Lightning Protection for Submergible Oilwell Pumps

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Abstract—**Lightning protection for electric submergible pumps has long been an area of concern. Related insulation failures are costly and not altogether uncommon. These are due in part to poor-quality grounds and the tightly spaced geometry of submergible motor windings. An improved protection scheme is proposed wherein separate high- and low-current grounds are used, and the rate of voltage rise is limited. These techniques divert the major portion of lightning energy before it reaches the downhole equipment, thus minimizing coil-to-frame voltage stress. It also slows the rate of voltage rise to reduce turn-to-turn stress within a single motor coil. Design considerations are developed for the protection of an electric submergible pumping system, particularly as they relate to the value, quality, and reliability of individual protection components and various grounding systems. Laboratory test data are presented, showing the effects of motor impedance on performance when characteristic lightning transients are applied. Finally, available field data are reviewed.**

Introduction

PROTECTION of induction motors and all electrical apparatus against the destructive effects of lightning is a science that is well documented and generally understood. From the turn of the century to approximately 1940, numerous experiments on lightning protection were conducted, and a theory was developed that is widely accepted today. Without this development it is doubtful that reliability would have been adequate for the electric power grids to achieve their present-day size.

A central issue in the development was agreement within the electrical equipment industry on definitions and testing methods to adequately specify voltage withstand ratings. Agreement was not easily reached because it depended on characterizing the exact nature of lightning, and it was well known that insulation failure was not just a function of the peak or crest voltage but depended also on the voltage waveform. When voltage rate-of-rise, in kilovolts per microsecond, was very large, disruptive discharge would occur on the leading edge of the waveform. However, with lower rates-of-rise disruptive discharge did not occur at the crest voltage but at some lesser value on the trailing edge (see Fig. 1) [1]. To obtain a uniform method for equipment rating, a standard lightning voltage waveform was accepted [2] where the risetime, from zero to crest voltage on the slope tangent line passing through the 30 and 90 percent crest voltage points, was 1.2 μs and the pulsewidth, from inferred zero to 50 percent crest voltage on the trailing edge, was 50 μs (Fig. 2). By testing numerous identical samples with this voltage waveform, a basic impulse insulation limitation (BIL) for the equipment was assigned as the highest crest voltage that could be consistently withstood.

Protecting motor insulation against possible damage by such impulse voltages is a two-step process. Obviously, voltages must be limited to values less than the withstand crest voltage, and this function traditionally has been performed by lightning arresters. However, turn-to-turn insulation strength may be less than coil-to-frame, and waveforms with high rates of voltage rise apply high stress to this insulation, as illustrated in Fig. 3 where propagation delay through the coil is longer than the voltage risetime. At a given instant, coils still at zero voltage ahead of the wave are adjacent to coils that have experienced appreciable voltage rise. Motors used in the electric submergible pump (ESP) are also affected by this phenomenon because each coil turn extends the entire length of the motor (as much as 30 ft) with propagation delay of 0.122 μs per turn or 2.19 μs per 18 turns, assuming a dielectric constant of four. By limiting the rate of voltage rise the gradient between coils is substantially reduced, as shown by the dashed line. The circuit configuration in Fig. 4 performs these two functions and was recommended for induction motor protection prior to 1944 [1]. Attenuation and filtering provided by the inductor–capacitor combination limits the rate of rise. A "special arrester" was recommended to limit voltages in excess of the capacitor rating which might be produced by contactor showering arcs at the filter's resonant frequency.

ESP Motor Protection Criteria

Determining an appropriate threshold and rate-of-rise for the lightning arrester and filter, respectively, depends on the philosophy used to protect the unique ESP motor. ESP motors correspond to none of the NEMA frame-size, horsepower, or voltage ratings [3]. A cross-sectional area typical of fractional horsepower motors is dictated by well-casing diametric limitations, and length requirements are established by the mechanical power necessary to supply both hydraulic lift power and pump losses. A typical 200-hp ESP motor can be less than 6 in diameter and over 30 ft in length. Also the motor is oil-filled to withstand common bottomhole pressures. Despite the very small coil-turn radius, windings operate up to 4 kV to develop the necessary horsepower. Whereas BIL

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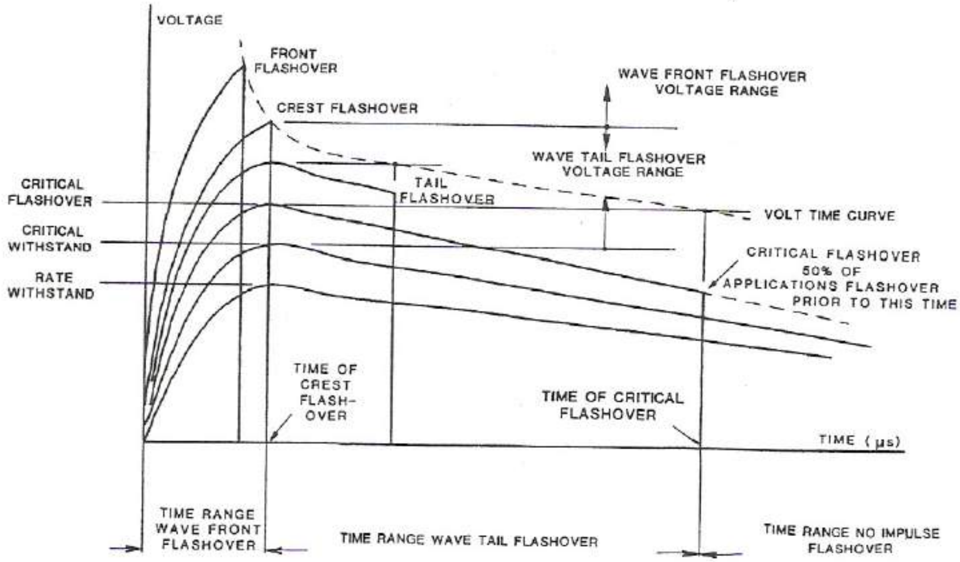

Fig. 1. Series of impulse waves illustrating terminology and definitions associated with impulse voltage testing.

