Analysis of Transmission Lines With Arrester Termination, Considering the Frequency-Dependence of Grounding Systems

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Abstract—The paper proposes a modeling technique to analyze the response of a transmission line terminated by a lightning arrester connected to a grounding system buried in a lossy ground. In this technique, the transmission line is modeled in the frequency domain with the aid of Baum–Liu–Tesche equations, while the grounding system to which the arrester is connected is treated using a general electromagnetic approach. The electromagnetic approach is based on the solutions to Maxwell’s equations obtained by using the method of moment in the frequency domain. The arrester nonlinearity is included in the frequency-domain analysis using the arithmetic operator method. To examine the performance of the proposed modeling technique, numerical results are presented for a single-conductor transmission line connected to a typical lightning arrester. The results are first compared for a simple grounding configuration of a vertical rod with those obtained using the well-known electromagnetic transient program, showing the validity of the proposed technique. The generality of the technique is then demonstrated by studying the results of a more complex case of a typical grounding grid. It is shown that the early time responses of the lightning overvoltages are affected by the harmonic impedance of the grounding system.

Index Terms— Arresters, grounding electrodes, lightning, nonlinear systems, overvoltage protection.

I. INTRODUCTION

LIGHTNING arresters are widely used in power systems in order to suppress the overvoltages in a power system to prevent damage to equipments and disruption of the service. The arresters are selected through the insulation coordination study that is usually carried out for power system overvoltages including, switching surges as well as lightning overvoltages [1]. Insulation coordination is generally determined from the known characteristics of the electrical system of interest and expected surge voltages, and the performance of selected surge arresters. Additionally, the transient impedance of the grounding system where the arrester is connected, might also affect the protection performance of lightning arresters. For instance, in the case of a high-transient grounding impedance, the overvoltages might exceed the lightning impulse withstand level (LIWL) of power system apparatuses [2]. In order to correctly specify an arrester to protect a transmission line, it is thus desirable to consider the frequency dependence of the grounding system to which the arrester is connected.

A proper analysis of the protection performance of a lightning arrester requires that the grounding system where the arrester is connected be adequately incorporated in the modeling. This might be a challenging task in the sense that one should consider both the nonlinearity of the arrester and the frequency dependence of the grounding system. Although time-domain modeling techniques [e.g., electromagnetic transients program (EMTP)] are well suited to treat nonlinear loads, they cannot be easily used to deal with problems involving frequency-dependent grounding systems. In fact, these techniques require a time-domain representation of the grounding system to be used properly in the analysis. Several analyses have been conducted for the proper modeling of grounding systems to be incorporated in a time-domain analysis of the transient performance of the electrical systems (e.g., [3]–[5]). These modeling techniques represent the grounding system with either lumped equivalent circuits or distributed ones. However, most of these techniques are based on a quasi-static approximation that may not provide accurate results for high-frequency excitations.

To the best of our knowledge, no comprehensive study is available in which the frequency dependence of the grounding system is included in a rigorous way in cases where lightning arresters are involved. The objective of this paper is to investigate the performance of lightning arresters considering the frequency dependency of respective grounding systems. The modeling technique is based on the frequency-domain solution of the governing electric field integral equation (EFIE) for the grounding system using the method of moments (MoM) [6]–[9]. Using the MoM, the grounding system is treated in a rigorous way in the frequency domain from which the frequency-dependent transient impedance of the grounding system is obtained [9]. The nonlinear behavior of lightning arresters is also included in the frequency-domain analysis with the aid of the arithmetic operator method (AOM). The AOM is an extension of the generalized power series analysis and uses basic arithmetic operations on signal spectra in the frequency domain [10]–[14]. It is capable of analyzing any analytically modeled nonlinear elements without any limitations in terms of load nonlinearity while avoiding repeated fast Fourier transform (FFT) and inverse fast Fourier...
transform (IFFT) operations. The applicability of the AOM to model nonlinear systems depends on two factors. First, the input to the system under consideration must be composed of discrete spectra, e.g., a collection of sinusoids. Second, the transfer function of the system under consideration must be reducible to a rational polynomial expression, with transcendental functions being permitted under the provision that their infinite series representation can be truncated [13]–[15].

