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REVIEW OF THE POTENTIAL EFFECTS OF WIND TURBINE WAKES ON OVERHEAD TRANSMISSION LINES

TABLE OF CONTENTS

PURPOSE AND SCOPE	1
PART 1 – REPORT DETAILS	2
1 REVIEW OF WIND-INDUCED MOTIONS OF TRANSMISSION LINES	2
2 BASIC CONSIDERATIONS OF A WIND TURBINE AND ITS WAKE AERODYNAMICS	11
3 REVIEW OF PAPERS ON THE EFFECTS OF WIND TURBINE WAKES ON OHLS	17
4 REVIEW OF RELEVANT PUBLISHED DATA ON TURBINE WAKES	22
5 OVERHEAD LINE BUFFETING	31
6 GENERAL DISCUSSION OF OHL BUFFETING	38
7 CONCLUSIONS	39
8 RECOMMENDATIONS	41
9 FORMS AND RECORDS	41
PART 2 - DEFINITIONS AND DOCUMENT HISTORY	41
10 DEFINITIONS	41
11 AMENDMENTS RECORD	41
PART 3 - GUIDANCE NOTES AND APPENDICES	41
12 REFERENCES	41
13 LIST OF FIGURES	43

PURPOSE AND SCOPE

Wind turbines have become an increasingly common feature of the landscape in many countries. It is estimated that, at the beginning of 2007 there were 90,000 turbines installed worldwide with a total generating capacity of 70 GW. By the end of 2008, according to the World Wind Energy Association's figures, the installed capacity is estimated to have risen to 120 GW. In the UK, the Government has now adopted a target of 15% of UK generating capacity to be provided from renewable resources by 2020. To meet this target it is envisaged that some 34 GW of wind power capacity will be installed, 14 GW of which are to be constructed at onshore sites. Currently, the installed onshore capacity is 2.6 GW, with 772 MW under construction and a further 3.4 GW awaiting planning approval. Since the capacity of a typical modern turbine is about 2 to 3 MW, it is clear that wind turbines are also set to become a common feature of the UK landscape.

By their nature, wind turbines are installed in open country and it is inevitable that they will share some locations with National Grid's established 275 and 400 kV transmission lines which traverse England and Wales. In addition, new National Grid routes will be required in some locations to provide connections for these installations to the transmission system. Wind farms have a fundamental effect on the local wind environment downstream of them. Since transmission lines are 'wind-sensitive' structures, the question has been raised as to whether the proximity of a wind turbine can have an adverse effect on the service performance of a line. This report considers the various aspects of the way in which the natural wind affects transmission lines and how this may be changed by the presence of a turbine. The availability of information to complete the assessment is examined and how best to acquire further information.

National Grid's current policy is that the distance between an overhead line (OHL) and a wind turbine should be not less than five rotor diameters. This represents a significant constraint on land use for wind farms. A particular focus for the report, therefore, is the wind environment within a few diameters downstream of the turbine to see whether a reduction in this recommended separation between an OHL and turbine can be justified.

In general terms, the effect of an on-load, operational wind turbine on the downstream wind environment is to reduce the mean wind speed compared to the incident upstream value, to increase the turbulence levels and to inject some rotational flow components. When not operating, turbines are essentially no different to the variety of industrial and commercial structures with which National Grid's

transmission lines have experienced trouble-free cohabitation over many years. Since the direct loads on towers and conductors in the wake of a turbine are reduced as a result of the reduced mean wind speed, such loadings are not considered.

The steel members of transmission towers can be subject to wind-induced vibrations. In general, this is significant only for towers with long, unbraced elements (length-to-width ratio of some 250 or more). Some towers, internationally, have been designed with few bracings in the cross-arms, especially in climates where ice loading does not occur and where high wind loads are rare. Vibration-induced fatigue, particularly of the unbraced, sloping member at the top of each cross-arm, has then occasionally occurred. National Grid's transmission tower designs are not of this type and they have not suffered from a general wind-induced vibration problem. However, National Grid has identified a specific problem with the J L Eve L6 tower design which utilises a 'notched' steel bracing that is known to be susceptible to failure; this is believed to be due to wind loading causing fatigue over time, the notch acting as a stress raiser (EMI 925, Issue 3, refers). Consideration should be given to replacing all such bracings if the location of a wind turbine is considered likely to increase the buffeting of a tower with notched bracings. No further consideration is given to tower vibration in this report.

This report starts with a review of the known types of wind-induced motion of National Grid's transmission lines. It is followed by some basic considerations of the likely features of a wind turbine wake and how these might affect an overhead line's response to the wind. A review is then undertaken of papers which deal directly with this question, followed by papers which provide turbine wake data relevant to a consideration of the problem. In order to understand the implications of these data, they are employed in a modal response model of an OHL subject to a turbulent wind.

PART 1 – REPORT DETAILS

1 REVIEW OF WIND-INDUCED MOTIONS OF TRANSMISSION LINES

In order to assess the possible effects of the wake from a wind turbine on a downstream transmission line, it is first necessary to review the salient features of the various types of wind-induced motions that OHLs exhibit. There are four main types of such vibration/oscillation, namely: aeolian vibration, subconductor oscillation (also known as subspan oscillation or wake-induced oscillation), galloping and turbulent buffeting. These will now be considered in some detail.