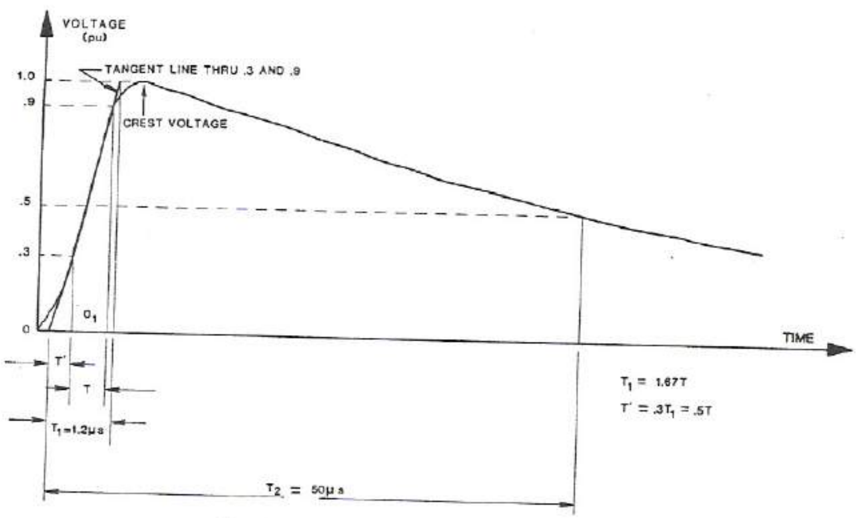

Fig. 2. Standard lightning simulation impulse.

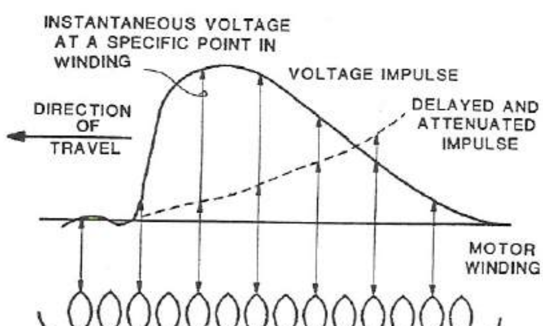

Fig. 3. Propagation of short rise time impulse through winding illustrating turn-to-turn stress.

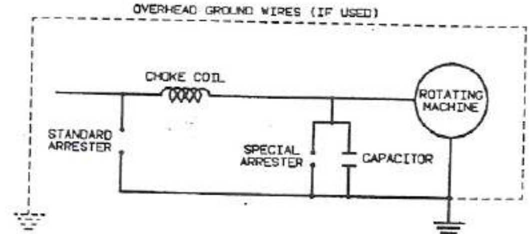

Fig. 4. Choke coil and capacitor method of protecting rotating machine.

ratings are common for transmission and distribution equipment, there is no such standard for NEMA motors [4], much less for ESP motors. A partial explanation for this is that BIL determination requires destructive testing whereas ac and dc tests can be nondestructive. BIL ratings are generally higher than either ac or dc ratings.

Common industrial practice [3] specifies ac voltage withstand as

$$V_{acw} = 2 \times V_r + 1000 \quad (1)$$

where V_r = rated motor voltage. An attempt has been made to relate ac and dc withstand voltages [5], and for this type of equipment dc withstand is usually defined as

$$V_{dcw} = 1.7 \times V_{acw} \quad (2)$$

However, no similar attempt to correlate BIL to ac or dc withstands could be found. The ESP voltage rating V_r, which relates to optimum magnetic flux and frequency, correlates rather poorly in the above definition of ac withstand. One possible reason is that all wire gauges used are insulated with the same materials and to the same thickness. So, although ratings increase with end-turn bending radius and decrease slightly with wire length, reliance on the NEMA formula is questionable. Single-section ESP motors are tested to this standard, but tandem motors are always tested to maximum withstand rating since in-service voltage depends on the number of tandem sections in series and can be quite high. Ac and dc withstand voltage testing is usually done with current-limited sources, which ensure a nondestructive test by removing applied voltage when overcurrent is detected.

ESP insulation materials are rated in excess of 200°C and exceed NEMA class H requirements. Nonetheless, they are an area of continuing proprietary research for applications in geothermal and other hot wells, because most insulations hydrolyze at common well temperatures and pressures when well brines contaminate the motor oil. Further, it is known that less than totally destructive voltage transients progressively deteriorate insulation. Since both effects act to lower actual in-service voltage withstand, then to maximize equipment run life, voltage transients should be restricted considerably below new insulation ratings and preferrably as close to motor rated voltage as practical. This was the ESP motor protection philosophy used to establish threshold and rate-of-rise design criteria for lightning protection devices.