In order to evaluate the performance of the proposed modeling technique, various case studies will be presented. These include a single-conductor transmission line connected to a lightning arrester at one end whose respective grounding system is buried in a lossy soil characterized by its conductivity and relative permittivity. The structure is excited by a lightning impulse current striking the phase conductor at a point far from both ends and the grounding system is either a simple vertical rod or a complex grid.

The paper is organized as follows. Section II describes the theory of the proposed modeling technique. In this section, the frequency-domain modeling of transmission line, grounding system and lightning arresters are outlined. In Section III, the validity of the proposed model will be discussed by presenting the results of several case studies. General conclusions will be given in Section IV.

II. THEORETICAL MODELING

Consider a transmission line above a lossy ground hit directly by a lightning stroke, as shown in Fig. 1. Without losing generality, the transmission line is considered to be a single overhead conductor, with a lightning arrester at the end. It is also assumed that the lightning arrester is connected to a simple grounding rod buried in a lossy ground with relative dielectric constant \( \varepsilon_1 \) and conductivity \( \sigma_1 \). The arrester behaves as a nonlinear load with a general \( i-v \) characteristics [16], defined as

\[
i_a = p v_a^\alpha
\]

where \( i_a \) is the arrester current and \( v_a \) is the arrester voltage and \( p \) is a real coefficient. For silicon carbide (SiC) arresters, the value of \( \alpha \) varies between 2 and 6 while it takes a value between 10 and 60 for MO arresters [17].

The overvoltage study of this transmission line is carried out by considering the whole electrical system including the transmission line and the lightning arrester in conjunction with the grounding system where the arrester is connected. Owing to the fact that the grounding system is frequency dependent, the analysis will be conducted in the frequency domain. The transmission line is treated using the Baum–Liu–Tesche (BLT) equations, which permits the determination of the currents and voltages at the terminating loads of the line [18]. The inclusion of the grounding system is done by means of its harmonic impedance that adequately represents the frequency dependence of the grounding system and which is determined using a rigorous electromagnetic approach, in which the electric field integral equation is solved using the method of moment in the frequency domain. In fact, the transient analysis of the problem is reduced to the solution of a nonlinear problem, as shown in Fig. 2. The upcoming sections go into elaborate details about the modeling approaches and the solution methodology.

A. Transmission Line Modeling and BLT Equations

With reference to Fig. 1, the transmission line is assumed to have an arrester which is connected to the grounding system and located at the end of the line \( (x = L) \). The arrester could be considered in series with the harmonic impedance \( Z_g(f) \) of the grounding system. To the left of the total terminal load (arrester and the ground impedance, at cut A and B in Fig. 2) the remaining part of the system is assumed to be a linear system and is described by the transmission line equations for a line having a length \( L \), a propagation constant \( \gamma \), and a characteristic impedance \( Z_c \). The linear portion of the transmission line can be represented by an equivalent Norton circuit, as shown in Fig. 3. In the frequency domain, the input admittance of this circuit is given by \( Y_{in} = 1/Z_{in} \), where \( Z_{in} \) is the input impedance of the line. The short-circuit current is defined as \( I_{sc} = V_{oc}/Z_{in} \), where \( V_{oc} \) is the open-circuit voltage [18]. The upcoming formulations
illustrate the use of the BLT equation in the frequency domain to obtain \( Z_{in} \) and \( V_{oc} \).