1.1 Aeolian Vibration (AV)

AV is caused by vortex-shedding from the conductor in light-to-moderate winds having a direction close to the normal to the span. The flow around the conductor causes boundary layers to form on its top and bottom surfaces and these layers thicken as they move rearwards. The loss of momentum in the boundary layers prevents them from staying attached over the entire conductor surface and finally they separate to form two, free shear layers. These shear layers are unstable and they roll-up to form vortices, with their axes parallel to the conductor's axis. They roll-up alternately so that, as one vortex is formed and shed into the wake from near the top of the conductor, another starts to form near the bottom of the conductor. The shed vortices form an alternating pattern in the conductor's wake, known as a 'vortex street'. The vortex-shedding is not correlated along the entire span of the conductor and the length to which it is correlated depends on the prevailing circumstances, as discussed later.

As a vortex forms, the instantaneous flow around the conductor is asymmetric about the wind direction, producing an instantaneous lift force normal to the wind. As the vortex is shed and another vortex starts to form on the other side of the conductor, the sign of this lift force reverses. Thus, vortex-shedding produces a periodic, fluctuating lift force acting on the conductor. The frequency of this force is given by the Strouhal equation:

$$S = \frac{f_v \cdot d}{U} \dots\dots\dots (1)$$

where S is the Strouhal number, f_v is the frequency of the vortex-shedding (Hz), d is the conductor diameter (m) and U is the wind speed normal to the span (ms^{-1}). For a stranded conductor, the Strouhal number has a value of around 0.19. To put this into perspective, if we have a 3 ms^{-1} wind, the frequency of vortex-shedding is about 20 Hz for a 28.6 mm diameter Zebra ACSR conductor and 29 Hz for a 19.53 mm diameter Lynx conductor.

Since a span of conductor is an elastic system, this fluctuating lift force causes a forced, vertical vibration of the conductor. The natural frequencies of a span are given by:

$$f = \frac{n}{2L} \sqrt{\frac{T}{m}} \dots\dots\dots (2)$$

where f is the natural frequency of the conductor (Hz), n is the mode number, L is the span length (m), T is the tension (N) and m is the mass per unit length (kg/m). For a 366 m standard span of Zebra at a tension of 26 kN, its fundamental frequency ($n=1$) is 0.17 Hz; similarly, for a standard span of Araucaria AAAC strung at 44.4 kN, its fundamental is 0.19 Hz. The modes are often referred to in terms of the number of half-waves or *loops*, $n=1$ corresponding to the 1-loop mode.

Just like a stretched string, the span has a series of natural frequencies, corresponding to integer values of n in Equation 2. So, for example, a vortex-shedding frequency of 20 Hz would lie very close to the 116th harmonic ($n=117$) for a standard span of Zebra, corresponding to a frequency of 19.89 Hz.

The mode shapes are sinusoidal and given by the equation:

$$y = A \sin \frac{n\pi z}{L} \dots\dots\dots (3)$$

where A is the modal amplitude and z is the distance along the span. For the 116th harmonic of the above span, the wavelength of the vibration would be 6.3 m.

Equation 2 is correct for all spans except tension-tension spans where, depending on the sag-to-span ratio, the fundamental vertical frequency (actually, motion in the plane of the catenary) may be significantly higher than that predicted by Equation 2. However, all the subsequent, in-plane natural frequencies will correspond to the values predicted by Equation 2 with $n > 1$. All the horizontal natural frequencies (motion out of the plane of the catenary) are given quite accurately by Equation 2.

Although AV can be thought of as a forced response to a periodic lift force, the phenomenon is more complex than this might imply. There is positive feedback mechanism between the motion of the conductor and the vortex-shedding. This has three aspects:

- a) The fluctuating lift force increases with increasing conductor vibration amplitude. However, the vibration amplitude is limited to a maximum of, typically, one conductor diameter, peak-to-peak (p-p): higher amplitudes disrupt the vortex-shedding process.
- b) The correlation of the vortex-shedding along the conductor length is increased with vibration amplitude: this increases the effective lift force.
- c) The phenomenon of 'vortex-locking' allows the vortex-shedding to retain its frequency despite small changes in the wind speed, disobeying Equation (1). The extent to which it can do this depends on the vibration amplitude but some $\pm 10\%$ is possible.

The final amplitude of AV is a result of a balance between the energy input from the vortex-shedding and the energy dissipated by the conductor's self-damping, by the span end-fittings and by any damping systems applied to the conductor, such as Stockbridge dampers or, in the case of bundled conductors, spacer dampers. The conductor's self-damping is a strong function of vibration amplitude, frequency and conductor tension, and provides the limit to the maximum frequency of AV. The self-damping also depends on the make-up of the

conductor, ACSR generally having greater self-damping than AAAC, for example. At low frequencies, the conductor has very little self-damping and the vibration amplitude is limited aerodynamically to around one diameter p-p, as previously mentioned. Where necessary, added damping systems control the response over the intermediate frequency range.

The typical range of wind speeds associated with AV is 1 to 7 ms⁻¹. Wind speeds of around 1 ms⁻¹ or less tend to be very variable in direction, as they are driven by localised thermal differences in the atmosphere. The upper, wind speed cut-off is largely associated with the suppression of vibration by conductor self-damping and, in conditions which favour vibration, such as high conductor tension or very smooth wind conditions, AV can occur at speeds of 9 or 10 ms⁻¹.

