Numerical Study of Influence of Ice Location on Galloping of an Iced Conductor

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ABSTRACT

In the field of overhead transmission lines, galloping is of immediate concern due to its impact over the mechanical reliability and serviceability of electrical power networks. It is known that smooth circular cylinders are immune to gallop and there are only few galloping cases on bare conductors reported in the literature. Hence, galloping of transmission lines are usually accompanied by ice accretion. Although ice accretion over conductors is required for a galloping event, not all ice profiles lead to instability. Several icing parameters including the amount of ice accretion, shape of the ice profile, and the initial location (orientation) of the ice over a conductor combine to cause a galloping event. In this paper, the effects of initial orientation of the ice on flow-induced instabilities of an iced conductor are studied by employing an aeroelastic numerical approach. The numerical approach is a two-way loosely coupled fluid-structure interaction consisting of three key modules: Computational Fluid Dynamics (CFD), Computational Structural Dynamics (CSD), and communication and data-handling module. A smooth, glaze iced profile with maximum ice thickness of 37% of the conductor’s diameter is considered at various initial orientations over the conductor to study the risk of galloping at different ice orientations. Moreover, the numerical results are compared with the Den Hartog instability criterion in order to assess the possibility of galloping outside the Den Hartog region.

1. INTRODUCTION

Overhead transmission line conductors are flexible structures subject to unsteady wind-induced loading and consequent motion. The dynamic characteristics of such motion, namely its frequency and amplitude, are directly related to the magnitude and frequency content of the wind loading and the structural dynamic characteristics of the transmission line. Wind-induced motions can be classified into three general categories: 1) very small amplitude and high frequency (Aeolian vibrations), 2) small amplitude and moderate frequency, recognized as wake- and vortex-induced oscillations, and, finally, 3) a self-sustained, high-amplitude, and low frequency flutter instability known as galloping (Blevins 1994, EPRI 2006, Lilien et al. 2007, Paidoussis 2002).
et al. 2011). These three categories of conductor motion are also distinguished by other factors such as energy transfer mechanism, type of motion, and different forms of damage they may induce to transmission line components. For example, Aeolian vibrations and wake-induced oscillations have moderate to high frequency low-amplitude characteristics that may cause wear and fatigue of conductor components, while larger galloping motion, in addition, may cause flashover between adjacent phases, which may lead to power outage and direct cable damage, cable tension increase and dynamic loading on the supporting towers and connecting hardware, and, in extreme situations, cable rupture, structural damage, and tower failure. Severe cases, such as tower failure and blackouts, may occur during winter and in remote areas, complicating the repair process. These damages and other impacts of transmission line vibrations on the reliability and serviceability of electrical power networks are well studied in the literature (e.g. see (EPRI 2006, Lilien et al. 2007)). Each year, millions of dollars are spent worldwide to repair such damages and/or overcome the cost of subsequent economical impact. Hence, wind-induced motions of conductors, and in particular galloping, are an important consideration in designing transmission lines, especially in geographical regions prone to atmospheric icing.

The character of instability in galloping is velocity-dependent and damping-controlled (Païdoussis et al. 2011). It means that during a galloping event, a bluff body receives energy supplied by wind, and the effective damping present in the mechanical line system plays an important role in decreasing or increasing the amplitude of displacements. Effective damping combines both structural and aerodynamic damping energy dissipation phenomena, and in order to have an oscillatory instability, it is required to have a negative effective damping where energy is absorbed by the system rather than dissipated.

Normally, in the case of bare conductors, wind loading, damping, and inertia forces do not impose large motions in the vertical direction when subject to horizontal incident wind, but the aerodynamic conditions of the conductor change dramatically in the presence of atmospheric icing accretions (Lilien et al. 2007). In fact, it is shown that bare smooth-surface cylinders are practically immune to very large amplitude, galloping-type oscillations (Païdoussis et al. 2011), and there are only few galloping cases on bare overhead line conductors reported in the literature (Farzaneh 2008). Therefore, in almost all of the observed conductor galloping events, ice accretion and threshold wind speed combinations are present. Moreover, the orientation of the ice deposit with respect to incident wind, the iced conductor profile, and the magnitude of the incident wind velocity are combined parameters that influence the likelihood of galloping, although it is difficult to assign a confidence level to such predictions. In this paper, the effects of the ice orientation and magnitude of the incident wind velocity on overhead conductor galloping are studied using a computational aeroelastic approach through a number of test cases.