With reference to Fig. 4, the general solutions for the voltage and current at both ends of a line terminated with load impedances with a series voltage and parallel current source at \( x = x_s \), are given as [18]

\[
\begin{align*}
\left( \begin{array}{c}
V(0) \\
V(L)
\end{array} \right) &= \left( \begin{array}{cc}
1 + \rho_1 & 0 \\
0 & 1 + \rho_2
\end{array} \right) \left( \begin{array}{cc}
-\rho_1 & e^{\gamma L} \\
e^{-\gamma L} & -\rho_2
\end{array} \right)^{-1} \\
\times & \left( \begin{array}{c}
e^{\gamma x_s} (V_0 + Z_L I_s)/2 \\
-e^{\gamma (L-x_s)} (V_0 - Z_L I_s)/2
\end{array} \right) \\
\left( \begin{array}{c}
I(0) \\
I(L)
\end{array} \right) &= \frac{1}{Z_e} \left( \begin{array}{cc}
1 - \rho_1 & 0 \\
0 & 1 - \rho_2
\end{array} \right) \left( \begin{array}{cc}
-\rho_1 & e^{\gamma L} \\
e^{-\gamma L} & -\rho_2
\end{array} \right)^{-1} \\
\times & \left( \begin{array}{c}
e^{\gamma x_s} (V_0 + Z_L I_s)/2 \\
-e^{\gamma (L-x_s)} (V_0 - Z_L I_s)/2
\end{array} \right). \tag{2a}
\end{align*}
\]

Expressions given in (2a) and (2b) constitute the BLT equations, where \( I_o \) and \( V_o \), are, respectively, the current and voltage sources at the excitation point and \( \rho_i \) is the voltage reflection coefficient at each load defined as follows:

\[
\rho_i = \frac{Z_{Li} - Z_e}{Z_{Li} + Z_e}. \tag{3}
\]

For the determination of \( Y_{in} \) in Fig. 3, the line is supposed to be open-circuited at the end (\( x = L \)) and with the matching impedance at the beginning (\( x = 0 \)). Using the BLT equations, the load voltage \( V(L) \) can be evaluated. The ratio of this open-circuit voltage to the excitation current gives the input impedance

\[
Z_{in} = \frac{V(L)}{I_o} = Z_e \frac{1 + \rho_1 e^{-2\gamma L}}{1 - \rho_1 e^{-2\gamma L}} \tag{4}
\]

in which the reflection coefficient \( \rho_1 = 0 \), hence, the input impedance is identical to the characteristic impedance of the line.

Similarly, the open-circuit voltage can be determined by calculating \( V(L) \) due to a single lumped voltage source at \( x = 0 \). Setting the reflection coefficient at the end \( \rho_2 = 1 \), the open-circuit voltage reads

\[
V_{oc} = V_0 e^{-\gamma L} \frac{(1 - \rho_1)}{1 - \rho_1 e^{-2\gamma L}} = V_0 e^{-\gamma L}. \tag{5}
\]

When a lightning return-stroke hits the transmission line, the injected current separates into two equal halves, each half traveling in either direction of the line. At the first moment, the voltage at the strike point could be calculated by multiplying the characteristic impedance of the line by the injected lightning current. Thus, the appropriate short-circuit current in Fig. 3, is given by

\[
I_{sc} = \frac{V_{oc}}{Z_{in}} = \frac{I_p Z_e e^{-\gamma L}}{Z_e} = I_p e^{-\gamma L}. \tag{6}
\]

where \( I_p \) is the injected lightning impulse current.

The effect of lossy ground on the line parameters is taken into account by use of Carson’s equations [19].

**B. Arrester Treatment in the Frequency Domain**

With reference to Fig. 3, the transmission line is represented by its equivalent circuit where the short-circuit current \( I_{sc} \) and the input impedance \( Y_{in} \), both being defined as functions of frequency. The frequencies required for composing \( Y_{in} \) are the spectral content of the voltage at the arrester terminal, while \( I_{sc} \) is calculated only at the excitation frequencies. It should be noted that the arrester nonlinearity changes the frequency content of the voltage at the arrester terminal.