It may be readily appreciated from the foregoing that anything which tends to disrupt vortex-shedding will have a major effect on the final amplitude of vibration. High turbulence intensities, where the variations in wind speed can overcome the vortex-locking, or short length-scale turbulence from buildings and trees, etc, which can also disrupt the span-wise correlation, have a major effect in reducing the vibration amplitude. In contrast, open flat terrain can have light winds with very low levels of turbulence. This is especially true in conditions where thermal inversions frequently occur, since these have a damping effect on turbulence. Desert areas and flat coastal areas commonly experience such light, low-turbulence winds, especially in the morning and in the evening. Examples of the effect of turbulence on the vibration levels of an undamped span are given in an Electra paper [1]. Figure 1 is taken from [1] and shows the effect of turbulence levels on the free-span, vibration bending angle, Ψ . This angle corresponds to the slope of the conductor at a free-span vibration node and is a measure of the severity of the dynamic bending because it takes account of both the amplitude and the wavelength of the vibration. The curve labelled 'LAM' corresponds to laminar flow (0% turbulence intensity) and the remaining curves correspond to turbulence intensities of 5%, 10% and 15%, respectively. It can be seen that a turbulence intensity of 10% reduces Ψ by more than 50% over most of the frequency range. Such a reduction in Ψ would increase the conductor's fatigue life in terms of number of cycles to failure by, typically, more than an order of magnitude.

The principal concern with regard to AV is fatigue of the conductor strands at the mouth of the suspension clamps. The mechanical impedance of the clamp tends to force a vibration node on the conductor; the clamping of the strands also maximises the dynamic bending stresses in them. The slight interstrand movement that takes place as the conductor bends causes interstrand fretting and this accelerates the formation of fatigue cracks in the strands, which generally start in the second layer strands. Although the mouth of the suspension clamp is the most common location for such fatigue to occur, any clamp attached to the conductor which has sufficient mechanical impedance at AV frequencies to force a vibration node can be a site for fatigue. This applies to poorly designed Stockbridge dampers, aircraft warning devices, bird flight diverters, etc.

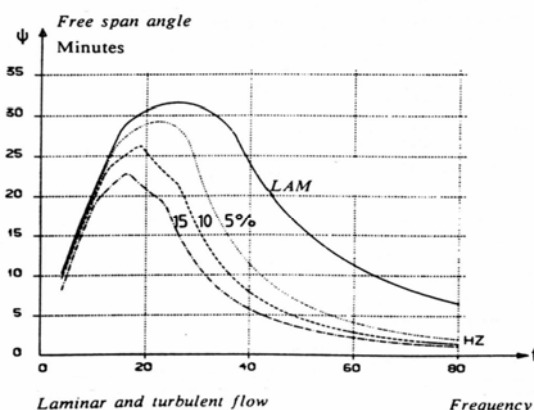


Figure 1 - Effect of Turbulence Intensity on Aeolian Vibration (taken from [1])

Bundled conductors where one subconductor lies in the wake of another, such as horizontal twin bundles, are generally considered to experience lower levels of AV than single conductors. However, AV levels can still be significant in favourable circumstances and may lead to conductor and/or spacer damage if the latter are too rigid.

Although spacer dampers were not originally employed on CEGB conductor bundles, problems with some of the early spacer designs, caused by subconductor oscillation, have subsequently led to spacer dampers being specified in most cases.

Stockbridge dampers do wear out and require replacement on some National Grid lines from time-to-time and these may be assumed to be the lines where AV is most active. Earthwire dampers are usually first to show signs of fatigue followed later by phase dampers. An example of such a line is the Exeter-Chickerell route, running along the southwest Devon/Dorset coast, a short distance inland, and subject to the light winds that occur in such locations. This form of damper damage – where the messenger cable strands finally fatigue – must not be confused with deformed messenger cables arising from severe galloping. In the latter case, the messenger cable is bent as a result of the acceleration applied to the damper weights by the large vertical transients that can occur during galloping or ice drop.

In conclusion, AV is only usually a problem where inadequately damped conductors are exposed to low turbulence, light-to-moderate winds for prolonged periods.

1.2 Subconductor Oscillation (SCO)

As the name implies, SCO can occur only on bundled conductors. It embraces a number of types of motion, as discussed later, but their frequencies all tend to be associated with the subspan length. Thus, for subspans of around 60 m, Equation 2 gives typical SCO frequencies on National Grid lines of about 1 Hz. Harmonics at, say, 2 Hz have been reported internationally but have not been observed on National Grid lines.

Unlike AV, SCO is a pure, aeroelastic instability: if the subconductors were made rigid or otherwise restrained from moving, no fluctuating aerodynamic force with a 1 Hz frequency would act on them. The instability arises because the downstream subconductor in a horizontal pair lies in the aerodynamic wake of the upstream conductor – hence the alternative name of ‘wake-induced oscillation’; vertical twin bundles experience no such instability. The exact position of the downstream subconductor in the wake depends on the regulation of the bundle and the blowback resulting from the prevailing wind. Although the mechanism of SCO is complex, a simplified explanation of the origin of the instability is readily given.

