ± 6.4%	34
K	.76	.94	20.38 ± 10.4%	21

The weak dependence of α on secondary radial coil dimensions for the domain $r_i < 0.6$, and $r_o > 0.6$, is illustrated for the laboratory setup ($\acute{r} = 0.55$) in Figure 7. The optimum

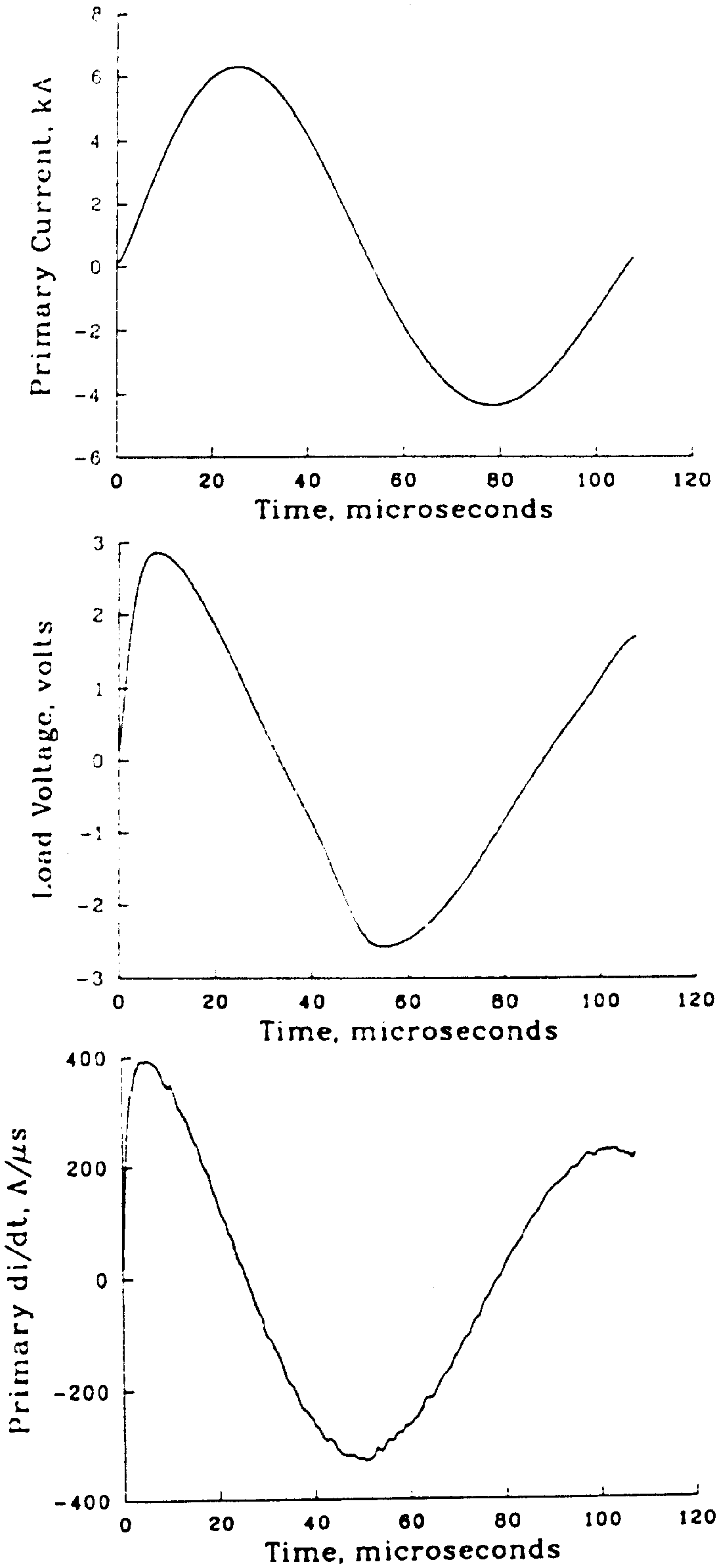

Figure 5. Measured primary coil current (top) and secondary load voltage (middle) and differentiated primary coil current ($\dot{I}_1$, bottom).