Let \( \nabla_r \) and \( \nabla_g \), respectively, correspond to the arrester terminal voltage (that is called arrester residual voltage) and the voltage along the transient impedance of the ground \( (Z_g) \), and \( I_r \) denotes the current flowing through the arrester. Applying the Kirchhoff’s current law (KCL) at the load (arrester and the grounding system) leads to

\[
-T_{sc} + \nabla_{in} (\nabla_r + \nabla_g) + T_r = 0 \tag{7}
\]

where

\[
\nabla_r = [V_{r,0}, V_{r,1}, V_{r,2}, \ldots, V_{r,2p-1}, V_{r,2p}]^T \tag{8}
\]

\[
T_{sc} = [0, I_{sc,1}, -I_{sc,2}, \ldots, 0, 0]^T \tag{9}
\]

\[
T_r = [I_{r,0}, I_{r,1}, I_{r,2}, \ldots, I_{r,2p-1}, I_{r,2p}]^T \tag{10}
\]
\[ Y_{in} = \begin{bmatrix} 0 & 0 & \cdots & 0 & 0 \\ 0 & Y_{11} & \cdots & -Y_{12} \\ Y_{11} & Y_{12} & \cdots & 0 \\ 0 & 0 & \cdots & 0 \end{bmatrix} \] (11)

\( P \) is the number of frequencies in the output spectral vector, \( Y_{rk} \) and \( Y_{ik} \) are the real and imaginary parts of \( Y_{in}(\omega_k) = Y_{rk} + jY_{ik} \), and \( V_{r,k}, I_{r,k} \) denote, respectively, the \( k \)th components of the arrester terminal voltage waveform \( vr(t) \), and the arrester current waveform \( ir(t) \)

\[ v_r(t) = V_{r,0} + \sum_{k=1}^{P} \{ V_{r,2k-1} \cos \omega_k t + V_{r,2k} \sin \omega_k t \} \] (12a)

\[ i_L(t) = I_{r,0} + \sum_{k=1}^{P} \{ I_{r,2k-1} \cos \omega_k t + I_{r,2k} \sin \omega_k t \} \] (12b)

Also, \( i_{sc,k} \ (k = 1, 2, \ldots, K \) is the \( k \)th component of the short-circuit current waveform \( i_{sc}(t) \)

\[ i_{sc}(t) = I_1 \cos \omega_1 t + I_2 \cos \omega_2 t + \cdots + I_K \cos \omega_K t \] (12c).

Denoting the arrester \( i-v \) relationship as \( i = f_u(v_r) \), we rewrite (7) as

\[ \mathcal{T}_{sc} = \mathcal{T}_r + Y_{in} Z_g \mathcal{T}_r + Y_{in} V_r \] (13)

where \( Z_g \) is the matrix form of the grounding system harmonic impedance

\[ Z_g = \begin{bmatrix} 0 & 0 & \cdots & 0 & 0 \\ 0 & Z_{11} & \cdots & -Z_{12} \\ Z_{11} & Z_{12} & \cdots & 0 \\ 0 & 0 & \cdots & 0 \end{bmatrix} \] (14)

\( Z_{rk} \) and \( Z_{ik} \) are the real and imaginary parts of \( Z_g(\omega_k) = Z_{rk} + jZ_{ik} \). The analysis method for determining \( Z_g \) is based on the frequency-domain solution of the governing EFIE by the MoM [6]–[8], [20].

It should be noted that for given excitation frequencies and the maximum order of arrester nonlinearity, the so-called basic intermodulation product description (BIPD) table defines the basis for the spectral vectors by determining all of the nonnegative combinations or weightings of these frequencies up to the maximum order of arrester nonlinearity. A detailed description of the BIPD table is found in [10] and [14].