The mean wind speed – and, consequently, the mean drag force – experienced by the downstream conductor is a function of its position in the wake of the upstream conductor, being a minimum on the wake centre line and increasing to a maximum at the edge of the wake. This non-uniform wind speed acting on the downstream subconductor also results in it experiencing a lift force which varies with position in the wake: however, the lift force will be ignored for the present. Consider the downstream subconductor lying above the wake centre line, say, and undergoing a small perturbation that moves it downstream. If it returns upstream closer to the wake centre line, then it will be opposed by a smaller drag force than that which acted on it when moving downstream. Over the cycle, it will therefore have received some net energy from the wind and the perturbation will grow until the energy dissipation via aerodynamic and mechanical damping is equal to the energy extracted from the wind.

The fact that the subconductor has to change its vertical position in the wake during the cycle means that the motion involves both horizontal and vertical components. The downstream subconductor has to describe an ‘open’ locus, usually an ellipsoid of some form, and the energy extracted from the wind is proportional to the area of the ellipsoid. Such a stable, open locus is not possible unless the frequencies of the horizontal and vertical motions are identical and there is a suitable phase relationship between them, similar to the motion of a conical pendulum. In fact, the frequencies of the vertical modes of a bundle are

slightly higher than the corresponding horizontal modes because of the effects of the catenary on the former. However, the fact that the aerodynamic drag and lift forces are functions of position means that they act like aerodynamic springs, adding their effective stiffnesses – which may be positive or negative – to the mechanical stiffnesses of the system and modifying the system frequencies. It so happens that, for certain areas of the wake, these aerodynamic stiffnesses act to bring the frequencies of pairs of horizontal and vertical modes closer together. These stiffnesses are proportional to wind speed so that, at some critical value, frequency coalescence occurs and an instability becomes possible.

The oscillation modes of a bundle are usually categorised as either ‘rigid body’ modes, where the distance between the subconductors does not change, or ‘subspan modes’, where pairs of subconductors move in antiphase. The rigid body modes are either torsional (rolling), or horizontal or vertical ‘snaking’ of the bundle. The subspan modes are either horizontal or vertical.

For twin bundles, the instability may be a combination of any two of the rigid body modes, or a combination of a horizontal subspan mode with a torsional mode. The last of these is perhaps the most common on National Grid twin lines, with vertical/torsional instability have been observed on a few occasions (Lydd-Dungeness route). On National Grid quad lines, combinations of horizontal with vertical subspan modes and horizontal subspan with torsional modes are the most common. The critical wind speed for National Grid twin bundles tends to be 10 ms^{-1} or more. However, for the quad bundles it is closer to 7 ms^{-1} , although SCO at speeds of some 4 ms^{-1} has been observed on highly unstable spans. Quad bundles are particularly unstable because the horizontal and vertical subspan frequencies are close together, both being fixed by the spacer locations: elliptical motion with the horizontal amplitudes sufficient to cause subconductor clashing on 305 mm bundle separations were commonplace on the original L6 construction.

If spacers or spacer dampers are not sufficiently well-designed, then the first effects of sustained SCO are usually to loosen the spacer clamps or to cause severe wear in the articulation or flexible parts. Loosening of the clamps may lead to rapid conductor damage and final failure. In the medium term, wear in the suspension point linkages – such as the elongation of holes and the grooving of cotter pins or of suspension clamp trunnions – will occur and are good indicators of spans most subject to SCO. In the long term, wear and fatigue of the conductor may occur at the spacer or spacer damper clamps.

The wake of the upstream subconductor extends to only some five conductor diameters either side of the wake centre line at the location of the downstream subconductor. Because the instability depends on the gradients of aerodynamic forces experienced by the downstream subconductor, the onset of SCO is very sensitive to the tilt of the bundle. This is why it is common to see some phases with active SCO while other phases on the same span are stable. Highly turbulent winds increase the stability of the bundle both because the path of the wake fluctuates and the blow back of the bundle changes, thereby changing the location of the downstream conductor in the wake.

1.3 Galloping

The vast majority of occurrences of galloping result from an ice accretion forming on the conductor. Other forms of galloping associated with the aerodynamics of specific conductor cross-sections – such as the original Severn Crossing conductor – will not be considered, as they are rare and usually relate to a few specific spans.

When an ice accretion forms on a conductor it dramatically changes the conductor’s aerodynamics, resulting in lift and moment being generated, while also modifying the drag characteristics. In many cases, the presence of the lift and moment can lead to an aeroelastic instability. This is generally one of two types: either a one degree-of-freedom vertical instability (Den Hartog instability) or a two degree-of-freedom, vertical/torsional instability (flutter). The latter is almost certainly the most common type, especially for bundled conductors because their vertical and torsional frequencies are very close together.

The resulting galloping has a vertical amplitude which is usually of order at least a metre and which can approach – and, in rare cases, exceed – the sag of the span. Galloping usually occurs in the fundamental mode, commonly referred to as one-loop galloping, with a frequency of around 0.17 Hz (see Equation 2 et seq.), but the second and third harmonics – the two- and three-loop modes, respectively – are also often observed.

The most common form of ice accretion on UK lines is caused by wet snow, although freezing rain and freezing cloud/fog (rime ice) can be the cause on particular occasions and are generally associated with particular locations. In contrast, wet snow blizzards can affect almost any line in the country. In general, wind speeds during galloping events are in excess of 10 ms^{-1} and galloping amplitudes tend to increase with wind speed up to their maximum value.

Apart from having suitable aerodynamic characteristics, the shape of the ice accretion has to be sufficiently similar in form along a large proportion of the span in order for the span as a whole to be unstable, otherwise the aerodynamic damping of the remainder of the span, together with the span's mechanical damping, will ensure that instability cannot develop. Anything which tends to disrupt the formation of a fairly uniform ice shape will have a stabilising tendency.