Grounding

All attempts to protect equipment from lightning damage will be ineffective without an adequate low-resistance ground. Ground resistance can be readily evaluated by measurement with an appropriate instrument, and the amount by which ground resistance can be reduced is well known for the following techniques:

1) increased ground rod length,
2) chemical treatment of soil around rod,
3) use of multiple ground rods, and
4) increasing spacing between multiple rods.

Variation of rod diameter produces only minor changes in ground resistance, but soil moisture content, which varies with the time of year, is known to have a decided effect [6].

The importance of low ground resistance is easily demonstrated because a 1-Ω resistance experiencing a typical 20 000-A lightning impulse current has a peak voltage of 20 kV. Many oil wells are located in arid parts of the world, where low ground resistance is difficult to achieve. However, it can be generally stated that a 50 percent reduction in ground resistance will reduce the ground terminal transient voltage by a proportional amount. Whatever traditional or specialized ground improvement techniques are employed, they are easily evaluated by ground resistance measurements.

One major problem with all equipment grounding techniques in the oil field is inductance of the ground lead. Well site logistics prohibit locating protection equipment at the most optimum points, thereby requiring some finite length of ground wire. The inductance of a straight, solid, round, nonmagnetic wire can be calculated from

$$L = 2 \times 10^{-7} X_1 \left[\ln\left(\frac{2X_1}{r_c}\right) - \frac{3}{4} \right] \text{ H} \quad (3)$$

where r_c is the wire radius in meters, and X_1 is the wire length in meters [7].

A 1-0 AWG wire has 1-μH/m inductance while a #4 AWG wire has 1.2 μH/m. Using a 1-0 AWG wire 10-m long, a lightning current rising at 2000 A/μs causes a 20-kV drop along the wire. Thus even though the lightning arrester is conducting, a sizeable voltage transient appears across the ground lead and adds to the voltage across the ground resistance (Fig. 5(a)). Since voltage across the capacitor C cannot change instantaneously, this total voltage is impressed across the cable and motor insulation, often with damaging results.

An obvious solution to the above problem is to use two separate grounds, one for the lightning arrester and another for the capacitor and MOV (Figs. 5(b) and (c)). When this is done, the majority of the ground resistance and lead inductance voltage appears across the inductance L, and insulation voltage is constrained within acceptable limits. Experience has shown that insulation voltage restriction is strongly influenced by resistance of the available ground grid, and depending on this resistance, one of two different connection schemes has proven to be most effective.

When a ground grid resistance less than 5 Ω can be obtained without excessive difficulty, connection of the high-current ground (HCG) to the grid is preferred (Fig. 5(b)). Applying the principle of voltage division, casing ground resistance is normally much less than 0.1 Ω, low current ground (LCG) lead inductance is much less than L, and C is ideally an ac short circuit for high frequencies. Thus when a voltage impulse arrives at the input, most of the voltage appears across L, and the voltage across C and the LCG lead inductance, equal to the insulation voltage, is small. Wellhead voltage changes very little because LCG current and casing ground resistance are small. Sometimes a separate LCG ground is preferred rather than connecting to the casing. This is discussed below.

In some instances, it is difficult and expensive to obtain

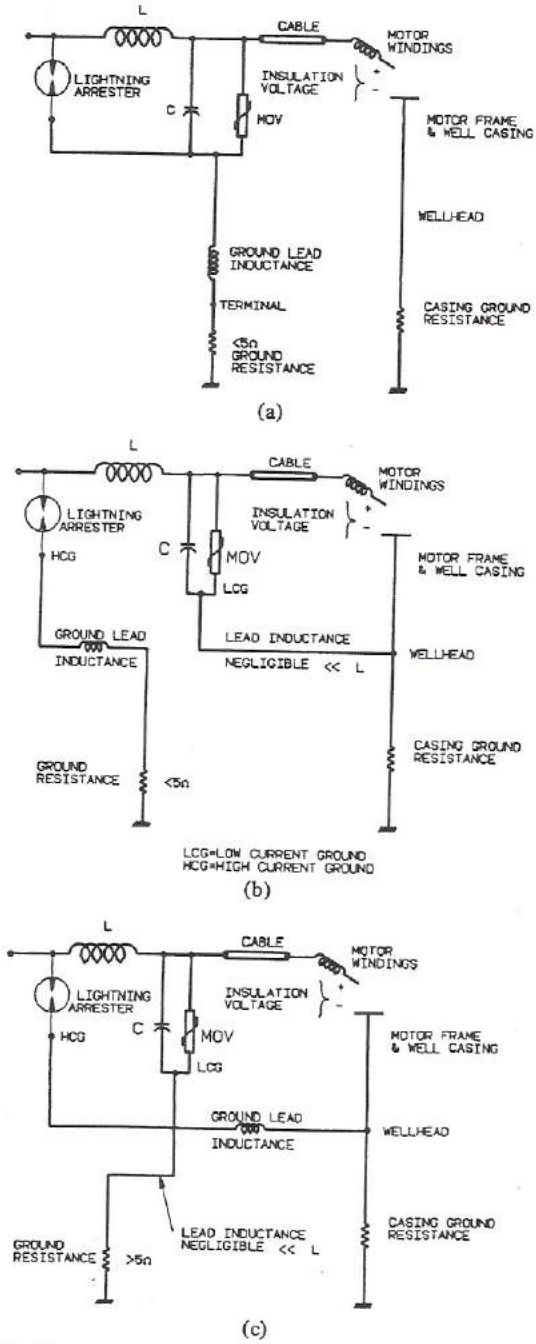

Fig. 5. (a) Common ground connection with $< 5\Omega$ ground resistance. (b) Separate grounds $< 5\Omega$. (c) Separate grounds $> 5\Omega$.