To solve (13) for \( \mathcal{T}_r \), we need to expand \( \mathcal{T}_r \) in terms of \( \mathcal{V}_{r} \). This is done by converting the arrester \( i-v \) characteristics (1) in the frequency domain. Recall from the theory of Fourier transforms that repeated multiplication of time-domain functions corresponds to repeated convolution in the frequency-domain, i.e.,

\[ [v(t)]^n \rightarrow \mathcal{F} \{ V(f) \} V(f) \cdots V(f) \] (15)

We can now use the arithmetic operator method (AOM) to describe the convolution operations as matrix vector operations. The AOM uses basic arithmetic operations on signal spectra in the frequency domain [13]. The use of AOM for calculating \( V_r(\omega) \) at each harmonic frequency is thoroughly described in [21].

III. NUMERICAL RESULTS

To demonstrate the validity of the proposed technique, we consider a single conductor transmission line above a lossy ground with a lightning arrester at the end. The conductor diameter is \( d = 5.62 \text{ cm} \) and the vertical height at mid-span is \( h = 22 \text{ m} \). The conductor is lossy and its parameters (resistance, inductance, and capacitance) are considered to be frequency dependent. The effect of a lossy ground is taken into account by Carson’s formula [19]. The resistance and inductance of the line are, respectively, shown in Figs. 5 and 6 [22]. The line capacitance, \( C = 0.0074 \mu\text{F} \) is fixed within the frequency range that is required for the analysis while the effects of the line
conductance and the ground admittance are neglected [23]. The arrester is connected to a grounding system buried in a homogeneous soil. Without losing generality, the nonlinear characteristic of the considered arrester is shown in Fig. 7. In the simulations, a 20-kA, 2/20-μs lightning current is supposed to directly hit the transmission line at a point far from both ends of the line; hence, the line is considered with a matched load at its left-end terminal (see Fig. 2). Notice that the early time parameters of the lightning current (i.e., rise-time and peak values) are within the range of typical values that have been adopted by many researchers (e.g., [24]). For the sake of simplicity, it is assumed that soil ionization does not occur around grounding electrodes, and hence, soil is selected to be a linear medium. It is worth noting that the nonlinearity introduced by ionization of the soil [25] is not relevant to the common case where the surge arrester is located in a substation. This is because the extensive size of the grounding system limits the current density to values below the applicable ionization threshold. On the other hand, there are emerging applications to long power lines 500 kV and above where a third surge arrester is placed in the middle of the line to limit the magnitude of the transient overvoltages caused by switching operations. In such cases, the size of the grounding electrode will be limited, perhaps to a single ground rod. Ionization of the soil may then occur and introduce nonlinearity. The nonlinear behavior of soil ionization can be represented by a nonlinear load that is readily included in the proposed modeling technique [26].

Prior to the solution, we first obtain the characteristic impedance and the propagation constant of the transmission line. The harmonic impedance of the grounding system [i.e., \( Z_g(\omega) \)] is calculated by making use of the MoM. The BLT equations are used to obtain the Norton’s equivalent circuit of the transmission line [i.e., \( I_{in}(\omega) \) and \( Y_{in}(\omega) \)]. The whole system is then analyzed in the frequency domain, using the AOM. To show the validity of the proposed method, we first compare our results with those obtained using EMTP, considering a simple configuration vertical rod). Then, to illustrate the applicability of the proposed method, we also present results related to a complex grounding system.