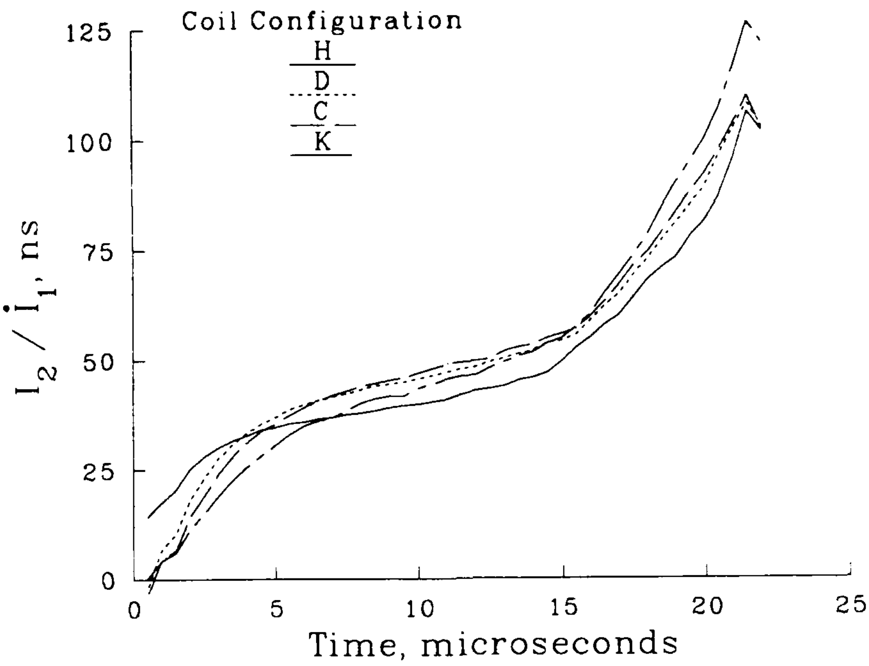

Figure 6. Transfer functions for coils H, D, C, and K.

normalized secondary coil dimensions are $r_i = 0.56$ and $r_o = 0.69$. The corresponding calculated transfer function is 35×10^{-9} seconds; this is a 14 % increase over the largest measured, and is near the uncertainty in the experimental data.

The value of $I_2/\dot{I}_1$ varied as much as 10.4% as the capacitor bank voltage was increased. Nonlinear effects arising from the presence of the steel barrel containment become apparent when considering the energy transferred to the secondary load. Therefore, the efficiency, η, defined as the ratio of the secondary load energy to the stored capacitive energy, is given as

$$\eta = \frac{\int \frac{V_2(t)^2}{R_{load}} dt}{E_{stored}}. \quad (9)$$

For all test voltages configuration C was found to transfer energy most efficiently while coil configuration H was found least efficient. Efficiency was found to decrease as the capacitor voltage was increased. The variation of magnetic permeability with magnetic flux density at the surrounding barrel wall alters the induced ohmic losses. The effect is illustrated in Figure 8. As would be expected, relatively more energy was transferred to the 0.174 Ω load at the higher voltages.