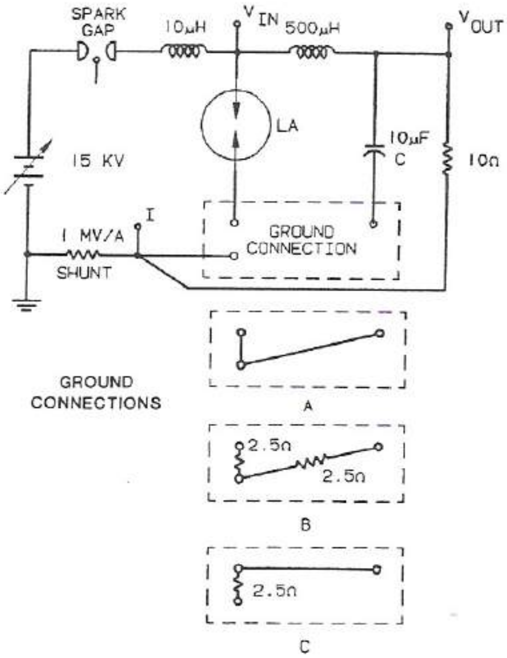

Fig. 6. Test circuit for ground connection evaluation.

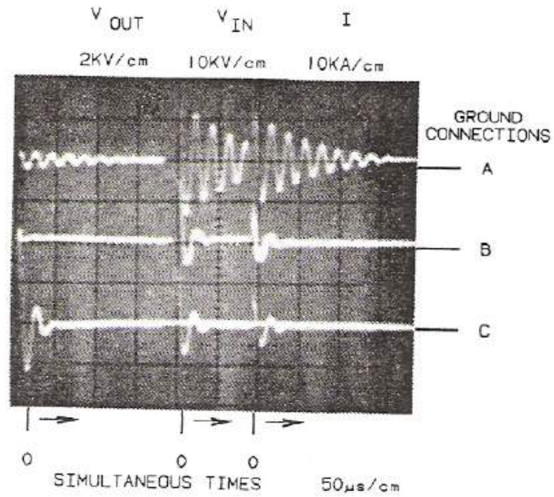

Fig. 7. Effects of ground connection on output voltage amplitude.

ground grid resistance less than 5 Ω, and the major portion of lightning impulse energy cannot be shunted through the lightning arrester. When this occurs, the alternate connection, shown in Fig. 5(c), is preferred. Invariably, the casing has the lowest ground resistance of anything within miles of the well site, and lightning energy can be dissipated here without an appreciable increase in wellhead voltage. Voltage across the ground lead inductance is mostly impressed across L. Wellhead and LCG-terminal voltages tend to rise and fall together, minimizing insulation voltage.

The case for separation of grounds is vividly made by the different ground schemes presented in Fig. 6 and their associated performance in Fig. 7. A variable high-voltage dc supply with triggerable spark gap and 10-μH waveshaping inductor was used as the excitation source for all the various arrester and filter configurations tested. In Fig. 6 ground connections A and C show a common connection, except that C incorporates a simulated 2.5-Ω ground resistance. Output voltage, V_{out} in column one of Fig. 7, shows nearly a 4-kV impulse for C and some inductive ringing for A. However, when the two grounds are connected to separate ground resistances in B, the measured output voltage is less than 500 V.

Production operations are sometimes opposed to grounding surface equipment to the wellhead, particularly when cathodic protection is used to reduce casing corrosion. The main reason for this is usually concern about lightning damage to the cathodic protection dc voltage source, normally 40 V or less. Many of the principles discussed in this paper apply equally to lightning protection of dc voltage sources, and the more

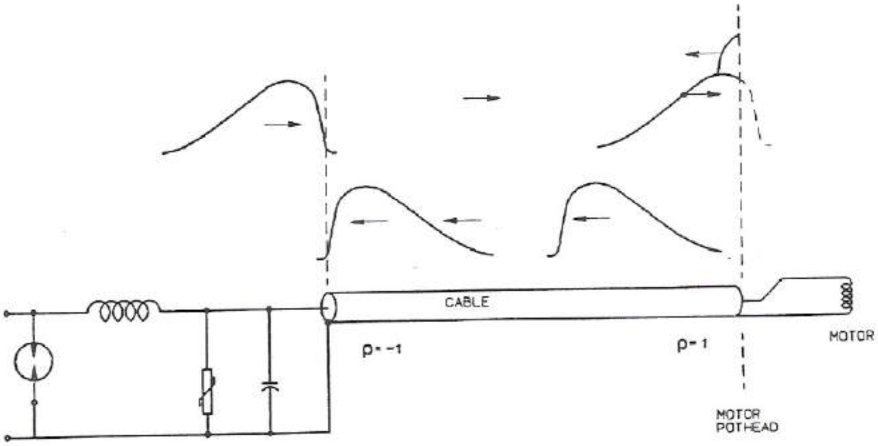

Fig. 8. Impulse propagation in ESP system.

reputable manufacturers have long since implemented effective lightning protection. A second concern is that unbalanced currents through the three capacitors can produce an ac component in the LCG lead, which periodically reverses the polarity of cathodic currents. The dc current can be raised to prevent this. If these concerns can be allayed, such installations should be easy to protect because galvanic action in the soil necessitating cathodic protection implies adequate soil moisture and low ground resistance, and the sacrificial anodes themselves represent a good ground. Lastly, switchboards and their surface equipment should be adequately grounded for personnel safety.