A. Vertical Grounding Rod

We consider the case of a lightning strike to the mid-span of the transmission line terminated with an arrester (Fig. 1). The arrester is connected to a vertical grounding rod of circular cross-section of radius \( r = 12.5 \) mm, buried in a soil with conductivity \( \sigma_1 = 0.01 \) mho/m and a relative permittivity \( \varepsilon_r = 10 \). Fig. 8 shows the input impedance (also called the harmonic impedance to ground) of the vertical grounding rod for three different rod lengths, computed using the general electromagnetic approach. As can be seen in this figure, the harmonic impedance shows a frequency-independent behavior in the low-frequency range, where the magnitude of harmonic impedance is equal to the static resistance at low frequencies. It is also seen that harmonic impedance takes different values at higher frequencies, demonstrating the frequency dependence of the grounding system. This is quite different from the static model of the grounding system, which fails in providing accurate results at high frequencies when used for the calculation of the transmission line overvoltages. One should note that at higher frequencies, the grounding system might be dominantly either inductive or capacitive. If the inductive behavior prevails the capacitive one, the harmonic impedance is likely to have a sharp increase for higher frequencies. This behavior is clearly confirmed in the results shown in Fig. 8. The harmonic impedance of the grounding system at higher frequencies is dramatically affected by the electromagnetic properties of the soil as well as the geometry of the grounding system [9]. To further understand the grounding system behavior, the harmonic impedance of a vertical rod of length \( l = 6 \) m and a circular cross-section of radius \( r = 12.5 \) mm, buried in a ground characterized by different values for the soil conductivity (0.1, 0.01, and 0.001 S/m) are shown in Fig. 9. From this figure, it can be clearly seen that for soils with low conductivity, the harmonic impedance takes smaller values at higher frequencies. This is due to the fact that for low conductivities, grounding system dominantly behaves capacitively, which results in smaller impedance. Further study of the results shown in Fig. 9 accentuates the frequency dependence of the grounding system.
A distributed-parameter circuit can be used in EMTP to adequately represent the harmonic impedance of the vertical grounding rods \[5\], \[9\]. In this approach, the rod is divided into \(N\) fictitious segments and each segment of the rod is represented by an \(R–L–C\) section (Fig. 10). For each segment the parameters are identical and defined as

\[
R_n = \frac{\rho N}{2\pi l} \left( \log \frac{4l}{r} - 1 \right) \quad (\Omega)
\]

\[
C_n = \frac{2\pi \varepsilon l}{N} \left( \log \frac{4l}{r} - 1 \right) \quad (F)
\]

\[
L_n = \frac{\mu_0 l}{2\pi N} \left( \log \frac{2l}{r} - 1 \right) \quad (H)
\]

where \(l\) is the length of the vertical rod and \(r\) is the radius. \(R_n\), \(L_n\), and \(C_n\) are, respectively, the resistance, inductance, and capacitance for each segment. We use this kind of representation in order to model the vertical grounding rod in EMTP.

Fig. 11 shows the input impedance of the vertical grounding rod obtained by the distributed-parameter circuit model, for two different rod lengths, \(l = 6\) m and \(l = 10\) m, respectively. The rods were subdivided into \(N = 10\) segments. Also shown in this figure, are the harmonic impedances obtained by the electromagnetic (EM) approach. It can be seen that the results obtained by the distributed-parameter circuit approach are generally consistent with their EM counterparts up to \(f = 500\) kHz. At higher frequencies, however, the magnitudes of the harmonic impedances obtained by these two approaches diverge. It should be noted that the distributed-parameter model is based on the assumption of having transverse electromagnetic (TEM) propagation on a uniform infinite conductor in a homogeneous medium and neglects the effects of the earth–air interface \[9\]. As opposed to the distributed-parameter circuit approach, the EM approach uses the least simplifications and generates more accurate results for a wide range of frequency \[6\].

The frequency range that is required for the analysis depends upon the wave shape of the excitation current in time domain. For this purpose, one should determine the time sampling rate in a way that fulfills the Nyquist–Shannon theorem. The frequency range adopted for the simulation is \(0–500\) kHz. The upper frequency is high enough for the \(2/20\) \(\mu\)s current waveform considered, in this study. Within the adopted frequency range, the harmonic impedance obtained using the distributed-parameter circuit approach is in reasonable agreement with that obtained using the EM approach. Therefore, it appears legitimate to use this configuration to test the proposed approach by comparing its results to those obtained using EMTP.