2. NUMERICAL AEROELASTIC APPROACH

Current methods to predict galloping fail in many practical cases, mainly due to their inherent limitations and simplifications. On the one hand, full-scale experimental set-ups are expensive, and such tests are compromised by the difficulty to replicate
realistic natural icing conditions as not all weather conditions and ice deposit profiles can be simulated by icing tunnels or natural icing experiments. On the other hand, computational methods for predicting and preventing conductor galloping through unsteady flow calculation become of great interest. Computational aeroelastic analysis dispenses with the common quasi-steady assumption, and makes it possible to capture the time-accurate response of the structure under different ambient air flow conditions. In addition, the analysis can simulate different natural conditions and ice profiles that are non-uniform along the span, which are more realistic and therefore contribute to the added credibility and acceptability of the numerical aeroelastic approach.

2.1. Fluid Dynamics

The unsteady Reynolds-Averaged Navier-Stokes (URANS) equations are solved using FENSAP-ICE, a second order time accurate, 3D finite element compressible Navier-Stokes solver (Habashi et al. 2004, Habashi 2009). The Arbitrary Lagrangian Eulerian (ALE) formulation of the Navier-Stokes equations is applied to compute the time-accurate solution of the flow field with moving meshes. The non-dimensional ALE formulation of URANS equations used in FENSAP-ICE can be expressed as follows:

\[
\frac{\partial \rho}{\partial t} - \nabla \cdot \left( \rho \mathbf{u} \right) = 0 ,
\]

\[
\frac{\partial \rho \mathbf{u}}{\partial t} - \nabla \cdot \left( \rho \mathbf{u} \mathbf{u} \right) + \left( \rho \mathbf{u} \right) \nabla p = - \nabla \cdot \mathbf{\tau} - \left( \rho \mathbf{\bar{u}} \mathbf{u} \right) ,
\]

where \( \rho \) is air density, \( t \), time, \( \mathbf{u} \), the \( i^{th} \) component of velocity, \( \mathbf{\bar{u}} \), mesh velocity, \( p \), pressure, \( \mathbf{\tau} \), stress tensor, \( \text{Re}_{\infty} \), free-stream Reynolds number, and \( \mathbf{\bar{u}} \mathbf{u} \) is the Reynolds stress tensor. Eq. (1) holds for the conservation of mass equation, and Eq. (2) shows the conservation of momentum (Navier-Stokes) equations. These equations suffer from a closure problem, i.e. Reynolds stress tensor needs to be computed. Various turbulent-viscosity models, such as algebraic, one-equation, two-equation, Reynolds-stress and higher order models are developed (see e.g. (Pope 2000)) to solve the closure problem. In theory, accuracy increases with the level of turbulence model, but the computational cost and simplicity of coding are important factors in unsteady applications. Hence, the turbulent-viscosity model is chosen as follows,

\[
\mathbf{\bar{u}} \mathbf{u} = -2 \nu_t \mathbf{S} + \frac{2}{3} k \delta y
\]

In this equation, the local mean rate of strain, \( \mathbf{S} \), is calculated using the mean flow velocities, and the eddy viscosity, \( \nu_t \), and the turbulent kinetic energy, \( k \), are estimated from the one-equation Spalart-Allmaras model (Bardina 1997).

Finally, it should be noted that at each time step, a Laplace equation is solved in order to handle the moving nodes on the fluid/structure boundary and determine the new equilibrium position and velocity of the internal nodes, i.e. the mesh velocity, \( \mathbf{\bar{u}} \), of the fluid domain. This mesh velocity field is used in Eq.(2).
2.2. Equations of Motion

The incremental equations of motion of a structure idealized as a linear multi-degree-of-freedom system can be expressed in the form of Eq. (4).

\[
[M][\Delta \dot{q}] + [C][\Delta \dot{q}] + [K][\Delta q] = \{\Delta F\}
\] (4)

In this equation, \([M]\) is the mass/inertia matrix, \([C]\), the structural viscous damping matrix, and \([K]\), the stiffness matrix. For the problem at hand \(\{q\} \equiv (x, y, \theta)^T\) is the displacement vector measured from the initial static equilibrium and \(\{F\}\) is the external dynamic fluid loading vector resulting from the conductor surface loading. The dot operator holds for the time derivative, and the \(\Delta\) is the forward difference operator in time. The equations of motion are solved in full-space by direct time-step integration using the second order unconditionally stable Newmark-Beta operator (Bathe 2006).