Another concern is whether or not to connect the utility ground to either the ground grid or casing. Without a reasonably low resistance utility ground, surface equipment, notably transformers and switchboards, can be damaged. However, an overhead ground wire can also conduct lightning energy into well site grounds, possibly inflicting ESP damage itself, and therefore in regions with low-resistance ground a separate ground connection is preferred. However, operating codes imposed by local utilities often supercede all of these arguments.

Loading Effects of the ESP

An ESP motor on a length of power cable must be considered a distributed parameter load when lightning impulses are the excitation source. The cable must then be modeled as a transmission line with an approximate surge or characteristic impedance $Z_0 \simeq 50\ \Omega$ terminated by the motor. A representative upper frequency for the standard lightning impulse (Fig. 2) is readily calculated from

$$f=\frac{2.2}{2\pi t_r}=\frac{2.2}{2\pi\times .8\times 1.2\ \mu\text{s}}=365\ \text{kHz} \tag{4}$$

where t_r is the 10 to 90 percent risetime [8]. Motor leakage reactances at this frequency are on the order of 10 kΩ and, relative to Z_0, could be considered an open-circuit termination on the cable. Consequently, the reflection coefficient is

$$\rho=\frac{Z_L-Z_0}{Z_L+Z_0}\simeq 1 \tag{5}$$

where Z_L is the motor load or termination impedance. Thus a lightning impulse propagating down the cable is almost totally reflected when it arrives at the motor pothead. Worse yet, pothead voltage is momentarily doubled at the instant the crest voltage arrives at the pothead. This effect has been measured [9] and along with tight geometrical constraints helps to explain the prevalence of pothead insulation failures.

For assessing the loading effect, only the characteristic impedance of the cable needs to be considered for reasonable lengths of cable. Propagation velocity is approximately 500 ft/μs, and for 5000 ft of cable the reflected wave would reach the input 20 μs after the initial impulse. At that time it would be inverted and reflected because the filter capacitor represents a low source impedance, which produces a reflective coefficient $\rho = -1$. The amplitude of the impulse is somewhat attenuated due to cable losses, as it propagates and reflects up and down the cable, and the peak amplitude of the first reflection at the pothead is the largest. Some of these effects are illustrated in Fig. 8.

Loading effects on filter performance are illustrated by impulse tests with the Fig. 9 circuit for 10 and 100 Ω loads applied to two different L–C combinations. In the first combination $C = 10\ \mu$F and $L = 1$ mH air core, the Air1 inductor shown in Fig. 16, and it was noted that the magnitude of the output voltage V_{out} in Figs. 10 and 11 was unchanged, although the time base for one trace in both figures was extended to 500 μs/cm to show more of the signal. Primary harmonics for the impulse were several orders of magnitude above the filter's resonant frequency $f_0 = 1.6$ kHz, and at those harmonic frequencies, variation of the damping factor $\xi = \sqrt{L/C}/2R_L$ for the two loads had little effect because of the steep attenuation roll-off (Fig. 17). Consequently, variations in load resistance had little effect on the output voltage.

On the contrary the combination of a 1-mH iron core inductor, Fel in Fig. 16, and a 0.5-μF capacitor in the second filter was strongly affected by changing loads and damping factors (Figs. 12 and 13). When the filter was not so heavily loaded, output voltage exceeded 3.5 kV, and it was still over 2 kV with the 10-Ω load. Obviously Fel had a very low coefficient of coupling to produce the extreme reduction in inductance at higher frequencies, and for typical impulse

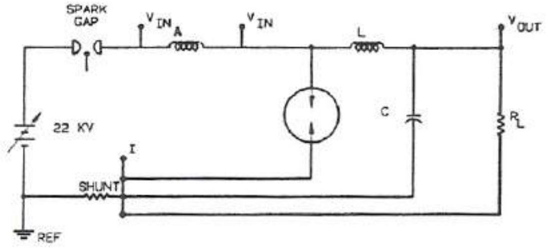

Fig. 9. Test circuit for evaluation of load resistance effect.

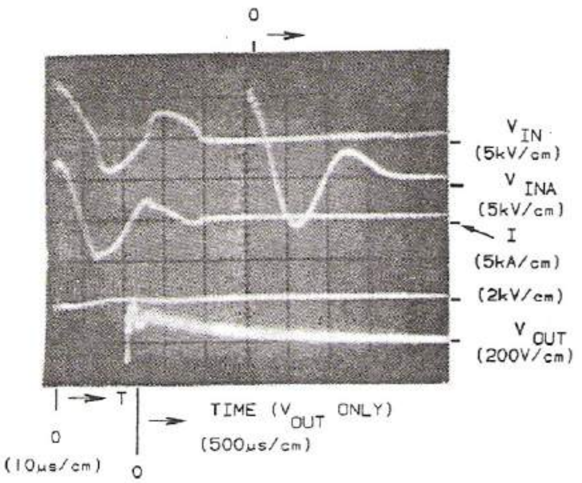

Fig. 10. Lightning filter performance with $R_L = 100\ \Omega$, $L = 1$ mH air core, and $C = 10\ \mu$F.