1.4 Turbulent Buffeting

The natural wind is turbulent and the instantaneous wind speed is a random function of space and time. As a result, the instantaneous drag force per unit length of conductor is a random function of time and position along the span. Provided that the so-called 'reduced wind speed', U_r , defined as:

$$U_r = \frac{U}{f \cdot d} \dots\dots\dots (4)$$

is of order $[10]^2$ or more, then the local instantaneous drag force, $F_D(t)$, will be given by the standard equation:

$$F_D(t) = \frac{1}{2} \cdot \rho \cdot U(t)^2 \cdot C_D \dots\dots\dots (5)$$

where ρ is the density of air and C_D is the drag coefficient. This is known as the quasi-steady approximation. For a Zebra conductor with a wind speed of 10 ms^{-1} , say, this corresponds to conductor motion frequencies of up to about 3 to 4 Hz. At higher frequencies, accelerative effects associated with the so-called 'virtual mass' of the conductor increase the instantaneous drag force [2].

The span will blow back to a mean position corresponding to the mean wind speed and then swing about this position, according to the temporal and spatial distribution along the span of the wind's turbulent eddies (gusts). Some vertical motion will also occur as a result of the vertical component of the turbulence. The span will respond as an elastic system excited by a random forcing function.

Figure 2 shows the typical power spectrum, S_u , of the longitudinal turbulence velocity, u , normalised by its mean square, for an hourly mean wind speed of 20 ms^{-1} at a height of 10 m in open country (surface roughness coefficient = 0.005) Note that the term 'longitudinal', throughout this report, refers to the mean wind direction. These parameter values have been chosen as they are the most relevant to wind turbine sites in the UK. A number of spectral models of atmospheric turbulence are available, such as Davenport [3] and Harris [4], which differ slightly, particularly at the very low frequencies: for consistency throughout this report, the Davenport model is used. The corresponding turbulence intensity, defined as the rms of the longitudinal turbulence velocity, u , divided by the mean wind speed, is 17% at the 10 m height. This value reduces with height but for the purposes of this illustrative example, 17% has been retained. The points marked on the curve correspond to the 1-, 2- and 3-loop frequencies of a standard span of Zebra ACSR. It can be seen that the amount

of energy in the turbulence at these frequencies is one to two orders of magnitude less than the maximum value which corresponds to gusts with timescales of several minutes. It can also be seen that the amount of energy at SCO frequencies of around 1 Hz is very small.

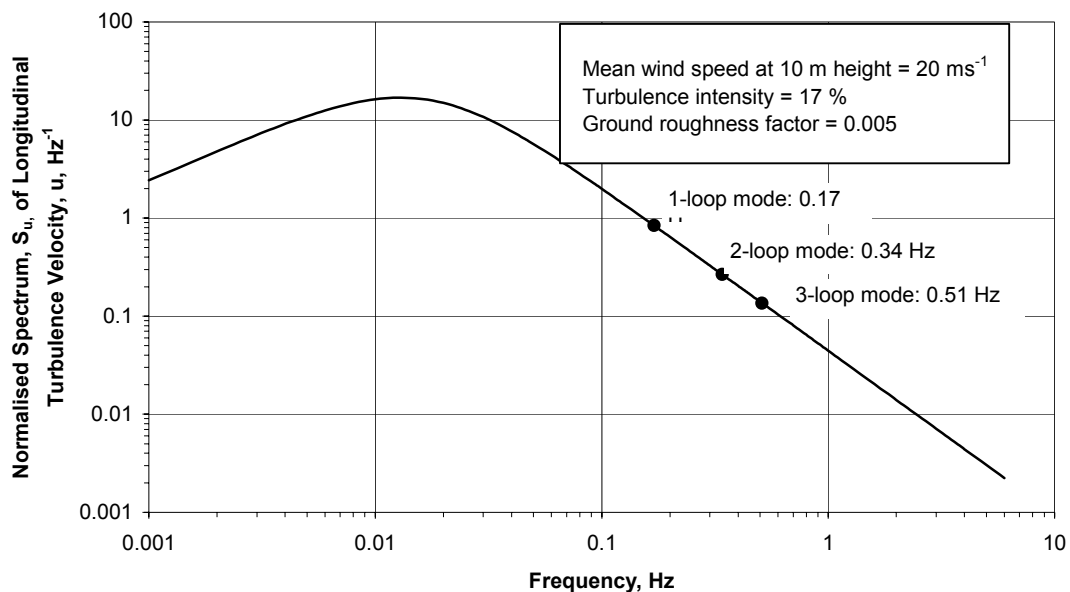


Figure 2 - Power Spectrum of Longitudinal Component of Turbulence, u, Normalised by the Mean Square

The spectrum of the u-component of turbulence velocity in Figure 2 has been used to calculate a rough approximation to the horizontal buffeting response of the span in its 1-loop mode. The approximation treats the span as a linear, lumped, damped, spring/mass system with a natural frequency equal to the 1-loop frequency and excited by a fluctuating drag force derived from the turbulence spectrum. C_D has been set equal to 1.0 and the mechanical damping of the conductor to 0.01 (damping ratio). While the latter figure is an estimate, it is, in fact, largely irrelevant as the aerodynamic damping at a wind speed of 20 ms^{-1} turns out to be an order of magnitude greater, in agreement with Davenport [5]. The turbulence spectrum for strong winds is known to be approximately invariant with height, and the increase of mean wind speed with height across the span has been ignored in these calculations.