Fig. 12 shows the transmission line overvoltages (for a line length of 400 m) computed with the proposed approach, considering two different grounding rod lengths, 3 and 6 m. For comparison, results obtained by EMTP are also given in the same figure. It can be seen that our results are generally in very good agreement with their EMTP counterparts. The observed slight differences might essentially be due to the difference between the two harmonic impedances used in the calculations. As to the case of the arrester residual voltage, the result associated with the ground rod of length \(l = 6\) m, obtained by the proposed method, is shown in Fig. 13. Also shown in this figure is the residual voltage obtained by EMTP. It can be clearly seen that these results are also in good agreement, further demonstrating the validity of the proposed method. The difference in the late time undershoot might be due to the numerical errors introduced.
Fig. 12. Lightning overvoltage at the end of the transmission line of length \( L = 400 \) m considering two different grounding rod lengths, 3 and 6 m, and circular cross-section of radius \( r = 12.5 \) mm, buried in a soil with a conductivity \( \sigma_1 = 0.01 \) mho/m and a relative permittivity \( \varepsilon_1 = 10 \).

Fig. 13. Arrester residual voltage when the transmission line is of length \( L = 400 \) m and the vertical grounding rod is of length \( l = 6 \) m and circular cross-section of radius \( r = 12.5 \) mm, buried in a soil with a conductivity \( \sigma_1 = 0.01 \) mho/m and a relative permittivity \( \varepsilon_1 = 10 \).

through the Fourier series that is used to obtain the time domain waveform.

To further evaluate the accuracy of the proposed method, results for various transmission line lengths are shown in Fig. 14. It can be seen that, as a result of the considered wire and ground losses, the traveling waves on the transmission line experience attenuation. For comparison, results obtained by EMTP are also given in the same figure.

B. Grounding Grid

To demonstrate the ability of the proposed method to incorporate complex grounding systems, we consider the case in which the arrester is connected to a grounding grid (Fig. 15). The grounding grid in this case is an equally spaced 20 m \( \times \) 20 m square. The depth of the grid is 1 m and the conductors are of radius \( r = 7 \) mm. The analysis is done for two different soil conductivities (i.e., \( \sigma_1 = 0.01 \) and 0.001 mho/m) having the same relative permittivity of \( \varepsilon_1 = 10 \). Fig. 16 shows the harmonic impedances of the grounding grid. Results associated with the transmission line overvoltage at the arrester termination are shown in Figs. 17 and 18 for \( \sigma_1 = 0.01 \) and 0.001 mho/m, respectively. Also shown in these figures are the computed transmission line overvoltages, using a static model for the grounding system. A comparison of the results shown in these figures indicates vivid differences between the waveforms of overvoltages obtained using the two modeling approaches. This observation justifies the necessity of considering the frequency dependence of grounding systems in the transient analysis of transmission.
IV. CONCLUSION

We have proposed a modeling technique to analyze a transmission line terminated by a grounded lightning arrester. The grounding system can be any complex configuration of conducting wires buried in a lossy ground. The technique involves frequency-domain representations of the transmission line, lightning arrester and grounding systems. The transmission line is treated in frequency domain with the aid of the BLT equations while the grounding system is modeled using the MoM in the frequency domain. The arrester nonlinearity is included in the frequency-domain analysis, using the arithmetic operator method. The main feature of the proposed technique is its efficiency in incorporating the frequency dependency of the grounding system in the overvoltage study of transmission lines protected by lightning arresters. It can also be used to analyze the lightning performance of transmission/distribution lines, particularly in cases where frequency dependence and nonlinearities (such as corona effects) are to be investigated. The accuracy of the proposed modeling technique is verified by comparing the numerical results for the case of a single-conductor transmission line connected to a typical lightning arrester with those obtained using the EMTP. Also, the efficiency of the technique is demonstrated considering the case of a complex grounding system, involving a complete grid of buried conductors. It is shown that the early time response of the lightning overvoltages might be markedly affected by the harmonic impedance of the grounding system. Results presented in this paper further substantiate the importance of frequency dependence of grounding systems for correct estimation of lightning overvoltages in transmission lines required in the insulation coordination study of power systems.

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REFERENCES

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