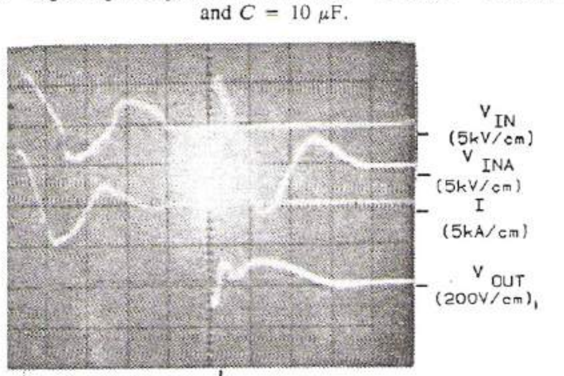

Fig. 11. Lightning filter performance with $R_L = 10\ \Omega$, $L = 1$ mH air core, and $C = 10\ \mu$F.

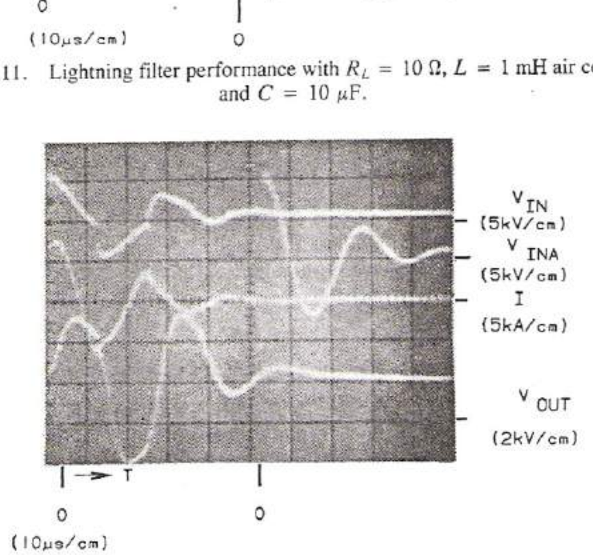

Fig. 12. Lightning filter performance with $R_L = 100\ \Omega$, $L = 1$ mH iron core, and $C = 0.5\ \mu$F.

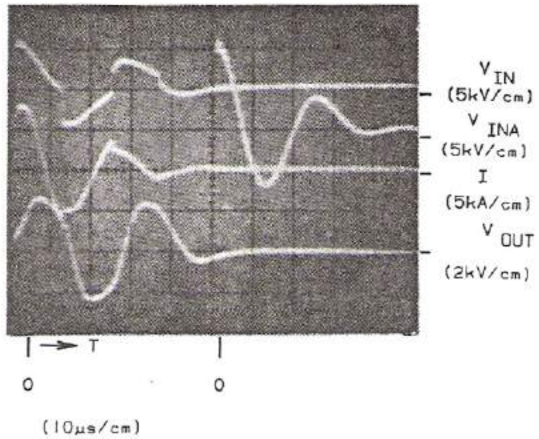

Fig. 13. Lightning filter performance with $R_L = 10\ \Omega$, $L = 1$ mH iron core, and $C = 0.5\ \mu$F.

harmonics the calculated resonant frequency was in the same range as the harmonics producing the phenomena recorded. Thus proper selection of components is essential to minimizing effects of loading.

Component Selection

For proper operation of the lightning filter the individual components must be carefully selected. The large energy levels associated with lightning strikes, the abundance of frequencies in the high end of the spectrum, and the restrictions of the ground network combine to put unique design constraints on the filter components.

The most important specification for the lightning arrester is the front-of-wave sparkover level. This value should not exceed 650 percent of the motor's rated operating voltage. In general, expulsion-type arresters exhibit a sparkover voltage which is excessive, and the same is true of most primary arresters. The arrester chosen for a lightning filter should be a secondary arrester capable of conducting at least 20 000 A and should have an energy rating of at least 1500 J. Fig. 14 shows the filter output characteristics when a typical valve-type secondary arrester is installed in the test circuit of Fig. 9, while Fig. 15 shows the characteristics when a silicon-oxide-type arrester is used. Note that the amplitude of the output with the valve-type arrester installed is approximately 450 V, while with the silicon-oxide arrester the output amplitude is approximately 250 V. Thus, until something better can be found, silicon oxide arresters appear to perform the best in this circuit. In laboratory testing under ideal ground conditions, the silicon-oxide arrester exhibited ringing at the resonant frequency of the 0.5-μF high-voltage impulse generation capacitor and the 10-μH waveshaping inductor. Under practical conditions, when a small resistance was present in the ground return path, this oscillation was well damped.

Without question the primary specification for the inductor is that its inductance should stay relatively constant, at least up to 500 kHz. Fig. 16 illustrates variation of inductance with frequency for air-core and iron-core inductors and shows that the inductance of the air-core coils Air1 and Air2 remains essentially constant independent of frequency, while that of the iron-core coils, particularly Fe1, drops dramatically with in-

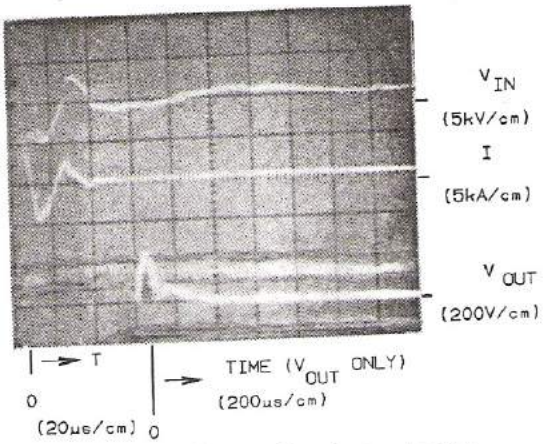

Fig. 14. Test results for valve-type arrester.