2.3. Coupling Algorithm

A two-way loosely coupled approach is applied in which the fluid and solid equations are successively and separately solved (with independent solvers) using non-matching grids. Then, the latest information provided by each part of the coupled system is called by the other part in order to proceed in time. The coupling algorithm includes three main modules: the fluid dynamics solver, the solid structural dynamics solver, and the load/motion transfer operator that relays relevant analysis parameters between the two solution domains. The solution process starts with an initial flow field that provides the surface fluid tractions along the fluid/structure mesh interface. Next, using the conservative load transfer operator, surface tractions are integrated to yield the resultant nodal forces to be applied as external loads on the solid mesh. The solution of Eq. (4) provides the displacement, velocity and acceleration vectors of the solid mesh nodes at every time step. After each time increment, the conductor displacements are imposed via the compatibility condition to the nodes of the fluid mesh along the fluid/structure interface. Then, the flow solver introduces this interface motion in the fluid flow formulation to compute the fluid mesh motion in the entire domain, and then solves the flow field. This loop proceeds in time until the total analysis duration is achieved.

2.4. Model and Parameters

In order to study the effect of the initial ice deposit orientation with respect to incident wind flow on conductor galloping, a symmetric glaze iced conductor with maximum ice thickness of 37% of the conductor diameter is considered at different initial orientations \((\phi)\) relative to the incident wind. Fig. 1 illustrates the geometry of the model, the flow boundary conditions, while the top right insert shows the profile of the iced conductor and defines the angle \(\phi\). The incident wind velocity range of 10-30 m/s is considered. The natural frequencies of translational galloping oscillations of the iced conductor on flexible supports, representing the mid span oscillations of a typical high voltage line
conductor, are 0.995 Hz and 0.845 Hz in the horizontal and vertical directions, respectively, and the rotational frequency is twice the vertical frequency. The iced conductor mass, moment of inertia, and stiffness are chosen in such a way to match these conditions. The total damping is comprised of structural and aerodynamic damping in which the aerodynamic damping is included through the FSI calculation, and the structural damping which is prescribed in the conductor model. Structural damping is typically very small (up to 0.5% of critical damping for vertical motions, and up to 2% for rotation), and assigning accurate values is a difficult task; however, structural damping cannot be neglected at low frequencies such as in the present study. On the basis of wind tunnel experiments and previous studies (Lilien et al. 2007, Borna et al. 2011a, Borna et al. 2011b), the structural viscous damping ratio is set to 0.08% for horizontal and vertical motions and 1.5% for rotation. It should be emphasized that as the results will be compared against Den-Hartog’s instability criterion, the 1.5% is chosen for rotational damping ratio in order to weaken the effect of rotation on the amplitude of transverse oscillations.

3. RESULTS AND DISCUSSION

In this section, the computational results of a series of 35 test cases are presented and discussed to study the effects of ice deposit orientation with respect to incident wind flow and wind velocity on conductor displacements. First, the aerodynamic performance of the iced conductor is examined at different ice orientations in order to investigate the instability region based on Den-Hartog’s criterion. Then, numerical fluid-structure interaction analysis of the iced conductor is performed at various initial ice orientations and at different velocities.
3.1. Den-Hartog instability zone

The first explanation for galloping as an aerodynamic mechanism was presented by Den Hartog (Den-Hartog 1932). He proposed a relation between aerodynamic coefficients and their gradient with respect to the angle of attack \((\alpha = -\varphi)\) in order to predict a negative effective damping condition that would cause the instability of the system. Den Hartog’s criterion states that a body is prone to gallop in vertical direction when the rate of change of the lift coefficient \((C_l)\) with respect to the angle of attack becomes negative and exceeds the drag coefficient \((C_d)\), that is when \(dC_l/d\alpha + C_d < 0\).