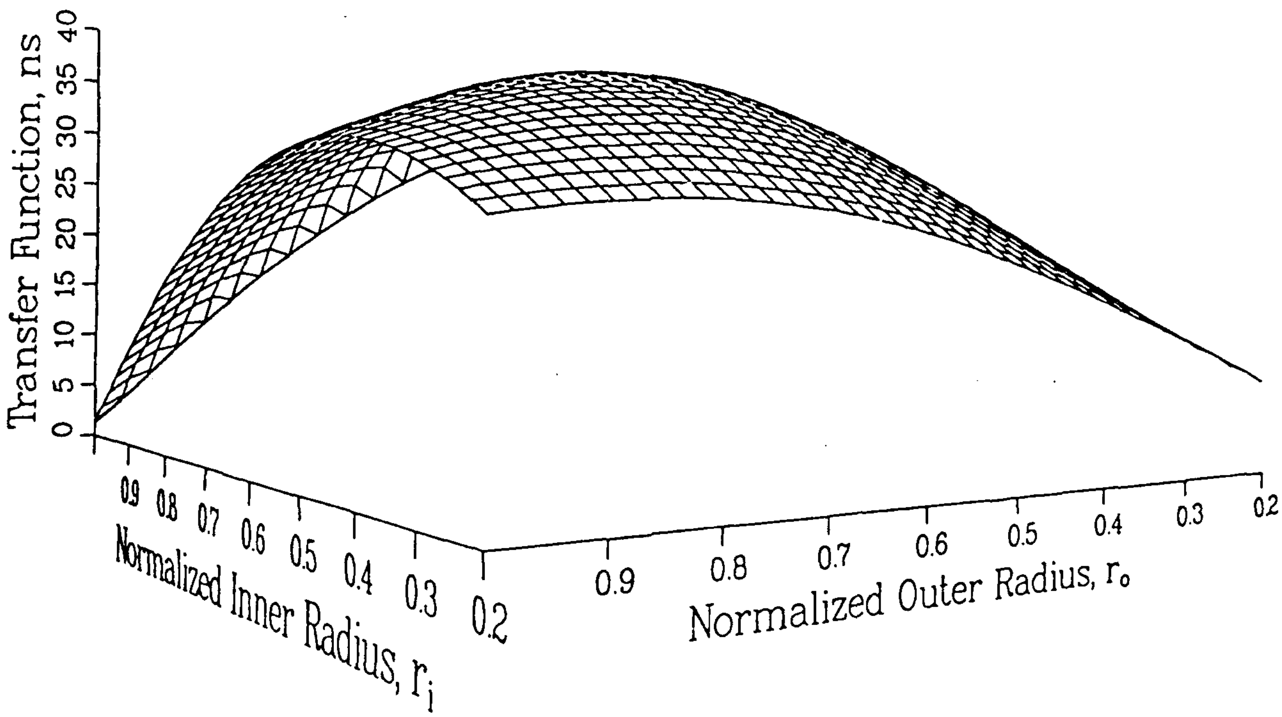

Figure 7. Transfer function surface corresponding to the laboratory setup ($\acute{r}$ = 0.55) plotted as a function of secondary radii.

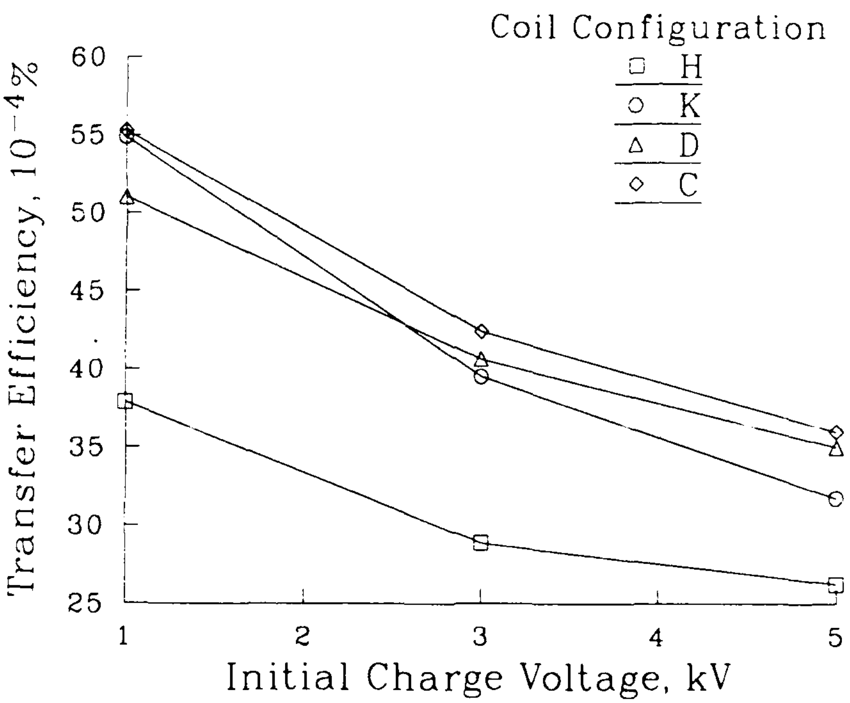

Figure 8. Transfer efficiency versus initial capacitor voltage.

IV. CONCLUSIONS

Corresponding to the optimum performance configuration, the primary coil mean radius and the secondary coil inner and outer radii attain nearly constant values with increasing primary/secondary coil separation distance. The transfer function was found to be relatively insensitive to small changes in coil radial dimensions near optimum. The theoretical model assumed simple dimensional boundaries while the experiment used actual gun barrel hardware which produced small dimensional deviations from the assumed model. These too were found to offer undiscernible differences between tranfer function calculations and experimental data. Pulse times greater than $7\mu s$ produced little variation in the transfer function among the tested coil configurations. Limiting the diffusion of the magnetic flux in the surrounding barrel steel with a fast discharge pulse should lead to the conditions assumed in the transformer model and optimum transformer performance. Inclusion of the permeable, surrounding barrel material in the analysis will require further work. Additional experimental work is needed in order to verify the theoretical model presented. Better selection of coil configurations is needed to explore the theoretical model in the domains $r_i > 0.4$, $r_o < 0.6$ and $r_i > 0.6$, $r_o > 0.9$, where the change in α is most sensitive to the change in coil dimensions.

These results are intended to support a refined system design for further experimental verification of the low impedance inductive ignition concept.

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