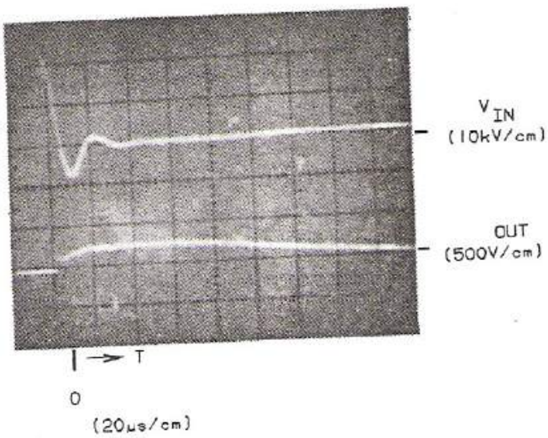

Fig. 15. Test results for silicon oxide type arrester.

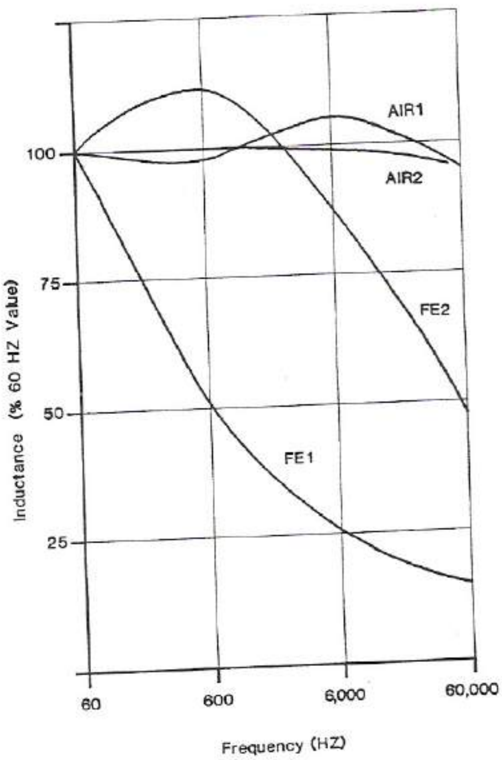

Fig. 16. Variation of inductance with frequency.

creasing frequency. Since the spectral content of the lightning waveform includes frequencies up to several hundred kilohertz, the air-core coils tested definitely provide superior operation in the lightning filter.

Inductor copper loss is the major source of heat in these filters, and to keep operating costs down, inductor wire must have the lowest resistance that is practical. Obviously, to obtain the desired inductance with minimum wire resistance, or concurrently minimum wire gauge and length, requires magnetic core materials, but at the same time inductance must be relatively constant to 500 kHz. The latter requirement implies minimum leakage reactance or near-unity coupling coefficient and a core material with low hysteresis and eddy-current losses at high frequencies.

Testing of various capacitor types in the Fig. 9 circuit indicated that, within reason, the quality of the capacitor has negligible effect on the transient attenuation of the filter, or in other words, the lead inductance of the capacitor was negligible. Of course, only capacitors with appropriate voltage and direct-on-line starting surge ratings were tested, and capacitors with low dissipation factors were selected to minimize heating and improve reliability.

The resonant frequency of the low-pass filter must be sufficiently low to provide adequate attenuation of the complete frequency spectrum of the lightning waveform, without affecting the motor at normal operating frequencies from 40 to 90 Hz. This requirement must be balanced against the need to keep L and C reasonable, both in physical size and in cost. In practice, f_0 on the order of one to three kilohertz can be realized by choosing $L = 0.5$ to 1.0 mH and $C = 5.0$ to 10.0 μF. Such values designed to tolerate the voltages and currents involved with ESP's are available commercially or can be fabricated economically.

Filter performance is presented in Fig. 17. It should be noted that near the resonant frequency f_0, the damping factor $\xi = \sqrt{L/C}\,/2R_L$ has a pronounced effect on output amplitude; but at $10\,f_0$ and above, the frequency spectrum of the lightning waveform, attenuation is at the rate of 40 db/decade (12 db/octave), independent of ξ. Entering the data presented in Figs. 10–13 and 16 demonstrates this design philosophy. From Figs. 12 and 13 an oscillation in V_{OUT} was observed at 42 kHz. At this frequency, Fel = 37 μH. Performance for the two circuits is given in Table I.

At 42 kHz the Air1 circuit had measured output larger than calculated, but obviously the output was at a much lower frequency. On the contrary the Fel circuit at 42 kHz had larger calculated output than measured, but output magnitude was increasing and could easily have reached higher values if oscillations were sustained. This confirms the simple truth that Bode plots assume sinusoidal steady-state excitation, and lightning impulses are transient in nature.

Should a brief sustained oscillation occur at circuit resonant frequency, possibly produced by a contactor showering arc, output voltage can go to 10 or 11 times input voltage, depending on ξ, and exceed the capacitor rating. To protect against this, and also to provide a second limit on motor transient voltage, a metal-oxide varistor (MOV—the "special arrester" in Fig. 4) must be connected across the capacitor.