In order to numerically investigate this criterion for the studied iced conductor profile, the unsteady flow field around the profile at various orientations is solved and the unsteady loading over the body is computed. The calculations are continued until the vortex patterns behind the body are fully developed and then the time averaged aerodynamic coefficients are calculated. In Fig. 2, the computed time-averaged aerodynamic coefficients of the non-moving (fixed) iced conductor versus the initial ice deposit orientation are plotted; the derivative of the lift coefficient is also included in the figure. Based on Den-Hartog’s galloping criterion, the iced conductor is subject to instability only at a very small area around 180° (see Fig. 2), which confirms that this criterion, due to its simplicity, can only describe a small portion of the instability domain and is a poor predictor of instability limits of a multi-degree-of-freedom system. In other words, there might be other instability conditions outside of the Den-Hartog’s instability zone.

![Graph showing computational time-averaged aerodynamic coefficients versus ice deposit orientation](image)

**Fig. 2.** Computational time-averaged aerodynamic coefficients versus ice deposit orientation

3.2. Aeroelastic instability zone

In the case of heavily separated flow over bluff bodies, such as the present case, the unsteady loading and aerodynamic damping are functions of both the incident velocity
(or more precisely Reynolds number) and the profile of the bluff body. By means of the computational aeroelastic approach, these effects are included in the study; therefore, predicting instabilities can be more accurate. In Fig. 3, displacement trajectories of the center of mass (galloping ellipses) of the iced conductor at incident velocity of 10 m/s are plotted. At $\varphi = 0$, the amplitude of oscillations is very small and the galloping ellipse is horizontal. By increasing $|\varphi|$, the oscillations become larger and the galloping ellipses stretch in the vertical direction. As shown in the figure, the vertical displacements reach their maximum value at $\varphi = \pm 30^\circ$, which coincides with the maximum time-averaged lift coefficient (see Fig. 2). By further increasing $|\varphi|$, the time-averaged lift coefficient decreases and drag increases. Hence, the amplitude of vertical displacements decreases very fast and the horizontal amplitude increases; as shown in the figure; at $\varphi = -60^\circ$, the galloping ellipses stretch horizontally. The maximum horizontal displacement occurs at $\varphi = \pm 90^\circ$, which coincides with the maximum drag coefficient. As $|\varphi|$ is further increased, the time-averaged lift coefficient grows to a second peak while the drag decreases; therefore, the galloping ellipses shrink horizontally and stretch vertically to a local maximum at around $\varphi = \pm 120^\circ$; however, the amplitude of vertical displacements in this case is smaller than the amplitude predicted for $\varphi = \pm 30^\circ$. Finally as $|\varphi|$ reaches $180^\circ$, all displacements are greatly reduced. As indicated by the galloping ellipses, all displacements for all initial orientations diminish through time; this means that the iced profile is immune to large galloping displacements at incident wind velocity of 10 m/s.

![Fig. 3. Center of mass motion at various initial iced profile orientations with respect to incident wind velocity of 10 m/s](image)

The same study is performed for incident wind velocities of 20 m/s and 30 m/s. Fig. 4 shows the galloping ellipses for several ice deposit orientations at incident wind velocity of 20 m/s. As the first observation, one can see that the amplitude of translational vibrations in both directions increases with wind velocity. Moreover, it can be seen that the general trend of the structural response is more or less similar to that of the 10 m/s case except at $\varphi = \pm 60^\circ$ where the large displacements increase and reach a limit cycle.
(see also Fig. 7). This indicates that for $\varphi = \pm 60^\circ$, the aerodynamic damping is negative and its magnitude is larger than the value of structural damping assigned in the model. The instability of this particular iced profile orientation is not predicted by Den-Hartog's criterion.