The normalised spectrum of the buffeting response is shown in Figure 3. In this case, the spectrum has been plotted on log:linear axes as $f \cdot S_x$ versus frequency: that is, the spectral component of response multiplied by the frequency of the component. This form is often used when displaying response spectra plotted against a logarithmic frequency axis: since the differential $df = f \cdot d(\log f)$, this presentation preserves the area ratios under the spectral curve, allowing the contributions to the total response from different parts of the spectrum to be compared directly.

The conductor's response comprises a resonant part close to the span's 1-loop frequency and the remaining, non-resonant part, often referred to as the 'background' response. By selective integration of the spectrum, it can be determined that the contribution to the mean square of the background response is 0.65, while the contribution over the resonant part of the response from 0.1 Hz to 0.2 Hz is 0.35. This corresponds to a ratio of 1.36 in their respective rms contributions to the buffeting response. It can be seen that the peak of the resonant response actually occurs at about 0.16 Hz rather than at the natural frequency of 0.17 Hz. This is a result of the gradient in the energy content of the turbulence across the bandwidth of the conductor's receptance.

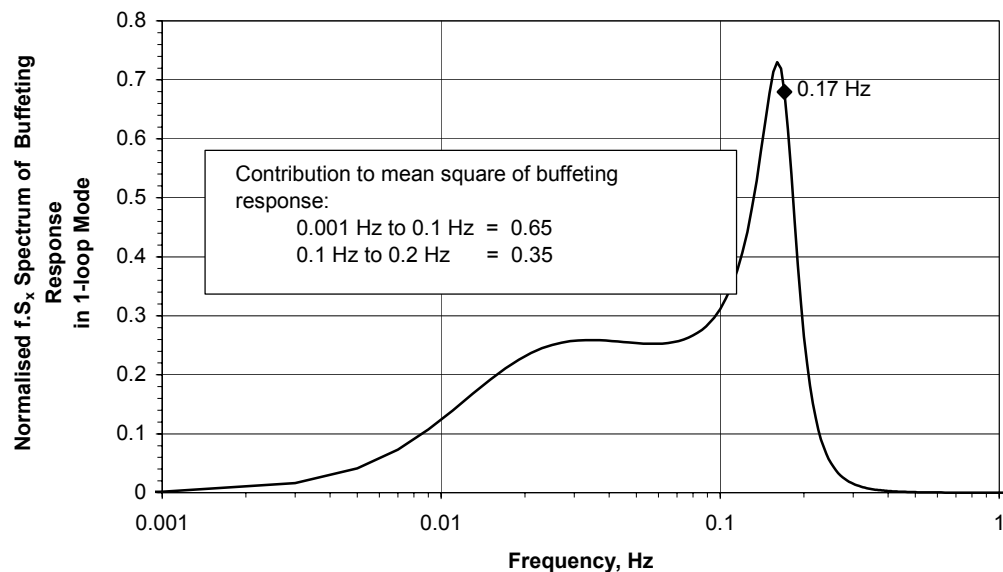


Figure 3 - Approximate $f.S_x$ Spectrum of Horizontal Buffeting Response, Normalised by the Mean Square (wind conditions as for Figure 2)

Quite apart from the simplification of the span dynamics mentioned above, using only the power spectrum of the turbulence to derive the power spectrum of the drag force effectively assumes that each turbulent gust occurs simultaneously over the entire span, that is, that the turbulence is fully-correlated across the span length. In fact, the integral length scale of turbulence is of order several hundred metres longitudinally, but typically only some 100 m or less transversally [4], that is, along the span. Such length scales are based on contributions from all turbulence frequencies and will be weighted towards the main energy content which occurs at the lowest frequencies; at the higher frequencies involved in the span's buffeting response, the length scales will be much shorter. Thus, Figure 3 will overestimate the response, especially at the higher frequencies.

It can be shown from the theory of random vibrations, for example [6], that the cross-spectra of the turbulence as a function of position along the span are required for a complete representation of the buffeting response. Davenport [3] published an empirical equation for the non-dimensional cross-spectra (coherences) of the u -component of turbulent velocity with transverse separation. Double integration of this function across the span is required for each frequency component of the response spectrum. The resulting spectrum of the buffeting response is shown in Figure 4. It can be seen that the low-frequency contribution to the mean square background response has grown to 0.87, while the resonant contribution is now only 0.13. This corresponds to a ratio of 2.59 in their respective rms contributions and explains why spans are generally seen to just sway with the gusting of the wind rather than to exhibit significant oscillation at their fundamental frequency.

The data presented in Figures 3 and 4 must be viewed as qualitatively - rather than quantitatively - correct, hence the magnitudes are presented in normalised form. However, they do indicate the general form of the buffeting response and the importance of a knowledge of the power spectra and of the cross-spectra of the turbulence to any calculation of the conductor's response.