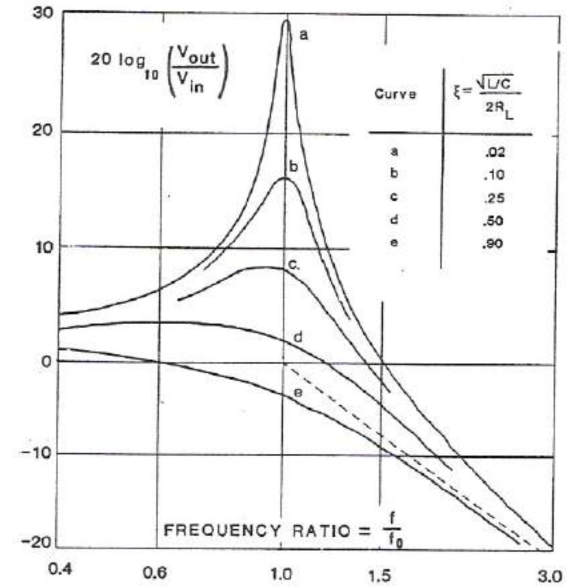

Fig. 17. Bode plot for two-pole lightning filter.

TABLE I
CIRCUIT PERFORMANCE

	R_L	ξ	Decibels at f_0	V_{out}/V_{in}	Decibels at 42 kHz	V_{out}/V_{in} Calculated	V_{out}/V_{in} Measured
Airl + 10 μF	100	0.05	20	10	−57	0.0015	0.028 − 0.063
f_0 = 1.6 kHz	10	0.5	0	1	−57	0.0015	0.018 − 0.04
Fel + 5 μF	100	0.043	21.5	11.9	11	3.53	0.58 − 1.03
f_0 = 37 kHz	10	0.43	1.5	1.2	0	1.0	0.49 − 0.69

Field Tests

Several attempts were made to obtain meaningful field test data, but at best the data obtained were incomplete and inconsistent. Many different factors affect ESP run life and correction of one problem will only improve matters if all other factors remain constant. This is seldom the case. There are major obstacles encountered.

1) Seldom is only one thing changed and the well-site equipment left in that state long enough to obtain meaningful results. In this instance an improved motor control was concurrently deployed and power company reclosure cycles were extended to 2.5 s.
2) Over short periods of time, weather and particularly thunderstorms are random.
3) Some of the information could be of a proprietary nature and the owner is concerned about divulging it.
4) Variations in well performance, gas breakthrough, change in water cut, change in acidizing chemicals, etc., can have pronounced effects on equipment run life.

In the absence of concrete evidence that lightning filters are a distinct asset, evaluations are left to those most closely associated with the equipment and its problems. In general, their consensus has been that fewer ESP units are damaged by a passing thunderstorm when lightning filters are used than was previously the case without them. Consequently, over 400 units are installed and in operation today. We believe these operators' economic commitments to the filters are as significant a testimonial as is currently possible.

Conclusion

Principles of induction motor lightning protection were reviewed, and specific requirements for ESP motor protection were presented. The classical protection circuit was analyzed and measured for two specific reasons.

1) *Proper component specification for ESP protection.* Improperly chosen components produce too high a resonant frequency, inadequate attenuation, and damping that is too low. Quality parts are necessary to ensure proper operation over the required frequency spectrum and long term reliability.
2) *Separation of grounds.* For virtually all oil field installations, separation of grounds is essential to provide adequate equipment protection for the typical ground grid resistances encountered. Grounding techniques may be impacted by cathodic protection considerations and local power utility codes.

The authors believe that a properly designed and applied transient filter can and will reduce lightning induced failures in ESP's. This paper has shown analytically and by laboratory tests that imposed transients can be significantly attenuated before reaching the critical downhole pumping system. Operators currently believe these results are confirmed by field experience.

Therefore it may be concluded that properly designed and applied transient filters are a sound economic choice when lightning is known to be a problem.

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REFERENCES

[1] *Electrical Transmission and Distribution Reference Book*. East Pittsburgh, PA: Westinghouse Electric Corp., 1944, chap. 14.
[2] *IEEE Standard Techniques for High-Voltage Testing*, IEEE Std. 4-1978, Institute of Electrical and Electronic Engineers, 1978, New York, NY.
[3] *Nema Standards Publication Motors and Generators*, Nov. 1972, MG1-1972, Parts 1 and 12; ANSI C52.1-1973, National Electrical Manufacturers Association, New York, NY.
[4] *Impulse Voltage Standards for Rotating Machines*, Insulation Subcommittee, Working Group 792, Rotating Machinery Committee, IEEE Power Engineering Society, New York, NY.
[5] *IEEE Recommended Practice for Insulation Testing of Large AC Rotating Machinery with High Direct Voltage*, 1977, IEEE Std. 95-1977, Institute of Electrical and Electronic Engineers, New York, NY.
[6] *Getting Down to Earth*. Blue Bell, PA: Biddle Instruments, 1982.
[7] L. J. Giaculetto, *Electronic Designers Handbook*. New York, NY: McGraw-Hill, 1977.
[8] J. Millman and H. Taub, *Pulse, Digital and Switching Circuits*. New York, NY: McGraw-Hill, 1965.
[9] Unpublished correspondence, GE Industrial Sales Division to Ethyl Corporation, Nov. 1970.

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