![Fig. 4. Center of mass motion at various initial iced profile orientations with respect to incident wind velocity of 20 m/s](image)

In Fig. 5, the galloping ellipses for several ice deposit orientations for incident wind velocity of 30 m/s are shown. Similar to the 20 m/s case, the amplitude of the translational vibrations in both directions increases with incident wind velocity which shows the velocity-dependency of the displacements; however, the structural response is significantly different from the previous two cases and more unstable regions are present. As shown in the figure, the oscillations at different initial ice orientations damp quickly except for the following regions: $\varphi = \pm 30, \pm 60, 180^\circ$. This shows that the negative aerodynamic damping at these regions prevails the structural damping, increasing the likelihood of large amplitude instabilities. At $\varphi = \pm 30^\circ$, as illustrated by the vertically inclined ellipse in Fig. 5, the horizontal oscillations decrease rapidly; however, the vertical displacements increase and reach a limit cycle with the highest peak-to-peak amplitude among all other test cases. This response is quite different from the lower incident wind velocities. In the case of $\varphi = \pm 60^\circ$, the oscillations are similar to the 20 m/s; i.e. the oscillations reach a limit cycle in which the peak-to-peak horizontal amplitude at limit cycle is the highest among other orientations. Finally, at $\varphi = 180^\circ$, the horizontal oscillations damp quickly, while the vertical displacements increase with a very small rate. The small amplitude increase rate is due to zero time-averaged lift coefficient (see Fig. 2). Therefore, the instability for $\varphi = 180^\circ$ can be type of vortex-induced vibrations in which the oscillations are caused due to load fluctuations, and the amplitude at limit cycle is expected to be in the order of the conductor diameter. It should be noted that although the aerelastic computations are accomplished for a same amount of physical time for all test cases, only in the latter case, the vertical oscillations are not reached a limit cycle. This response and the slow pace of the instability can be seen well at the relevant phase plot in Fig. 6.
Fig. 5. Center of mass motion at various initial iced profile orientations with respect to incident wind velocity of 30 m/s

Phase plots, rate of change of a variable versus the variable itself, are practical illustrations to study response of a system and analyze the instabilities and determine any potential limit cycles. In the following figures, the phase plots of the transverse displacements, i.e. the transverse velocity of the oscillations versus the transverse displacements are provided at select ice orientations for 20 m/s and 30 m/s incident wind velocities. Fig. 6 represents the phase plots for transverse displacements at $\phi=180^\circ$. As shown, the amplitude of the oscillations increases gradually for 30 m/s, yet there is no stable limit cycle for the duration of the computations. However, for 20 m/s, the phase plot shows that the oscillations slow down and the amplitude of the displacements decrease rapidly. The phase plots for $\phi=-60^\circ$ (Fig. 7) confirm one stable limit cycle for both incident wind velocities. By investigating phase plots at $\phi=-30^\circ$, see Fig. 8, we can see that at 20 m/s the oscillations damp out very fast, while at 30 m/s, displacements converge to a large amplitude limit cycle.

Fig. 6. Phase plot of transverse displacements at $\phi=180^\circ$ for incident wind velocities of 20 m/s and 30 m/s
4. CONCLUDING REMARKS

Computational aeroelastic instability of an iced conductor with various iced profile orientations at three incident wind velocities of 10 m/s, 20 m/s and 30 m/s is studied and the results are compared with the predictions using Den-Hartog’s instability criterion. The Den-Hartog’s instability analysis shows only a small instability zone around $\phi = \pm 180^\circ$ while the aeroelastic computations show no instability for this particular orientation at incident wind velocities of 10 and 20 m/s. However, at velocity of 30 m/s, the computations reveal a slowly growing instability. Due to zero time-averaged lift at $\phi = \pm 180^\circ$, this instability is not expected to end up to a large amplitude oscillation.

Moreover, aeroelastic computations show that there is no unstable initial ice orientation for incident wind velocity of 10 m/s. For incident wind velocity of 20 m/s, a large amplitude limit cycle oscillation is observed at $\phi = \pm 60^\circ$, and for 30 m/s case,
unstable zones at $\varphi = \pm 60^\circ$ and $\pm 30^\circ$ are detected, which are not predicted by Den-Hartog's model.

In summary, the results show the failure of the Den-Hartog’s aerodynamic criterion to predict all potential instability zones and provide evidences that galloping instability is a velocity-dependent and damping-controlled, namely controlled by aerodynamic damping. Hence, accurately predicting the likelihood of galloping instabilities requires an aeroelastic approach.

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