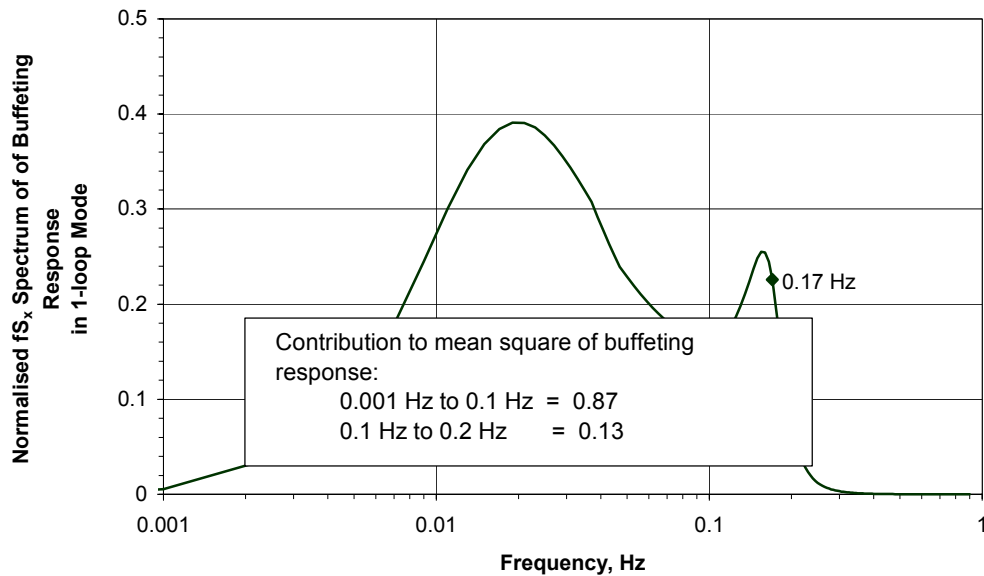


Figure 4 - Normalised $f.S_x$ Spectrum of Horizontal Buffeting Response using Cross-spectra of Turbulence (wind conditions as for Figure 2)

A further result may be deduced which will be of use later on in this report in the discussion of turbine wakes. It can be shown that the r.m.s of the resonant buffeting response, x'_R , of a given span of conductor is provided by the following relationship:

$$x'_R \propto \sqrt{(\bar{U} \cdot S_R)} \dots\dots\dots (6)$$

where $\bar{U}$ is the local mean wind speed, S_R , is the integral of the turbulence power spectral density (PSD), S_u , across the bandwidth of the resonance considered. This relationship holds good provided that the shape of the power spectrum and of the cross-spectra of the turbulence across the resonance bandwidth do not change significantly and the drag coefficient is a constant. Thus, if the mean wind speed increases while the turbulence remains unchanged, then the resonant response will increase.

The turbulence spectrum of Figure 2 has been normalised by the mean square (variance) of the longitudinal turbulence velocity, $\bar{u}^2$. For a non-normalised spectrum, the integral under the curve is equal to $\bar{u}^2$ and the longitudinal turbulence intensity is defined as:

$$I_u = \sqrt{\frac{\bar{u}^2}{\bar{U}^2}} \dots\dots\dots (7)$$

S_R in Equation 6 has dimensions of velocity squared and, for similar spectral shapes, is proportional to $\bar{u}^2$. Using this proportionality for S_R in Equation 6 leads to:

$$x'_R \propto I_u \bar{U}^{3/2} \dots\dots\dots (8)$$

Thus, a change in wind speed would be expected to have a greater effect on the resonant response than a similar change in the magnitude of the turbulence intensity. Clearly, a range of outcomes is possible, depending on the joint variation in a turbine wake of the mean wind speed and the turbulence intensity.

Historically, the effect of turbulent buffeting on National Grid's lines has been limited. The majority of maintenance issue that have arisen concerns wear on the suspension fittings of phase and earthwires, especially at exposed sites. The original earthwire suspension fitting design consists of two, interlocking, circular-section shackles. When a gust of wind strikes one span to a greater extent than the neighbouring span, bearing in mind that the transverse length scale of a gust is of order 60 m, a differential conductor tension is experienced at the

suspension point: this causes the suspension hardware to swing laterally in order to equalise the tensions. Since the suspension hardware is short – of order 200 mm - the angular swing is large and causes wear at the contact point between the shackles. In comparison, the length of the suspension insulators and hardware of the phase conductors is much longer, some 8 m, resulting in their angular deflection under the same differential wind loading being much smaller. For new line designs and during the maintenance and refurbishment of existing earthwires, the twin shackle design of suspension hardware is being replaced by a more wear-resistant, tongue-clevis design.

The most likely problematic situations would be very long spans of large conductors with modest horizontal phase separations. From Figure 2, it can be seen there would be a greater proportion of the turbulence energy at the very low, fundamental frequency of such spans, the greater conductor mass would reduce the effective aerodynamic damping and, with inadequate horizontal phase separation, the possibility of phase-to-phase flashover might then arise in high winds.

Clearly any upstream condition which leads to turbulence with enhanced levels of energy at the natural frequencies of the spans of an OHL will increase the buffeting response of the line.

2 BASIC CONSIDERATIONS OF A WIND TURBINE AND ITS WAKE AERODYNAMICS

The basic features of a wind turbine and the most likely characteristics of the aerodynamics of its wake are now introduced and considered with regard to OHLs.

2.1 Some Design Characteristics of Large Modern Wind Turbines

According to the British Wind Energy Association website, there is already a wide range of turbine sizes installed in England and Wales, ranging from a 31m diameter rotor on a 30 m tower (0.3MW) at Four Burrows in Cornwall to a 92 m diameter rotor on an 80 m tower (2.75 MW) at Ness Point Suffolk. Table 1 gives some data for current designs of large wind turbines for onshore installations, taken from the website of the Danish manufacturer, Vestas. Clearly, a range of design values can be found in the literature and those presented are intended only as a general indication of magnitude for current large turbine designs. An installation of an array of the Vestas 3 MW design is shown in Figure 5.



Figure 5 - An Array of 3 MW Vestas Wind Turbines with 90 m Diameter Rotors

Generating capacity (MW)	2 to 3
Rotor diameter, D (m)	90
Number of blades, N	3
Height of rotor axis above ground (m)	80 to 105
Blade tip ground clearance (m)	35 to 60
Maximum height of rotor tip (m)	125 to 150
Mean diameter of supporting tower (m)	3 to 4
Minimum wind speed for design capacity, U_d (ms^{-1})	12 to 15
Cut-in wind speed, (ms^{-1})	2.5 to 4
Cut-out wind speed, (ms^{-1})	25
Rotational speed, Ω , (rpm)	9 to 19
Approximate tip speed ratio, TSR	5 to 6

Table 1 - Data for Some Current Designs of Large Onshore Wind Turbines

The blade passing frequency (BPF), λ , is given by:

$$\lambda = \frac{\Omega N}{60} \dots\dots\dots (9)$$

Using the data in Table 1, λ is found to fall in the range 0.45 to 0.95 Hz.

It should be noted that, in some turbine designs, the blade pitch is controlled in such a way as to maintain an approximately constant rotational speed.

2.2 Basic Description of the Features of a Wind Turbine Wake

In what follows, only the wake from an isolated turbine, operating on load, will be considered. From basic aerodynamic considerations, this wake may be expected to exhibit a number of features, some of which are listed below. Because of the complexity of the flow field, it is virtually impossible to provide a single satisfactory illustration of it: a highly simplified schematic together with a few photographs and measured data, all taken from various references, are provided to assist in assembling a representation.

- The average wind speed in the wake will be less than that of the incident wind. The extent of the reduction will be commensurate with the proportion of energy in the incident flow that is being extracted by the turbine.
- The wake will expand immediately downstream of the turbine in order to satisfy the continuity equation. Again, the extent of the expansion will be commensurate with the proportion of energy in the incident flow that is being extracted by the turbine.
- The mean velocity will be a minimum on the wake centre line and increase to a maximum at the edge of the wake.
- The mean velocity profile will slowly become more uniform with increasing downstream distance and the average velocity deficit across the wake will approach zero. The wake will still be detectable some 10 or more rotor diameters downstream. A highly simplified representation of the foregoing aspects is contained in Figure 6, taken from [7].

- e) There will be a turbulent shear layer moving downstream behind the trailing edge of each blade associated with the boundary layer of the blade: this will have a swirl component. The width and nature of this shear layer will depend on whether the blades have any separated flow areas. Typically, at high power generation some areas of the blades are stalled.
- f) There will be a continuous, tip vortex shed from each blade tip. The core of each tip vortex will follow a helical path downstream of the rotor at the edge of the wake (see Figures 7 and 8), rotating in the opposite direction to the rotor. Because of the stability of the balance between the radial pressure gradient and the tangential velocity gradient that is set up in a vortex system, the helical tip vortices may be one of the most enduring discrete aspects of the wake. The vortices will slowly expand and convect faster air from outside the wake to within the wake, assisting the wake expansion. The strength and helix angle of the tip vortices will depend on the proportion of energy being extracted from the wind and the tip speed ratio.
- g) There will be a cylindrical shear layer at the edge of the wake where the faster air outside the wake meets the slower air within the wake. Most of the additional wake turbulence will be generated here. The helical tip vortices will sit in the shear flow and assist in convecting the turbulence into the wake and turbulent diffusion will spread it across the wake. The strength of the shear layer and of the turbulence intensity generated within it will depend on the proportion of energy being extracted from the wind.
- h) There will be a weaker set of vortices within the wake shed at the root of the blades.
- i) There will be a separated, turbulent shear flow shed from the turbine's supporting tower: this will be 'perturbed' as each blade passes the tower. This turbulence will diffuse across the wake
- j) There will be a turbulent wake shed from the nacelle of the turbine which houses the gearbox, generator, etc. This wake will be complex because of the swirling flow from the rotor hub; it may or may not involve a separated shear layer, depending on the shape of the nacelle.
- k) The turbulence in the approaching flow will be largely superimposed on the turbulence generated in the turbine wake because of the much larger length scale associated with the former.
- l) The wake will consist of the 'near wake' in which the individual contributions to the wake can be more or less separately identified. These contributions will merge as they move downstream to form the 'far wake'.
- m) The turbulence intensities in the wake will vary across the wake and will be greater in intensity and extend to higher frequencies than in the incident flow. The turbulence will become more uniform and slowly decay with distance from the rotor.
- n) The degree of symmetry of the wake about a horizontal plane will be influenced by the mean velocity gradient in the approaching flow.
- o) The wake will 'meander' horizontally and vertically as a result of the long length-scale, very low frequency components of turbulence in the approaching flow.

It may be appreciated from the foregoing that the turbine wake represents one of the most complex flow fields available for study within the general field of single phase aerodynamics, lacking only a compressible flow aspect.